Mechanical and electro-magnetic performance of Nb₃Sn superconductors for fusion

Characterization and design optimization of
Cable-In-Conduit Conductors for ITER magnets

Arend Nijhuis

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CHARACTERIZATION AND DESIGN OPTIMIZATION OF Nb₃Sn CABLE-IN-CONDUIT
CONDUCTORS FOR ITER MAGNETS

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Disclaimer: the views and opinions expressed herein do not necessarily reflect those of the ITER Organization; Close Support Unit EFDA; as well as Fusion For Energy.

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SUPERCONDUCTORS FOR FUSION

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voor Judith en Fei

List of symbols

A [m^2]	cross section area
A_{bar} [m^2]	strand cross section area with barrier
A_{Br} [m^2]	strand cross section area with bronze
A_c [m^2]	projected strand to strand contact area
A_{Cu} [m^2]	strand cross section area with copper
A_c [m^2]	minimum projected contact area for a strand crossing
A_{fil} [m^2]	strand cross section area with filaments
A_{sc} [m^2]	area of cross-section occupied by superconductor
A_m [m^2]	area of cross-section occupied by matrix material
b	normalized magnetic field
B [T]	magnetic field
B_a [T]	external applied magnetic field amplitude
B_{c2} [T]	upper critical magnetic field at operating temperature
B_{c20} [T]	upper critical magnetic field at 0 K
B_{c2m}^* [T]	inhomogeneity averaged upper critical magnetic field at 0 K
B_i [T]	internal magnetic field induced by coupling currents
B_{max} [T]	maximum magnetic field in the cable
B_{min} [T]	minimum magnetic field in the cable
B_p [T]	full-penetration magnetic field
C_0	Nb ₃ Sn strain scaling parameter
C_l	fitting parameter in Nb ₃ Sn strain scaling parameterization
$C_{a,1}$	second invariant in Nb ₃ Sn axial strain sensitivity
$C_{a,2}$	third invariant in Nb ₃ Sn axial strain sensitivity
Cu:nonCu	fraction copper to non copper in strand cross section
D [m]	cable diameter or width
d_f [m]	diameter filamentary region or filament diameter
d_s [m]	diameter wire
E_c [V/m]	critical electric field (10 $\mu\text{V/m}$)
E_i [GPa]	initial Young's modulus of the strand
E or E_{tr} [GPa]	Young's modulus of the strand, E (transverse)
E_a [GPa]	Young's modulus of the strand, E (axial)
F [N]	force
F_c [N]	transverse contact force
F_{cyl} [N]	hydraulic force
f [Hz]	frequency
f_{cbm} [m]	maximum possible average deflection of the cable
f_{cbm} [m]	maximum possible deflection available for bending
F_{max} [N]	transverse peak force
f_{mc} [m]	maximum cable compression at F_{max}

F_n [N]	transverse force with n indicating the order ($n=1$ for L_{w1})
f_{sb} [m]	strand deflection from bending, bending amplitude
$f_{sb,n}$ [m]	strand deflection from bending, n indicating the order ($n=1$ for L_{w1})
f_{sbm} [m]	maximum bending amplitude
f_{sc} [m]	strand deformation per strand crossing and line contact
f_{sm} [m]	maximum possible average deflection per strand
F_{vol} [N]	volumetric transverse force
I_a [m ⁴]	momentum of inertia
I [A]	current
I_c [A]	critical current
I_{c0} [A]	critical current before loading, virgin state
I_s [A]	strand current
I_t [A]	transport current
J [A/m ²]	current density
J_c [A/m ²]	critical current density
J_{sc} [A/m ²]	current density in area of cross-section occupied by superconductor
J_m [A/m ²]	current density area of cross-section occupied by matrix material
k_θ	$\cos^{-1}(\theta)$ correction factor
l_0 [m]	initial length of a sample subjected to strain
L_p [m]	strand or cable twist pitch length
L_w [m]	characteristic bending wavelength
$L_{w,n}$ [m]	characteristic wavelength with n indicating the order ($n=1$ for L_{w1})
L_ϕ [m]	characteristic length, $4.3 \cdot 10^{-3}$ m
M [A/m ³]	magnetization; magnetic moment per unit volume
M_b [m ³]	bending moment
n_k	shape factor including volume fraction involved in coupling current path
$n\tau$ [s]	effective coupling current decay time constant
n -value	index characterizing the steepness of the V - I curve in the E range of 10-100 μ V/m [2]
N	number of dominant coupling loss time constants
N_l	number of strand layers
N_s	number of strands in the cable
p	fitting parameter in Nb ₃ Sn scaling parameterization
P_{cpl} [W/m ³]	coupling current power loss per unit volume
P_{hyst} [W/m ³]	hysteresis power loss per unit volume
q	fitting parameter in Nb ₃ Sn scaling parameterization
q_l [N/m]	distributed electromagnetic load per unit length
Q [N/m]	electromagnetic load between strand crossings

Q_M [J/cycle]	mechanical loss dissipated in one loading cycle
Q_{cpl} [J/m ³ ·cycle]	coupling current loss per unit volume per cycle
Q_{hyst} [J/m ³ ·cycle]	hysteresis loss per unit volume per cycle
S	strain dependent term in Nb ₃ Sn scaling parameterization
R_c [Ωm]	interstrand contact resistance
RRR	residual resistivity ratio
t	normalized temperature
T [K]	temperature
T_{c0} [K]	critical temperature at 0 T
T_{cm}^* [K]	inhomogeneity averaged critical temperature
T_{cs} [K]	current sharing temperature
T_{op} [K]	operating temperature
V [V]	voltage
v_f	cable void fraction
W_b [m ³]	section factor written as $d_s^3 \cdot \pi/32$
$\cos^{-1}\theta$	$\cos \theta$ factor representing the actual cable cross section
α	fitting parameter in strain scaling parameterization
β	fitting parameter in strain scaling or coupling loss parameterization
Δl [m]	elongation of a sample subjected to strain
ΔT [K]	temperature margin
ε	axial strain, sum of applied and pre-compression strain
ε_{axial}	axial strain, sum of applied and pre-compression strain
$\varepsilon_{0,a}$	remaining strain component when $\varepsilon_{axial}=0$
ε_b	bending strain in the Nb ₃ Sn filaments
ε_{bo}	peak bending strain in the Nb ₃ Sn filaments
ε_{irr}	irreversibility strain limit
ε_{sen}	strain sensitivity between -0.7 and -0.3%
ε_{shift}	measurement related strain in Nb ₃ Sn scaling parameterization
ε_{th}	thermal pre-compression of the Nb ₃ Sn filaments
γ	fitting parameter in strain scaling parameterization
φ	angle between crossing strands
θ	angle between strand and conductor longitudinal axis
κ	factor for bending moment, depending on position in wire
μ_0 [H/m]	magnetic permeability of free space; $4\pi \cdot 10^{-7}$
v_{bar}	strand volume fraction with barrier
v_{Br}	strand volume fraction with bronze
v_{Cu}	strand volume fraction with copper
v_{fil}	strand volume fraction with filaments
ρ_m [Ωm]	matrix interfilament resistivity
ρ_t [Ωm]	effective transverse inter-filamentary resistivity
ρ^* [Ωm]	resistivity criterion used to define the I_c (at 10 $\mu V/m$)

σ [Pa]	contact or tensile stress
σ_a [Pa]	average stress
σ_{bar} [Pa]	tensile stress in barrier over cross section area of strand
σ_{Br} [Pa]	tensile stress in bronze over cross section area of strand
σ_{Cu} [Pa]	tensile stress in copper over cross section area of strand
σ_{fil} [Pa]	tensile stress in filaments over cross section area of strand
σ_{hom} [Pa]	homogeneous stress in filaments over cross section area of strand
σ_{max} [Pa]	maximum contact stress in the cable
τ [s]	time constant for coupling currents to decay
τ_k [s]	dominant cable coupling current decay time constant
ω [s ⁻¹]	angular frequency

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1 Introduction

Superconducting materials are crucial for the development of nuclear fusion. The large magnet systems as build now for ITER rely on NbTi and Nb₃Sn superconducting strands used in the so-called Cable-In-Conduit Conductors. The stability of the CIC conductors is a requirement for a machine that eventually serves for delivery of electrical energy to the power grid. Important factors for stability are the AC losses and possible degradation due to mechanical deformation caused by the huge electromagnetic forces. This thesis deals with the understanding of the effects of strand and cable deformation on the performance and optimization of the transport properties of the superconductors. In the first chapter an introduction is given to magnetic confinement in fusion reactors with reference to the ITER magnet system.

This chapter is partly based on papers [23], [24] and [25].

1.1 Nuclear fusion

Nuclear fusion is the process at the core of the Sun from our planetary system. What we see as light and feel as warmth is the result of a fusion reaction: if light nuclei are forced together, they will fuse with a yield of energy because the mass of the combination will be less than the sum of the masses of the individual nuclei. In the core of the stars, large gravitational forces create the extreme density and temperature necessary for fusion. Nuclear fusion is a nuclear reaction by which two or more atomic nuclei join to form a heavier nucleus. In the process, matter is not conserved and part of the mass of the fusing nuclei is converted into energy. The energy release is due to the action of two opposing forces, the nuclear force drawing together protons and neutrons, and the Coulomb force causing protons to repel each other. However, at very short spans of atomic nuclei, nuclear force can overcome electric repulsion, hence building up nuclei up to iron and nickel from lighter ones by fusion. This causes net energy release from the net attraction of these particles.

At temperatures in excess of 100 million degree, nuclei no longer form neutral atoms but exist in the plasma state, in which the ionized atoms are accelerated to high speed. Under such conditions, they can overcome the Coulomb barrier and approach each other close enough for the attractive nuclear force to achieve fusion.

A promising fusion reaction, allowing for the highest energy gain at the lowest reaction temperature, involves two H isotopes, deuterium (D) and tritium (T). In the fusion of deuterium and tritium, one helium nucleus, one neutron, and energy are produced. The helium nucleus carries an electric charge, which responds to the magnetic field of the fusion reactor and thus remains confined within the plasma. However, the largest part of the energy produced is carried away from the plasma by neutrons, which have no electrical charge and are therefore unaffected by magnetic field. In fusion power plants, the neutrons will be absorbed by the plasma facing walls in the plasma chamber, transferring their energy as heat that will be used to produce steam and, by way of conventional turbines and alternators, electricity.

On Earth, thermonuclear fusion is an attractive source for generating energy because the necessary fuels are abundantly available and a rather limited amount (250 kg per year for a 1 GW fusion power plant, half of it deuterium, half of it tritium) is needed in the reaction. In addition, fusion emits no pollution or greenhouse gases from the fusion process itself. Its major by-product is helium: an inert, non-toxic gas. Moreover, the fusion process is considered inherently safe, because any variation in the required conditions will cause the plasma to cool down within seconds, stopping the reaction. Among the drawbacks of fusion is the radioactivity acquired by the reactor inner walls (see Figure 1.1) as well as the radiation damage introduced in the plasma facing components forcing a rather massive, periodic and time consuming replacement of these parts.

Figure 1.1. Plasma chamber of JET, showing the inner wall of the fusion reactor.

Although, the half-life of the radioisotopes produced in the reaction is less than those from nuclear fission, fusion waste is more radioactive. Concerns also exist about the possible release of tritium. With a half-life of 12 years, tritium can remain in the environment for more than 100 years after its creation.

Since its start in the 1950s, research on controlled fusion for energy has largely progressed and several fusion machines, mainly of the tokamak type (see section 1.2), have been constructed, which can generate and maintain plasmas for several minutes. At present, the largest operating fusion experiment in the world is the Joint European Torus (JET) in Culham (UK). The machine also holds the record for the generation of fusion energy with 16 MW during 1 second, and a continuous fusion capacity of 4 MW during 4 seconds. ITER, presently under construction, represents the next important step towards the exploitation of fusion energy, being the first pulsed device designed to produce a power of ten times the input power, while JET only produced 70% of its input power from fusion reaction. The volume of the plasma, and therefore the size of the reactor, seems crucial for the ratio between output and input power. The cross sections are compared in Figure 1.2.

In spite of the substantial progress, several technological challenges remain on the road leading to electricity production by commercial fusion power plants [1]:

- To achieve the conditions enabling a net energy production, plasmas must be maintained at high density and temperature for a few hours or even in steady state. This requires the minimization of energy loss due to small-scale turbulences and plasma instabilities. At the same time, a large fraction of the heating power must be radiated by the confined plasma to avoid excessive heat load on localised components of the machine. Operating plasma regimes

simultaneously ensuring high plasma confinement and radiation have yet to be qualified.

- The power necessary to maintain plasmas at high temperatures is ultimately exhausted in a narrow region of the reaction chamber called the divertor. Although, exhaust systems able to withstand heat fluxes up to 20 MW/m^2 for some time (which is of the same order as the heat load on the sun's surface) have been produced for ITER, solutions for the larger power load on the divertor and longer duration expected in future fusion machines still need to be developed.
- Neutron resistant materials able to withstand the neutron flux and maintain their structural and thermal conduction properties for long operation times need to be developed. Although some candidate materials already exist (EUROFER for the breeding blanket, tungsten for plasma facing component armour and copper alloys for the divertor coolant interface), much more R&D is needed for improvements. Also the completion of their characterisation under relevant conditions requires dedicated irradiation facilities.
- Tritium self-sufficiency is mandatory for future fusion power plants. This requires the research of efficient breeding and extraction systems as well as tritium loss minimisation.
- Continuous or quasi-continuous operation of the fusion process, but also for the entire plant with many relatively vulnerable technologies like cryogenics and vacuum systems has to be demonstrated.

Figure 1.2. Cross-sections and radii of the plasma in European tokamak fusion reactors, showing the significant scale difference between JET, its predecessors and ITER.

In order to address the above issues, the DEMO project [1] is currently envisaged as the next step between ITER and prototype fusion power plants. On a longer time scale, alternative magnetic confinement fusion devices may also reach sufficient technological maturity. In this respect, the stellarator is the most promising configuration offering advantages over tokamaks, such as capability for steady-state operation and lower occurrence rate of plasma instabilities [1]. Currently, the stellarator machine W7X using a superconducting magnet system as well is in its early stage of operation in Greifswald (Germany) [2].

To be economically successful, fusion will need to demonstrate its potential for competitive cost of electricity. Extended operation times and high efficiency of the power conversion cycle have to be ensured for commercial fusion power plants. To reduce construction costs, materials, in particular for the first wall and divertor, allowing extended operational time and simple fabrication routes have to be identified. Plasma regimes of operation with improved confinement will also contribute to reduce plant size and cost. In the future, high temperature superconductors when their cost is drastically reduced, may eventually replace the actual NbTi and Nb₃Sn superconductors in the magnets, avoiding the use of large amounts of liquid helium and increasing the reliability of the machine by larger stability margin.

1.2 Magnet systems for fusion

High plasma densities must be achieved to enable the required fusion reaction and energy release. With increasing density, the plasma temperature rises to such extreme values that no material withstands direct contact with such plasma. The solution is to use a magnetic field to confine the plasma and prohibit contact with the first wall. Consisting of charged particles, plasmas can be shaped and confined by magnetic forces.

The simplest magnetic configuration is a long solenoid, where the magnetic field lines run parallel to the axis of the cylinder. Such a magnetic field prevents charged particles being lost radially, but does not confine them at the ends of the solenoid. To solve the problem two approaches can be used. One consists in blocking the ends with magnetic mirrors; the other in trapping the plasma particles by bending the magnetic field lines so that they close on themselves in a doughnut shape.

The latter solution has resulted in the development of the tokamak reactor (Russia 1950) today's most used design in magnetic confinement for fusion experiments, see Figure 1.3. Magnets around the walls of the toroid shaped tokamak produce the toroidal field. However, a toroidal magnetic field alone would provide poor confinement because its strength decreases from the centre of the reactor to more outwards, so that the particles tend to drift centrifugally. For complete confinement, an additional poloidal field is required, causing the plasma particles to remain spinning in a helical pattern thus keeping them constantly moving in the center area of the torus and away

from the walls. Most of the poloidal field in tokamaks is provided by the toroidal electrical current that flows inside the plasma.

Acting as the primary windings of a transformer, the central solenoid induces a current through the plasma, which act as the secondary winding that also contributes to heating the plasma through ohmic heating. Depending on the specific design of the reactor, additional sets of control coils (like the Poloidal Field coils in Figure 1.3) may be added to the tokamak magnet system which, generating a vertical magnetic field, contribute to the plasma confinement.

Figure 1.3. Magnetic confinement in a tokamak fusion reactor with toroidal and poloidal coils in the magnet system.

1.3 ITER

The ITER project comprises building the world's largest (830 m^3 of plasma volume) and most advanced experimental tokamak fusion reactor at the Cadarache site in southern France. The ITER magnet system is the largest and most integrated superconducting magnet system ever built. Its stored magnetic energy is 51 GJ. As a comparison, the second largest superconducting magnet system is the Large Hadron

Collider machine at CERN, which has a stored magnetic energy of 11 GJ distributed over a magnet ring of 27 km in circumference [3]. The organisation of the project is described elsewhere [4]. The machine is expected to demonstrate the feasibility of producing more power from fusion than is used to sustain it, a challenge not yet been achieved by previous fusion reactors. The project goal is an energy gain factor Q of 10, corresponding to an output of 500 MW for 50 MW of input power.

To generate the high magnetic field needed to confine, shape and control the plasma, the use of superconducting magnets is indispensable. The ITER magnet system comprises 48 superconducting coils [5], see Figure 1.4:

- 18 Toroidal Field (TF) coils;
- 1 Central Solenoid (CS), composed of 6 coil modules;
- 6 Poloidal Field (PF) coils;
- 9 pairs of Correction Coils (CC).

Figure 1.4. The magnet system of ITER [5].

The 18 Toroidal Field coils are designed to carry a steady-state current, generating 5.3 T at the plasma outer radius. Their dimensions (each coil is 14 m high and 9 m wide) and total toroid weight exceeding 6,000 t make them one of the largest components of the ITER machine. The heart of the magnet system is the Central

Solenoid, featuring an inner radius of 1.3 m and outer radius of 2.08 m for a total height of 12 m. The Central Solenoid is composed of six stacked modules, whose currents can be independently driven to enable the testing of different operating scenarios. The Central Solenoid design is driven by the required magnetic flux leading to a peak magnetic field ramp rate of 1.3 T/s in the inner windings [6].

The Poloidal Field system consists of six horizontal solenoidal magnets placed outside the toroidal magnet structure, with diameters in the range 8 to 24 m [7, 8].

Correction coils inserted between the Toroidal and Poloidal Field coils and distributed around the tokamak complete the ITER magnet system, correcting the error field modes. The ITER Correction Coils include three sets of six coils each, namely the bottom, the side and the top Correction Coils, distributed symmetrically around the tokamak and inserted between the Toroidal Field and the Poloidal Field coils (Figure 1.4). Although lighter than the other coils and carrying a lower current, the Correction Coils are large in size (up to 8 m wide) and feature a non-planar shape. The type of superconducting material, peak operating current and magnetic field of all superconducting coils are listed in Table 1.1.

Table 1.1. Type of superconductor, peak operating currents and magnetic fields of the ITER magnets [7, 8].

Coil	strand	current [kA]	B_{peak} in windings [T]
TF	Nb ₃ Sn	68	11.8
CS	Nb ₃ Sn	40 - 45	13
PF	NbTi	48 - 55	6
CC	NbTi	10	5

ITER plasma scenarios

Details of the scenarios envisaged for ITER operation can be found in [8]. Figure 1.5 shows the current in the Poloidal Field coils and in the six modules of the Central Solenoid during a 15 MA plasma pulse, which represents the reference ITER operating scenario.

Four main phases can be recognized from the coil currents point of view:

1. in the interval $-310 < t < 0$ s the currents in the coils are ramped up to their nominal values. The current charge process lasts for 300 s and it is followed by a plateau of 10 s;
2. at $t=0$ s the Start of Discharge (SOD) takes place, when the currents in the coils are rapidly varied to induce and shape the initial plasma. Very high magnetic field variations take place especially in the first 1.5 s, but a significant ramp rate is observed up to 80 s;

3. the successive phase between $80 < t < 700$ s, when a burning plasma is maintained and eventually slowly cooled down, is characterized by lower currents, and hence less severe magnetic field variations;
4. finally between $700 < t < 950$ s all coil currents are ramped down to zero. A long 0 kA current plateau is then kept for 900 s, after which the successive plasma pulse is initiated by ramping up the currents again.

Being characterized by the highest field change rates, the Start of Discharge represents the most critical operating phase for the dynamic stability of the superconducting magnets, as further discussed in section 1.4.

Figure 1.5. Nominal operating currents in the 6 modules of the ITER Central Solenoid and 6 Poloidal Field coils during the 15 MA plasma scenario [8]. For $t < 0$ s currents in the coils are ramped to the nominal values. The discharge starts at $t = 0$ s.

1.4 Superconductors

1.4.1 Materials

Superconductivity is a phenomenon whereby certain materials, when cooled to low temperature, can conduct steady-state currents without electrical resistance. The transition from the normal conducting to the superconducting state occurs at a critical temperature T_c characteristic for the material. The superconducting state is also bounded

to a magnetic field lower than the upper critical magnetic field B_{c2} and to current densities below a critical value J_c . The critical temperature, upper critical field and engineering current density, i.e. the critical current density normalized to the wire cross-section, of practical superconducting materials are summarized in Table 1.2.

In crystal-like superconductors, like Nb_3Sn , the critical behaviour is significantly affected by strain as well. In these materials, lattice deformation alters B_{c2} and T_c , while in the case of severe deformation micro-structural cracks limit the transport current. Given the above limitations, the performance of a superconductor is generally described by means of a critical surface in the J - B - T space for a given strain state. For combinations of the three parameters corresponding to points below the critical surface, the material is in the superconducting state. The material is instead normal conducting for points above the critical surface.

Table 1.2. Critical temperature, upper critical field and characteristic engineering critical current density for practical superconductors [9].

Material	T_c (0 T) [K]	B_{c2} (0 K) [T]	J_c (B , 4.2 K) [A/mm ²]
NbTi	9.3	15	1,000 (6 T)
Nb ₃ Sn	18.3	24-28	700 (15 T)
YBaCuO	92	≈160	400 (20 T)
Bi ₂ Sr ₂ CaCu ₂ O _z	120	>100	600 (20 T)

In practical applications of superconductivity, the operating temperature is often set around 4.5 K. In this case the relevant parameter to characterize the performance of the different superconducting materials is their critical current density variation with the applied magnetic field.

Figure 1.6 illustrates the engineering critical current density dependence on the applied magnetic field at 4.2 K for several practical superconductors.

For the most used materials, i.e. NbTi and Nb₃Sn, Equations 1.1 [10] and 1.5 [11], respectively, are commonly adopted to describe the J_c change with temperature, magnetic field and, eventually strain.

$$\text{NbTi:} \quad J_c = \frac{C_0}{B} (1 - t^{1.7})^\gamma b^\alpha (1 - b)^\beta, \quad [\text{A} \cdot \text{mm}^{-2}] \quad (1.1)$$

where t is the reduced temperature, b reduced magnetic field and B applied magnetic field. C_0 , γ , α and β are fitting parameters for the specific wire. The normalized temperature is defined in Equation 1.2:

$$t = \frac{T}{T_{c0}}, \quad (1.2)$$

where T is the operating temperature and T_{c0} is the critical temperature at 0 T. The normalized magnetic field is given in Equation 1.3:

$$b = \frac{B}{B_{c2}(T)}, \quad (1.3)$$

where B_{c2} is the upper critical magnetic field at the operating temperature T . This is expressed in Equation 1.4:

Figure 1.6. Engineering critical current density versus applied magnetic field for several practical superconductors at 4.2 K [9].

$$B_{c2}(T) = B_{c20}(1 - t^{1.7}), \quad [T] \quad (1.4)$$

where B_{c20} is the upper critical magnetic field at 0 K.

$$\text{Nb}_3\text{Sn: } J_c = \frac{C_1}{B} \cdot S \cdot (1 - t^{1.52})(1 - t^2)b^p(1 - b)^q, \quad [A/mm^2] \quad (1.5)$$

with t normalized temperature, b normalized magnetic field, S strain dependent term and B applied magnetic field. C_1 , p and q are fitting parameters for the specific wire. The normalized temperature is defined as:

$$t = \frac{T}{T_c^*(\epsilon)}, \quad (1.6)$$

where

$$T_c^*(\epsilon) = T_{cm}^* \cdot S^{\frac{1}{3}}, \quad [K] \quad (1.7)$$

with T_{cm}^* the inhomogeneity averaged critical temperature. The reduced magnetic field is defined as:

$$b = \frac{B}{B_{c2}^*(\varepsilon, T)}, \quad (1.8)$$

where

$$B_{c2}^*(\varepsilon, T) = B_{c2m}^*(0) \cdot S \cdot (1 - t^{1.52}), \quad [T] \quad (1.9)$$

with $B_{c2m}^*(0)$ the inhomogeneity averaged upper critical magnetic field at 0 K. The strain dependent term S is defined as:

$$S = \frac{C_{a,1} \cdot \left[\sqrt{\varepsilon_{shift}^2 + \varepsilon_{0,a}^2} - \sqrt{(\varepsilon_{axial} - \varepsilon_{shift})^2 + \varepsilon_{0,a}^2} \right] - C_{a,2} \cdot \varepsilon_{axial}}{1 - C_{a,1} \cdot \varepsilon_{0,a}} + 1, \quad (1.10)$$

where $C_{a,1}$ and $C_{a,2}$ are the second and third invariant of the axial strain sensitivity, ε_{axial} is the axial strain (sum of applied and pre-compression strains), $\varepsilon_{0,a}$ is the remaining strain component when $\varepsilon_{axial}=0$, and ε_{shift} is the measurement related strain given by Equation 1.11:

$$\varepsilon_{shift} = \frac{C_{a,2} \cdot \varepsilon_{0,a}}{\sqrt{C_{a,1}^2 - C_{a,2}^2}}. \quad (1.11)$$

NbTi is the principle material used in practical applications of superconductivity and it allows operation in magnetic field up to about 8 T. In the ITER project, NbTi is used in the Poloidal Field coils and in the Correction Coils. The Nb₃Sn compound is adopted instead when magnetic fields up to 16 T at 4.5 K are required. However, due to its brittle nature that makes it difficult to process, the usage of Nb₃Sn has been so far relatively limited. Indeed the ITER Central Solenoid and Toroidal Field coils are its first large-scale applications beyond laboratory and NMR magnets.

Both NbTi and Nb₃Sn are low-temperature superconductors requiring cooling with liquid helium in the 1.9-5 K range. As illustrated in Figure 1.7 for NbTi and Nb₃Sn, the specific heat of the materials at cryogenic temperatures is significantly reduced compared to room temperature (2000 times lower).

Figure 1.7. Specific heat at constant pressure versus temperature for NbTi and Nb₃Sn at cryogenic temperatures.

It follows that even a small energy release cause a large temperature rise, and thus lead to a quench, i.e. a sudden and irreversible transition from the superconducting to the normal state. Following a quench, re-cooling of the magnets down to their operation temperature is necessary, which, apart from being costly, causes an interruption of the normal operation. Therefore, to avoid the occurrence of quenches, magnets must be built with a temperature margin ΔT such as to ensure reliable operation against the most critical scenarios (i.e. plasma disruption in tokamak machine) and in the entire windings, including susceptible areas like the high-magnetic field region and joints. The temperature margin ΔT is defined as [12]:

$$\Delta T = T_{cs} - T_{op}, \quad [\text{K}] \quad (1.12)$$

where T_{cs} , and T_{op} are the current sharing temperature and operating temperature, respectively. In practical superconductors, the voltage – temperature transition from the normal to the superconducting state is not sharp, but occurs over an extended temperature range. As a consequence, a criterion needs to be set to establish the boundary between superconducting and resistive conditions. For low temperature superconductors, the current sharing temperature T_{cs} is commonly defined as the value at which a longitudinal electric field of $10 \mu\text{V/m}$ is detected along the sample while ramping the temperature at fixed current.

The longitudinal electric field in a strand E_z is a non-linear function of the current, of the temperature and of the magnetic field. It is calculated by solving the following equations:

$$I = J_{sc}A_{sc} + J_m A_m, \quad [\text{A}] \quad (1.13)$$

$$E = E_c \left(\frac{I}{I_c(T, B, \epsilon)} \right)^{n(T, B, \epsilon)} = J_m \rho_m(T, B, \epsilon), \quad [\text{V/m}] \quad (1.14)$$

where A_{sc} and A_m are the areas of the cross sections occupied by superconductor and matrix material, respectively, J_{sc} and J_m are the current densities in the superconductor and in the matrix material, E_c and J_c are the critical parameters of the superconductors and ρ_m is the electrical resistivity of the matrix material. The axial strain of the Nb_3Sn filaments and the stabiliser is represented by ϵ . For a given current and a possible change in the matrix resistivity, the I_c will change accordingly.

The n -value or the resistive transition index is normally determined from the EI curves in the electric field range of $10\text{-}100 \mu\text{V/m}$ with the commonly used power law representation in Equation 1.14. The n -value is generally considered as a quality index and is for well-developed conductors mostly in the range of 20 to 50.

The current sharing, i.e. the possibility to re-distribute over-currents among wires, also plays a role for the T_{cs} . The operating temperature T_{op} is the local temperature of the superconductor determined by the cooling conditions and energy loss within the windings.

The residual-resistivity ratio (RRR) of superconductors is defined as the ratio of the resistivity of a material at 273 K and at cryogenic temperature. The cryogenic temperature is usually chosen at 20 K for Nb₃Sn and 10 K for NbTi.

1.4.2 Strands

For stability and AC loss reasons [13], practical superconductors are shaped as wires (diameter about 1 mm), or when used in cables called strands, comprise a large number of thin superconducting filaments (diameter 1-50 μm) which are twisted and embedded in a low-resistivity matrix of normal metal.

In this thesis we mainly concentrate on Nb₃Sn strands. The main specifications of the ITER Nb₃Sn strands are detailed in Table 1.3, while a few characteristic strand cross-sections are shown in Figure 1.8. The Residual Resistance Ratio (RRR) is the ratio of the strand resistivity between zero and 20 K, mainly determined by the purity of the copper stabiliser.

Since the resistivity of superconductors above their critical temperature T_c is relatively high, a low resistive path for the current is necessary in the case of transition to the normal state. This is to avoid excessive ohmic heating and, in the worst-case scenario, even melting as further discussed in section 0. Cu for NbTi, CuSn/Cu for Nb₃Sn, Ag for BSCCO-2212 and steel/Cu/solder for YBCO wires are used as matrix materials, exhibiting an electrical resistivity several orders of magnitude lower than the one of the filaments in the normal state. Filament diameters of 5 μm or below are required for the ITER strands to limit AC hysteresis loss, as detailed in Table 1.3 and section 1.5.

Table 1.3. Specification of ITER Nb₃Sn strands [14].

	TF	CS
Strand diameter [mm]	0.820 ± 0.005	0.830 ± 0.005
Twist pitch [mm]	15 ± 2	15 ± 2
Twist direction	Right hand	Right hand
Cr-plating thickness [μm]	$2.0 + 0 - 1$	$2.0 + 0 - 1$
Cu-to-nonCu volume ratio	1.0 ± 0.1	1.0 ± 0.1
Filament diameter [μm]	≤ 5	≤ 5
RRR of Cr-plated strand	> 100	> 100
Critical current [A] at 4.22 K and 12 T	≥ 190	≥ 228
Resistive transition index in the 10-to-100 $\mu\text{V/m}$ range	> 20	> 20
Max. hysteresis loss [kJ/m^3] per strand unit volume at 4.22 K over a ± 3 T cycle	500	500

Figure 1.8. Transverse sections of (a) bronze and (b) internal-tin processed strands for ITER Toroidal Field conductors. The scale bars correspond to 100 μm [14, 15].

The subdivision into small filaments also improves the stability of the strands against local thermal disturbances, allowing fast heat and current transfer to the surrounding matrix. Filament twisting is introduced to reduce inter-filament coupling currents induced by time-varying magnetic field (see section 1.5). In twisted wires, the magnetic flux linked to the current loops changes sign every half-twist pitch. For sufficiently short twist pitches, only limited space is available for large transverse currents to build up.

The same strategy is used for cabled conductors composed of several tens or even more than thousand strands. The resistance of the crossover contacts R_c determines the interstrand coupling loss and should therefore be controlled to keep the generated heat below acceptable limits. On the other hand, the R_c can strongly affect the stability of a cabled conductor by influencing the degree of current sharing among strands. Thus R_c should not be chosen too high in order to guarantee sufficient margin for current redistribution. These two phenomena define the bandwidth for safe and reliable operation of magnets made with cabled conductors.

The value of R_c depends on a variety of factors such as the internal strand lay-out, cable geometry, compacting factor, production conditions, impregnation, heat treatment, strand surface properties, contact deformation, transverse load, interstrand surface micro-sliding and wearing. Nb_3Sn strands need a reaction heat treatment to allow diffusion of tin into the niobium with temperatures up to 650 $^{\circ}\text{C}$ for 2 - 4 weeks. The Nb_3Sn strands are coated with a relatively hard and highly-resistive Cr-coating to avoid sintering of strands during the heat treatment and moderate their coupling in the final cable. The heat treatment has a large influence on the R_c [16, 17].

1.4.3 Cable-In-Conduit Conductors

To achieve the large currents needed to generate high magnetic fields and restrict the self inductance of the coils to allow fast sweeping of the current, many strands are taken

parallel and cabled as illustrated in Figure 1.9 and Figure 1.10. The ITER magnets rely on the Cable-In-Conduit Conductor (CICC) concept, by which up to 1500 strands are twisted in multiple cabling stages around a central spiral and then inserted in a metallic jacket [18, 19]. The He leak-tight jacket surrounding the cable primarily provides mechanical reinforcement by taking up practically all of the Lorentz force in the coil windings during operation. The difference between the jackets of the ITER Toroidal Field and Central Solenoid CICC is due to the expected difference in thermal and electro-magnetic forces on the conductors and is shown in Figure 1.11.

Figure 1.9. View of an ITER conductor cross section and showing how 1500 strands are twisted in multiple cabling stages around a central spiral and then inserted in a metal jacket.

Figure 1.10. View of an ITER conductor showing the underlying multistage structure.

The winding pack for the TF design is based on the use of radial plates and circular conductor cross section in a double-pancake configuration. This design is chosen because of the expected high insulation reliability and the possibility to detect faults before significant damage occurs. Insulation faults are the most probable cause of magnet failure and also considering the difficulties involved in the replacement of a TF coil. The Lorentz forces acting on each conductor are transferred to the radial plate, without accumulation of forces on the conductor and its insulation.

For cooling, supercritical He flows through the interstices between the strands (typical void fraction 30%) and the central channel. The combination of forced He flow and large strand-coolant contact (wetted perimeter) results in optimal cooling and thus stability against significant power dissipation. A central channel delimited by a metal spiral is added to reduce the He pressure drop, while facilitating He circulation between the strand bundles.

Copper strands are included in the cable to offer a low resistivity current path in the case of a quench, a sudden transition of the superconductor to the normal state. The presence of copper strands reduces the hot spot temperature during a quench and hence improves the protection against damage due to overheating. Metal barriers (wraps) are added around the last stage sub-cables (petals) to reduce inter-strand coupling in pulsed operation. The main specification of the ITER Nb₃Sn CICC is detailed in Table 1.4.

Table 1.4. Specification of ITER Toroidal Field and Central Solenoid CICC [3, 18, 19].

	TF	CS
Cable pattern	$((2SC+1Cu) \times 3 \times 5 \times 5 + C) \times 6$	$(2SC+1Cu) \times 3 \times 4 \times 4) \times 6$
Core C	3Cu x 4	-
Central spiral [mm]	8 x 10	7 x 9
SS petal wrap	0.10 mm thick 50% coverage	0.05 mm thick 70% coverage
SS cable wrap	0.10 mm thick 40% overlap	0.08 mm thick 40% overlap
Nr. SC Nb ₃ Sn strands	900	576
Nr. copper strands	522	288
Void fraction	29.7%	33.5%
Cable diameter [mm]	39.7	32.6
Jacket [mm]	Circular Ø43.7	Circle in square 49 x 49
Jacket material	316LN	JK2LB

Figure 1.11. Cross-sections of the Toroidal Field (left) and Central Solenoid (right) conductors [18].

1.4.4 Stability of CIC conductors

To achieve the envisaged repetition rates of plasma cycles in ITER, and hence proper operation of the reactor, it is essential to build magnets with a well-proven stability margin. Loss of stability implying significant costs in terms of operation time would be unacceptable for a power plant. Following a quench event, re-cooling of the magnets down to their operation temperature would be required, causing interruption of operation. In the worst case, quench can result in a damage of the conductor if the temperature of the hot spot and stress on the system are not properly restrained. Considering that the ITER magnet system cost amounts to more than a quarter of the total machine cost and that spare pieces may not be readily available, the chances on occurrence of such events must be absolutely minimised. Different phenomena can occur in superconducting cables and joints that limit their performance.

Current unbalance among the strands originates in the natural spread in the contact resistances between strands and joints. When, as a result of the current non-uniformity, the current in a given strand exceeds the critical current determined by the local magnetic field and temperature, a transition to the normal state may occur. The successive evolution of one initial normal zone into a quench or into recovery of the conductor depends on the possibility for the saturated strand to expel excess current into the surrounding strands, thereby limiting ohmic heating and temperature rise.

The exposure to time-varying magnetic field induces currents in multi-strand cables, both within the individual strands and between them. The flow of coupling currents through the resistive parts of the strands and cable produces ohmic heating, and thus a temperature increase. Such loss can therefore potentially lead to a quench. Moreover, the addition of induced coupling currents can cause the strand total current to become higher than the critical current, leading to local normal zones in strands and eventually quench of the entire conductor. This requires precise knowledge of the evolution of the interstrand contact resistance with cycling of the electromagnetic forces. Low interstrand resistances allow improved current uniformity by facilitating current sharing

among strands. On the other hand, highly resistive barriers around strands and petals are required to limit eddy, inter-strand and inter-cable coupling currents in pulsed operation. As in most superconductivity applications, these two competing requirements dictate a carefully balanced design of strands, cables and joints.

1.5 AC loss

Several analytical models have been developed, which allow reasonable predictions of the losses within single multi-filamentary strands for specific magnetic field variations. However, the determination of inter-strand coupling loss in CIC conductors and joints is challenging due to their direct dependence on the cable geometry and to the variability of the inter-strand and strand-to-joint contact resistances. The main ideas are briefly sketched below. For hysteresis and intra-strand coupling losses the descriptions in [13, 20, 21] are followed; for the inter-strand coupling loss the method proposed in [22, 23] is adopted instead.

1.5.1 Hysteresis loss

When exposed to a changing magnetic field, shielding currents of density $\pm J_c$ build up in the outer layer of superconducting filaments, which screen the interior from the changing magnetic field. Figure 1.12 illustrates the case for a round filament without transport current in a transverse magnetic field.

Figure 1.12. Screening current density and magnetic field profiles in a superconducting filament (without transport current) in a transverse time-varying external magnetic field $B_{a,i}$. (a) $B_{a,a} < B_p$, (b) $B_{a,b} = B_p$ and larger, (c) $B_{a,b} - 2B_p < B_{a,c} < B_{a,b}$, (d) $B_{a,d} = B_{a,b} - 2B_p$ and smaller.

As the magnetic field increases, the boundary between the screening current region and the current-free region shifts towards the centre of the filament because the critical

current density is limited. The full penetration of the filament with screening currents is reached at the so-called full-penetration magnetic field B_p , which for transverse magnetic field and cylindrical filaments can be expressed as

$$B_p = \frac{\mu_0 J_c d_f}{\pi}, \quad [\text{T}] \quad (1.15)$$

with J_c the critical current density and d_f the filament diameter.

The magnetisation, defined as the magnetic moment per unit volume, produced by the screening currents caused by transverse and parallel magnetic fields is given by Equations 1.15 and 1.16, respectively

$$M = \frac{2J_c d_f}{3\pi}, \quad [\text{A/m}^3] \quad (1.16)$$

$$M = \frac{J_c d_f}{6}. \quad [\text{A/m}^3] \quad (1.17)$$

Characteristic magnetization loops, strongly depending on the strand design, are shown in Figure 1.13.

Figure 1.13. Magnetization loops of various ITER Nb₃Sn type strands at $T=4.2$ K, $B_a=3$ T and $f=10$ mHz.

The energy dissipation per cycle in a unit volume is usual to the area enclosed by the hysteresis loop, which may be written as

$$Q_{hyst} = \oint M dB. \quad [\text{J/m}^3 \cdot \text{cycle}] \quad (1.18)$$

The result can be equivalently expressed in terms of power loss per unit volume

$$P_{hyst} = M \dot{B} = \frac{2}{3\pi} J_c d_f \left| \frac{dB}{dt} \right|. \quad [\text{W/m}^3] \quad (1.19)$$

When the filaments carry a transport current I_t , as in operational cables, the magnetization is reduced. However, since the work done by the power supply to keep I_t constant adds to the work needed to generate the external magnetic field, the overall loss is increased by a factor $1 + (I_t/I_c)^2$.

This relation is valid when I_t is constant with an applied dB/dt , for alternating I_t a factor $1 + 1/3(I_t/I_c)^2$ is used but since the impact of this factor is not dominant, we use $1 + (I_t/I_c)^2$ for our calculations:

$$P_{hyst} = \frac{2}{3\pi} J_c d_f \cdot \left[1 + \frac{I_t^2}{I_c^2} \right] \cdot \left| \frac{dB}{dt} \right|. \quad [\text{W/m}^3] \quad (1.20)$$

For closely packed filaments, proximity effects may exist in the strands due to Cooper pairs tunnelling through the normal barrier between adjacent filaments. In this case, an effective diameter d_{eff} corresponding to the size of the bundle of coupled filaments must be used in Equations 1.18 and 1.19 instead of the pure filament diameter d_f .

1.5.2 Inter-filament coupling loss

Figure 1.14 shows a multi-filamentary wire subjected to a uniform external magnetic field B_a , changing with a rate dB_a/dt . The arrows indicate the path followed by inter-filament coupling currents. Coupling currents flow along the filaments and cross over through the resistive matrix every half-twist pitch. The matrix crossing currents mainly follow a vertical path, parallel to the changing magnetic field.

Figure 1.14. Twisted multifilamentary composite in a changing transverse magnetic field showing the path used to calculate the flux linkages.

The coupling currents thus give rise to an axial cosine-like current distribution around the wire, which generates a dipole field in its interior. Hence, a uniform internal magnetic field B_i is generated by the induced coupling currents:

$$B_i = B_a - \dot{B}_i \tau, \quad [\text{T}] \quad (1.21)$$

where

$$\tau = \frac{\mu_0}{2\rho_t} \left(\frac{L}{2\pi} \right)^2, \quad [\text{s}] \quad (1.22)$$

with L the twist pitch and ρ_t the effective transverse inter-filamentary resistivity. The constant of proportionality is called the time constant of the system and it represents the time needed by the coupling currents to decay after the external magnetic field has stopped changing. The power per unit volume dissipated by the induced currents is obtained integrating $J^2 \rho$ along the current path, which results in

$$P_{cpl} = \frac{\dot{B}_t^2}{\rho_t} \left(\frac{L}{2\pi} \right)^2 = \frac{2\dot{B}_t^2}{\mu_0} \tau. \quad [\text{W/m}^3] \quad (1.23)$$

The loss per cycle due to a sinusoidal magnetic field of amplitude $B_a \sin(2\pi ft)$ is given by

$$Q_{cpl} = \frac{B_a^2}{\mu_0} \frac{2\pi\omega\tau}{(1+\omega^2\tau^2)}. \quad [\text{J/m}^3 \cdot \text{cycle}] \quad (1.24)$$

The equations above were derived for a wire with circular cross-section. In order to generalize the formulas, the coefficient 2 has to be replaced by the shape factor n of the wire (which is 2 for a circular strand and is not the same n as used in Equation 1.14). Detailed treatments of the inter-filament coupling loss for different cross-sectional shapes, types of magnetic field variation and frequencies can be found in [13, 20, 21]. Apart from a factor related to the shape, the inter-filament coupling loss can be described by a single parameter, the time constant, determined by the twist pitch and the effective transverse resistivity, which is a function of the filament-to-matrix contact resistance, effective matrix resistivity and cross-sectional layout of the filaments. The behaviour can be compared to the characteristic of an LR circuit by which the resistance is the transverse resistivity per unit length, the twist pitch determines the self-inductance and the driving voltage is caused by the changing magnetic field.

1.5.3 Interstrand coupling loss in a CICC

In Equation 1.24 the coupling loss for a single time constant system is represented by the energy loss per cycle versus frequency, assuming that the hysteresis loss per cycle at low excitation i.e. full penetration of the applied field, is independent of the frequency:

$$Q_{cpl} = \frac{B_a^2}{\mu_0} \frac{\pi\omega \cdot n\tau}{(1+\omega^2\tau^2)}. \quad [\text{J/m}^3 \cdot \text{cycle}] \quad (1.25)$$

The applied field is $B_a \sin(2\pi ft)$ and n the shape factor. The slope α of the linear section of the loss curve, $Q_{cpl}(f)$, at low frequency provides the effective coupling current time constant $n\tau$ as described before:

$$n\tau = \frac{\alpha \mu_0}{2\pi^2 \cdot B_a^2}. \quad [\text{s}] \quad (1.26)$$

The $n\tau$ value can be used for AC loss calculations of magnets operating at low ramp rates. Then the hysteresis loss is taken to be independent of frequency. At higher ramp rates the coupling loss saturates and subsequently decreases with frequency due to shielding of the interior of the conductor. When only one dominant coupling loss time constant is present, then the coupling loss over an extended frequency range is given by Equation 1.24. Depending on the cabling geometry, it is possible to have a large

number of differently shaped coupling current paths due to many strand contacts with a large spread in contact resistances and coupling current loops [23]. The actual contact resistances strongly depend on the crossing angle, coating, and pressure as well as on cable production and operational history [22, 24].

In multiple stage cabled conductors, this may lead to a large number of time constants even without clear dominant time constants. In fact, it means the loss represented by a particular time constant is created in only part of the total cable volume and a broad spectrum of time constants can exist. Part of the different loops will be strongly saturated or shielded and others hardly.

In the case of a strand, the volume fraction is approximately the volume of the closely packed multifilamentary zone scaled to the total strand volume [21, 23]. For interstrand coupling loss in a multistage CICC, the volume involved in a coupling current path is not known with any reasonable precision and is generally different for each time constant in the cable. For this reason, Equation 1.24 was extended to a new, but simplified conceptual model, assuming the presence of N dominant time constants all interacting by shielding with a weighted volume fraction included in n :

$$Q_{cpl} = \frac{\pi B_a^2}{\mu_0} \left[\left(\frac{\omega \cdot n_N \tau_N}{(1 + \omega^2 \tau_N^2)} \right) + \sum_{k=1}^{N-1} \left(\frac{\omega \cdot n_k \tau_k}{(1 + \omega^2 \tau_k^2)} \right) \prod_{l=k+1}^N \left(\frac{1}{(1 + n_l \omega^2 \tau_l^2)} \right) \right],$$

[J/m³ · cycle]. (1.27)

The shape factor n_k includes the volume fraction involved in the creation of coupling current loss represented by the time constant τ_k .

Note that Equation 1.26 still implies that the effective $n\tau$ value from Equation 1.24 determined by the low frequency limit can be regarded as the summation of the contributions $\sum n_k \cdot \tau_k$, for all time constants:

$$n\tau = \sum n_k \tau_k \quad [\text{s}]. \quad (1.28)$$

Equation 1.26 can be used to fit N effective time constants, $n_k \cdot \tau_k$ to the measured coupling loss curve. A fit of the coupling loss of an 84 strands braided CICC conductor is shown in Figure 1.15.

However, the relation should be seen as a conceptual model. The accuracy of a set of time constants obtained from a fit of Equation 1.26 to a measured loss versus frequency curve $Q_{tot}(f)$ highly depends on the range of frequency (and magnet field amplitude) in relation to the time constants present in a cable. Besides this, the choice of N , the accuracy of the measured data, and the frequency interval play an important role. In practice, such analytical models can show relatively large uncertainty for scaling to different applied magnetic field amplitudes. Therefore the best solution is a detailed numerical cable model with all strand paths accurately incorporated and implementing correct interstrand resistance data from experiments. Such a model, named JackPot ACDC has been created in the meantime offering important new insights on interstrand coupling loss and current distribution [26, 27, 28, 29, 30].

Figure 1.15. Coupling loss versus frequency of an 84 strands CIC conductor from [25], scaled for several amplitudes of the applied AC field B_w , $B_{dc}=1$ T.

Temporarily a very practical approach was followed by using empirical scaling of the coupling loss of prototype ITER CIC conductors [22]. The overall experimental $Q_{cpl}(f)$ of eight short samples were examined. The $n\tau$ values determined on the initial slope of the loss curves were compared for the virgin state and after 40,000 cycles. A correlation is expected between the initial slope of the loss curve and the DC inter-strand and inter-bundle contact resistance when the frequency of the applied AC field goes towards zero, approaching the DC condition limit. For ITER type of CIC conductors a general basic empirical formula was derived, only correcting for deviations of the cable diameter and the considered dominant cabling pitches. For the coupling loss time constant the principle relation is:

$$n\tau = \frac{\beta \cdot L_p^2 \cdot D^2}{R_c}, \quad [s] \quad (1.29)$$

in which β is a fitting constant, the unit for L_p (cable stage pitch length) is m, D (diameter of the considered cable element) in m and the measured contact resistance R_c in Ωm . As we only consider AC loss at low frequency, from the initial slope of the loss curve approaching zero frequency, it is allowed to simply add the loss components generated in different cable stages. After processing all available experimental data it appeared that it is sufficient to consider only the R_c from the first triplet (IS), inter-sub-petal and petal stages (IB) (see Figure 1.9). The three contributions can be just added to obtain the overall interstrand $n\tau$ and finally the intrastrand component must be added for the overall cable $n\tau$.

Previous efforts to scale the coupling loss with the R_c with only the first stage (IS) R_c were successful as a first order approach for in particular sub-size CICC's, but the method can be improved considerably for full-size ITER CICC's [22]. The standard

deviation of the relative difference between an $n\tau$ obtained from the experiment and from Equation 1.28 amounts to 40% using only the first stage R_c . The following improved relation can be used for full-size ITER CICC's:

$$n\tau_{tot} = \frac{\beta_1}{R_{c1}} + \frac{\beta_2 \cdot L_{p2}^2 \cdot D_2^2}{R_{c2}} + \frac{\beta_3 \cdot L_{p3}^2 \cdot D_3^2}{R_{c3}} + n\tau_{strand}. \quad [s] \quad (1.30)$$

The index 1 refers to the first cabling stage triplet results (IS), index 2 to the sub-petal cabling stage (IB) and 3 to the last cabling stage (petal, IB). The $n\tau_{strand}$ represents the intra strand coupling loss component while $n\tau_{tot}$ is the overall cable coupling loss time constant. After analysis of all available experimental cable data, $\beta_1=0.630$, $\beta_2=0.072$ and $\beta_3=0.017$. The standard deviation from the disparity between the calculated and experimental results is 13%. Using more parameters like additional twist pitches from lowest order cabling stages did not improve the correlation.

1.6 Degradation in ITER Nb₃Sn conductors

1.6.1 ITER Model Coils

In Figure 1.10 and Figure 1.16, the wavy pattern of the strands in ITER CICC's containing more than 1000 strands with a strand diameter of about 0.8 mm and an open porous structure with a void fraction of 33% can be clearly seen. The considerable size of the superconductors suitable for application in ITER and the challenging operating conditions (high current, magnetic field and sweep rate) have the consequence that only limited R&D is carried out on full-size cables because of the required time scale and budget. For this reason the original design of the CICC's for ITER was not based on broad full-scale component tests proving adequate and stable operation but leaving scope for uncertainty in performance.

Figure 1.16. Cable structure of an ITER CS1.1 (left) and CS2 (right), conductors used for the Central Solenoid Model Coil.

During the Engineering Design Activity (EDA) in the 1990s there were no more doubts that the ITER magnet system had to be constructed with superconducting coils. It was

decided to construct and test a central solenoid model coil (CSMC) [34] and a toroidal field model coil (TFMC) [31]. The necessary magnetic field levels between 11 and 13 T required use of the strain sensitive Nb_3Sn as the superconducting material and this was a great challenge for conductor and winding fabrication technology. This resulted in the development of new structural materials that had to be compatible with the heat treatment and modified construction principles of the winding pack and coil structure. All this needed the confirmation in an overall test, which was covered by the ITER model coil program, testing the envisaged CICC engineering concept for ITER in the CSMC and the TFMC.

Still today, the Inner and Outer modules of the Central Solenoid Model Coil and Central Solenoid Insert Coil are the largest pulsed superconducting coils ever built and operated (see Figure 1.17, left).

Testing of the CSMC and the CS Insert took place at the Japan Atomic Energy Research Institute (JAERI) in 2000. The CSMC mass is about 120 ton, the outer diameter is about 3.6 m and the stored energy is 640 MJ at 46 kA and a peak magnetic field of 13 T. The CS Insert Coil (CSIC), a well-instrumented 140 m long Nb_3Sn single layer solenoid and installed in the bore of the CSMC, was also tested in 2000. The maximum transport current in the CSIC is 40 kA and the peak background field was 13 T. The TFMC was designed to achieve a transport current of 80 kA [31] and reaching the ITER toroidal field (TF) coil equivalent Lorentz force of about 800 kN/m [32, 33] (see Figure 1.17, right). The TFMC was tested with the EURATOM LCT coil in the TOSKA vessel at the Karlsruhe Institute of Technology, KIT [31].

Figure 1.17. Left: an Insert Coil installed in CSMC facility. Right: the TF Model Coil (with the EURATOM LCT coil and inter-coil structure) being inserted in the TOSKA vessel.

The initial results of the CSMC test already showed a low n -value, significantly lower than the single strand but the most concerning observation was the T_{cs} further degrading with cycling [34]. This is clearly illustrated by the electric field versus temperature

curves, taken after different number of cycles in Figure 1.18 and the plot of the T_{cs} versus loading history in Figure 1.19.

Figure 1.18. Electric field of voltage pair V9-V10 on the center turn of the coil versus temperature curves of the ITER CSMC CS-Insert Coil showing progressive degradation (decrease in T_{cs}) with increasing number of load cycles (courtesy N. Mitchel, ITER Organisation).

Figure 1.19. Current sharing temperature evolution versus the number of load cycles of the ITER CSMC CS-Insert Coil showing progressive degradation (decrease in T_{cs}) with increasing number of load cycles [34].

The behaviour resembled the reduction of the n -value observed in the initial large Nb₃Sn superconducting fusion coils like in the Russian T-15 [35] and the Westinghouse LCT coils [36]. These react-and-wound conductors showed both significant broadening of the transition (n -value decrease) and a significant degradation of the superconducting properties due to the large strand deformation during winding. As the electromagnetic $I \times B$ load increases, the degradation of the CSMC becomes more severe [37].

The TFMC test also showed a dependency on the $I \times B$ load and for the highest load of 800 kN/m a T_{cs} of 6.0 K. The TFMC test likewise showed that the n of the strands in the conductor is significantly smaller (<10) than that of the isolated strand, which could not be explained by the non-uniformity of the current distribution among strands. No electromagnetic load cycling was applied to the TFMC.

1.6.2 Initial improvements

Since the first tests of the CSMC, a significant effort was spent for understanding the degradation of Nb₃Sn CIC conductors compared to the single strand performance [38, 39, 40, 41]. Although many papers were published on this subject, they merely describe the degradation giving possible interpretations varying from current non-uniformity to quench damage or strand bending. Most of the explanations are supported by models but unfortunately, none of these provided an adequate solution to the problem although it was expected that a lower void fraction and shorter cabling pitches could at least partly confine the degradation.

In the meantime, it was proven experimentally that mechanical support of the strands improved the performance significantly [42]. After the first evaluations, the ITER conductor design had already been modified compared to the CSMC layout [43]. It was assumed that the resistance to the degradation increases by using a steel jacket, providing thermal pre-compression in reducing the tensile strain levels being associated with strand bending and crack formation [38, 44]. The void fraction was reduced from 36% to 33% and the non-copper material in the cross section was increased by 25%. Newly developed, so-called high- J_c advanced Nb₃Sn strands were being pursued in an attempt to compensate for the performance loss.

Nevertheless, at the start of the ITER construction, it was accepted that the transverse load would inherently lead to a severe degradation of the transport properties due to strand deformation. Although not conclusively proven, some of the analysts in the fusion community suggest that about 30% to 40% of the Nb₃Sn layer in the strand material of the cable would be lost [38, 43]. This would implicitly suggest that the cable in conduit concept for the large ITER type of conductors was beyond the limits of efficient application. In view of this thought, the development of advanced high- J_c strands would not immediately lead to an appropriate solution either, if a significant fraction of the Nb₃Sn material then breaks in the cable and no saturation would be reached in the degradation with load cycling.

The sensitivity to the large forces, causing transverse load on the strand bundle in the CICC, reducing the performance significantly due to the relatively large strain sensitivity of the brittle Nb_3Sn filaments in the wires, was revealed by the Model Coil tests. A serious drawback of the initial conductor layout, characterised by its flexible strand bundle due to its porosity (void fraction 33%) for forced liquid helium flow, allowing for considerable local and periodic strand deformation (see Figure 1.16).

It was evident that a method to solve this severe degradation would lead to significant cost savings when requiring less superconducting strand or a significant improvement of the ITER magnets performance in terms of operational margin.

The possible solution for the limited R&D carried out on full-size cables because of the restricted time scale and budget can be supplemented by experimental testing of parametric variations on smaller experiments and a dedicated and validated cable model that can bridge the gap between first trials and a further optimized design. This method is implemented here with the focus on understanding the degradation of Nb_3Sn ITER type CIC conductors.

1.7 Scope of the thesis

1.7.1 Objectives

The electromagnetic forces on the cable bundles in CIC conductors cause severe strain effects in the strand's Nb_3Sn filaments. The analysis of the voltage current traces taken from the CSMC model coil and Insert coils revealed that the reduced performance of the cables, compared to the prediction based on the single strand characteristics, could not be explained solely by the current unbalance caused by the non-uniformity of the joints or the effect of the self-field [41, 45, 46, 47, 48, 49].

The different thermal contraction between the steel conduit material and strand bundle causes contraction of the cable bundle in axial direction when cooling from reaction heat treatment at 923 K to operating temperature of 4.5 K. This already creates periodic strand bending and this bending strain is again moderated by the coil hoop stress during magnet operation. On top of this, during magnet operation the cable is subjected to the above-mentioned transverse Lorentz forces from current and magnetic field. This imposes periodically distributed magnetic loads along each strand due to the current they carry, but also cumulated loads from other strands transferred at the strand-to-strand contacts. More specifically, we can distinguish between axial compression, spatial periodic bending and local contact stress along the strands. In the case the tensile irreversibility limit is exceeded, irreversible damage occurs due to filament breakage. However, also the increasing strain variation along length and cross section of the filament bundle, or in other words broadening of the strain distribution, results into a decrease of the transport properties, I_c , n -value and T_{cs} . For this reason the quantitative

impact of the various strand deformations in CIC conductors needs to be studied in sufficient detail in order to correlate this to an improved understanding of CICC performance and eventually implement this into adequate recommendations for design optimisation.

Various approaches to characterize superconducting behaviour versus strain have been employed throughout the last decades. For technological wire, most attention has been paid to axial and, to a lesser extent transverse strain experiments as these represent the strain components relevant for most applications in magnet technology. As bending strain in the Nb_3Sn wire is largely a distribution of axial strain over the wire cross-section, knowledge of the I_c versus axial strain dependency is required for analysis of the bending strain influence.

The bending strain response depends on the internal strand layout in terms of twist pitch and internal resistance, determining the possible amount of current transfer between the filaments [50]. The contact stress from periodic crossing strands and line contacts in the strand bundle of CICC has a degrading effect as well [50, 51, 52, 53]. The magnitude and periodicity of the bending and contact strain in combination with the strand stiffness and the ability of a strand to redistribute the current between the filaments determine the effect on the critical current and n -value.

As the quantitative influence of the various load types on strands from different processing techniques was unknown, it was necessary to build a setup with dedicated probes to simulate the equivalent ITER magnet operating conditions on a single strand. To this purpose, a new test facility, the “Test ARrangement for Strain Influence on Strands” (TARSIS) was designed and built [54]. The influence of various loads and deformations, i.e. axial tensile stress, bending, pinching by crossing strands or homogeneous load, which frequently occur in a CIC conductor, can be studied separately or combined with different TARSIS probes. The advantage of this TARSIS setup is that beside precise measurement of the strand deflection or contact deformation, also the applied force is monitored for full axial and transverse stress-strain analysis. As the stress, originated from differential thermal expansion and electromagnetic load, is the driving factor for the final strain distribution, the axial and transverse stiffness of the strand (and cable) should also play an important role in the final performance.

The main objective of this work is to achieve a better understanding of the scaling from strand performance to the final CICC behaviour in ITER magnets aiming at improvements of the CIC conductor design. The aim is to study, understand and improve the performance of full-size Nb_3Sn CIC conductors when subjected to severe electromagnetic transverse loading. A comprehensive experimental work on studying the properties of strands and cables under deformation similar to CIC conductor conditions forms the basis of the analysis. The experimental output of TARSIS is interpreted in combination with experiments from the Twente Cryogenic Cable Press [55].

The relevance of the analysis is to solve the problem of the performance degradation found in ITER Nb₃Sn CIC conductors during the Model Coils and initial short sample tests, threatening the progress on building the ITER magnet system already before the start of ITER. Not understanding the performance loss could have serious implications in delaying the start of ITER or constructing the magnet system for ITER.

Full-size short conductor sample and model coil tests are an essential part of the ITER magnet R&D program. Nevertheless, the assembly of samples and their test is time consuming and expensive, which makes it practically impossible to perform systematic studies on real conductors to cover all cases. Therefore, in order to study and optimize the conductors under real operating conditions, smaller laboratory scale experiments and simulations are mandatory. The code TEMLOP is developed to integrate the outcome of the various smaller scale experiments performed at the University of Twente. TEMLOP is featuring the description of a CIC conductor with strand-level details and is used to analyse the performance of cables, implementing the experimental outcome of the tests on strands and CICC in the Press.

1.7.2 Structure of the thesis

The structure of the thesis is outlined in Figure 1.20.

In chapter 1 a short introduction is given on nuclear fusion, the magnet systems for fusion reactors and in particular ITER, and the necessity of superconductors. In addition it explains what stability and operation margin means for CIC conductors in terms of AC loss and degradation of ITER Nb₃Sn conductors.

Chapter 2 is treating experiments on strand axial strain characteristics and presents the TARSIS setup. Important for the stress driven deformations in CICC are the tensile stress-strain characteristics, the critical current under axial strain variation, and eventually the correlation between filament crack damage and load conditions.

Chapter 3 is dealing with spatial periodical bending of Nb₃Sn strands, the application of different wavelengths and describes a relevant method using pure electromagnetic force for validation of the TARSIS bending method. In addition the role of axial stiffness and axial compression is discussed in relation to periodic bending.

In chapter 4 the TARSIS probe for transverse compressive contact stress on strands is presented and the results are discussed. The salient resemblance of the influence of spatial periodic and homogeneous transverse stress is compared for bronze and internal-tin type of strand.

Chapter 5 gives an overview of axial and transverse loading test results obtained on actual ITER Nb₃Sn strands. It is critical to explore the variation in sensitivity to different load types of strands used for ITER with different design layout and produced by dissimilar processing methods.

Figure 1.20. Outline of the thesis, the connecting lines show the main flow of information among the chapters.

Chapter 6 is about testing of cabled conductors under load in the Twente Cryogenic Cable press for transverse cyclic loading. The evolution of AC loss, interstrand contact resistance, transverse cable stiffness and mechanical loss are extensively studied under transverse loading conditions.

In chapter 7, the details of the model for Transverse Electro-Magnetic Load OPTimization (TEMLOP) in Nb_3Sn CICC are presented. The electromagnetic mechanical model for transverse load performance enables computations resulting into recommendations for design improvement.

Chapter 8 describes the successful experimental verification of the cable pitch length effect, as predicted by the TEMLOP outcome, and the subsequent revision of ITER TF cable pattern.

Chapter 9 gives an overview of the conclusions.

2 Strand axial strain characteristics

For predicting full-size conductor performance, understanding the impact of strand stiffness, transport properties dependence on axial strain and crack initiation is crucial. Since the stress distribution, caused by differences in thermal expansion and electromagnetic load, is the driving factor for strain alterations in Nb₃Sn strands in a Cable In Conduit Conductor, the axial and transverse stiffnesses of the strand play a crucial role in the conductor's performance. Axial tensile stress-strain measurements were performed on Nb₃Sn strands manufactured by the internal tin, bronze route and powder-in-tube (PIT) methods. We compared the stress strain characteristic with a straightforward 1D model using an independent materials database. The measurements are performed with the TARSIS setup (Test ARrangement for Strain Influence on Strands).

With the PACMAN spring, more than 200 VI curves were measured on an internal-tin route Nb₃Sn strand at various strain, temperature and magnetic field values to identify their critical surfaces. It is also investigated whether the scaling parameters can be determined from a limited dataset instead of an extensive number of test data.

The impact of filament crack distribution on the strand's transport properties is determined for ITER-type Nb₃Sn superconductor strands, one prepared with the bronze route and the other with the internal-tin route. Finally, correlations between crack distribution and transport properties are investigated.

The content of this chapter is for a large part based on papers [14], [15], [17], [54], [67], [82], [104], [105], [120], [121] and [153].

2.1 Tensile stress-strain characteristics

2.1.1 Introduction

The axial and transverse stiffness of the strand play a crucial role in the final performance. The strand stiffness determines how the stress distribution, driven by alterations in thermal expansion and electromagnetic load, is transmitted into strain variations along Nb₃Sn strands in a CICC. Since the strain state of Nb₃Sn filaments in strands determines the transport properties, basic experimental stress-strain data are required on strand level for accurate modelling, analysis and eventually for optimizing cable and magnet design.

For applied transverse stress, we can distinguish homogeneously distributed loads on a wire, being for example sandwiched between flat surfaces [56, 57], spatial periodically applied stress by using crossing strands [51], and bending strain. For bending strain we can differentiate between spatial periodical loading with a wavelike bending pattern [40, 47] and bending along a bend surface or cylinder with constant radius [58, 59, 60, 61, 62, 63, 64, 65]. Bending strain can be considered as distributed axial strain in the strand cross section. The I_c dependency on axial strain is measured with the PACMAN spring device while the other measurements are performed in the new setup TARSIS (Test ARrangement for Strain Influence on Strands). TARSIS is developed for investigation of the impact of strand stress and strain on transport properties, like bending, pinching, but also for axial tensile loading without transport current. For axial strain measurement a double extensometer connected to the sample enables to determine the strain level whereas a load cell is used to monitor the stress level [66, 67, 68].

We performed axial tensile stress-strain measurements on several types of Nb₃Sn strands, among others used for the manufacture of the ITER Central Solenoid and Toroidal Field Model Coils, and reacted and non-reacted Nb₃Sn strands manufactured by the internal tin, bronze route and powder-in-tube (PIT) methods. Samples were tested at 4.2, 77 and at 293 K. We present the computation of the stress-strain characteristic with a straightforward 1D model using an independent materials database, showing good agreement with the experimental results. The details from the take-off origin of the measured stress-strain curves are discussed and data are evaluated with respect to commonly used functions for fitting the stress-strain curves. For higher levels of applied stress (100 MPa), we found characteristically a higher strain for bronze route wires compared to a powder-in-tube and internal-tin type of strand.

2.1.2 Test probe for stress-strain

The samples are tested with the axial tensile stress-strain probe in the TARSIS configuration (without transport current). The arrangement is shown in Figure 2.1. The

sample is inserted in the shafts of the pulling bars. A pair of extensometers (TML, Co. type CFLA, 1 mm grid length, 350 Ω) is used to measure the elongation of a sample wire [66]. The extensometer has low weight, minimal influence on the measured force, high sensitivity, low noise and a symmetrical configuration.

Figure 2.1. Left: principle of the double-extensometer. Middle: wire sample with soldered brass tubes. Right: sample in the shafts of two stainless steel bars with the double extensometer configuration.

Figure 2.2. Left: photograph of the Precision Vertical Linear Stage (PVLS). Right: representation of the rig for strand stress-strain measurements with sample and double extensometer configuration.

The advantage of a double extensometer configuration is the ability to compensate for small bending effects. The extensometers with a gauge length of 25 mm (error ± 0.5 mm) are clamped on the sample wire with two springs.

The TARSIS configuration is shown in Figure 2.2. The upper stainless steel bar can move up and down driven by the Precision Vertical Linear Stage (PVLS), which is placed on top of the stress-strain test rig as depicted in Figure 2.2 (left). The PVLS is built up around a precision satellite spindle connected to a position driven DC-motor. The specifications for maximum force, travel and resolution are 20 kN, 200 mm and $0.05 \mu\text{m}$, respectively. A software driver allows for automated setting of the linear stage. The upper end of the stainless steel bar is attached to a 50 kg load cell. The voltage across the load cell is linear with the load and is 0.4 mV/kg .

The required load puts in this case a less severe demand on the linear stage. About 300 N suffices to reach a stress level of 300 MPa in a wire with a cross sectional area of 1 mm^2 . Reacted Nb_3Sn wires deform plastically at stress levels above 20-30 MPa. To determine the Young's modulus the first part of the stress-strain curve below 0.02% strain has to be measured properly. To acquire about 100 data points for this strain range a ramp speed of $20 \mu\text{m/s}$ and a sampling frequency of 1 kHz are required. The stress-strain measurements will be performed at temperatures of ~ 293 , 77 and 4.2 K.

During sample insertion, the stainless steel bar connected to the PVLS is pulled to the position of about 0.1 mm before actually starting to load the sample. In this position, no force is exerted on the sample wire. For experiments in liquid nitrogen and in liquid helium this distance is at least twice as much, anticipating the sample thermal shrinkage when cooling down.

Pulling the upper stainless steel bar starts the stress-strain test run. The motor speed is set to a displacement of the PVLS of $10 \mu\text{m}$ per second, corresponding to a strain rate of $4 \cdot 10^{-4}/\text{s}$. The signals of both extensometers and load cell are sampled with 25,000 scans per second and the average of every 2500 scans is stored in a text file (10 data points per second).

With the extensometer gauge length and the calibration of the extensometers the tensile strain in the sample wire is defined as

$$\varepsilon = \frac{\Delta l}{l_0}, \quad (2.1)$$

with ε the strain, Δl the elongation (extension) and l_0 the initial length. The tensile stress is

$$\sigma = \frac{F}{A}, \quad [\text{Pa}] \quad (2.2)$$

with F the force and A the initial wire cross-sectional area.

2.1.3 Sample preparation

The reaction heat treatment is applied in a 1 mm diameter straight quartz tube. Here, we choose for a gripping method of soldering two brass tubes at both ends of a sample wire, allowing a well aligned fixation and minimal handling, see Figure 2.1 (middle) and (right). Holes of 0.90 mm diameter are drilled concentrically in the 6 mm diameter brass rods with a length of 18 mm for soldering the 0.81 mm diameter wire samples inside. A conical entrance at one end of the brass tube prohibits flow of the solder from the capillary space to the flat surface in contact with the clamps after pulling bar and base plateau of the frame (see Figure 2.1 (right)). A flat surface is required to ensure a well-defined uni-axial tensile force. Sn0.3%Ag is used to solder the sample wires inside the brass tubes because of its mechanical strength. The sample wires are 100 mm long (including the brass tubes) so the distance left between the grips is 64 mm.

The difference between the 0.90 mm hole in the brass tube and the 0.81 mm diameter of the sample wire leaves some space for misalignment of the wire. Care is required during sample preparation to ensure that the wire is in line with the applied force, not allowing for any axial misalignment.

The supposed variation in mechanical properties, potentially caused by the thermal cycle unavoidably connected to soldering, has been investigated for a wire by using a method by which the wire was clamped by small bolts. This gripping method was used for several non-reacted LMI samples without thermal cycle, samples pre-cooled in liquid nitrogen and samples that were first soldered inside the normal brass tubes but taken out again and fixed in brass tubes with bolts. The difference in test results between the samples was marginal, indicating that soldering of the samples has no relevant influence within the measurement accuracy.

2.1.4 Calibration of the extensometers

To assess the (linear) relation between displacement and voltage for the extensometers, several calibration measurements were performed at 293, 77, and 4.2 K. A digital micrometer, attached to the stainless steel bar to monitor the extension, and a coaxial rod-in-tube mock-up sample (without friction) was used to mount the extensometer. In principle there is no exerted force on the load cell with this calibration tool when pulling (except for the spring force of the extensometers and the friction of the micro meter itself). For most calibrations, steps of 50 μm were taken up to a maximum displacement of 1.5 mm. The calibration factor is determined from a linear fit through the measured data.

The spread between individual calibration measurements is within $\pm 0.8\%$ at room temperature, $\pm 2\%$ at 77 K and $\pm 4\%$ at 4.2 K. The noise level of the extensometers and also the load cell contribute to the overall uncertainty. For the experiments in liquid helium, the noise level of the extensometers was relatively high. The high noise level is likely a result of thermal voltages and local boiling of helium due to dissipation in the

extensometers. For measurements in liquid helium the noise could be reduced significantly by decreasing the current through the extensometers from 3 to 1 mA.

2.1.5 Simplified model computation (1D)

The stress-strain curves were calculated with a one-dimensional first order approximation based on the temperature dependent material properties collected in the Appendix of [69]. The mentioned appendix provides a collection of materials data recommended for use in elasto-plastic modelling of Nb₃Sn strands at relevant temperature ranges. Simple scaling was used that fit the available data within the scatter expected from for example grain size, being difficult to take into quantitative consideration. Where there are no measured data, trends were established following the elasto-plastic behaviour of similar materials.

We assumed simplified conditions for the computations. The materials in the cross section are uniform along the length. Using a one-dimensional model we neglect the effect of radial contraction and Poisson ratio. The initial strain of the strand components born during fabrication and heat treatment history [69] are disregarded and assumed zero. For the average stress over the cross section of a strand we use:

$$\sigma_a = \frac{F}{A_{Cu} + A_{Br} + A_{fil} + A_{bar}}, \quad [\text{Pa}] \quad (2.3)$$

in which σ_a is the average stress, F is the axial tensile load and A is the cross section area (in this case equal to the volume fraction v) of the composite element for copper matrix, bronze, filaments and barrier materials (niobium and/or tantalum), respectively. For a certain applied tensile strain, the stress in the strand components is computed with fits derived from the relations proposed in [69] for each constitutive material.

Thus we can write the composed measured stress, σ_a , as a function of the applied strain ε as:

$$\sigma_a(\varepsilon) = v_{Cu} \cdot \sigma_{Cu}(\varepsilon) + v_{Br} \cdot \sigma_{Br}(\varepsilon) + v_{fil} \cdot \sigma_{fil}(\varepsilon) + v_{bar} \cdot \sigma_{bar}(\varepsilon). \quad (2.4)$$

The volume fractions of the components for four types of strand are listed in Table 2.1. For the VAC and LMI strands the data are directly taken from [69], the other numbers are derived from micrographs of the cross section.

The computed results for the initial modulus of elasticity at low applied tensile strain are presented in Table 2.1 and Figure 2.3 For bronze we assume a Sn content of 3% in all cases and for the SMI-PIT strand we consider the residue in the hollow filaments left after reaction as mechanically inert. The peak at 100 K is due to the strong temperature dependency of the E -modulus of Nb₃Sn below 100 K. For bronze there is a gradual increase of the E_T -modulus of 12%, while copper, niobium and tantalum show a slight decrease with rising temperature. The computed initial E_T -moduli at 4 K of all four strand types are listed in Table 2.1. The values are in good agreement with those presented in [69] for the VAC, LMI and PIT strands and both bronze route strands are quite similar in behaviour (the FUR strand is not considered in [69]).

Table 2.1. Sectional fraction of materials and computed initial modulus E_i of elasticity at 4.2 K of the strands investigated after heat treatment.

	Cu	Nb ₃ Sn	Nb	Ta	bronze	E_i @ 4 K [GPa]
Bronze (VAC)	0.600	0.148	0	0.040	0.212	135
Internal tin (LMI)	0.600	0.204	0.060	0.020	0.116	130
PIT (SMI)	0.518	0.184	0.298	0	0	122
Bronze (FUR)	0.564	0.144	0	0.023	0.270	134

The results of the computations of the stress-strain characteristics of the four strand types are presented in Figure 2.4, Figure 2.5 and Figure 2.6. The computed curves for the bronze FUR strand is not shown because the behavior is similar to the VAC bronze strand, but slightly lower because the volume fraction tantalum is less in the FUR strand. The computed stress-strain curves of the 4 types of strand at a temperature of 4.2 K are presented in Figure 2.7.

Figure 2.3. Computed initial modulus of elasticity as a function of temperature for four strand types.

Figure 2.4. Computed stress-strain curve at 4.2, 77 and 293 K for the VAC bronze strand.

Figure 2.5. Computed stress-strain curve at 4.2, 77 and 293 K for the LMI internal tin strand (the curves for 4.2 and 77 K practically coincide).

Figure 2.6. Computed stress-strain curve at 4.2, 77 and 293 K for the SMI powder-in-tube strand.

Figure 2.7. Computed stress-strain curves at 4.2 K for four types of strand.

2.1.6 Experimental stress-strain curves

2.1.6.1 Reproducibility and strain offset correction

Five different types of Nb₃Sn/Cu wire, with a diameter of 0.81 mm were experimentally investigated in 48 test runs at 4.2, 77 and ~293 K. In sequence of testing: SMI wire, (PIT) method, reacted and non-reacted (no Cr-plating); LMI wire, internal tin, reacted and non-reacted (TMFC production, with Cr-plating); FUR wire, bronze route method, reacted (no Cr-plating); VAC wire, bronze route method, reacted (with Cr-plating) all in [70].

Usually the experiments end with rupture of the wire unless the elongation is beyond 15%, at which the test is interrupted in order not to overstrain the extensometers. The reproducibility of the method is verified by repeating tests on the same wire type under similar conditions. Results are displayed for three LMI, two VAC and three SMI wire samples in Figure 2.8, Figure 2.9 and Figure 2.10, respectively. We choose to match the experimental curves at high stress level represented as shifts in the strain axis.

For all wires we find a good reproducibility of the curves at high stress level with wire breakage at practically the same applied stress, but with alterations at initial loading. In earlier work it was reported that the initial part of the stress-strain curves varies due to presumably pre-bending during fabrication or handling [71].

However, it was reported that beyond a certain stress level, stress strain behaviour is basically identical regardless of the gripping method (soldering, sand paper or with brass sleeve and glued) and the inter-grip distance from 40 to 100 mm, although relative shifts in strain axis were inevitable.

Figure 2.8. Measured stress-strain characteristics of three reacted LMI internal tin wire samples at 4.2 K. Samples 1 and 2 intersect the origin while sample 3 deviates from the other two.

Figure 2.9. Stress-strain characteristic on two reacted VAC bronze route wire samples at 4.2 K.

Figure 2.10. Stress-strain characteristic of three reacted PIT (SMI) wire samples at 4.2 K.

Before the sample is exposed to a well-defined axial tensile force, settling of the brass tubes in the shaft and final straightening of the wire, depending on the initial misalignment and straightness will often occur. The Nb_3Sn filaments in the strands are brittle and the copper stabiliser, as well as the bronze in the composite, were fully annealed during the reaction heat treatment at around 950 K.

So the copper and bronze are in mostly yielded condition due to thermal compressive stress on cooling down from reaction temperature and the composite may behave partly plastically from the start of testing. Determining the offset on the strain axis correctly is not always straightforward, not only because of the immediate yielding of the copper and bronze, but also due to the experimental conditions as mentioned above.

2.1.6.2 *Measurement results compared to computations*

In Figure 2.11 the data from the VAC wire measured at three different temperatures is displayed, illustrating a consistent behaviour in the change of the stress-strain relation with temperature for this strand, being well in agreement with the computed stress strain curves shown in Figure 2.4. Both, stiffness and ultimate tensile strength of the wire increase at reduced temperature.

In fact only for this wire we observed a clear correlation in the temperature dependency between the measured and computed results. Figure 2.12 illustrates the good agreement between the straightforward 1D model and the measured stress-strain curves of a reacted VAC bronze wire sample at 293 K.

At first instant, differences between experiment and computation can be due to uncertainties in the used database of available material properties from [69]. Further on it depends on the actual volume fractions of the materials forming the wire components, the tin content and gradient in the bronze and the Nb₃Sn filaments, potential influences from reaction residuals as for example in the case of PIT wire and the possible error introduced by neglecting the radial effect in the applied 1D model.

For the computations performed on the LMI wire, Figure 2.5 displays that the slopes of the computed curves at higher strain level are practically similar. In view of the good reproducibility of the curves at high stress level in combination with the unavoidable alterations at initial loading, differences are not significant within the measurement accuracy. This is in agreement with the experimental results shown in Figure 2.13 for the LMI strand, confirming similar slopes for the increase of the stress with strain at high strain levels and hardly any temperature dependence.

In this sense, also for the internal tin LMI strand, the computed and measured stress-strain curves show an outstanding match at a temperature of 4.2 K. The measurement and computation for 77 and 293 K are presented in Figure 2.13 and Figure 2.14, respectively.

For the SMI-PIT strand in Figure 2.15 we do not find a clear correlation in sequence with the computed data in Figure 2.6. On the other hand we find again a good match between the computed and measured curves of three samples at 4.2 K in Figure 2.16. The small deviations at 77 and 293 K may be caused by uncertainties in the materials database. In particular the temperature dependence of the niobium yielding is crucial for the PIT strand.

In summary it is concluded that the 1D model, using an independent materials database is in good agreement with the experimental results.

Figure 2.11. Stress-strain characteristics of reacted VAC bronze wire samples, measured at 4.2, 77 and 293 K.

Figure 2.12. Computed and measured stress-strain characteristics of a reacted VAC bronze wire sample at 293 K.

Figure 2.13. Stress-strain characteristics of reacted LMI internal tin wire samples, measured at 4.2, 77 and 293 K (curves for 77 and 293 K coincide).

Figure 2.14. Computed and measured stress-strain characteristics of reacted LMI internal tin wire, at 4.2 K (measured and computed curves practically coincide).

Figure 2.15. Stress-strain characteristics of reacted SMI PIT wire samples, measured at 4.2, 77 and 293 K.

Figure 2.16. Computed and measured stress-strain characteristics of reacted SMI PIT wire, at 4.2 K.

2.1.7 Fitting methods for stress-strain curves

Several commonly used fit functions for application to stress-strain data from superconductors can be found in literature. The fitting parameters are based on the unit GPa for stress and the dimensionless strain $\Delta l/l_0$ [-] (not in %). The following four fitting functions are applied:

- Linear regression (initial Young's modulus E_i): $\sigma = E_i \cdot \varepsilon + R_0$,
- Pure exponential: $\sigma = a (\varepsilon - b)^n$,
- Ramberg-Osgood: $\varepsilon = A \sigma^n + \sigma/E$ [72],
- Fifth order polynomial: $\sigma = a_5 \cdot \varepsilon^5 + a_4 \cdot \varepsilon^4 + a_3 \cdot \varepsilon^3 + a_2 \cdot \varepsilon^2 + a_1 \cdot \varepsilon + a_0$.

First, the experimental stress-strain curves were corrected for offset where possible. For the non-reacted wires, the stress-strain curves remain linear up to relatively large stresses, so the initial Young's modulus given by the linear regression fit is quite accurate and within ± 10 GPa error. Since the experiments here include both reacted and non-reacted wires, we chose in addition an alternative approach for determining the offset in strain for reacted wires.

The measured stress-strain curves are corrected for an offset in both stress and strain taking two data points at $\sigma = 0.02$ and 0.04 GPa, respectively. Zero strain is then at the intersection of the line through both points with $\varepsilon = 0$ (extrapolation). Since the sample is initially unloaded, the offset in stress is corrected by defining the first measured data point as zero stress. Most of the settling effects have become insignificant when a stress level of about 0.02 GPa (~ 10 N) is reached.

A consequence of the linear initial section for non-reacted wires is that the pure exponential fit can be omitted immediately, since this type appears definitively not appropriate. Examples are given in Figure 2.17 and Figure 2.18 where the measured data are plotted for reacted and a non-reacted LMI and SMI samples.

For the reacted samples, all proposed methods are applied. It was appropriate to test the Ramberg-Osgood method on all measured stress-strain curves, since one of its fitting parameters is the initial Young's modulus which can be directly compared to the initial Young's modulus obtained from the linear regression fit.

However, it appeared that also the Ramberg-Osgood fit does not lead to a proper match to the data for reacted wires, due to the immediate yielding and only the initial range of stress is covered adequately. For reacted wires, a pure exponential fit is more appropriate in covering accurately a relatively large range of the stress-strain curve. The same exponential fit method is used in [71] to determine the offset.

Figure 2.17. Stress-strain data of a reacted (R) and non-reacted (NR) internal tin LMI strand, measured at 293 K.

Figure 2.18. Stress-strain data on a reacted (R) sample at 4.2 K and non-reacted (NR) samples at 4.2, 77 and 293 K of the PIT SMI strand. The curves for the un-reacted wire at 77 K and 4.2 K practically coincide until a strain of 0.011 where the wire breaks at 77 K.

A fifth order polynomial represents the measured data accurately. The results from the polynomial fits are listed in Table 2.2. For the polynomial fits we used a minimum threshold of 0.01 GPa, all stress data below were omitted.

Table 2.2 Coefficients of 5th order fit function at 4.2 K.

Wire	5 th order polynomial coefficients					
	a_0	a_1	a_2	a_3	a_4	a_5
LMI	0	79.7	-15431.9	2174125.6	-145933759.7	3754788474
SMI	0	66.2	-10282.2	1456020.6	-77227469.7	891711029
VAC	0	75.3	-16736.5	2496325.0	-175821582.5	4647873315
FUR	7.9	57.4	-10416.7	1371623.7	-82379282.3	1760903007

2.1.8 Comparison of tested strands

The most representative curves of all reacted wire types measured at 4.2 K are compared in Figure 2.19. Apart from the initial alterations, which in particular seem to occur most at the lowest temperatures, the stress-strain curves obtained for a particular wire type are well reproduced and so curves are selected here that represent an average behaviour per type. Also here we can confirm a quite good agreement between the computed and measured data. When we compare the experimental data from Figure 2.19 to the computed ones presented in Figure 2.7, we find excellent matches for the VAC bronze, internal tin and PIT type of strands while the measured result for the FUR bronze wire is slightly below the computation result.

Figure 2.19. Stress-strain characteristics for reacted wires of the bronze route, internal tin and PIT wires at 4.2 K.

Both bronze route wires are quite similar in yield rate but they differ at the start of the stress-strain curve. This dissimilarity is also observed at 77 K although the curves almost converge at room temperature.

For the modulus of elasticity, determined by linear regression on the initial region of the stress strain curve, we find a relatively large scattering, sometimes even up to more than 30% from the average. At 4.2 K, we obtain for the LMI wire an elastic modulus of 80 GPa ($\pm 8\%$), for SMI 68 GPa ($\pm 34\%$), for VAC we get 64 GPa ($\pm 1\%$) and for FUR 39 GPa ($\pm 15\%$). From the polynomial fit function in Table 2 the coefficient a_1 could represent the modulus of elasticity and is in good agreement to the linear regression for LMI and SMI wires, while rather high for both bronze route wires. At room temperature, the modulus of elasticity from the initial part is between 80 and 95 GPa for all wires.

Linear regression is also applied to the almost linear yielding profile of the curve above about 0.2% strain. Here we find a ‘dynamic’ elastic modulus or ‘yielding modulus’ ($\delta E / \delta \epsilon$) of 29 GPa for the LMI wire and 40 GPa for SMI. For both bronze route wires the yielding modulus amounts to 21 GPa.

The main difference between the reacted bronze route (VAC, FUR), internal tin (LMI) and powder in tube (SMI) wires, is the initial stiffness reflected by the modulus of elasticity being largest for the LMI internal tin strand but this wire yields more than the PIT type at higher level of stress. The initial stiffness of the VAC bronze route strand is close to the PIT wire but the bronze route wires yield significantly more than the other wires at high stress level.

From all tested wires, the non-reacted SMI wire is by far the strongest, because of the small Cu-nonCu ratio in combination with the Nb tubes surrounding the non-reacted filaments. For the reacted SMI wire, the stress remained fairly constant after reaching a certain stress level, while further elongating. When the average axial compression for the Nb₃Sn filaments is about -0.4%, a level that can be anticipated for CICC's with a stainless steel conduit [73, 74, 75] relevant variations in performance of full-size ITER conductors can be anticipated among different wire processing techniques as indeed observed later [3, 76].

We experienced that the modulus of elasticity shows a considerable spread per wire type. Alternatively, the modulus of elasticity can be determined by the slope of the unloading curve. Cyclic loads were performed on reacted wires at 4.2 K and the results are shown for the SMI and LMI wires in Figure 2.20 and Figure 2.21, respectively. At several strain levels, the tensile force on the sample was entirely released (using the same motor speed for the release) and then applied again.

For the same strain range we find about 117 GPa for the LMI strand and 96 GPa for the SMI type determined with linear regression from the unloading lines. For both bronze wires the modulus from the unloading line is 100 GPa although for the bronze wires the linear regression is less precise. These values are somewhat higher than obtained from the measured initial rise of the curve. The tendency is similar except for both bronze wires where a higher modulus is expected compared to the computation from Table 1.

Figure 2.20. Cycling stress-strain test on reacted SMI-PIT wire at 4.2 K. Force has been released at several strain levels and then applied again. For the first unloading line $E_t=99$ GPa and for the second unloading line $E_t=96$ GPa.

Figure 2.21. Cycling stress-strain test on reacted LMI internal tin wire at 4.2 K. Force has been released at several strain levels and then applied again.

2.2 Critical current under axial strain variation

2.2.1 Introduction

In order to compare the performance of full-size ITER conductors with single strands, the latter must be accurately parameterized in the applicable range of magnetic field, temperature and axial strain. For a full parameterization of the critical current with a scaling law, within a broad variation of applied magnet field, temperature and axial strain, still a large number of data is required. The deviatoric strain model [77, 78, 79, 80], however, has the potential to extrapolate from a limited dataset, but this was not thoroughly verified. The model, extensively applied in the past to fit the measured critical surface of various Nb_3Sn strands, has proven to be an accurate description for elastic deformation data [81, 82].

Aim is to demonstrate that the model can also be used to reconstruct the critical surface by extrapolation from a partial data set of measured critical currents. If the overall fitting accuracy stays within acceptable limits, the experimental time and cost can be significantly reduced. This is particularly important for qualification and acceptance tests when performed on a large number of superconducting strands.

The results obtained on one of the advanced strands from [83] and [84] are presented here. The experiments were performed on the PACMAN spring device [85], which allows measurement of the voltage-current (I / V) transition of superconducting wires up to 800 A as a function of axial strain at elevated temperatures and variable magnetic field. The strand was measured in a magnetic field between 4 and 11 T, temperature between 4.2 and 10 K and applied axial strain ε ranging from -0.9% (compressive) to +0.3% (tensile). The critical currents were then used to derive the superconducting and deformation related parameters for the scaling of measured results.

The broad set of measured critical currents obtained on the internal-tin type of strand (in total up to 200 I - I_c curves), provides verification of the extrapolative potential of the “improved” deviatoric strain model.

PACMAN. The strand was produced by Oxford Instruments, Superconducting Technology (OST) using the internal-tin route. The related parameters including the J_c results at 4.2 K and 12 T as stated by the supplier are listed in Table 2.3.

Table 2.3. Properties of the OST-type II strand [86, 87].

Strand diameter (Cr coated), mm	0.81
Cu:non-Cu ratio	1.05
Manufacturing technology	internal Sn
Diffusion barrier	Ta
Non-Cu hysteresis loss ± 3 T, kJ/m ³	700
Non-Cu J_c @ 4.2 K, 12 T, 10 μ V/m, A/mm ²	1100
n – value @ 12 T, 4.2 K	34
Heat treatment schedule, °C,h	210,50+340,25+450, 25+575,100+660,100

The heat treatment of the strand is carried out in a vacuum furnace. Prior to the heat treatment, the chrome layer on the wire surface was removed with a 37% solution of hydrochloric acid, where after the sample was pre-shaped and fixed around a Ti-6Al-4V sample holder in order to fit on the PACMAN spring. The heat treatment schedule applied (see Table 2.3) was recommended by the supplier and is identical to the one applied to the full size conductors tested in SULTAN [87] in order to avoid uncertainties in performance comparison between the single strand and the cable.

2.2.2 Experimental arrangement

After the heat treatment the sample is transferred carefully to the PACMAN spring and fixed with Sn-5wt%Ag solder at about 500 K. Figure 2.22 shows the PACMAN spring with the sample soldered. The PACMAN spring [85] is a circular bending beam made of Ti alloy with a T-shape cross-section. The strain on the outer surface of the beam is measured by two strain gauges and controlled by applying a torque. The rotation induced by a motor-warm gear combination at room temperature is transferred to the low-temperature region through a set of concentric tubes, coupled mechanically to the two revolving halves of the PACMAN spring support. The feedback implemented in the motor control unit is used to compensate for the strain change caused by the Lorentz force.

The strain homogeneity remains within $\pm 1.5\%$ along 78 mm of the sample [85]. The potential taps for the critical current measurements are placed symmetrically around the centre of the sample and spaced 20 mm apart. The temperature variations are applied by

placing the PACMAN spring with the sample under a polyimide insulator cup, thus creating a helium gas volume. Cooling and heating paths on the spring are symmetrical. Two thermometers are used so that temperature gradients over the measured sample length can be minimized and balanced well within ± 20 mK.

For the data representation and further analysis the axial strain of the sample is defined along the sample's centreline. Its value is calculated from the measured strain on the spring surface. The strain map on the spring is determined by FEM computation [85]. Since the strain gauge resistivity depends on magnetic field and temperature, a correction is applied for changing ambient conditions. This is solved by placing an additional strain gauge, exposed to the same field and temperature conditions as the main one, but not glued to the spring's surface. The feedback system in the data acquisition software reads both strain gauges and applies a correction when required. The setup is inserted in a superconducting solenoid, with the magnetic field perpendicular to the sample.

Figure 2.22. Overall view of the PACMAN spring with the OST sample soldered.

2.2.3 Critical current and n -value versus strain

About 200 VI curves as a function of applied strain ($-0.9\% \leq \epsilon \leq +0.3\%$), temperature (4.2, 6, 8 and 10 K) and magnetic field (6, 8, 10 and 11 T), were measured on the OST strand. The conventional power law ($E = E_c(I/I_c)^n$) representation is used fitting the measured VI curves between the electric field values of 30 and 300 $\mu\text{V/m}$. The critical current was then determined by extrapolating down to $E_c = 10 \mu\text{V/m}$. This is done because the shape of the VI curves deviates slightly from the power law at low levels of electric field due to yet not sufficient length for current redistribution between the sample's extremities and the location of the voltage taps, caused by the solder.

In [88] it was demonstrated that this method leads to an accurate determination of the critical parameters when the VI curve is somewhat affected by the current redistribution from the solder layer parallel to the wire.

In Figure 2.23 a selected number of measured critical current points is shown. The maximum critical current amounts to 675 A at 4 T and 4.2 K. The irreversible degradation of the strand and the corresponding I_c reduction starts above +0.2% intrinsic strain (according the method used in section 5.2.2). This is confirmed by repeating the measurements at zero applied strain. At 11 T, 4.2 K and zero applied strain the critical current became 275 A with n -value 21 after application of +0.35% strain. These values are then compared to the initial ones (before any tensile strain was applied) of 306 A and an n -value of 32.

One compressive cycle to -0.6% of strain and back to zero strain leads to a noticeable recovery of the critical current from 275 to 285 A, but the n -value remains unchanged. This effect, also observed earlier (i.e. [89]), may be attributed to a strain release of the filaments due to the plastic deformation of the matrix material.

Figure 2.23. Critical current as a function of intrinsic strain, temperature and magnetic field. The points are measured on the PACMAN and the lines are calculated with Equation 1.5.

The conventional representation of n -values (electric field range of 30-300 $\mu\text{V/m}$) extracted for all measured VI curves as a function of critical current is shown in Figure 2.24. It was demonstrated among others in [90] by the example of five Nb_3Sn wires, that the relation between n -value and critical current can be parameterized by a power law with two free parameters.

However, the relation is not a universal function of $I_c(B, T, \epsilon)$ since both parameters appeared to be temperature and strain dependent. Although empirically, it seems that two power law functions, one for all n -values measured at 4.2 K, various fields and strains, and the other for temperatures above 4.2 K, are sufficient to parameterize the n -value within 15% deviation for the strand in subject.

Figure 2.24. n -Values of the OST-II strand as a function of critical current at 4.2, 6, 8 and 10 K together with the power law fits.

2.2.4 Deviatoric scaling model and its application

The original, so-called deviatoric strain model [91] to describe the dependence of the critical current in Nb_3Sn composites on axial strain, field and temperature was also adopted by the ITER Organisation [92]. The expression for the critical current in the Nb_3Sn composites can be written as Equation 1.5, in which three superconducting parameters C_1 , $B_{c2m}^*(0)$, T_{cm}^* and four deformation related parameters C_{a1} , C_{a2} , $\epsilon_{0,a}$ and ϵ_m have to be determined experimentally.

Applying a least square fit of Equation 1.5 to the entire data set can do this. The results of the best found fit to the I_c data obtained below the irreversible strain limit (i.e. +0.2%) are presented in Figure 2.23 as solid lines, and the scaling parameters are listed in Table 2.4. Deviations between the calculated lines and the experimental data points are clearly observed at strain values above +0.2% where irreversible damage of filaments occurs.

In order to demonstrate the applicability of Equation 1.5 to the measured I_c data even outside the reversible regime, the fitting procedure was repeated for the entire strain range. The scaling parameters can be found in Table 2.4 in the columns entitled “Entire dataset”, and the calculated lines (dashed) are in the same Figure 2.23.

From the comparison of the scaling parameters in Table 2.4, we can conclude that the four deformation related parameters are really different depending on the strain range used for the analysis, while the values of $B_{c2m}(0)$, $T_{cm}(0)$, C_1 are practically not affected. This fit is less physically sound but it proves that the model can account for the asymmetry (about $\epsilon=0$ axis) in the $I_c(\epsilon)$ behaviour.

Table 2.4. Critical current scaling parameters for the OST strand following Equation 1.5.

Strain regime	reversible regime, $\epsilon < 0.2\%$		entire strain range	
Parameter	entire dataset	partial dataset	entire dataset	partial dataset
C_{a1}	51.31	51.61	81.69	83.98
C_{a2}	2.74	2.88	35.25	37.27
$\epsilon_{0,a}$ [%]	0.14	0.14	0.15	0.19
ϵ_m [%]	-0.043	-0.041	-0.057	-0.056
$B_{c2m}(0)$ [T]	33.77	33.67	34.02	33.72
$T_{cm}(0)$ [K]	16.16	16.14	16.21	16.14
C_1 [AT]	17415	17446	17390	17432
Avg. abs. error [A]	3.1	3.1	3.5	3.5
St. dev. error [A]	2.7	2.8	3.2	3.2

The question remains as to what is the minimum dataset needed to retrieve the critical current for a reliable extrapolation. Reducing the number of measured data points for each sample would result in cost and time savings, particularly important for routine acceptance tests. In [78, 80] the minimum required dataset was proposed as follows:

- $I_c(B)$ measurements at 4.2 K and about 12 K at arbitrary strain value, but normally at ϵ_m . The magnetic field ranges from as low as possible, up to the maximum available;
- $I_c(\epsilon)$ measurement at 4.2 K and at a magnetic field where the critical current remains significant, i.e. above 10 A over the entire strain range. An overall least square fit of Equation 1.5 to both datasets simultaneously is expected to fully determine the critical surface. The validity of this approach was demonstrated also on a Furukawa bronze-route wire in [78].

With the new dataset in hand the recipe can be checked, following a slightly different routine. At first, an $I_c(\epsilon)$ dependence at an arbitrary field and temperature value must be identified. Care should be taken when applying a tensile strain to a wire above ϵ_m , as this may lead to a permanent damage of the superconducting filaments. For ITER strands, it makes sense to stay close or within the B , T and ϵ window of application.

As an example we choose the curve at 11 T and 4.2 K. The thermal pre-compression strain (a location of the maximum in $I_c(\epsilon)$) can be determined with a good accuracy if sufficient number of points is taken. Once ϵ_m is known, the measurement of the $I_c(\epsilon_m, B)$ must be performed for at least two different temperature settings. In our case, the measurements were performed at four field- and four temperature settings, so 16 I_c points in total.

The values of seven scaling constants resulted from the fit to the entire (200 I_c 's) and to the partial sets of data points (30 I_c 's) are compared in Table 2.4. Although the parameters in the columns entitled "entire dataset" and "partial dataset" are slightly different, the absolute error between the experimental and calculated data as well as the standard deviation, stay within acceptable limits. A bigger difference in the scaling

parameters derived from the entire and partial datasets for the case of the entire strain range can be explained by the lack of experimental data above 0.2% of strain.

Sensitivity analysis showed that reducing the number of data points, in particularly of the $I_c(\varepsilon_m, B, T)$ set, would result in higher error bars, so the proposed set of 30 data points can be considered as a minimum required for this type of strand.

The final conclusion on the general applicability of the method and the minimum number of required I_c measurements could be further substantiated after comparisons between Nb₃Sn strands from various processing techniques.

However, we can mention that in the present analysis the extrapolative ability of the model was verified within the specified range of parameters as strain, magnetic field and temperature, where the experimental data are available. In other words, it is sufficient to have a limited number of $I_c(\varepsilon, B, T)$ measurements to be able to calculate the I_c at any (ε, B, T) combination within the same range. For characterisation of ITER strands we adopted the same procedure but increased the minimum number of data points to at least 60.

2.3 Filament crack distributions

2.3.1 Introduction

The strain dependence of the transport properties varies with the architecture of the strand, such as filament diameter and uniformity of the A15 region [93]. Since Nb₃Sn is a brittle material, applications should be designed to keep the Nb₃Sn filaments in compression. Although ITER cables are under thermal axial compression due to the steel jacket, they carry currents of up to 68 kA in a magnetic field of up to 12 T, subjecting the cabled strands to substantial transverse Lorentz force.

This causes strands to deform under distributed uni-axial, bending and contact loads. Under the most severe loading conditions, local deformations may lead to high tensile strain concentrations, possibly causing permanent damage to the strands in the form of microscopic filamentary cracks. Hence, the degradation of the transport properties due to crack growth is an important factor in determining the operational limits of the conductors and optimising their design.

Several works have addressed crack dynamics in Nb₃Sn monofilament wires [94] as well as strand failure and filament fracture in early multifilament wires [95], in ITER-type strands [96] and in various types of reinforced strands [97]. These earlier works disclosed that beyond an irreversible tensile strain threshold, crack initiation induces permanent degradation of the critical current (I_c). A further increase of the applied tensile strain causes an accumulative degree of I_c degradation due to further cracking of the filaments. Although I_c degradation and filament fracture taken separately are well

studied, a method to correlate crack dynamics with the progressive degradation of the transport properties in multifilament Nb₃Sn strands was still missing.

In contrast, there have been systematic investigations of the tensile strain sensitivity of I_c in BSCCO tapes [98, 99, 100, 101]. These studies consider current transfer between a broken filament and the metal sheath [98] and a diminishing fraction of current carrying filament [99, 100, 101] to estimate the overall degree of I_c degradation, as well as the change in the voltage-current characteristics with increasing crack density.

A model to predict the influence of cracks in Nb₃Sn strands also needs to take into account the crack mechanism and how it affects the current sharing within the strand geometry, including the transfer between concentric filament ring layers instead of only taking into account a simple two layer model.

Furthermore, such a strand model would be an essential basic element for the development of a more comprehensive cable and coil model, employed for the evaluation of the acceptance and performance tests on Nb₃Sn CICC for ITER and high field magnets.

A more quantitative understanding of the underlying phenomena that determine the shape of the voltage-current relation would enable a better distinction between the main degrading effects in short sample tests; the current non-uniformity effect from the terminations [102] and the transverse load degradation. Arriving at this point it is essential to note that the transverse load degradation is not only connected to crack initiation and propagation, but also to a broadening of the strain distribution, causing current redistribution among the filaments, resulting in degradation of the transport properties. It is important to make this distinction since it may also affect the choice of the qualification criteria to be used. A relation between the crack mechanism, local strain distribution and current sharing is also helpful in the analysis of measurements on strands, extracted from the full-size cabled conductors subjected to high electromagnetic loads [103].

The work described in this section marks the start of a more comprehensive work on filament crack analysis and study of the intra-strand resistances, all coming together eventually in a detailed numerical strand model. This extensive work is outside the scope of this thesis but has reached a mature state in the meantime [14], [15], [104], [105], [106], [107], [108], [109], [110], [111], [112], [113], [114], [115], [116], [117] and [118].

Here we focus on the investigation of two types of strands with different architecture, one prepared with the bronze route and the other with the internal-tin route. Careful mechanical polishing allowed unambiguous identification of the microscopic fractures of filaments caused by axial straining of the strands. With the PACMAN strain device (Figure 2.22), the voltage-current characteristics at zero applied strain were measured after several successive applications of incrementally increasing tensile strain. The spatial distribution of cracks and the degradation of I_c after tensile load applications show clear qualitative differences between the two strand types.

2.3.2 Preparation of sample pucks

Two ITER-type multifilament strands were investigated; a bronze strand manufactured by European Advanced Superconductors (EAS) and an internal-tin strand manufactured by Oxford Superconducting Technology (OST2). Technical details are found in [86, 87, 119]. They have different filament diameters (bronze $\sim 3 \mu\text{m}$, internal-tin $\sim 5.5 \mu\text{m}$) and different architecture (Figure 1.8). In the bronze strand, the filaments are arranged in a uniform honeycomb structure while in the internal-tin strand the filaments contain tin-rich cores in a hexagonal arrangement.

Both types of Nb_3Sn strand, bronze and internal tin process (Figure 1.8) are used for the European prototype CICC for the Toroidal Field coils and tested as short samples in the SULTAN facility in Villigen. Measurement of irreversible degradation of the critical current I_c under tensile strain is made using the PACMAN spring device. The strand is soldered along its entire length onto the PACMAN, which can then apply both compressive and tensile uni-axial strain to the sample. At each applied strain, the VI characteristic is measured to determine the I_c and the n -value at the voltage criterion of $10 \mu\text{V/m}$. To find the strain irreversibility limit, the deformation is reduced to zero after each tensile strain measurement and the zero-strain critical current and n -value are measured again. Applied strain levels range from $\epsilon = -0.5\%$ to $+0.8\%$ at 4.2 K and in a magnetic field of 12 T perpendicular to the sample.

In addition the mechanical stress-strain behaviour of the two types of strand is measured with the TARSIS set-up at 4.2 K (Figure 2.1 and Figure 2.2) [120]. Free-standing strands are axially stretched in a liquid helium bath while the applied load and strain are measured with a load cell and with spring-loaded extensometers. Strain is applied up to a given peak value continuously and then released again, so that mechanical hysteresis due to plastic yielding can be recorded. This measurement is repeated on a different strand sample for each subsequent peak load, resulting in a set of strands that are submitted to peak loads up to $\epsilon = 0.7\%$ in steps of 0.1% .

To correlate the electrical irreversibility with the occurrence of micro-cracks in the Nb_3Sn filaments, segments of each of the strained strands are polished to obtain longitudinal cross sections for examination with a SEM. The polishing process is done carefully to avoid the introduction of additional damage. Samples are vacuum-impregnated in epoxy resin, plane-ground with SiC paper and fine-ground with 9, 6 and $3 \mu\text{m}$ diamond paste on nap-less polishing cloths. To ensure that during every polishing stage the damage of the previous one is fully removed, the sample puck thickness is monitored with a micrometer. Grinding a thickness higher than twice the previous grit size results in the desired damage-free virgin ‘witness’ samples. After fine polishing with $1 \mu\text{m}$ diamond paste and a final step with silica colloid, the surface is dip-etched in an HF/HNO_3 (10%/27%) solution to enhance contrast and thus facilitate the detection of possible micro-cracks. SEM images were taken over the entire width of the filamentary region and over an axial sample length of 5.5 mm , which roughly

corresponds to 1/3 of the twist pitch for both strand types. More details of the polishing procedure are described elsewhere [96].

2.3.2.1 Mechanical analysis

Figure 2.25 (left) shows an example of the mechanical stress-strain curves of both types of strand at 4.2 K, while in Figure 2.25 (right) the mechanical hysteresis $\Delta\epsilon$ after unloading is plotted as a function of applied peak load. The data for the bronze and internal tin strands are consistently similar, with e.g. both strand types exhibiting virtually the same yield strain $\epsilon_y \approx 0.4\%$. Here we define ϵ_y as the strain resulting in $\Delta\epsilon = 0.2\%$. Closer inspection of Figure 2.25 (right) shows already an onset of plastic behaviour at $\epsilon \sim 0.2\%$. Note that this experiment records the macroscopic mechanical behaviour of the composite as a whole and does not reveal the microscopic distribution of stress and strain throughout the twisted multifilamentary strand architecture.

Figure 2.25. Left: axial stress-strain curves measured at 4.2 K. Right: mechanical hysteresis $\Delta\epsilon$ after excursions to different applied strain ϵ_{max} for bronze and internal tin strands. Each data point represents a new strand sample.

2.3.2.2 Electrical analysis

The strain dependence of I_c for the bronze strand obtained with the PACMAN is shown in Figure 2.26(a). Arrows in the figure illustrate the order of successive measurements. Data are first taken in the reversible compressive regime, the applied strain is released to zero and then the tensile regime is explored. As indicated, each successive incremental tensile measurement is followed by a zero-strain one. Data are collected with ϵ increasing in steps of 0.025%, but for visual clarity only data points every 0.05% are shown in Figure 2.26(a).

To compare the electrical measurement with the microscopy results, the strain applied with the PACMAN spring needs first to be converted to the corresponding strain in the microscopy experiment, i.e. we need to account for the difference in thermal pre-compression of the filaments in the two experiments. In the PACMAN experiment the sample is soldered onto the spring over its entire length and the thermal pre-compression is largely determined by the properties of the TiAlV spring material that

cools from soldering at about 550 to 4.2 K. On the other hand, in the stress-strain rig used to prepare the microscopy samples, the sample is allowed to cool down freely.

Figure 2.26. (a) I_c versus applied strain ϵ measured on the bronze strand with the PACMAN. Arrows and numbers indicate the measurement sequence. The dashed line corresponds to the irreversible strain limit. (b) Degradation of the zero-strain I_c and n -value resulting from the strain excursions to different ϵ_{max} values. The inset shows the same analysis for the internal tin wire.

This strain difference $\Delta\epsilon$ is evaluated by comparing the I_c value of a free-standing strand to the strain dependence of I_c obtained with PACMAN measurements in the compressive regime. On its turn, we assess the I_c of the free-standing strand from the measurements carried out on a standard ITER barrel, actually made of the same TiAlV material where only the sample ends are fixed. This is obviously not exactly

representative for the strain state of a free strand but it is as close we can get without performing additional experiments. Further details of this correction procedure are given in [104].

The actual strain state of the Nb₃Sn layer can be estimated from the position of the maximum in the $I_c(\epsilon)$ curve [97] and is about -0.05 % in this case. For a free standing sample the thermal pre-compression of the Nb₃Sn layers at 4.2 K would be more severe and is estimated at about -0.2% for internal tin and close to -0.3% for bronze processed wires. After excursions at higher strain, we observe degradation in the zero-strain I_c and n -values compared to the virgin zero-strain measurement. The degradation measured at zero-strain can be quantified as ΔI_c and Δn , defined by:

$$\Delta I_c(\epsilon_{max}) = \frac{I_c(\epsilon=0)_{\epsilon_{max}} - I_c(\epsilon=0)_i}{I_c(\epsilon=0)_i}, \quad (2.5)$$

$$\Delta n(\epsilon_{max}) = \frac{n(\epsilon=0)_{\epsilon_{max}} - n(\epsilon=0)_i}{n(\epsilon=0)_i}. \quad (2.6)$$

where the subscript i indicates the virgin zero-strain measurement and ϵ_{max} is the maximum strain applied in the last tensile excursion. Clearly, the n -value is more sensitive to degradation than I_c , starting to decrease irreversibly already at $\epsilon_{max} \approx 0.5\%$ while ΔI_c only starts to drop around $\epsilon_{max} \approx 0.6\%$.

At the time of these tests, the irreversible strain limit ϵ_{irr} was defined as the ϵ_{max} value that results in an n -value degradation $\Delta n = -10\%$, for this bronze strand $\epsilon_{irr} = 0.65\%$ was obtained. Note that this would correspond to -0.6% strain of the Nb₃Sn layer since the maximum in Figure 2.26(a) is found at ϵ -applied of -0.05%. When the later on introduced method of section 5.2.2 would have been used, $\epsilon_{irr} = 0.60\%$ would have been obtained.

The methods only lead to small differences although the procedure proposed in 5.2.2 gives better agreement with methods proposed later in for example [16, 17, 121], being more sensitive to a lower voltage criterion. For a freestanding strand the irreversibility would appear at $\sim 0.9\%$ applied strain due to thermal pre-compression. The inset in Figure 2.26(b) shows the same analysis performed on data obtained from the internal tin strand, which gives $\epsilon_{irr} = 0.22\%$ in the Nb₃Sn layer, corresponding to $\sim 0.4\%$ applied strain for a freestanding strand.

The V - I curves at zero applied strain, obtained after applying strain levels up to 0.88% for the bronze strand and up to 0.26% for the internal-tin strand, are practically identical to their respective virgin (unstrained) state curves. We find that the onset of crack growth and the start of observable changes in VI curves coincide.

2.3.3 Statistical distribution of cracks

Micro-cracks we recognise observably traverse at least one full filament width. Across the imaged sections, we count the number of observable cracks with respect to the transverse distance measured from the midpoint of the strand's width and thus obtain a

transverse distribution of cracks. In the case of bronze strand, the filamentary region has a honeycomb structure with uniform filament density, apart from the region near the diffusion barrier. As one might expect from this high degree of translational symmetry, cracks occur everywhere across a highly strained strand (Figure 2.27(a)). No clear preference in crack site is found in both transverse and longitudinal directions of the strand. In comparison, for the internal-tin strand (Figure 2.27(b)) cracks occur concentrated near the centre of the strand.

Figure 2.27. Distribution of cracks with transverse distance for (a) the bronze strand strained to 1.1% and (b) the internal-tin strand strained to 0.8%.

Figure 2.28. Distribution of cracks with respect to distance to the core edges. Data are obtained from several segments of internal-tin strand strained to 0.8%.

Also the crack distribution with respect to the distance from the tin core edges, obtained from several polished segments from the same strand (Figure 2.28), shows that cracks are concentrated near the tin core edges, an observation also noted by Jewell et al. [96]. In both strand types, cracks start to propagate for highly strained samples and we have categorised cracks by the number of neighbouring filaments that they traverse. For example, an isolated crack is identified as a first order crack while a crack across two neighbouring filaments is called a second order crack (Figure 2.29). The histograms of categorised cracks show crack initiation and propagation as the applied tensile strain increases for both strand types (Figure 2.30).

Figure 2.29. Micro-cracks of different order, 1st (a,d), 2nd (b,e) and 7th (c,f) observed in bronze (top) strand strained to 1.1% and internal-tin (bottom) strands strained to 0.8%. Scale bars correspond to 10 μm .

Figure 2.30. Number of cracks with a given order observed in (a) bronze and (b) internal-tin strands strained to various degrees.

We define the crack density (cracks/mm) of a strand by observing the number of cracks per mm longitudinal length per filament. The average number of observable filaments in the transverse direction of the images is used for this calculation. In both strand types, the increase in crack density with applied strain is well described by a power function in applied strain (Figure 2.31). Compared to the bronze strand, in which no cracks are observed below a threshold strain, beyond which the crack density increases rapidly, the internal-tin strand is susceptible to cracks at much lower applied strain but the crack density remains low.

The difference in observed crack densities seems due to different crack mechanisms that take place in the two strands. In the highly strained bronze strand, a large number of cracks occurring in short intervals are observed more or less homogeneously across the entire strand length. On the other hand, in the highly strained internal-tin strand a small number of cracks occur concentrated, leaving the remainder of the strand largely crack free. A similar difference in crack mechanism has been reported in [95], where the different fracture pattern was attributed to the difference in filament diameter.

Figure 2.31. Crack density plot for (a) the bronze and (b) the internal-tin strand. Lines are fitted with a power-law function versus applied tensile strain.

2.4 Conclusion

The strand's axial stress-strain characteristic influences the performance degradation of Nb_3Sn CICC's, not only when subjected to electromagnetic load but also for the thermal pre-strain due to cooling down from reaction heat treatment. For this reason the versatile TARSIS facility with devices for axial and transverse strand deformation was developed. TARSIS enables measurement of the transport properties (voltage-current transition), the applied force and deflection in the course of cyclic loading.

The stiffness of the strand restrains the response to an applied stress level, determining the final change in the strain and therefore the critical current. Two bronze route, one internal tin and one powder-in-tube type of Nb_3Sn wires have been tested with good reproducibility at three temperatures, 4.2, 77 and 293 K.

Resolving the zero strain level and initial modulus of elasticity appears difficult due to immediate yielding of the copper and bronze in the wires and differences between samples from similar design.

We used a straightforward 1D method for computation of the stress-strain curves, based on the present state of general available material properties, for various temperatures. The computations appear to be in good agreement with the experimental results. Deviations between experimental results and computations seem mainly caused by:

- uncertainties in the database of available material properties,
- the actual wire composition with tin gradients in bronze and Nb_3Sn ,
- possible influences from reaction residuals as in the case of PIT wire,
- neglecting radial influences in the 1D mechanical model.

None of the more broadly used fitting functions comply satisfactory when applied with all wires in the full range of strain and a polynomial function simply seems most appropriate.

The difference in sensitivity between various types of strand strongly depends on the considered range of stress. We found that the quasi-initial elastic modulus at low applied stress is highest for the LMI internal tin strand, but this wire yields more than the PIT type for higher stress. At higher yielding stress levels for the type of wires in subject, we find a considerably higher strain for the softer bronze route wires compared to internal tin and powder-in-tube fabrication method respectively.

When the average axial compression for the Nb_3Sn filaments is in the range of -0.4%, a level that can be anticipated for CICC's with a stainless steel conduit, we may expect relevant variations among different wire processing techniques in performance.

The effect of uni-axial strain variation on the transport properties of ITER strands and scaling their characteristics to full-size conductor performance is performed with the PACMAN tool. In total more than 200 voltage-current curves were measured on an internal-tin route Nb_3Sn strand at different strain, temperature and magnetic field values to identify its critical surface. The scaling parameters of the deviatoric strain model were derived either from the fit applied to the entire set of critical currents or to a partial set (30 voltage-current curves), according to the proposed procedure. It is demonstrated that the scaling parameters extracted from both methods are practically identical.

The impact of filament crack patterns on the strand's transport properties is investigated on two ITER-type Nb_3Sn superconductor strands, one prepared with the bronze route and the other with the internal-tin route. After application of high axial tensile strain, densely and uniformly spaced cracks were observed in the bronze strand, while less, but more correlated cracks occurred in the internal-tin strand. Crack initiation was observed

in the bronze strand after an applied tensile strain of more than 0.8 %, while for the internal tin strand cracks were found already in unloaded specimen with further crack growth beyond 0.3% applied strain. In both strand types the crack density increases and cracks propagate with increasing strain. With the PACMAN strain device, the voltage-current characteristics at zero applied strain were measured after several successive applications of incrementally increasing tensile strain. For the first time, a clear coincidence between the onset of I_c degradation and crack growth was presented.

3 Effect of periodic bending in Nb₃Sn strands

Knowledge of the influence of bending on the critical current of Nb₃Sn strands is essential for understanding the reduction in performance due to thermal and transverse electromagnetic loads. In particular, for the large Cable In Conduit Conductors meant for ITER, bending is expected to be the dominant mechanism for degradation. The transport properties of bronze-, powder in tube- and internal tin- processed Nb₃Sn strands were measured when subjected to spatial periodic bending using dissimilar bending wavelengths. Two of these strands were used in model coils for ITER. We found that the strands behave virtually according to the so-called low interfilament resistivity limit for uniform bending, introduced by Ekin, suggesting good current transfer between filaments. The critical current versus strain sensitivity can vary for different strand types but since the thermal and electromagnetic forces are the drivers for strand bending in CICC, the stiffness of the strands definitively plays a key role.

An independent confirmation of the results from TARSIS bending was found by using a standard ITER I_c test barrel adapted with spatial periodic slots in the center grooves.

Nb₃Sn strands, tightly swaged in a steel tube, imposing a compressive strain, were used to simulate experimentally the conditions representative for an ITER CIC conductor. It is found that the degradation of the I_c due to bending is much more severe for a strand swaged in a stainless steel tube than straightforwardly simulated by present models assuming simple axial strain dependency. An empirical scaling can be alternatively used, based on the experimental results obtained with TARSIS on a strand in a stainless steel tube, representative for the strand's behaviour under axial compressive strain condition.

The content of this chapter is for a large part based on papers [54], [123], [129], [131], [137], [145], [153], [199], [202] and [209].

3.1 Introduction

Various approaches to characterize superconductor behaviour versus strain have been employed throughout the last decades. For technical wires, most attention has been paid to axial and, to a lesser extent transverse strain experiments since these represent the strain components relevant for most applications in wire wound magnets. Since bending strain in the Nb₃Sn wire is largely a radial distribution of axial strain over the wire cross-section, knowledge of the I_c versus axial strain dependency, as discussed in section 2.2, is obviously required for analysis of the bending strain influence.

The bending strain response in a strand's transport performance depends on the internal strand layout in terms of twist pitch, cross sectional layout and internal resistances, determining the extend of current transfer between the filaments [14], [15], [50], [104], [105], [106], [107], [108], [109], [110], [111], [112], [113], [114], [115], [116], [117] and [118].

In order to investigate experimentally the performance reduction of various strand types under periodic bending, dedicated TARSIS probes were designed and build. To start with, the boundary conditions for the design of the probe are discussed, followed by a detailed description of the bending probe [54].

Accordingly, the first experimental results of periodical strand bending with cyclic loading using different bending wavelengths are presented. The tests were performed on a powder-in-tube processed wire (indicated as SMI) using different bending wavelengths from 3.8 to 8.3 mm. The inter-filament current transfer length, important for the evaluation of the ability of inter-filamentary current redistribution, was extracted from AC loss measurements and found in reasonable agreement with the so-called low interfilament resistivity model also briefly explained [122].

So as to find an independent confirmation of the bending test with TARSIS, another testing technique utilising a standard ITER I_c test barrel, but with spatial periodic slots machined in the centre grooves, was developed and applied to the same wire [123]. Two additional strand types manufactured with different processing techniques were tested with the TARSIS bending probe.

A bronze processed strand (indicated as VAC) was taken from the central solenoid model coil (CSMC) production and used in the inner turns of the solenoid with highest transverse load. The second strand is an internal tin processed type (indicated as LMI) taken from the Toroidal Field Model Coil production [31, 124]. The test results clearly demonstrated the importance of strand axial stiffness, as studied in section 2.1, in relation to bending stress. Finally the influence of strand thermal contraction on the transport properties, proportionate to bending wavelength and deflection is explored.

3.2 TARSIS periodic bending probe

The most important parameters are the required sample length for accurate testing, the representative bending wavelengths, the applied load and associated bending deflection. The average wavelength L_w for bending is determined on a full-size ITER Central Solenoid Model Coil (CS1) conductor and is 6 mm with a standard deviation of 2 mm. A photograph of the cable bundle as depicted in Figure 1.16, clearly shows the deformation at strand crossings and varying periodicity. The sample length should be sufficient to cover deflection measurement on three positions along the loaded length and three corresponding voltage tap pairs probing the homogeneity of the load. The forces can be deduced from the electromagnetic forces on the strands in the CICC under magnet operating conditions.

The estimation of the range of strand bending deflection is based on transverse loading tests of a CS1 conductor in the Twente Cable Press, as presented in chapter 6. The test is considered representative for severe accumulated loading and leads to elastic and plastic deformation of the strands in the cable bundle. Figure 6.24 shows the compression versus applied force or $d(F)$ curves of a CS1 conductor for various numbers of cycles and two applied peak loads [89]. A first press peak load of 450 kN/m is taken as an average for electromagnetic loading under ITER magnet operating conditions. The actual electromagnetic peak load in ITER is 650 kN/m. The total compression of the bundle goes up to 1100 μm with a plastic component of roughly less than 50%. This is in good agreement with the dimensions of the cross section measured after the experiment and presented in Figure 6.23 [89].

The cable compression is transferred into local strand deformations like periodic bending, strand-to-strand point contacts (pinching) and more homogeneous transverse load (line contacts) along the strand. The roughly estimated average range of ‘bending’ amplitude of strands is the transverse cable compression divided by the square root of the total number of strands. For 1152 strands the approximate bending amplitude, including pinching of the strands is then about 30 μm [89, 125].

It can be argued that the stress pattern of a cable in a magnet differs from that in the press, not only in transverse- but also in longitudinal direction. For electromagnetic load, severe accumulated loading occurs in the lower part of the cable cross section depicted in Figure 3.1. The cable in a coil is under axial compression due to the thermal pre-strain caused by the conduit while in the press there is probably limited pre-strain due to the short length. In addition, the hoop stress in a coil, just like strand bending, relaxes the axial strain in the high self-field area of the cable cross section (low accumulated load zone).

Nevertheless, the results of the cable mechanical compression in the press are in good agreement with the understanding of the pressure drop found in the Model Coil tests, being explained by a gap of 1.3 mm between conduit and cable at one side caused by the Lorentz forces (so-called third channel) [126].

Figure 3.1. 2D vertical stress σ_y in the cable [$\times 10$ MPa] (see legend with grey scaled values) for the 'magnet' case using a volume force inside the cable, simulating the Lorentz force due to a magnetic field of 13 T and a transport current of 50 kA [127].

The force F simulates the transferred accumulated magnetic load between crossing strands in a CICC, reaching amplitudes of about $30 \mu\text{m}$. Next the load, in terms of distributed load and point load with clamped ends principle, can be calculated with standard formulas. The distributed load between two supporting strand crossings in the schematic in Figure 3.2(b) is Q_l . The point force F , in Figure 3.2(a) represents the transferred magnetic point load from the upper crossing strand.

Figure 3.2. Schematic view of a point load (left) and a distributed load (right) with clamped ends.

We can write

$$Q = q_l \cdot L, \quad [\text{N}] \quad (3.1)$$

and

$$q_l = B \cdot I. \quad [\text{N}] \quad (3.2)$$

The point load F depends on the location in the cable. The corresponding standard (elastic) equations for the deflection are

$$f_{max} = \frac{Q \cdot L^3}{348 \cdot E \cdot I_a} \quad [\text{m}] \quad (3.3)$$

and

$$f_{max} = \frac{F \cdot L^3}{192 \cdot E \cdot I_a}, \quad [\text{m}] \quad (3.4)$$

where I_a is the moment of inertia while W_b is the section factor. The peak strain is then estimated by

$$\varepsilon = \frac{M_b}{W_b \cdot E} \quad (3.5)$$

in which

$$M_b = \frac{Q \cdot L}{24} \quad [\text{Nm}] \quad (3.6)$$

and

$$M_b = \frac{F \cdot L}{8}, \quad [\text{Nm}] \quad (3.7)$$

respectively. The accumulated load F exceeds by far the purely distributed bending load caused by only the current in one strand. In a typical CICC exposed to a magnetic field of 12 T, a current of 43 A per strand and a wavelength L of 5 mm, the distributed bending load F is 2.6 N. This would result in a deflection f of less than 1 μm and an applied peak strain ε of 0.04% for a Young's modulus E of 150 GPa (see Table 2.1 and Table 2.2). This demonstrates that the reduction in performance by distributed load only is quite moderate.

However, the local accumulated peak load in an ITER conductor is 90 N, assuming that the number of strand layers is $\sim 1152^{0.5}$ [54]. If fully transformed into bending, this would lead to a deflection of 18 μm and an additional peak bending strain of 0.7% (elastic model and $L=5$ mm). In reality the yielding of the copper matrix would cause an enhanced deflection. This induces strain levels seriously affecting the I_c in Nb_3Sn filaments when no further constraints are present in the cable limiting displacement and deformation.

For the design of the setup, the above-derived range of deformation and force is taken as a reference. Considerable attention is paid to enable an accurate measurement of the deflection of the wire. For this purpose three extensometers are applied which are adapted to this particular device, see Figure 3.3 (top) [66]. A slot and a small notch at the contact point enable controlled positioning. The four strain gauges are wired in a bridge configuration. Calibrations at 300 K show a sensitivity of about 1 V/m.

Another crucial issue is the condition of the reacted sample and unnecessary handling shall be avoided. Therefore the complete setup is made of TiAlV-alloy and used for both heat treatment of the sample and measurement. After reaction the sample remains

in position, the extensometers are connected and voltage taps and current leads are soldered to the wire. A drawing of this realised periodical bending device is shown in Figure 3.3. The wire runs along the circumference of a cylinder and is supported by pins. A cap with round bulges with the same radius as the pins is placed on opposite side of the strand. The cap is suspended with flexible sheet-bearings, which allow only vertical movement without friction. The Lorentz force on the sample is pointing inwards.

For accurate measurement, the alignment of pins and cap is critical. To minimise the remaining misalignment, the cap is mounted with a small compressive force, assuring that the wire is in contact with all pins and round bulges prior to reaction. After reaction the wire is tension free but still in contact with all pins and bulges. The three extensometers are distributed evenly along the circumference of the cylinder. The probe was further optimised by implementing an additional turn of the sample below the section to be loaded, in order to avoid voltage due to current transfer in the test section equipped with voltage taps (see Figure 3.4).

A thrust bar driven by the Precision Vertical Linear Stage shown in Figure 3.5, fits onto a ball bearing on top of the cap. The stage is placed on top of the cryostat and transfers the force through the thrust bar to the test rig in liquid helium, where the load is transferred from the cap to the crossing wires. The force applied with the thrust bar is measured with a calibrated load cell outside the cryostat.

Figure 3.3. TARSIS periodical bending probe according to the initial design. The wavelength is 5 mm.

Figure 3.4. Left and middle: pictures of the bending probe showing an additional turn of the sample below the section to be loaded. Right: extensometer adapted for the TARSIS periodical bending probe.

Figure 3.5. Drawing and photo of the Precision Vertical Linear Stage on top of the cryostat to control the force and displacement during the TARSIS tests.

3.3 Application of different wavelengths

The first results for periodic bending with different wavelengths were obtained on a Nb₃Sn PIT strand manufactured by SMI (NL). The diameter is 0.8 mm, it has 504 filaments with a Cu fraction of 0.53 and the critical current is 300 A at 12 T with twist pitch 10 mm. Five cylinders and corresponding caps are produced with wavelengths of 3.8, 5.0, 7.1, 8.3 and 10.0 mm, respectively (distance between loading points on the cap) representing characteristic values of the bending radius in a CICC [46]. The measurements are performed at 4.2 K and 12 T. The voltage current traces at various applied bending loads are recorded up to an electric field level of more than 100 $\mu\text{V/m}$.

Some electric field E versus current characteristics, taken at various applied loads from zero to 17.7 kN/m are presented in Figure 3.6 for a wavelength of 7.1 mm. The longitudinal electric field in a strand E_z is a non-linear function of current, temperature, and magnetic field (see Equation 1.14). The n -values are normally determined from the EI curves in the electric field range of 10-100 $\mu\text{V/m}$ with Equation 1.14. The EI characteristic, as the current increases, gradually shifts from no to full current transfer and the characteristic strongly depends on the local strain distribution and interfilament resistivity. Normally we assume strands to have a constant n value obeying a power law fit but for increasing strain variations, the EI curve does not follow anymore the power law. This is illustrated by a few selected EI curves at different loads in Figure 3.6 where it is clear that not only the steepness of the curves changes with increasing load but also the shape.

Due to the curvature in the EI traces, the choice of the window affects the n -value. The virgin curve and those obtained at low load follow the power law. At higher load, the initial slope becomes shallower and becomes degraded by a current redistribution effect [46]. At increasing levels of E , the curves become steeper again. The influence of the electric field level on the relative transport capacity for bending is illustrated in Figure 3.7. A higher level of electric field yields a better performance under bending.

In Figure 3.8, the I_c/I_{c0} is plotted against the load per meter for various bending wavelengths. The intermediate steps to lower load levels demonstrating irreversibility are not included for clarity but is presented separately for $L_w=8.3$ mm in Figure 3.9. The corresponding n -values, plotted against the applied load per meter, are depicted in Figure 3.10. The critical currents for all EI curves, taken at an electric field level of 10 $\mu\text{V/m}$, are normalized to the I_{c0} in the virgin (unloaded) condition of the strand. The n -values, in this case, are determined from the EI curves in the electric field range of 5-20 $\mu\text{V/m}$. A plot against the load per unit length seems a comprehensible representation in direct relation to a cable in a magnet. We observed that the I_c increases slightly at the first moderate loads but must be increased to more than 1,000 N/m to reach a noticeable reduction. Exceeding this level leads to a clear irreversible behaviour, consequently leading to a further reduction of I_c . The reduction in I_c versus load per meter enhances with longer wavelengths.

Figure 3.6. Electric field versus current of the SMI PIT strand for a wavelength of 7.1 mm at 4.2 K, 12 T (F in kN/m in the legend).

Figure 3.7. Normalized critical current versus peak bending strain for a wavelength of 5.0 mm at 4.2 K, 12 T (F in N/m in the legend).

Figure 3.8. Normalized critical current versus applied load per meter for bending wavelengths L_w of 3.8, 5.0, 7.2, 8.3 and 10.0 mm at 4.2 K, 12 T.

Figure 3.9. Normalized critical current versus applied load per node for a bending wavelength L_w of 8.3 mm with subsequent intermediate release of the load at 4.2 K, 12 T.

Figure 3.10. n -Value versus applied load per meter for a bending wavelength L_w of 8.3 mm with subsequent intermediate release of the load at 4.2 K, 12 T.

Figure 3.11. Normalized critical current versus applied load per node for bending wavelengths L_w of 3.8, 5.0, 7.2, 8.3 and 10.0 mm at 4.2 K, 12 T.

The first results clearly show the impact of strand bending at accumulated loading on the I_c and n -value of a Nb₃Sn strand. Moreover, the cyclic behaviour plainly shows a component representing a permanent reduction and a factor, which is a reversible function of the applied load.

The permanent reduction is associated with plastic deformation, yielding of the soft copper and stabilizer and bronze content, causing a permanent change in the strain state of the filaments, while the reversible component is related to elastic deformation.

As far as it concerns a first order quantitative analysis, the results presented here are not obtained on a CSMC strand but can be interpreted with the estimated average bending amplitude in ITER Nb₃Sn CICC's. The estimation of the 'bending' amplitude is a result of strand bending and transverse loads (30 μ m). The largest applied bending amplitude on the sample with a bending wavelength of 5 mm in TARSIS is 20 μ m with a corresponding load of 22 N/node. This is practically the expected order of magnitude for the accumulated load in ITER CICC's under operation. The bending amplitude at similar load per meter is significantly higher for the longer wavelengths (in the order of 100 μ m). The reduction in I_c becomes more significant with larger bending wavelengths (load per meter).

It is important to analyse whether the load at the point contacts also plays a role in being partly responsible for the reduction in strand performance. If the I_c/I_{c0} is plotted against the load per node (Figure 3.11) practically the same correlation is found for the three largest wavelengths. The I_c reduction seems rather independent from the bending wavelength for constant transverse stress at the load points (nodes).

However, for the probe with $L_w=3.8$ mm the reduction in I_c is appreciably larger and so the diameter of the pins (1.5 mm) in relation to their distance become significant. This can be substantiated by investigating the effect of contact stress on the I_c as presented in chapter 4. Here it was found that there is no reduction in I_c when subjected to contact stress with crossing strands up to at least 20 N/node. This suggests that the larger reduction in I_c measured with the probe having L_w of 3.8 mm is likely affected by the load profile geometry. For this reason the results of this probe is omitted for pure bending analysis.

The measured applied load and strand deflection are compared to the computed strand deflection in Figure 3.12. The deflection is calculated by numerical finite element modelling of bending assuming linear elastic deformation of the strand as illustrated in Figure 3.13. The initial parts of the curves deviate from the computed ones due to the change of the E -modulus with increasing strain in the experiment, while for the computed results a constant E -modulus is assumed. Also here it appears that the mechanical behaviour for bending with a wavelength of 3.8 mm strongly deviates from the computed one compared to the other wavelengths.

Finally the reduction in I_c versus the applied peak bending strain is shown for all wavelengths larger than 3.8 mm in Figure 3.14. The peak bending strain in the Nb₃Sn filaments is determined by the amplitude of the bending deflection and corresponds with the strain in the outer perimeter of the filamentary bundle area. It is found that under the tested conditions, the influence of the bending wavelength is only minor and the peak bending strain seems dominant. Obviously a relation between the bending wavelength and the twist pitch of the wire can be anticipated but this is not explored at this stage.

Figure 3.12. Applied bending load per meter on the strand versus the deflection amplitude for bending wavelengths L_w of 3.8, 5.0, 7.2, 8.3 and 10.0 mm at 4.2 K, 12 T ('m' stands for measured and 'c' for computed).

Figure 3.13. Numerical finite element modeling of bending strain assuming elastic deformation of the strand (courtesy Hugo Bajas).

Figure 3.14. Normalized critical current versus applied load per node for bending wavelengths L_w of 5.0, 7.2, 8.3 and 10.0 mm at 4.2 K, 12 T.

3.4 Validation by pure electro-magnetic force

3.4.1 Introduction

The development and first results of the TARSIS probe to study the effect of spatial periodic bending on the transport properties, i.e. the voltage-current transition in terms of critical current (I_c) and n -value, is reported in the previous section. Here, a supplementary and simple method is employed as a validation of the TARSIS bending probe concept. The influence of bending strain is investigated by using a spatial periodic strand support on a broadly used standard I_c measurement barrel in combination with the Lorentz force on the strand. The bending force ($B \times I$), is varied by changing the applied field B .

The peak bending strain in the Nb_3Sn filaments is determined by the amplitude of the bending deflection, which is deduced from the mechanical axial tensile stress-strain properties of the wire presented in section 2.1. Three different spatial periodic wavelengths are applied. The “barrel-with-slots” (BWS) method can be applied easily and straightforwardly with minor effort and cost in laboratories having a standard I_c measurement facility for superconducting wire.

For bending strain we can differentiate between spatial periodical loading with a wavelike bending pattern [40], [89] and bending along a bend surface or cylinder with constant radius [58], [59], [60], [61], [62], [63], [64] and [65]. The experimental results presented in literature, in particular on bending of Nb_3Sn strands, show dispersion in results, basically depending on the experimental method. Therefore, a direct comparison of results is mostly inappropriate because some methods use pre-bending at room temperature, while others obtain pre-compression utilizing a material with a high coefficient of thermal expansion (CTE).

An approach that utilizes repeated bending at room temperature as a method to improve the transport properties of Nb₃Sn wires, causes an increase of the I_c [128] (up to a factor of two at high fields) with a pre-bending strain up to 0.5%. The bending strain is defined as the peak strain at the outer circumference of the wire's filamentary region. The observed increase in I_c is attributed to a relaxation of the filament axial compressive strain due to an overall elongation of the wire, likely in combination with a reduction in radial pre-stress as well.

Another experiment [59], investigating the impact of bend strain (bending just once and on a constant radius) of up to 0.3% applied at room temperature, revealed an I_c improvement exceeding 30%. An experiment with applied spatial periodic bending at 4.2 K, almost similar to the method applied with TARSIS except for a precise measurement of the bending amplitude, leads to only a decrease in I_c with increasing bending deflection. A drawback of this method is that the exact zero strain condition is defined to coincide with the first observation in I_c decrease [40]. A precise measurement of the deflection is therefore crucial to resolve exactly the zero strain.

In summary, the variety in methods for wire bending experiments still leaves ample opportunity for different interpretation. For this reason an alternative route for exploring experimentally the effect of periodic bending on the I_c of a Nb₃Sn strand is developed. A description of the method is given and the results obtained on the same powder-in-tube processed Nb₃Sn wire as used in the previous section on the TARSIS bending probe for direct comparison presented.

3.4.2 Barrel with slots

The strand is wound in the spiral groove of a molybdenum-sulphide coated and pre-oxidised TiAlV cylinder, which's CTE matches closely that of Nb₃Sn. It is well-known as the ITER barrel that is broadly applied as a standard for critical current measurements [70]. The wire is wound under tension with a counter weight of ≈ 1 kg to assure a constant sample pre-tension and defined preparation procedure. Slots are machined on recurring distances in the wall of the cylindrical sample holder at the two turns in the middle to allow for periodic bending when subjected to electro-magnetic force pointing towards the centre axis of the barrel, see Figure 3.15.

Basically the wire follows a periodic support of bridges with open spaces in between. The span of the slots is identical to the distance between them, determining the bending periodicity or wavelength as shown in Figure 3.16 left. To restrict wire movement during the heat treatment, the wire ends are fixed by screws on the two copper rings attached on either end to the TiAlV barrel. After the heat treatment the wire ends are soldered to the copper rings to which the current leads are connected (see Figure 3.16 right).

Figure 3.15. Standard ITER sample holder with grooves to guide the wire. The black areas in the two centre grooves represent the slots where the wire is not supported.

Figure 3.16. Left: detailed view of the slots under the wire in the two grooves from the barrel with wire. Standard ITER sample holder with slots along two turns in the middle. Right: the ends of the wire sample are soldered to the copper rings and the voltage taps are visible.

The sample is shunted by a parallel superconducting wire by soldering at the transfer point from the barrel to the copper rings for connection to the current leads. The shunts serve to provide better stability along this section. When the electromagnetic force on the windings points inwards to the centre of the barrel, the turns bridging the slots are subjected to bending. The other turns, supported along their entire length, reflect the properties of an unbend wire. The wire is not glued to the barrel and the depth of the groove assures a good guidance of the turns. Strand axial slippage is thought to be marginal due to friction between strand and spiral groove, in the case the Lorentz force is pointing inwards. Voltage taps are attached to the sections subjected to spatial periodic bending and the fully supported and unbend sections. The overall voltage of the sample, including the section soldered to the current leads, is measured as well to monitor the connection quality.

Three barrels were prepared with slot (wave) lengths of 5.0, 7.2 and 9.1 mm, respectively. All samples are heat treated in one batch, no changes in the wire tension is applied after the heat treatment and during soldering of the wire ends to the copper rings. The test is performed with an extended magnet field range starting at 15 T and

subsequently decreasing with steps of 1 T to finally 7 T. First the entire field range (down) with electro-magnetic force pointing inwards was explored and then repeated with the force pointing outwards (starting again at 15 T). A comparison can be made between both experimental methods, TARSIS and BWS, and with the analysis based on uniform bending.

3.4.3 Effect on critical current

3.4.3.1 Low and high transverse resistivity regimes

The prediction of the effect of bending strain on the I_c of a Nb₃Sn strand is firstly described by Ekin [58] in terms of uniform bending (with constant radius) which creates a periodic strain variation along the twisted filaments. Although the simple model does not include the effect of spatial periodic bending, it appears to describe the reduction in performance of a Nb₃Sn strand with low transverse resistivity closely [129]. The bending effect is not only affected by the applied bending strain but also depends on the inter-filament electrical resistivity. The electrical resistance between the twisted filaments determines the current transfer length [111], [112], [113], [114], [115], [116] and [117].

When the wire is bent with a single radius in one direction, the twist of the filaments determines the periodicity in strain variation. In a cable however, the pattern of crossing strands superimposes a cyclic varying strain pattern on top of the global cable twist. Current transfer between filaments therefore depends on the resulting longitudinal strain periodicity, the strand twist pitch and the interfilament resistance. One (unlikely) extreme is that current transfer between the filaments is not allowed either by a very high transverse matrix resistivity or a very short filament twist pitch. In this limiting case for uniform bending follows [58]:

$$I_c = \frac{2A}{\varepsilon_{b0}^2} \int_0^{\varepsilon_{b0}} J_c(B, T, \varepsilon_{th} - \varepsilon) \varepsilon d\varepsilon, \quad [A] \quad (3.8)$$

where A is the cross section area of the strand, ε is the strain in the filaments over a cross section of the strand, ε_{b0} is the peak bending strain in the outer ring of filaments, ε_{th} is the thermal pre-compression of the Nb₃Sn filaments and J_c is the critical current density. If on the other hand current transfer is allowed at low voltage level, the overall I_c of a strand is the sum of the filament currents at any section considering the local strain variation over the section. In this other limiting case, for (long pitch or) low matrix resistivity, which is likely more representative for most practical wires, follows:

$$I_c = \frac{2A}{\pi \varepsilon_{b0}^2} \int_{-\varepsilon_{b0}}^{\varepsilon_{b0}} J_c(B, T, \varepsilon_{th} + \varepsilon) \sqrt{\varepsilon_{th}^2 - \varepsilon^2} d\varepsilon. \quad [A] \quad (3.9)$$

In order to solve the Equations 3.8 and 3.9 one needs to know the axial $I_c(\varepsilon)$ relation, which is obtained experimentally [85]. The shape of the computed curves from Equations 3.8 and 3.9 is primarily driven by the critical current -strain variation of the particular Nb₃Sn strand.

The state of the art $I_c(\epsilon)$ scaling relations do not account for irreversible behaviour in the $I_c(B, T, \epsilon)$ data spectrum at high tensile loads and instead a polynomial fit can be used to account for the additional I_c reduction at high tensile strain due to filament cracks. In Figure 3.23 a polynomial fit to the experimental $I_c(\epsilon)$ data is included for the SMI PIT wire at 12 T and 4.2 K. The data are measured with the PACMAN strain probe using an I_c criterion of 10 $\mu\text{V/m}$. Based on this polynomial $I_c(\epsilon)$ fit, we calculate the bending effect extremes, assuming full or no inter-filament current transfer for the strand I_c range, giving an indication of the periodic bending behaviour.

3.4.3.2 Current transfer length and n -value

In a very simple analytical form, the current transfer length, L_{ct} for low and high strand transverse resistances can be formulated for a certain voltage criterion as:

$$L_{ct} = \left(\frac{0.1}{n}\right)^{0.5} \cdot \left(\frac{\rho_t}{\rho^*}\right)^{0.5} \cdot d_s, \quad [\text{m}] \quad (3.10)$$

in which n is the n -value characterising the VI -transition, ρ_t is the effective transverse matrix resistivity, ρ^* is the resistivity criterion used to define the I_c (at 10 $\mu\text{V/m}$) and d_s is the strand diameter [58]. In this relation the resistivity and the n -value changes during strand deformation due to yielding of the copper stabilizer but it is verified with TARSIS that the effect is only marginal in a bending experiment with a low number of cycles [118].

The longitudinal electric field in a strand E_z is a non-linear function of the current, of the temperature and of the magnetic field. Routinely we assume strands to have a constant n -value obeying a power law fit but for increasing periodic bending strain, the VI curve turns from the relatively higher resistivity to the lower resistivity regime along the VI transition with increasing current and it does not actually follow anymore the power law [129]. This illustrates the deficiency in application of the simple power law approach for the n -value in the interpretation of these bending experiments. Nevertheless, we will use the common formulation of the n -value, reflecting the steepness of the VI transition between 10 and 100 $\mu\text{V/m}$, in order to avoid confusion in discussions with reference to the traditional definition.

The intra-strand transverse resistance is derived from the strand coupling-loss time constant in Equation 1.21. This method of determining the transverse resistance between filaments from the inductive coupling between filaments is considered relevant as also for bending this particular inter-filament coupling is involved in current redistribution. When the current transfer length is calculated with $\rho^*=1.7 \cdot 10^{-14}$ [Ωm] and an n -value of 50, L_{ct} amounts to 4 mm. With a filament twist pitch of 10 mm we expect the low resistivity regime for this PIT strand.

3.4.4 Bending amplitude and strain

The principle of the TARSIS bending probe with point loads and clamped ends is sketched in the left part of Figure 3.17 [129]. The point load F represents the transferred

magnetic point load from the upper crossing strand in a cable or as utilised by the periodic pattern with bulges in the TARSIS bending probe. The bending moment distribution is easily calculated from the theory of straight indeterminate beams that can be found in textbooks.

The corresponding standard (elastic) equation for the deflection (f) in the case of point loads F , with clamped ends and length L (equal to the wavelength L_w) in Figure 3.17, representing the TARSIS test state is given by Equation 3.11:

$$f_{max} = \frac{F \cdot L_w^3}{\alpha \cdot E \cdot I_a}, \quad [\text{m}] \quad (3.11)$$

As in Equation 3.4 in which the term α is 192 for point load, E is the modulus of elasticity and I_a is the momentum of inertia:

$$I_a = \frac{\pi \cdot d_s^4}{64}, \quad [\text{m}^4] \quad (3.12)$$

The peak strain ε is now calculated with

$$\varepsilon = \frac{M_b}{W_b \cdot E}, \quad [\text{m}^4] \quad (3.13)$$

in which M_b represents the bending moment

$$M_b = \frac{F \cdot L_w}{\kappa}, \quad [\text{Nm}] \quad (3.14)$$

while W_b is the section factor

$$M_b = \frac{\pi \cdot d_s^3}{32}, \quad [\text{m}^3] \quad (3.15)$$

The term κ in Equation 3.14, giving the bending moment, depends on the position in the wire. For the principle of the TARSIS bending probe with point load and clamped ends, the value for κ is given 8.

The principle of bending load from distributed electromagnetic force due to the strand currents is schematically represented in the right sketch of Figure 3.17. The distributed load per unit length for the BWS method is $q=B \cdot I$ with B the applied magnet field and I the strand current. The distributed load along the slot between the two changeovers to the supporting grooves in the barrel, is then Q (in N), with $Q=q \cdot L_w$.

The corresponding standard equation for the deflection (f) in the case of distributed load Q , with clamped ends and length L_w in Figure 3.17, representing the BWS test condition is given by Equation 3.11 with $\alpha=384$ and distributed load Q instead of point load F . The deflection of the wire in the centre of the slot is clearly less than for a point load due to the higher α . For this distributed load, the value for β is 24 in the centre of the slot and 12 at the changeover from support to slot. As a result the peak bending strain at the changeover is higher than in the centre of the slot. For calculation of the peak bending strain we choose the lower value for β and in order to compensate for non-ideal clamping at the changeover, where the strand may be slightly lifted from the barrel, we choose $\beta=14$.

Figure 3.17. Left the schematic view of point load (TARSIS bending probe) and at right the distributed load (BWS method) with $L=L_w$.

The axial tensile stress-strain curve is determined previously with TARSIS on the SMI-PIT wire at 4.2 K and the result is plotted in Figure 2.20. The characteristic is used to calculate the deflection of the wire in the bending tests for the given wavelengths. As the linear region of the stress-strain curve for this wire is only linear for very low stress, the overall stress-strain behaviour can only be described accurately by using elasto-plastic properties. For simplicity in computation, but to model the strand axial elasto-plastic behaviour, the modulus E is now approximated by two linear regimes. In the elastic region below an axial strain of 0.028% the modulus is taken 100 GPa while for $\epsilon > 0.028\%$, assuming continuous yielding, the modulus is taken 39 GPa. For a first order approach, the deformation during cool-down from heat treatment temperature to 4.2 K is neglected just as the shift of the neutral axis during bending.

These simplified elasto-plastic model conditions were used previously to predict the load-deflection curves for the TARSIS bending probe on the same strand type, showing good agreement with the experimental data (see section 2.1).

For the BWS method, the peak bending strain is calculated for the applied magnetic fields and three bending wavelengths, taking into account the strand axial tensile stress-strain characteristics from Figure 2.20 and Equation 3.13, and the results are shown in Figure 3.18.

Figure 3.18. Peak bending strain in the filamentary region of the SMI-PIT strand at I_c ($10 \mu V/m$) for three bending wavelengths (listed in the legend) versus applied magnetic field.

3.4.5 Comparison of bending methods

Figure 3.19 illustrates the effect of bending on the critical current ($I_{c,b}$ bend section divided by I_{c0} supported section) for different I_c criteria, 1, 10 and 100 $\mu\text{V/m}$. I_{c0} is defined as the critical current without applied bending load while $I_{c,b}$ is the critical current with applied bending load. For a Lorentz force directed inward (regular current direction) we expect indeed a reduction in performance for lower magnetic field, i.e. higher bending force, see Figure 3.18. The initial lowest electromagnetic load at 15 T already leads to a performance reduction of 2%.

Figure 3.19. Normalized I_c for $E_c=1, 10$ and $100\text{ }\mu\text{V/m}$ versus applied field for regular current direction (F-in is representing the Lorentz force inwards) and reversed current direction (F-out) on turns surpassing slots (bend) and fully supported, wavelength 5.0 mm.

A lower electric field criterion is linked to a higher sensitivity for bending. Initially, at low bending strain the $I_{c,b}/I_{c,0}$ ratio is practically similar for 1, 10 and $100\text{ }\mu\text{V/m}$ while the difference in reduction becomes more pronounced with increasing force. When the current is reversed, starting again at highest applied magnetic field (lowest force), the $I_{c,b}/I_{c,0}$ ratio is about 0.995 for both higher E_c criteria while for $1\text{ }\mu\text{V/m}$ a small reduction gradually develops (the measurements were done after the ones with force inwards).

In Figure 3.20 the reduced I_c is shown versus the applied peak bending strain in the filamentary region for the three E_c criteria. In the same graph the computed curves are presented based on Equations 3.8 and 3.9 for low and high strand transverse resistivity and uniform bending. The computation of the curves is based on the measured I_c versus axial strain characteristic for an electric field criterion of $10\text{ }\mu\text{V/m}$ and the deviatoric strain scaling introduced in section 2.2.4.

Although the tendency of the measured data seems not to intersect unity at zero bending strain, the overall performance corresponds clearly to a low transverse resistivity behaviour. The results for the three wavelengths are collected in Figure 3.21 where the scale on the horizontal axis represents the $B \times l$ or Lorentz force with the unit N/m.

Figure 3.20. Normalized I_c versus peak bending strain from turns surpassing slots and entirely supported. The calculated reduced I_c for low and high transverse resistivity at $E_c=10 \mu\text{V/m}$ criterion are presented for comparison, the wavelength is 5.0 mm.

Figure 3.21. Normalized I_c versus applied Lorentz force from turns surpassing the slots at wavelengths of 5.0, 7.2 and 9.1 mm.

The measured data, as summarized in Figure 3.21, illustrate plainly a larger reduction in performance with longer bending wavelength for increasing applied load or bending strain. The data show that the sensitivity to bending becomes significant above an applied load of 1,500 N/m.

One of the main objectives of the BWS test is to make a direct comparison to the results obtained with the TARSIS bending probes. This is well illustrated in Figure 3.22 where the data obtained at 12 T are collected from the TARSIS bending probe and the BWS method. The direct comparison shows excellent agreement between both experimental methods.

Figure 3.22. Normalized I_c versus bending strain for the barrels with slots at wavelengths of 5.0, 7.2 and 9.1 mm, compared to the data obtained on TARSIS bending probes with four wavelengths, all for 4.2 K, 12 T.

3.5 Axial stiffness and periodic bending

3.5.1 Introduction

A bending strain affects the critical current density of the filaments because of the varying strain in axial direction of the twisted filaments and in the cross section of the filament bundle. This instigates a decrease of the critical current and a broadening of the voltage-current transition (i.e. a drop in the n -value). The eventual bending strain response depends on the internal strand layout in terms of strain sensitivity of the Nb_3Sn material, diameter of the filamentary region, twist pitch length and internal resistances, determining the possible amount of current transfer between the filaments [50].

As the stress, originated from differential thermal contraction and electromagnetic load, is the driving factor for the strand deflection and thus final strain distribution, the axial and transverse stiffness of the strand (and cable) play a key role in the final performance. What is more, the stress-strain characteristic varies with the strand processing method and the chemical composition (see section 2.1). Therefore, it is essential to know the axial stress-strain characteristic to enable an accurate description of the mechanical response to applied bending loads.

In chapter 3.3 the first results are presented of the probe for periodical strand bending with cyclic loading using different bending wavelengths applied to a powder in tube processed wire together with a detailed description of the setup and the bending probe [129]. The results from the TARSIS bending probe were performed using different bending wavelengths from 3.8 mm to 10 mm. The current transfer length, important for the evaluation of the ability for inter-filamentary current redistribution, was evaluated from AC loss measurements and found in agreement with the so-called low -

interfilament - resistivity model. After an independent confirmation of this result using a standard ITER I_c test barrel with spatial periodic slots in the centre grooves, two other strand types were tested on the TARSIS bending probe.

A bronze processed strand (indicated as VAC) used in the inner turns of the solenoid with highest transverse load is taken from the Central Solenoid Model Coil (CSMC) [34] production. The second strand is an internal tin processed wire (indicated as LMI) and is taken from the Toroidal Field Model Coil (TFMC) production [124]. All strands were tested on the TARSIS bending probe using bending wavelengths from 5 to 10 mm. The current transfer length is again assessed from AC loss measurements. The experimental data are presented in terms of applied load versus bending deflection and I_c and n -value versus applied bending strain. Eventually the performance of the strands is compared for bending deflection and bending load, illustrating the important role of the axial tensile stress-strain characteristic in transverse load degradation of CICC's.

3.5.2 Bending strain and critical current

The electrical resistivity of the matrix in the filamentary area determines the ability to redistribute current between the filaments and determines the current transfer length in relation to the filament twist pitch and the peak strain periodicity [122]. For a wire bent with a single radius in one direction, the twist pitch of the filaments determines the periodicity in strain variation. In a cable however, the pattern of crossing strands superimposes a cyclic varying strain pattern on top of the global cable twist. The wavelengths in this periodic pattern will vary with the chosen twist pitches. The current transfer between filaments therefore depends on both the resulting longitudinal strain periodicity and the interfilament resistivity.

In order to solve the relations for high and low transverse resistivity as introduced in chapter 3.4.3.1, one needs to know the critical current versus axial strain relation, $I_c(\epsilon)$, which is obtained experimentally. The $I_c(\epsilon)$ at 4.2 K and 12 T are taken from earlier measurements [130, 131]. The results for the three different strands using an I_c criterion of 10 $\mu\text{V/m}$ are depicted in Figure 3.23. Showing the three curves for the different strand types in one graph makes it immediately clear that the relative sensitivity to applied axial strain is substantially different with the highest sensitivity for the powder in tube type and lowest for the bronze type of strands investigated here.

Based on the $I_c(\epsilon)$ relation, thermal pre-compression and strand geometry, we can assess the bending effect assuming full or no inter-filament current transfer for the strand I_c range. An example, based on the powder in tube type of strand from [131] (see also Figure 3.23), is in Figure 3.24 for an axial thermal cool down strain of the Nb_3Sn filaments of -0.14%. In addition, the curves are calculated for an axial pre-compression of -0.53%, closely approximating the thermal contraction in the presence of a steel conduit [123]. The spread between full current transfer and no current transfer grows as the bending strain increases. Notice that there is no reduction in I_c for full current transfer at -0.53% axial pre-strain with increasing bending strain up to 1%.

Figure 3.23. Normalized critical current versus intrinsic axial strain for the LMI, VAC and SMI strands. The data provided by Durham are indicated in the legend by DU and the data measured at Twente with UT.

Figure 3.24. Effect of full and no inter-filament current transfer for bending strain on strand critical current with and without axial pre-compression (e in the legend represents the intrinsic axial strain of the Nb_3Sn filaments in the neutral plane while LIFR/HIFR represents the matrix resistivity limit [58]).

The I_c versus applied bending strain is calculated within the measured range of applied axial strain. We choose to use the measured axial $I_c(\epsilon)$ characteristics from Figure 3.23 to compute the I_c versus bending strain instead of using an $I_c(\epsilon)$ scaling relation in order to include the irreversible tensile strain effect beyond the maximum in the curves in Figure 3.23.

In this manner the prediction of the I_c versus bending strain already comprises the effect of irreversible degradation at higher tensile load instead of overestimating the I_c for extreme levels of bending strain. For this reason, the missing parts of the $I_c(\epsilon)$ curves were extrapolated to zero I_c . In the compressive regime, we followed virtually the shape of the VAC wire for both other strands while at the tensile regime we mimicked the SMI wire behaviour from the experimental data displayed in Figure 3.23.

3.5.3 Samples of differently processed strand

The two strands, which were applied in the model coils, are described in [43, 70, 133], the main characteristics are listed in Table 3.1. The internal tin type of superconducting wire is produced by the former Euro Metalli Superconductors LMI (Italy) and is used for the manufacture of the TFMC. The strand contains 36 sub-elements (Figure 3.25, left) each enclosing 150 filaments in hexagonal groups of 3. The sub-elements are surrounded by a double Sn diffusion barrier (Nb and Ta). Due to the hexagonal configuration of the filamentary sub-elements, the outer diameter of the filamentary region varies between 0.65 mm and 0.70 mm. For calculation of the peak bending strain, we simply use the average of 0.67 mm.

Figure 3.25. Photographs of the cross-section of the LMI internal tin, VAC bronze and SMI-powder in tube processed strands (from left to right).

The bronze processed strand is manufactured by the former Vacuumschmelze (Germany, presently Bruker European Advanced Superconductors) and is used for the manufacture of the inner layers of the CSMC. The cross section layout is shown in Figure 3.25, middle). It is configured as a single element with thick barrier (10 – 15 μm Ta) surrounding 55 sub-groups of 83 filaments. The diameter of the filamentary region is 0.48 mm.

The Nb_3Sn PIT processed strand is manufactured by the earlier Shape Metal Innovation (Netherlands). A photograph of the strand cross section is shown in Figure 3.25 (right). The outer diameter of the filamentary region with 504 filaments is 0.66 mm. After the reaction heat treatment, the hollow Nb_3Sn filaments with ring shaped cross section are surrounded by the remaining non-reacted Nb tubes. These Nb tubes have a round inner wall and a hexagonal shape outside. Inside the hollow filaments, residual components from the reaction are left.

The wires are wound on the cylindrical TARSIS sample holder for bending prior to the heat treatment with an axial pre-stress of 25 MPa. A slight transverse pre-load on the cap, before the heat treatment, is to guarantee contact with all load points and to avoid settling alterations during the initial loads of the bending test.

The Cr plating was removed before mounting the sample. A 100 μm thick glass-mica tape was used as a spacer between holder and wire during the heat treatment for two reasons. The tape serves to prevent sintering between strand and cylindrical holder. In

addition, the spacing created by the glass-mica tape after its removal reduces friction between strand and holder due to thermal pre-strain when cooling the probe to 4.2 K. The spacer is removed after the heat treatment and before the bending test.

3.5.4 Effect of stiffness on strand bending

3.5.4.1 Current transfer length

From the interstrand coupling loss time constant, the current transfer length can be derived. The AC loss measurements are carried out in liquid helium in an atmospheric pressure bath. The time varying magnetic field with an applied triangular shape with amplitude B_a of 3 T around the zero baseline, is applied perpendicular to the specimen axis [133]. From the values obtained for $n\tau$ and the filament twist pitch L_p , we derive the transverse resistivity ρ_t using Equation 1.21. Then as a first order approach, the current transfer length L_{ct} is assessed with ρ^* and the n -value using Equation 3.10. Table 3.1 summarises the main properties of the three strands used in the experiments with in the last row the estimated current transfer length.

Table 3.1. Properties related to the current transfer length of the three strand types.

Strand ID	LMI	VAC	SMI
Strand production method	internal tin	bronze	PIT
Diameter [mm]	0.806	0.803	0.810
Twist pitch, L_p [mm]	9.9	8.8	10
Cu:nonCu ratio	1.4	1.5	0.74
Coupling loss time constant, $n\tau$ [ms]	6	0.6	16
Twist pitch, L_p [mm]	9.9	8.8	10
RRR (R273K / R20K)	80	150	250
n -value 12 T, 4.2 K [-]	27	13	50
I_c , 12 T, 4.2 K [A]	153	113	300
ρ_t 4.2 K [Ωm]	$5.3 \cdot 10^{-10}$	$40 \cdot 10^{-10}$	$2.0 \cdot 10^{-10}$
ρ^* [Ωm]	$3.4 \cdot 10^{-14}$	$4.6 \cdot 10^{-14}$	$1.7 \cdot 10^{-14}$
Current transfer length, L_{ct} [mm]	6	21	4

3.5.4.2 Critical current and n -value

The measurements of the critical current are performed at a temperature of 4.2 K and a transverse applied magnetic field of 12 T. The voltage-current traces (VI) at various applied bending loads are recorded from an electric field level of 1 $\mu\text{V/m}$ up to at least 100 $\mu\text{V/m}$. The critical currents, taken at an electric field of 10 $\mu\text{V/m}$, are normalized to the I_{c0} in the virgin (unloaded) condition of the strand for display in Figure 3.26, 27, 28 and 29. The average I_{c0} in the virgin state for the LMI, VAC and SMI strand samples

are 147, 120 and 313 A, respectively. Excluded are the data taken with released load for tracing the load level at irreversible degradation for clarity.

The level of irreversibility itself is often found in the range of 0.3% to 0.4% applied peak bending strain although when the load on the bending probe is released, the deflection of the wire remains irreversible due to plastic deformation. Thus, it is not clear whether the associated irreversible I_c degradation is caused by the permanent plastic strand deformation or if filament damage is already involved.

This is illustrated in Figure 3.26 where the reduction in I_c has become irreversible after having exceeded a strain value of 0.50%. The load is released but the deflection remains with a peak bending strain value of 0.45% and an I_c reduction of a few percent. The permanent reduction of the n -value is larger, as shown in Figure 3.27. For the same travel in deflection, the n -value is reduced by 20%.

Figure 3.26. Normalized critical current versus applied peak bending strain for a bending wavelength L_w of 8.3 mm with subsequent intermediate release of the load.

Figure 3.27. n -Value versus applied peak bending strain for a bending wavelength L_w of 8.3 mm with subsequent intermediate release of the load.

Figure 3.28 shows the curves of the measured reduced I_c data for the three tested strands. In agreement with Figure 3.23 it is found that the SMI strand is most sensitive to bending strain, closely followed by the LMI strand and finally, the least impact is observed for the VAC strand.

Figure 3.28. Normalized critical current measured versus applied peak bending strain of the three tested strands.

In Figure 3.29 the normalized I_c data are compared for the three strand types obtained on the probe with 10 mm wavelength, but now versus bending amplitude. The normalized I_c versus the measured bending amplitude understandably shows the same tendency as found for the sensitivity to axial and bending strain with again the best performance for the VAC strand. Moreover, the relative impact of bending is even less severe for the VAC strand with respect to the data in Figure 3.28 because the diameter of the filamentary region is considerably smaller for the VAC strand (see Table 3.1). This leads to a smaller peak bending strain with respect to both other strand types at the same deflection.

Figure 3.29. Measured normalized critical current versus applied bending deflection for the three tested strands at a wavelength of 10 mm.

With the thermal and electromagnetic stress ultimately as the driving parameter for transverse load degradation, the normalized I_c versus applied load is a comprehensible way for getting a fair impression of the strand's performance. The suggestion arising from Figure 3.28 and Figure 3.29 can be that the bronze strand gives the best relative performance compared to both other strand types with the internal tin strand as second best.

In Figure 3.30 though, showing the normalized critical current as function of applied force load, we find the opposite result with the lowermost performance for the soft bronze strand, being the one most sensitive to bending load. The internal tin and powder in tube strands both perform significantly better. This emphasises the importance of the strand stiffness in evaluation of transverse load effects on CICC's.

Figure 3.30. Measured normalized critical current versus applied bending load for the three tested strands.

The n -value for a bending wavelength of 10 mm is shown as a function of the applied peak bending strain in Figure 3.31 for the LMI, VAC and SMI strands. The n -value is determined between 10 and 100 $\mu\text{V/m}$ and becomes less defined at higher load due to the increasing interfilament current redistribution changing the shape of the voltage current transition. This may cause some unbalance in the direct comparison of different strand types. It appears that for a peak bending strain level of 0.5% the n -value decreases by about 50% for the VAC wire and 20% for the SMI wire, while no reduction is observed for the LMI strand. Reaching a peak bending strain of 1%, the total decrease of the n -value is 50% for the LMI wire while the VAC and SMI wires both show 20% reduction.

Also for the n -value, considered a good indicator for degradation, it is interesting to plot the data against the applied load instead of the peak bending strain in one graph (see Figure 3.32). The difference in performance of the strands is again more pronounced due to the difference in strand axial stiffness.

Figure 3.31. n -Value versus applied bending strain for the LMI, VAC and SMI strands.

Figure 3.32. Normalized n -value versus applied bending load for the three tested strands.

3.5.4.3 Strand stiffness and coil performance

The single strand properties in terms of the composite's stiffness in axial and transverse direction and the change of the I_c and n -value under various transverse load conditions appear to be crucial design input parameters for CICC's. Notice that at least for the strands tested here, we may conclude that only one wavelength seems sufficient for testing. Moreover, only knowing the I_c versus axial strain in combination with the LIFR relation would then already be good for a first order bending strain characterisation, as the internal resistivity mostly seems sufficiently low. It needs to be verified whether this holds more generally for other industrial superconducting wires used in ITER as well. The current transfer length derived from the coupling loss time constant would already provide a first indication.

However, it is well illustrated by Figure 3.30 and Figure 3.32, showing the normalized critical current and n -value versus the applied load, that for evaluation of transverse load degradation in large CICC's not only the critical current versus axial strain (see Figure 3.23) and bending strain (see Figure 3.30) are required, but knowledge of the strand stiffness is necessary as well.

When assuming that bending is the dominant mechanism for transverse load degradation in Nb₃Sn CICC's, we can make a footnote on the relative coil versus single strand performance of the CSMC (inner layer) manufactured with VAC strand and the TFMC with LMI strand. Other effects possibly introduced by current non-uniformity, differences in cabling pattern and interstrand contact resistance are not considered at this stage.

Hence, for the established I_c criterion of 10 μ V/m, a better strand versus coil performance is expected for the TFMC compared to the CSMC. Figure 3.32 illustrates this for the steepness of the transition represented by the n -value, the strand to coil performance should be better for the TFMC than for the CSMC as well. These results are supported by analysis of the model coil data, showing that the effect of the so-called $B \times I$ load, representing the transverse load on the conductor, is more severe for the CSMC than for the TFMC [31, 32, 37, 74, 134, 135].

3.6 Bending and axial compression

3.6.1 Introduction

Thus far it is experimentally observed that periodic bending load response not only depends on the internal strand layout in terms of twist pitch, filament spread and barriers, spatial load periodicity and internal resistances, but also on the $I_c(\epsilon)$ strain sensitivity and mechanical stiffness. In the previous section, the current transfer length, important for the evaluation of the ability for inter-filamentary current redistribution, was evaluated from AC loss measurements and showed behaviour close to the so-called low - interfilament - resistivity model for uniform bending. However, to this point all the experiments were performed on strands exhibiting only the thermal pre-compression of the composite wire on the TARSIS and BWS barrel probes.

The conduit of an ITER CICC, with a considerably larger coefficient of thermal expansion than the cable bundle, transfers a significant fraction of its contraction to the cable bundle. This thermal cable contraction is mostly transferred into axial compression and periodic bending of the strands. The effect of this thermal axial pre-compression combined with bending, is not well explored and from an experimental point of view only the strand-in-tube option was considered [131, 136]. Insertion of Nb₃Sn strand in a steel tube with adequate wall thickness is a convenient method to explore experimentally the effect of thermal contraction in combination with periodic bending on strand performance.

Here, the experimental data are presented in terms of reduced I_c versus applied bending strain and AC loss, obtained on a powder-in-tube and bronze type of strand (see Table 3.1) with and without a tight fitting SS tube [131].

3.6.2 Assessment of bending strain effect on I_c

The I_c reduction for high and low transverse interfilament matrix resistivity is based on the critical current versus axial strain relation, $I_c(\epsilon)$ taken from Figure 3.23 and the relations from section 3.4.3.1. The relative sensitivity to axial strain is different with the highest sensitivity for the powder in tube type and lowest effect for the bronze type of strand.

Based on the $I_c(\epsilon)$ relation, thermal pre-compression and strand geometry, the bending effect can be assessed assuming full or no inter-filament current transfer for the strand peak bending strain range. An example, based on the powder in tube type of strand is given in Figure 3.24 for an axial thermal cool down strain on the Nb_3Sn filaments of -0.14% (for the bronze strand the compression is close to -0.30%). In addition, the curves were calculated for an axial pre-compression of -0.53% found for a PIT strand in a SS tube [131], approximating the situation with a steel conduit.

3.6.3 Bronze and internal tin strands

The bronze processed strand, manufactured by Vacuumschmelze, is used for the manufacture of the inner layers of the CSMC. The Nb_3Sn PIT processed strand is manufactured by Shape Metal Innovation (NL). Photographs of the bare strand cross sections are depicted in Figure 2.19. Strand sections were swaged tightly into a SS (AISA-304) tube. The dimension of the tube is chosen such that the ratio of SS and superconducting strand in the cross section is close to the ratio of a full-size ITER CICC (strand bundle vs. square conduit). The tube has an initial outer diameter of 1.30 mm and an inner diameter of 0.90 mm. After swaging, the strand diameter and inner tube diameter became 0.78 mm, calculated on the basis of the elongation. The final outer tube measured diameter was 1.21 mm. The volumetric ratio of strand to SS conduit is 0.71. The strand to conduit ratio for a CS1 type of CICC is about 0.58 and for a CS2 type it is 0.41.

3.6.4 Testing of compressed strands

Measurement of the I_c is performed at 4.2 K and 12 T. The voltage current traces at various applied bending loads are recorded from 1 $\mu V/m$ up to at least 100 $\mu V/m$. Two SMI samples were tested with wavelengths of 8.34 and 10.0 mm while for the VAC sample only 10.0 mm was used. The reduced I_c versus the applied bending deflection for two bending wavelengths is shown in Figure 3.33 for the SMI strand. For a certain deflection, the reduction in I_c is larger for a shorter wavelength. The normalized I_c versus applied peak bending strain for selected bending wavelengths is shown in Figure

3.34 and Figure 3.35 for the SMI and VAC strand, respectively, together with the calculated curves for low and high inter filament resistivity. The plots clearly show that the bare strands follow the simulation with low interstrand resistivity for both strand types. For extreme levels of applied bending strain exceeding 1% however, the I_c data turn below the simulation. This is because the measured $I_c(\epsilon)$ for axial strain does not cover the entire bending strain range since filament damage will occur at this elevated strain level. For pre-compression with the SS tube the performance is clearly below the result predicted by the simple simulation.

The SMI strand is slightly more sensitive to bending strain than the VAC strand, with and without tube, as expected from the axial $I_c(\epsilon)$ data. Note that the bending strain dependency for both wavelengths applied to the SMI strand is similar, confirming the reproducibility of the experimental method and the limited influence of the bending wavelength.

Figure 3.36 and Figure 3.37 show the n -value versus applied peak bending strain for the bare strand and with axial pre-compression, i.e. with and without a SS tube for the SMI and VAC strand, respectively. The n -value shows the same tendency as the I_c under influence of axial compression, with a severe reduction towards higher bending strain. The n -value is about a factor two lower under compression and reduces to extreme low values when approaching 1% bending strain.

In turn, to exclude that an increase of the matrix resistivity, due to mechanical deformation could be partly responsible for a decrease in the reduced performance of the jacketed strand compared to the simulation, AC loss measurements are carried out at 4.2 K on a bare and jacketed sample. The time varying magnetic field, with a triangular field sweep of ± 3 T around zero, is applied perpendicular to the specimen axis. The results presented in Figure 3.38, confirm that the coupling loss remains similar and so the interfilament resistivity is not changed due to jacketing. A volume correction was applied for the reduction in diameter after swaging the strand in the SS tube.

Figure 3.33. Normalized I_c versus measured deflection with and without axial pre-compression, i.e. with and without a SS tube, for the SMI strand.

Figure 3.34. Normalized I_c versus applied peak bending strain for bare strand and with axial pre-compression, i.e. with and without a SS tube for the SMI strand.

Figure 3.35. Normalized I_c versus applied peak bending strain of the measured data and calculations for bare strand and with axial pre-compression, i.e. with and without a SS tube for the VAC strand.

Figure 3.36. n-Value versus applied peak bending strain from the measured deflection for bare strand and with axial pre-compression, i.e. with and without a SS tube for the SMI strand.

Figure 3.37. *n*-Value versus applied peak bending strain from the measured deflection for bare strand and with axial pre-compression, i.e. with and without a SS tube for the VAC strand.

Figure 3.38. Total loss versus frequency of the applied field for the PIT type of wire with and without (w/o) SS tube (the lower line is from data without volume correction applied for the smaller diameter due to the compaction with tube).

The low interfilament resistivity limit describes adequately the reduction in critical current versus applied peak bending strain for the uncovered strands, while for a strand jacketed in a steel tube the calculation is by far too optimistic. The method of applying axial pre-compression with a steel tube, combined with periodic bending, reveals that the simple relation for uniform bending from 3.4.3.1 is not appropriate for analysis of the behaviour of strands under periodic bending, especially when under axial compression. A more fundamental approach is required taking into account the geometrical complex structure of bending wavelength, twist pitch length, strain sensitivity and internal resistance distribution. For this a new strand model is being build. Until the new strand model that will include the strand resistance distribution becomes available, an empirical scaling can be used based on the experimental results obtained with TARSIS on strands in SS tube.

3.7 Conclusion

The change in the voltage-current characteristics of a Nb₃Sn PIT strand is measured under periodic bending with wavelengths of 3.8, 5.0, 7.2, 8.3 and 10.0 mm using the TARSIS probe for bending, especially developed for studying strand performance under ITER CICC conditions. For strands manufactured with different processing techniques, it is found that the critical current reduction versus applied bending strain is similar for all applied wavelengths and equivalent to the so-called low interfilament resistance model, for the current transfer length of the strands varying from 4 to 21 mm.

The method is able to quantify the reduction in critical current and n -value of Nb₃Sn strands when applied to transverse strand loading creating a periodic bending pattern. The level of irreversibility is often found in the range of 0.3 to 0.4% applied peak bending strain. The cyclic behaviour in terms of critical current and n -value plainly shows a component representing a permanent reduction and a factor, which is a reversible function of the applied load. The permanent reduction is associated with plastic deformation, yielding of the soft copper stabilizer, causing a permanent change of the strain state of the filaments, while the reversible component is linked to the elastic deformation of the strand. An irreversible reduction of the critical current and n -value does not necessarily indicate filament damage. However, the same behaviour will occur in a CICC and has to be taken into account for understanding of its performance.

The stiffness of the strand representing the axial stress-strain response is crucial for spatial periodic bending and thus for transverse load degradation that is observed in large CICCs. Based on the results it is expected to find a better strand versus coil performance for the TFMC compared to the CSMC inner layer, which is in agreement with analysis of the model coil results presented by others.

The deflection in the TARSIS bending probe is a result of strand bending accompanied with transverse compressive load on the loading points. For the shortest bending wavelength of 3.8 mm, not only the bending deflection is important but also the transverse stress at the loading points is causing a reduction in performance, likely due to the relative dimensions of contact load area, wavelength and strand diameter.

The results of the TARSIS bending probe were validated satisfactory against a simple and straightforward experimental method to verify the effect of bending due to electromagnetic load on Nb₃Sn strands.

The effect of spatial periodic bending on the critical current, in combination with thermal contraction imposed by a stainless steel tube swaged around the strand, is measured on a bronze and powder-in-tube processed Nb₃Sn strand. For bare strands in general, an impact for bending is found on the critical current that resembles to a simple relation proposed for low interfilament electrical resistance. For strands in a steel tube causing a substantial thermal contraction, the simulation based on purely axial strain is by far too optimistic. AC loss measurements confirmed that the interfilament resistivity

is similar for strands with and without steel tube. This means that the difference between strand with and without a steel tube cannot be attributed to changes in the current transfer length. The geometrical complex structure of bending wavelength, twist pitch length, strain sensitivity and internal resistance distribution asks for a new effort in building a dedicated numerical strand model. Until a new strand model taking into account the strand resistance distribution is available, an empirical scaling can be used based on the experimental results obtained with TARSIS on strand in stainless steel tube.

4 Transverse compressive contact stress on strands

Local contact stress is one of the specific loads that frequently occur on strands in CICC. In particular for conductors with a large number of strand layers, the stress can become significant. For experimental investigation of the effect, a TARSIS testing arrangement was developed for generating a periodic contact load on crossing Nb₃Sn wire sections. In addition, the setup is made suitable for application of a homogeneous load along 125 mm. This way it is possible to evaluate differences related to the spatial distribution of stress and the effect on current sharing.

The effect of a spatial periodic loading by crossing strands and a homogeneously distributed load on the transport properties is compared for a bronze and an internal tin ITER TF Nb₃Sn strand. The irreversible stress differs for both load distributions and strand type, varying between 20 and 60 MPa. Remarkably, it is observed that the absolute value of the stress determines the reduction in critical current and n-value. The spatial distribution of the load for the tested configurations seems not relevant for the performance degradation, whether a crossing strand periodicity of 4.7 mm is taken or a homogeneous spreading. This conduct is in line with the results found for periodic bending with different bending wavelengths, illustrating that the current transfer length is in the range of load periodicity.

Analogous to what is observed for bending, the cyclic behaviour for critical current and n-value involves a component representing a permanent reduction as well as a factor expressing reversible (elastic) behaviour as a function of the applied load.

The content of this chapter is for a large part based on papers [54], [153], [155] and [207].

4.1 Introduction

The number of studies regarding the impact of a local transverse load at Nb₃Sn strand crossings is limited since the first papers appeared on this subject [51, 57, 137, 138], although for accelerator type of conductors efforts were made [139, 140]. More recent work concerning homogenous stress on Nb₃Sn can be found in [141, 142, 143]. Transverse stress can be quite severe at one side of the cable cross section, particularly in the case of large ITER type of CICC's depending on the cabling pattern. The accumulated load can reach local peak values up to about 100 MPa [144]. The aim of this work is to quantify the effect of transverse stress and in particular investigate the influence of a spatially periodic stress distribution. This may lead to a better understanding of how interfilament current sharing affects the average strand electric field and its distribution [117, 122].

For this reason and in analogy with the work on TARSIS type periodic bending, a probe was developed for measuring the effect of local contact loads from identical crossing superconducting Nb₃Sn strands. The probe is part of the TARSIS set-up for strand stress-strain characterization. With a small modification of the probe it is suitable to study the different impact of a homogeneous transverse contact load between two parallel planes and perpendicularly crossing and pinching strands. The design of the rig is presented and characteristic results, demonstrating the influence of homogeneous and periodic applied transverse contact load are reported.

4.2 Crossing strands test rig

4.2.1 Design requirements and peak load

For the specification of the device, the extreme loading on strands in the ITER Model Coil CCICs are adopted as a reference. The accumulated peak load at one side of the cable exceeds by far the distributed Lorentz force caused by the current in a strand itself. In a typical CICC exposed to a magnetic field B of 12 T, a current I_s of 43 A per strand, this load F is written as:

$$F = I_s \cdot B, \quad [\text{N/m}] \quad (4.1)$$

and amounts to 520 N/m. Since strands are not continuously supported but at intervals, this force causes bending. The average periodicity (wavelength L_w) for crossing strands (and bending) is determined for a full-size ITER CS1 Model Coil conductor and amounts to 6 mm with a standard deviation of 2 mm (see Figure 1.16). However, the local accumulated peak load in an ITER conductor with 1152 strands is 18 kN/m, assuming that the number of strand layers can be approximated by the square root of the total number of strands N in a cable [144]:

$$F = I_s \cdot B \cdot N^{0.5}. \quad [\text{N/m}] \quad (4.2)$$

The periodicity in the number of strand crossings is relatively high for the CS1 conductor with an average wavelength of 6 mm. The first cabling stage triplet has a twist pitch of 24 mm and the second stage quadruplet has a pitch of 50 mm. In the CS2 type of conductor, see Figure 1.16 (right), with much longer twist pitches of about 60 mm and 90 mm, respectively, a less frequent periodicity is found. Also a much smaller crossing angle is observed in the CS2 than the average 45 degrees in the CS1 type of conductor.

The CS1 conductor with 1152 superconducting strands, is used for the inner windings of the model coil with highest magnetic field while the CS2 conductor, exhibiting 700 superconducting strands and 350 copper strands, is used for the outer windings in lower magnetic field.

Obviously this implies a dependency between cable twist pitches and transverse load distribution along the strands. Most favourable is a uniform transverse load distribution, representing a line contact between parallel strands minimising the peak stress. In this case, the transverse peak stress, $\sigma_{\max}$, is simply the local peak load of 18 kN/m working on the projected area A_c of the strand cross section d_s taken for a unit length l of 1 m:

$$\sigma_{\text{hom}} = \frac{I_s \cdot B \cdot N^{0.5}}{d_s \cdot l}. \quad [\text{Pa}] \quad (4.3)$$

The peak transverse stress in this homogeneous case, σ_{hom} , is about 20 MPa. As soon as there locally is sufficient space for strand bending, the load at the crossovers will exceed this value, as A_c becomes smaller than in the case of an infinite line contact. The contact area of two crossing strands depends on the angle between the wires, φ , and the strand diameter:

$$A_c = \frac{d_s^2}{\sin \varphi}. \quad [\text{m}^2] \quad (4.4)$$

The local transverse stress is multiplied with a contact area correction factor k accounting for the spatial periodicity

$$k = \frac{d_s \cdot L_w}{A_c} = \frac{L_w \cdot \sin \varphi}{d_s}. \quad [-] \quad (4.5)$$

The local peak stress is then:

$$\sigma_{\max} = \frac{\sigma_{\text{hom}} \cdot L_w \cdot \sin \varphi}{d_s}, \quad [\text{Pa}] \quad (4.6)$$

which can be re-written as

$$\sigma_{\max} = \frac{I_s \cdot B \cdot N^{0.5} \cdot L_w \cdot \sin \varphi}{d_s^2}. \quad [\text{Pa}] \quad (4.7)$$

For example, when the periodicity of $L_w=6$ mm is adopted for the CS1 conductor with a strand diameter of 0.81 mm, the local peak stress can reach a level of 110 MPa.

4.2.2 Test rig design

The design of the first version of the periodical contact stress rig is depicted in Figure 4.1 and a photograph with wire sample and crossing strands (but without cap) is shown in Figure 4.2 (left).

Figure 4.1. Overall view of the X-strings test rig with the axis fixed to the flex sheets to guarantee a centred position.

Figure 4.2. Photograph of the initial X-strings rig with test wire (soldered voltage taps are visible) and crossing wires (left) and the improved rig with additional turn (right).

The wire runs along the circumference of a cylindrical plate with an outer diameter of 47 mm and is entirely supported by the flange of the outer barrel. The cylindrical support disc has radial grooves on top from centre to the outer perimeter to guide the crossing strand sections. This support plate with grooves is coupled to the outer barrel by means of flex sheets mounted to the central axis. A cap having a flat bottom surface is placed on top, transferring the load from the crossing strands between cap and support plate with grooves to the circular wire sample. The combination cap, crossing strands and round plate with grooves is suspended with flexible-sheet-bearings, allowing only for a vertical movement with minimum friction.

In Figure 4.2 (right) the modification for the additional turn is visible, minimizing the current transfer effect near the points where the sample leaves the circular plane in the loading section. In Figure 4.2 (right) it is clearly shown how both sample ends leave the (disc) plane with the crossing strands, to the current leads, fixed in position by a sleeve and six screws. The load on the crossing strands is applied by moving the thrust bar in the direction of the sample. The thrust bar providing the required linear displacement, is driven by the Precision Vertical Linear Stage described in section 2.1 [54, 129].

At three positions, at zero, 120 and 240 degree along the sample perimeter, a few crossing strands are left out, starting at the location of the parallel current leads, to assure a balanced load. The sample wire is supported by the outer wall of the barrel and further confined between the crossing strands and flange of the barrel. The Lorentz force on the sample, instigated by its transport current, is pointing radially inwards to guarantee controlled axial strain conditions. An important issue is the thermal pre-strain condition of the reacted sample, thus sample manipulation has to be avoided. For this reason, the complete setup is made of TiAlV-alloy and used for both reaction and measurement thereby avoiding sample transfer.

Special attention is paid to an accurate measurement of the wire deflection. For the deformation measurement, the extensometers also used for the bending rig are inserted in the three slots of the X-strands barrel. The slots for the three extensometers (only one slot is visible in Figure 4.2) are distributed evenly along the circumference of the barrel. The extenso meters are designed for application in all TARSIS probes. When the thrust bar is loaded thereby applying a contact load to the crossing wires, the extensometer detects a deflection similar to the change in axial position between cap and main barrel, which equals the total deformation of the crossing strands and wire sample together.

The current sharing voltage per unit length E is measured by means of three pairs of voltage taps along the loaded strand section, corresponding to the location of the extensometers. In this way the homogeneity of the load is measured as well. The distance between a pair of voltage taps is about 20 mm, always related to an integer times the applied loading wavelength. The spatial periodicity of the crossing strands is determined by the angle between the radial grooves in the round plate. Here we choose for a spatial periodicity (wavelength) of 4.7 mm.

The Nb₃Sn PIT strand manufactured by SMI, similar to the one taken for the initial bending tests in section 3.3 [129], is also taken for the first X-strand test. Its diameter is 0.81 mm, it has 504 filaments and a twist pitch of 10 mm. The outer diameter of the filamentary region is 0.66 mm. A photograph of the strand cross section is depicted in Figure 3.25.

The sample to be tested is mounted on the TiAlV-alloy holder as shown in Figure 4.2 and heat-treated with a cap, mounted as described above. The wire is wound on the cylinder prior to the heat treatment with an axial pre-stress of 25 MPa. To minimise the unavoidable remaining misalignment between strand and contact points, the cap is mounted with a low pre-tension, assuring that the wire is in contact with all the contact load points prior to its heat treatment reaction. After reaction the wire is tension free but still in contact with all crossing strands.

After the reaction heat treatment of the PIT wire, the hollow Nb₃Sn filaments (with ring shaped cross section) are surrounded by the remaining non-reacted Nb tubes. These Nb tubes have a round inner wall and a hexagonal shape outside. Inside the hollow filaments, residual components are left. The outer diameter of the hexagonal Nb tubes is 27 μm , the outer diameter of the Nb₃Sn hollow filaments is 20 μm and the outer diameter of the filament core with reaction residuals is 14 μm . After heat treatment, the voltage taps are soldered to the sample.

4.3 Strands crossing test

4.3.1 Critical current and n -value

Measurements are performed at 4.2 K and 11 and 12 T. The self-field in combination with applied field varies almost 1 T over the cross section of a full-size ITER conductor. The voltage-current traces at various contact loads are recorded for an electric field level between 1 and 100 $\mu\text{V/m}$.

The critical current versus applied load for a periodicity of 4.7 mm is presented in Figure 4.3. The load is increased stepwise and fully released twice, at 10 and 20 kN/m (only at 12 T), in order to check for permanent degradation. The reduction in I_c has become irreversibly degraded by several percent after loading with 10 kN/m, which corresponds to a load of about 50 N per cross-over, an overall strain (diameter deformation) of 8% and a transverse stress at the load point of 70 MPa (projected strand crossing area). After having reached a stress level of about 20 kN/m the permanent I_c degradation at zero load then amounts to 20%. After a load of 20 kN/m, the reduction in critical current versus load per meter between zero and peak load seems to develop practically linearly.

The different susceptibility to the applied stress for 11 and 12 T is illustrated in Figure 4.4. The critical current under load, determined at an electric field of 10 $\mu\text{V/m}$, is

normalized to the I_{c0} in the virgin condition of the strand. The stress is simply calculated for the projected strand crossing area. The qualitative behaviour observed in Figure 4.4 is in agreement with the expectation that the effect of strain increases with higher magnetic field level.

Figure 4.3. Critical current versus applied load per meter at 4.2 K and 11 and 12 T, with released load at 10 and 20 kN/m.

Figure 4.4. Normalized critical current versus applied stress (strand projected area) at 4.2 K and for 11 and 12 T.

An increase in the inter-filament current transfer and thus a higher voltage per meter results in a lower n -value. n -Values are normally determined from the EI curves in the electric field range of 10 to 100 $\mu\text{V/m}$ using the power law in Equation 3.8. The EI characteristic, as the current increases, gradually travels from no to full current transfer and the characteristic strongly depends on the local strain distribution and interfilament resistivity. Similarly to what was observed for periodic bending in section 3.3 for increasing strain variations, also for periodic contact stress the EI curves do not perfectly follow the power law at 10 to 100 $\mu\text{V/m}$ level. This is illustrated by the EI

curves, each at different load in Figure 4.5, the slope of the curves changes with increasing load and in the low electric field range also the shape.

The n -value for 12 T is shown as a function of the applied stress in Figure 4.6. It is striking to see that the n -value decreases rapidly with increasing stress and at for example 10 kN/m (70 MPa), where the I_c has degraded with 30%, the n -value has already declined from more than 50 in the virgin state to about 15.

Figure 4.5. Examples of measured electric field versus current curves for loads in the zero to 18 kN/m range at 4.2 K and 12 T.

Figure 4.6. n -Value versus applied load per meter at the strand crossings at 4.2 K and 12 T applied field.

4.3.2 Stress-strain behaviour of crossing strands

The transverse modulus of elasticity of reacted Nb_3Sn strands is not well known and varies with strand processing method, strand composition and applied loading profile

(plane parallel or spherical). In order to understand the performance of cabled conductors and the effect of deformation caused by thermal or mechanical loading, it is essential to measure the axial and transversal stress-strain characteristics of the particular strand. Only with this characteristic, it is possible to perform reasonable accurate computation of the mechanical response to applied loads, as the stiffness of the strand, in combination with the sensitivity to strain of the Nb₃Sn layer, will determine the current carrying performance under load [120, 145].

The axial tensile stress-strain curve, determined with TARSIS for the SMI-PIT wire at 4.2 K, is shown in Figure 2.20 [120]. The axial E modulus amounts to 70 GPa ($\pm 30\%$) when determined from the initial slope of the load line with a relatively large spread between samples. When determined with linear regression from the unloading lines a value of 100 GPa ($\pm 10\%$) is found. Linear regression is also applied to the almost linear yielding profile of the curve above about 0.2% strain. Here a ‘dynamic’ elastic modulus or ‘yielding modulus’ ($\delta E/\delta \epsilon$) is found of 40 GPa ($\pm 10\%$).

The transverse compressive stress-strain characteristic, determined with the probe for crossing strands, is shown in Figure 4.7 for two samples. In reality, the stress strongly depends on the real contact area, which swells gradually with increasing load. The transverse (dynamic) modulus of elasticity E can be defined for two distinct regimes. Up to 2% strain, corresponding to a strand deformation of almost 50 μm , the modulus is very low but for stress higher than 50 MPa, with continuous yielding, the E modulus is around 3 GPa while the dynamic modulus amounts to 4.2 GPa. The large deformation at relatively low stress below 50 MPa is likely due to the increasing contact area starting from a point contact towards a progressively growing circular contact area.

Another reason can be the uncertainty in the zero-deformation condition during the initial experiments, which has been improved in later tests. A precise measurement of the zero deflection strain appears very challenging but is crucial to resolve. An important further improvement in the understanding of the cable mechanical behavior is the initial strand deformation at the strand crossings due to cabling and compaction during conductor manufacturing. This will likely affect the initial part of the stress-strain curve as seen in Figure 4.7 and causes an earlier rise of the stress at lower strain. It can be expected that cabling and compaction will already cause a deformation of the strand leading to an increase of the initial contact surface. Probably a higher level of stress is required to reach the same initial deformation as compared to strands with a perfect circular cross section. This could be tested in the test rig for crossing strands by pre-loading virgin strand samples, performing the heat treatment and consequently carrying out the standard test as presented here.

Therefore the most representative value for the strand overall transverse modulus of elasticity in a CICC type of cable is expected close to the dynamic modulus. The plastic deformation at the strand crossing of a sample after the test is clearly visible in Figure 4.8, starting from essentially a point contact towards an almost circular imprint.

Figure 4.7. Transverse compressive stress-strain curve of an SMI-PIT sample measured with the TARSIS crossing-strands test rig at 4.2 K.

Figure 4.8. Imprint at the strand surface from an SMI-PIT sample after a transverse compressive crossing-strands test with the TARSIS rig at 4.2 K.

The simple first order approach of taking the projected strand crossing area is determining significantly the shape of the curves in Figure 4.7. The deformation of the strand at the crossings is already substantial at low load due to the very small initial contact area, which rapidly extends from a point contact to a circular imprint with increasing load. The final shape of this imprint is shown in Figure 4.8 after a peak load of 30 kN/m.

4.4 Spatial periodic and homogeneous transverse stress

4.4.1 Test rig

Photographs of the two versions of the transverse contact stress test rig with wire sample, crossing strands and homogeneous load ring and cap are shown in Figure 4.9. The wire runs along the circumference of a cylindrical plate with an outer diameter of 47 mm and is entirely supported by the flange of the outer barrel.

Figure 4.9. Photographs of the X-strands test rig with sample (soldered voltage taps are visible) and crossing wires (left); the same rig now adapted for uniform pressure (center), and the set-up with the top plate mounted (right).

The cylindrical support plate has radial grooves on top from centre to the outer perimeter to guide the crossing strand sections.

A cap having a flat bottom is placed on top, transferring the load from the crossing strands between cap and support plate with grooves to the circular sample wire. For application of a uniform load, a cap with a flat plane loading area replaces the disc with radial grooves and crossing strands thus creating a clamping load between two parallel planes. The two Nb₃Sn samples, one bronze (BR) produced by JASTEC (Japan) and one internal tin (IT) type manufactured by KAT (Korea), were taken from the ITER TF production. The diameter of both strands is 0.82 mm. The wire is wound on the cylinder prior to the heat treatment with an axial pre-stress of 25 MPa, which is mimicking the axial tension during cable manufacturing. The sample is mounted on the sample holder with a light transverse pre-load before heat treatment to guarantee contact with the load points and avoid settling alterations during the initial load of the test. The heat treatments are as specified by ITER IO. The distance between the voltage taps is 23 mm for the X-strands rig and 20 mm in the case of the uniform stress loading. The filament twist pitch is about 15 mm. For reference, the reduced critical current versus the intrinsic axial strain for the BR and IT strands, measured with the PACMAN test rig are shown in Figure 4.10. A higher strain sensitivity for the IT strand is observed.

Figure 4.10. Normalized critical current versus intrinsic axial strain at 4.2 K and 12 T for the bronze route (BR) and internal tin (IT) strands showing higher strain sensitivity for the IT strand.

4.4.2 Transverse stiffness of strands

The measured deflection for uniform load and crossing-strands on the BR and IT strands are compared in Figure 4.11. The applied load is determined from the applied force per meter. For estimating the transverse Young's modulus E , the strain is calculated as the strand deflection divided by the strand diameter of 0.82 mm and the stress is determined using the load per meter and strand projected area subjected to the load (contact length and strand diameter). The maximum absolute error in the deflection measurement is $\pm 3 \mu\text{m}$ and for load per meter $\pm 5\%$. Taking these approximations, the stiffness of the strands at higher uniform stress amounts to 1.2 for BR and 1.5 GPa for IT, respectively. The dynamic modulus $\delta\sigma/\delta\epsilon$ at higher stress level is about 2 GPa for both BR and IT.

Figure 4.11. Measured deflection versus applied force per meter for a uniform and a crossing-strands load on the BR and IT strands.

For crossing-strands, the stiffness of the strands peaks at a stress of 20 MPa to about 4.0 GPa and then saturates to 2.5 GPa for BR. For IT it gradually increases to 2.5 GPa towards a transverse stress of 90 MPa. The dynamic modulus $\delta\sigma/\delta\epsilon$ at higher stress is about 3.0 to 4.0 GPa for both BR and IT. These values are in the same range as found for the PIT strand in section 4.3.2.

For contact load per meter, the deflection obviously increases faster for crossing-strands case than for uniform load but the behaviour is significantly different for BR and IT wires, as illustrated in Figure 4.11. Especially the curve found for BR crossing-strands appears extraordinary. The range of deflection for applied stress is similar for uniform load and periodic crossing strands (see Figure 4.12), also compared to the results gathered in Figure 5.34.

For uniform stress the bronze route strand is softer than the internal tin type but for crossing strands this is opposite. The reason for this discrepancy is not clear although it can be noted that for uniform load, the strand is clamped between two plane surfaces with high Young's modulus, while in the case of crossing strands two strands are

pressed against each other. Further detailed mechanical modeling of strand and investigation of material properties is required to resolve this.

Figure 4.12. Measured deflection versus applied stress for a uniform and a crossing-strands load on the BR and IT strands.

4.4.3 Transport properties and stress distribution

For the PIT strand it was shown in Figure 4.5 that the E - I characteristic, as the current increases, gradually changes to reflect no to full current transfer. It can be understood that the characteristic strongly depends on the local strain distribution and interfilament resistivity. This is also illustrated for the IT strand by a few selected V - I curves measured at different loads in Figure 4.13. It is clear that not only the slope of the curves changes with increasing load but also the shape.

Figure 4.13. Examples of measured electric field versus current curves for an internal tin wire in virgin state at a stress level of 100 MPa, and subsequent release to zero at 4.2 K and 12 T.

The critical current versus applied load for a periodicity of 4.7 mm in the crossing strands rig is presented in Figure 4.14. The critical currents under load, taken at an

electric field of $10 \mu\text{V/m}$, are normalized to the I_{c0} in the virgin (unloaded) condition of the strand for display. The load is increased stepwise with intermediate release to zero in order to assess the stress at which irreversibility appears. The applied stress is calculated as the projected cross sectional area. So for crossing strands the area is taken as the force per strand crossing, divided by the square of the strand diameter. For a uniform load the stress is taken as the force divided by the strand diameter times the loaded length.

Figure 4.14. Normalized critical current for the BR and IT strands versus the applied contact stress in the crossing-strands test. All data taken at 4.2 K and 12 T.

The evolution of the n -value with increasing stress is depicted in Figure 4.15. The critical current and n -value versus the applied load for uniform load are presented in Figure 4.16 and Figure 4.17. The BR-wire is clearly less susceptible than the IT-wire, to both transverse crossing-strand and uniform stresses at higher load levels. However, for crossing-strands the irreversibility for both IT- and BR-wires starts to occur just below 20 MPa.

Figure 4.15. Normalized n -value of the BR and IT strands versus applied contact stress in the crossing-strands test. All data taken at 4.2 K and 12 T.

Figure 4.16. Normalized critical current for the BR and IT strands versus applied contact stress for a uniform load. All data taken at 4.2 K and 12 T.

Figure 4.17. Normalized n -value for the BR and IT strands versus applied contact stress for a uniform load. All data taken at 4.2 K and 12 T.

For uniform load an increase of the I_c with applied load is found for the BR-wire up to 30 MPa. This is likely attributed to stress release in the strand matrix, while the IT-wire shows only a gradual decrease of the I_c . The irreversibility stress for uniform load (best represented by irreversible degradation of the n -value) is not observed for the BR-wire while for the IT-wire it is starting already before reaching 20 MPa.

In the figures, the reduction in I_c and n -value are plot versus the applied stress in the contact zone. When the reduction is shown of the I_c versus force per unit length subjected to the load, then the sensitivity is much higher for crossing-strands as the load is concentrated in the contact points giving rise to a high local stress (see Figure 4.18, a similar tendency is observed for BR wire). For uniform load the applied force is constant along the length and its impact is understandably much less. Obviously this reflects the different conditions of wires in a CICC with long or short twist pitches.

Figure 4.18. Normalized I_c versus applied load per meter strand (IT) for crossing-strands and a uniform load. All data taken at 12 T and 4.2 K.

Figure 4.19. Normalized I_c versus applied contact stress in internal tin wire for crossing-strands and a uniform load. All data taken at 12 T and 4.2 K.

Figure 4.20. Normalized I_c versus applied contact stress in bronze route wire for crossing-strands and a uniform load. All data taken at 12 T and 4.2 K.

Figure 4.21. Normalized n -value versus applied contact stress in bronze route wire for crossing-strands and a uniform load. All data taken at 12 T and 4.2 K.

Figure 4.22. Normalized n -value versus applied contact stress in internal tin wire for crossing-strands and a uniform load. All data taken at 12 T and 4.2 K.

When the influences of local periodic and uniform stress are compared, the reduction in I_c shows the same tendency for both stress distributions (see Figure 4.19). Similar tendencies are observed for BR wire (Figure 4.20) and the evolution of the n -value (Figure 4.21 and Figure 4.22).

4.4.4 Periodic point versus uniform loads

The comparison reveals that the absolute value of the stress determines the reduction in I_c and n -value and the spatial distribution appears not relevant at least under the conditions the measurements were performed here; a crossing-strand periodicity of 4.7 mm versus uniform distribution. In fact this is in agreement with the results found for periodic bending with different bending wavelengths. For spatial periodic bending of three Nb₃Sn strands, manufactured with different processing techniques, it was found that the critical current reduction versus applied bending strain was similar for all

applied wavelengths of 5.0, 7.2, 8.3 and 10.0 mm (see section 3.3) [129, 145]. Also here the bending periodicity is less critical but the applied peak bending strain is dominant.

The next step in this study is to test a spatial periodicity with larger spacing between the crossing strands and to evaluate the required length for interfilament current redistribution in order to reach a lower average electric field along the wires. This will be evaluated with the strand model and the on going studies on intrastrand resistance distribution, twist pitch, current transfer length and position of voltage taps [113, 122]. The interstrand current transfer length in CICC's depends on the loading history and void fraction but is expected orders of magnitude larger than inside strands with relatively long interstrand current transfer lengths.

4.5 Conclusion

The effects on the transport properties of force loading cycles periodically applied on Nb₃Sn strands is experimentally investigated with a TARSIS test rig, equipped for direct measurement of the wire mechanical compression and applied force. For a Nb₃Sn PIT processed wire, exposed to contact pressure every 4.7 mm along the strand, a significant reduction is observed in the critical current and n -value in the range of stress expected in charged ITER CICC's. The critical current has degraded irreversibly somewhat before reaching a contact stress of 70 MPa. The transverse stiffness of the strand increases with further deformation progressing with a dynamic elasticity modulus $\delta\sigma/\delta\varepsilon$ of 4.2 GPa. The permanent reduction in critical current and n -value is associated with plastic deformation of the soft copper stabilizer, causing a permanent change in the strain state of the filaments or even cracks at higher strain, while the reversible component is linked to elastic deformation.

The effects of spatially periodic loading on the transport properties with crossing strands and uniform load between two parallel planes are compared for a bronze route and internal tin ITER TF Nb₃Sn strand.

The bronze route wire is clearly less sensitive than the internal tin wire, to both transverse crossing-strand and uniform stresses at higher load levels. However, for crossing-strands irreversibility for both bronze route and internal tin wire occurs already below 20 MPa. The irreversibility stress level for uniform load is not observed for bronze route wire while for internal tin wire it is reached just below 20 MPa. The sensitivity to irreversible behaviour clearly depends on the wire processing method.

When estimating the transverse Young's modulus E , the strain is taken as the wire deflection divided by the strand diameter of 0.82 mm. Using these approximations, the stiffness of the strands at higher uniform stress amounts to 1.2 GPa for the bronze route wire and 1.5 GPa for the internal tin wire. The dynamic modulus $\delta\sigma/\delta\varepsilon$ at higher stress level is about 2 GPa for both bronze route and internal tin wires.

For crossing-strands, the local area stiffness of the wires peaks at 20 MPa to about 4 GPa and then saturates to 2.5 GPa for the bronze route wire tested. For the internal tin wire it gradually increases to 2.5 GPa at 90 MPa. The dynamic modulus $\delta\sigma/\delta\varepsilon$ at higher stress is about 3 to 4 GPa for both bronze route and internal tin wires.

Interestingly, it is observed that the absolute value of the stress determines the reductions in I_c and n -value. The spatial distribution seems hardly relevant for the performance degradation, whether a crossing strand periodicity of 4.7 mm is taken or a uniform distribution. This seems in agreement with similar test results of Nb_3Sn wires exposed to periodic bending with different bending wavelengths.

For the tests described in this section, only one internal tin and one bronze route wire were used. The specific behaviour observed for the transverse stiffness may therefore be not representative for wires with dissimilar design or from different manufacturers. However, the independency of the transport properties with regard to the local stress distribution, periodic or uniform, seems consistent and therefore likely correlated with the current transfer length of the wires.

5 Qualification load tests on ITER Nb₃Sn strands

In the previous chapters it was demonstrated that there is a substantial variation in the strain sensitivity of Nb₃Sn strands manufactured by various companies using different processing techniques to distinct mechanical loads anticipated in CICC's. For ITER, a number of companies are involved in strand procurement. All strand types need to be tested, not only regarding their axial $I_c(B, T, \epsilon)$ strain scaling using PACMAN, but also for transverse loading in the TARSIS rig. The starting point of the characterization is measurement of the critical current as function of axial compressive and tensile strain for representative values of magnetic field and temperature. This to determine the strain sensitivity and the irreversibility limit corresponding to the initiation of cracks in the Nb₃Sn filaments. Next the influence of spatial periodic bending and contact load's is evaluated by using a period of 5 mm. The strand's axial tensile stress-strain characteristic is measured for comparison of the axial stiffness of the strands. Cyclic loading is applied for transverse loads following the evolution of critical current, n -value and deformation. This involves exploring the components representing a permanent (plastic) change and reversible (elastic) behaviour as a function of the applied load.

The experimental results enable discrimination in performance reduction per specific load type and per strand type, being in general different for each supplier. Metallographic filament fracture studies are carried out for comparison with electromagnetic and mechanical test results.

The content of this chapter is for a large part based on paper [118].

5.1 Introduction

Voltage-current characteristics are studied on strands in virgin state and during cyclic loading. For specifications of the setup, the most extreme loading conditions on strands in the ITER conductors are taken as a reference. It is recognized that there can be a significant influence of thermal pre-compression caused by the high coefficient of thermal expansion of the conduit with respect to the cable bundle. This thermal precompression is not contemporary in TARSIS bending and crossing-strands tests. The effect is discussed in chapter 3.6 and testing with less axial compression than present in the actual CICC is considered a conservative methodology. For determining the influence of axial compressive and tensile strain for magnet field and temperature, the PACMAN spring is used. The results obtained on most ITER Toroidal Field (TF) and a couple of Central Solenoid (CS) Nb₃Sn wires are reported and discussed.

The dissimilar behaviour of different wire types cannot be explained in a quantitative manner by the strand measurements only. It is recognized that internal strand geometry and properties have to be taken into account as well.

Reported here are the axial and transverse strand loading tests including results obtained on a standard ITER I_c barrel. The impact of strain sensitivity and inter filament resistivity on strand bending is illustrated and later on more precisely evaluated with a dedicated strand model [122]. The analysis of filament fracture observed in post-mortem TARSIS and full-size ITER samples carried out at the Applied Superconductivity Center at FSU NHMFL (USA), are used for the strand model and appears to be very effective for quantifying the influence of crack distributions on the final CICC performance.

The data of the ITER Nb₃Sn samples presented here were measured on seven TF strand types, actually being used in ITER and produced by five Domestic Agencies [146, 147]. For comparison one CS type of strand [148] and one CS-Insert strand type [34] were added. The strands have a Cu:nonCu ratio of 1.0, except the CS-Insert strand, which has a Cu:nonCu of 1.5. All TF strands have a diameter of 0.82 mm, while the CS strand has a diameter of 0.83 mm.

The diameter of the filamentary zone (see Figure 1.8), used to assess the peak bending strain, is taken from [149] except for the BR4 and both CS strands. As pointed out before, there are two main processes to produce Nb₃Sn strands: bronze route (BR) and internal tin (IT). As illustrated in Figure 1.8, BR and IT wires have different layout, and this will partly determine the electromagnetic and mechanical properties. The bronze route wires have finer filaments in comparison to IT. After heat treatment, IT wires exhibit larger voids and significantly more frequent sintering of filaments.

The Cr-layer is removed from the wire before mounting on the probe by etching with HCl (38%) to allow soldering of current leads and voltage taps. The samples are listed in Table 5.1. In some of the figures presented other sample results are added for reference to indicate possible variations between samples taken from different billets but from the same supplier. This is done for the BR1 and IT2 strands and when indicated with (2), the sample is from earlier productions.

Table 5.1. List of bronze route (BR) and internal tin (IT) wires tested, their designation in TF or CS coil and the intrinsic strain determined for the barrel critical current test.

ID	coil	diam. fil. area [μm]	ε -barrel [%]
BR1	TF	534	-0.175
BR2	TF	547	-0.185
BR3	TF	553	-0.197
BR4	TF	n/a	-0.176
IT1	TF	553	-0.090
IT2	TF	560	-0.140
IT3	TF	547	-0.174
BR5	CS	n/a	-0.197
BR6	CS-Insert	n/a	-0.191

5.2 Test results

5.2.1 Critical current measured on ITER barrels

Wires were tested on standard I_c barrels (see Figure 3.15 and Figure 3.16) with $E_c=10 \mu\text{V/m}$ for the I_c and the n -value determined between 10 and 100 $\mu\text{V/m}$, using the power law representation $E=E_c(I/I_c)^n$. The EI curves were measured in the 10 to 14 T range following the standard V-tap configuration (three pairs) covering five turns on the barrel. The results are gathered in Figure 5.1 and Figure 5.2. The I_c at 12 T and 4.2 K is significantly above 200 A except for the BR4 and BR6 wires. The n -value at 12 T is for all wires 30 or higher.

After the I_c -strain measurements and parametric scaling of the results, the intrinsic strain (ε -barrel) of the Nb_3Sn filaments can be resolved and is also listed in Table 5.1. The strain stays within a narrow window ranging from -0.197% to -0.175% for the BR wires with an average of -0.19%. For the IT wires the spread is larger and varies between -0.174% and -0.090% with an average of -0.13 %. Note that part of the spread in the strain may be introduced by handling during sample preparation, mostly after heat treatment, although controlled procedures are followed.

The additional samples BR1b and IT2b show that for IT wires sometimes a relatively large spread between billets can be found in I_c while BR wire seems less variable.

Figure 5.1 Critical current I_c versus applied magnetic field from 10 to 14 T at 4.2 K, measured on standard ITER I_c TiAlV barrels. The I_c is determined for a criterion of 10 $\mu\text{V/m}$.

Figure 5.2. n -Value versus applied magnetic field from 10 to 14 T at 4.2 K, measured on standard ITER I_c TiAlV barrels. The n -value is determined according the usual power law relation between 10 and 100 $\mu\text{V/m}$.

5.2.2 Critical current versus axial strain

For most samples tested regarding critical current as function of axial strain $I_c(B, T, \epsilon)$ using the PACMAN, at least 70 EI curves at various values of applied strain values (-0.9% to +0.4%), temperature (4.2 to 12 K) and magnetic field (4 to 14 T) were recorded. A characteristic result is presented in Figure 5.3.

The variable B and T measurements at constant strain are done at -0.6% and -0.1% (zero applied) strain. After taking all data in the reversible compressive regime, the tensile strain irreversibility limit ϵ_{irr} is determined by stepwise increase of the tensile strain and release to zero applied strain as shown in Figure 5.4.

Figure 5.3. Critical current I_c versus intrinsic strain of a Nb_3Sn strand with one $I_c(\epsilon)$ curve at 12 T and 4.2 K, a set of data for $I_c(B, T)$ at -0.6% compressive strain and at zero applied strain, near the peak in $I_c(\epsilon)$.

Figure 5.4. Critical current I_c versus intrinsic strain at 12 T and 4.2 K, with stepwise increase of the tensile strain and subsequent release to zero applied strain for determination of the strain irreversibility limit.

The ϵ_{irr} is defined as the strain level where the n -value shows an irreversible decrease, since the n -value is a very sensitive indicator for permanent degradation caused by filament cracks. This has been shown earlier by the combination of I_c -strain measurements and TARSIS axial tensile stress-strain testing with metallographic filament fracture analysis [14, 15, 104, 105]. An increase of I_c or n -value is observed in some cases but this is considered caused by a change in the plastic strain state of the stabilizer. The procedure for determining the ϵ_{irr} is clarified in Figure 5.5, where the knee (crossing of two linear fits) of the match to the n -value data (being most sensitive to detecting crack initiation, see section 2.3) indicates ϵ_{irr} . In this example +0.64 % intrinsic strain is ϵ_{irr} . Note that for I_c the knee is at higher tensile strain, which means that the 10 $\mu V/m$ criterion is not suitable for definition of ϵ_{irr} in relation to crack initiation [15].

Figure 5.5. Critical current I_c and n -value versus intrinsic strain at 12 T and 4.2 K, with stepwise increase of the tensile strain and subsequent release to zero applied strain for determination of the strain irreversibility limit. The knee of the fit to the n -value data indicates the irreversibility limit.

The irreversibility strain limits of the wires derived from the n -value are collected in Figure 5.6. All BR wires exhibit a limit above 0.45% while for IT wires scattering between zero and 0.4% is observed.

The average axial strain of the strands in a full-size ITER TF or CS conductor is expected to be about -0.4 to -0.5% [73, 150]. This means that the strain sensitivity in this compressive range is relevant for the performance of the conductors. Therefore a strain sensitivity ϵ_{sen} of the wires is defined by applying linear interpolation of the data in the compressive strain window between -0.7 and -0.3% in the figure as for example shown in Figure 5.3. This is performed for normalized I_c , thus giving a relative comparison in $\%^{-1}$. The susceptibility for all strands is grouped in Figure 5.7. It is concluded that the susceptibility of IT wires is higher than of BR wires, although the BR6 wire (earlier developed in the nineties) also exhibits a higher sensitivity.

Importantly, wire performance is often judged based on the I_c barrel performance but when in compression, as in a CICC with steel jacket, the performance in compression must be taken into account as well. Then the apparent advantage of a much higher I_c measured on a barrel, with only minor compression, can be strongly alleviated by a higher strain sensitivity. An example is given in Figure 5.8, the plot of I_c versus intrinsic strain of a BR- and an IT wire at 12 T and 4.2 K, illustrate that at zero applied strain, the difference in I_c is significant (25%) but in compression the performances converge (at $\epsilon = -0.5\%$ the difference is only 8%). The additional samples BR1b and IT2b show that for the irreversibility limit there can be some spread among IT samples, while in general ϵ_{irr} is quite consistent for BR wires. The strain sensitivity shows only very little variation from sample to sample. This is confirmed by broader studies not yet published. All experimental data below ϵ_{irr} were used to determine the scaling parameters for the ITER strain description, which are listed for all strands in Table 5.2.

Figure 5.6. Irreversibility strain limit of the wires derived following the n -value method. The IT3 wire is close to zero. The BR3 test was stopped at reaching 0.5% and the irreversibility limit is higher but not measured.

Figure 5.7. Strain sensitivity of the strands determined by the linearly interpolated slope of the normalized I_c in the compressive strain window between -0.7 and -0.3% strain.

Figure 5.8. Critical current I_c versus intrinsic strain of a BR and an IT wire at 12 T and 4.2 K, showing that at zero applied strain, the difference in I_c near the peak is significant, but in compression the performances converge.

Table 5.2. ITER strain scaling parameters as determined in year 2008 for the tested wires, BR5 and BR6 were not tested with the Twente PACMAN.

Parameter	IT1	IT2 ⁺	IT3	BR1	BR2	BR3	BR4
C_{a1}	47.52	44.24	44.97	45.46	49.62	49.62	50.63
C_{a2}	0.00	0.00	0.00	6.52	9.62	9.51	11.30
$\varepsilon_{o,a}$, [%]	0.218	0.166	0.284	0.244	0.217	0.259	0.290
ε_m , [%]	-0.067	-0.059	-0.087	-0.017	-0.078	-0.110	-0.047
$\mu_0 H_{c2m}(0)$, [T]	34.22	30.59	33.79	30.23	31.05	29.52	30.08
$T_{cm}(0)$, [K]	16.26	15.99	16.10	16.73	16.38	16.62	15.66
C_1 , [AT]	20,823	21,374	21,500	17,036	17,823	15,249	8,936
p	0.578	0.54	0.54	0.559	0.560	0.519	0.387
q	2.211	1.88	2.10	1.754	1.939	1.662	1.315
Deviation [A]*	1.9	3.0	2.1	1.0	1.0	1.2	0.9
ε_{sen} [% ⁻¹]	1.029	1.027	0.961	0.861	0.931	0.889	0.797
ε_{irr} [%]	0.30	0.06	0.00	0.65	0.48	0.50	0.46

* Average deviation between measured and scaled I_c .

⁺ The IT2 parameters differ from the ITER report in the ITER-IDM since a correction was applied to the p and q to approach more realistic values although the minimum least squares is slightly better for the IDM reported parameterization.

5.2.3 Stress-strain behaviour in axial tensile regime

Tensile stress-strain measurements were performed at 4.2 K and the results are collected in Figure 5.9. A relatively large spread is found in the strain values at a constant stress level for the different wire layouts. The spread in measured strain at an applied stress level of 150 MPa is outlined in Figure 5.10. Although the stress level of 150 MPa is somewhat arbitrary, it shows that the strain can vary by a factor of 1.6 (1.8 when also taking into account the CS-Insert strand).

The strain for all BR wires (orange bars) is higher than in the IT wires (blue bars). On average the BR wires exhibit about 0.2% higher strain at 150 MPa. Since the impact of thermal and electromagnetic loads in the ITER CICC are stress driven, the observed differences are relevant for their transport properties.

The strain level at which the wires collapse is shown in Figure 5.11. The BR wires break at higher strain values than IT wires and the difference can even reach 0.5% strain. For the stress level at which the wire breaks (see Figure 5.12) a similar tendency is found with higher levels of breaking stress for the BR wires. The low stress level of the BR6 wire (CS-Insert) is explained by the larger fraction of copper and less Nb₃Sn in the wire cross section.

Figure 5.9. Stress versus applied strain of the Nb_3Sn wires tested at 4.2 K.

Figure 5.10. Change of strain at an applied stress of 150 MPa at 4.2 K.

Figure 5.11. Strain level at which wire samples collapse.

Figure 5.12. Stress level at which wire samples collapse.

5.2.4 Effect of periodic bending

The bending tests were performed with the improved TARSIS test rig with 5 mm period (see Figure 3.4) at a temperature of 4.2 K and an applied magnetic field of 12 T. The voltage current traces taken at various applied bending loads are recorded from $1 \mu\text{V/m}$ up to at least $100 \mu\text{V/m}$. The critical currents are determined at an electric field of $10 \mu\text{V/m}$. The critical current is normalized to the I_{c0} in the virgin condition (unloaded) condition of the wire.

Typical examples of a full set of EI curves from zero load in virgin condition to full bending load at 10 kN/m are shown in Figure 5.13 and Figure 5.14.

It illustrates the shift from a nearly perfect power law behaviour in the virgin condition towards a distorted current sharing regime for increasing bending load, depending on the wire's internal transverse and longitudinal resistances. The curves in Figure 5.13 are from the IT3 wire and the ones in Figure 5.14 from the BR2 wire (logarithmic-linear scale). The local peak load in an ITER TF conductor is about 20 kN/m but this is for one side of the conductor with accumulated load of all strands [129].

The average deflection per strand in a CICC under full load is about $25 \mu\text{m}$ for a void fraction of 30%, including all types of deformation and not only bending [125]. For bending a peak load of 20 kN/m is less relevant as the deflection is limited by the option of strands to deflect, which is to some extent restricted by the presence of neighbouring strands and void fraction. Therefore a peak load of 10 kN/m was selected as a reference though the real average load level for bending may be even less.

A characteristic plot of the normalized critical current versus the applied peak bending load is presented in Figure 5.15. The load is increased stepwise and released a number of times with the intention to assess the first load level causing permanent reduction.

Figure 5.13. Example of electric field versus current for various applied bending loads from zero to 10 kN/m from the IT3 wire (log-lin scale) at 12 T and 4.2 K.

The reduction in I_c for bending with a 5 mm period becomes irreversible beyond about 1.5 kN/m (with some scatter depending on the strand type), which corresponds to a load of 11 N/node and a peak bending strain of almost 0.3%. Another way to look at the reduction of the I_c is to plot it against applied peak bending strain, which is done in Figure 5.16.

When exceeding the permanent reduction in I_c , the peak bending strain does not return to zero anymore after release of the force. The wire has reached a permanent plastic deformation, sustaining a strain distribution in the filament bundle. This does not mean that filaments are damaged as elaborated further on, but still an irreversible reduction of the strand I_c of 10% to 20% in unloaded state can be reached. At a load of 6 kN/m this corresponds to an I_c reduction of 20 to 30%.

For the n -value we find similar behaviour as for the I_c , shown in Figure 5.17, although the reduction with applied load is less gradual than observed for I_c . The irreversible reduction in n -value found at about 1.5 kN/m, confirms a permanent change in the transport properties [122].

Figure 5.14. Example of electric field versus current for various applied bending loads up to 10 kN/m from the BR2 wire (log-lin scale) at 12 T and 4.2 K.

Figure 5.15. Example of normalized critical current versus applied peak bending load at 12 T and 4.2 K with stepwise increase of the load followed by 10,000 cycles between almost zero and a peak load of 10 kN/m (wire BR5).

Figure 5.16. Example of normalized critical current versus applied peak bending strain at 12 T and 4.2 K with stepwise increase of the bending deflection followed by 10,000 cycles between almost zero and a peak load of 10 kN/m (wire BR5).

Figure 5.17. Example of n -value versus applied peak bending load at 12 T and 4.2 K with stepwise increase of the load followed by 10,000 cycles between almost zero and a peak load of 10 kN/m (wire BR5).

The cycling between almost zero and peak load does not lead to a perceptible degradation for bending. An example is presented in Figure 5.18 for the I_c and the same is observed for the n -value. The applied load versus the measured bending deflection depicted in Figure 5.19, with stepwise increase of the load, release to almost zero and a final peak load of 10 kN/m, shows the degree of permanent plastic deformation and relaxation following the elastic behaviour. The cycling with a peak load of 10 kN/m does not lead to further increase of the deflection (at full load) although in some cases a small increase with cycling is observed for released load (Figure 5.20), apparently associated with the observed increase of I_c with cycling at zero load (Figure 5.18).

Figure 5.18. Normalized critical current versus the number of cycles between almost zero and 10 kN/m for wire BR5, at 12 T and 4.2 K

Figure 5.19. Example of the applied load versus the measured bending deflection with stepwise increase of the bending deflection and release of the load to almost zero and a final peak load of 10 kN/m for wire BR5 at 12 T and 4.2 K.

Figure 5.20. Example of the bending deflection during 10,000 cycles between almost zero and a peak load of 10 kN/m for wire BR5 at 12 T and 4.2 K.

Figure 5.21 represents the normalized I_c versus the applied peak bending strain with an uncertainty of $\pm 5\%$. In particular for higher levels of strain, BR wires show less reduction than IT wires, while the overall spread remains limited to about 15 to 25% I_c decrease at a peak bending strain of 1%. This is likely related to the strain sensitivity of the I_c , observed at the axial strain tests.

As the deformation is load driven, the plots in Figure 5.22 show that most IT wires are less sensitive to the applied bending load. Although also BR4 shows good performance, which seems correlated to its large axial stiffness, comparable to that of the IT strands. The IT1 wire seems slightly more sensitive but this may be attributed to the larger sample-to-sample spread found for IT wires. IT samples sometimes reveal some spread between the three voltage tap pairs, which in general is not observed for BR wires. The BR6 strand is most sensitive to bending load due to its low axial stiffness.

The n -value reduces with applied load as shown in Figure 5.23. The n -value of the IT1 wire is relatively low but seems in line with the barrel test. The n -value of the IT2b wire in the bending test is higher than found in the barrel test, perhaps due to the large spread in results found for this initial billet.

The bending deflection versus the applied load is displayed in Figure 5.24. The specific deflection at 10 kN/m applied load for all samples except BR6 is shown in Figure 5.25. In summary, the BR samples expose the largest deflection while again BR4 is close to the IT strand performance. Anticipating a correlation between axial stiffness and bending deflection, the bending deflection at a load of 10 kN/m is shown versus the axial tensile strain from the stress-strain tests at a stress of 150 MPa for all samples in Figure 5.26. Although some scatter can be expected from the influence of transverse stiffness, also playing a very minor role in the bending test, the correlation is quite good.

Figure 5.21. Reduction in normalized I_c versus peak strain at bending deflection for all samples at 12 T and 4.2 K.

Figure 5.22. Reduction in normalized I_c versus applied bending load for all samples at 12 T and 4.2 K.

Figure 5.23. Reduction in n -value versus applied bending load for all samples at 12 T and 4.2 K.

Figure 5.24. Bending deflection versus applied load per meter for all samples at 12 T and 4.2 K, including the copper wire used for cabling.

Figure 5.25. Bending deflection at 10 kN/m applied load, 12 T and 4.2 K, for all samples except the FUR CS Insert. The BR samples expose the largest deflection except BR4.

Figure 5.26. Bending deflection at a load of 10 kN/m versus axial tensile strain obtained at 150 MPa for all samples at 4.2 K. The line is a guide for the eye showing the (almost) linear correlation between axial stiffness and bending deflection.

5.2.5 Periodic contact stress

A characteristic curve of the normalized I_c reduction versus the applied contact stress is presented in Figure 5.27. Cycling of the load has no noticeable influence on the I_c and n -value for contact stress (see Figure 5.28). The stress per crossing set of wires with stepwise increase of the load and release to almost zero up to a peak load of 90 MPa is presented in Figure 5.29. Although the average transverse load in an ITER CICC is below 20 MPa, the permanent reduction in I_c at 90 MPa is a few percent for BR wires. The subsequent release of load illustrates the permanent plastic deformation and the elastic trajectory. The elastic modulus rises with increasing plastic wire deformation.

Figure 5.27. Example of the normalized critical current versus applied stress with stepwise increase of the load followed by 10,000 cycles between almost zero and a peak load of 90 MPa (wire BR5) at 12 T and 4.2 K.

Figure 5.28. Normalized critical current versus the number of load cycles up to 10,000 cycles between almost zero and a peak load of 90 MPa (wire BR5) at 12 T and 4.2 K.

Figure 5.29. Example of the stress per crossing strand with stepwise increase of the load and release to almost zero up to a peak load of 90 MPa (wire BR5) at 4.2 K.

Figure 5.30. Deflection versus number of cycles up to 10,000 cycles at a peak load of 90 MPa (BR5) at 4.2 K, showing a gradual increase of the deflection with cycling without saturation.

Cycling at a peak stress of 90 MPa leads to continues increase of the deflection representing the wire dimpling (Figure 5.30), this is observed for all strands subjected to the test. The increase is limited to several μm 's, which is in the range of 5% of the overall contact deflection. It should be emphasized that it does not saturate with cycling up to 10,000 cycles.

The normalized critical current versus the applied stress up to a peak load of 90 MPa in Figure 5.31 illustrates the difference in contact stress susceptibility. The IT wires are significantly more susceptible to contact stress than BR wires. The normalized n -value versus applied contact stress in Figure 5.32 seems to behave rather similar for all types of wires without much dissimilarity between IT and BR. The contact stress for crossing strands, comparing the stress levels that are corresponding to 10% I_c reduction, is displayed in Figure 5.33. It illustrates that IT wires are significantly more receptive to contact stress than BR wires.

Figure 5.31. Normalized critical current versus applied stress up to a peak load of 90 MPa illustrating the difference in contact stress sensitivity.

Figure 5.32. Normalized n -value versus applied stress at 12 T and 4.2 K up to a peak load of 90 MPa showing rather similar contact stress sensitivity.

Figure 5.33. Contact stress for crossing wires comparing stress levels corresponding to 10% I_c reduction at 12 T and 4.2 K. The BR wires appear to be less sensitive than the IT wires.

The contact deformation of the wires versus applied stress is shown in Figure 5.34. The deflection of the copper wire used for the ITER TF CICC is included, showing a deflection that is two to four times larger than that of the superconducting wires containing a stiffer non-copper fraction. The average contact stress in a full-size TF conductor is roughly in the range of 20 MPa and the corresponding deflection is compared for all strands in Figure 5.35.

Figure 5.34. Deflection as a function of the contact stress per set of crossing wires up to a peak load of 90 MPa at 4.2 K for all wires including copper wire used in the TF ITER conductors.

Figure 5.35. Deflection for a contact stress per crossing set of wires at 20 MPa for all wires at 4.2 K, including copper strand used in the TF ITER conductors.

5.3 Filament fracture and bending

In references reporting about ITER Nb₃Sn CICC modelling, there is no consistency about the role of filament fracture in the degradation of the critical current when exposed to thermal and electromagnetic loads. In some works it is assumed that a large volume fraction of the strands (up to about 10%) contains cracks [38], while others report about only a minor portion, order of magnitude lower. Metallographic work for investigating the statistics of filament fracture occurring in TARSIS and full-size ITER samples after loading has been performed in collaboration with the ASC group at FSU, Tallahassee, USA.

In TARSIS bending tests, each wire type is subjected to three different peak loads representing an I_c reduction of 30%, a peak bending strain of 1.0% and a peak force 10 kN/m and continued with cycling of the load. The post-mortem crack analyses of TARSIS wire samples reported in [151, 152], shows that no cracks are observed in BR wires when loaded up to 6 kN/m (see Figure 5.36). This load level corresponds to a peak bending strain of about 1%, according to the simplified relation used in [129, 153] but actually a peak bending strain of about 0.8% following a more detailed FEM computation with higher accuracy as performed later [154]. The peak bending strain is defined as the maximum strain at the outer region of the filamentary zone. The span of the crack region is about 0.5 mm for BR wire and 1 mm for IT wires.

Figure 5.36. Number of cracks per mm of filament in a single contact point of each TARSIS tested sample as analysed at FSU. A uniform load is only tested on IT2 and BR2 wires [155]. Note that the number of filaments is roughly three times more in BR strands.

The crack density is concentrated at the tensile side for bending. In IT wire the filament fracture is observed in all samples, 30% I_c reduction, 1% peak bending strain and 10 kN/m peak loads. For BR wires, filament fracture is not evident until 10 kN/m is reached. For uniform load, only IT2 and BR2 were tested revealing that the BR wires did not produce any cracks when loaded up to 70 MPa [151, 152]. The reduction of I_c at 70 MPa uniform load is about 10% for the BR wire while it amounts to more than 40% for the IT wire. This is similar to what was observed for the crossing strand tests on the same strands.

These observations are in good agreement with the irreversibility limits found with the axial strain tests presented in 2.3 and Figure 5.6 and the observations reported in [15] and in Figure 2.31, that cracks can already be present in non-loaded IT wire. Thus IT wires are more prone to filament fracture at low tensile strain levels although the number of cracks grows faster for BR wires when exceeding the ϵ_{irr} , which is higher for BR wire.

As a result, at a relatively high bending load of 10 kN/m, IT wires seem not more prone to cracking than the BR wires investigated. This difference is likely also due to the

difference in strand axial stiffness. For a given bending load, this will result into a smaller deflection and a lower level of deleterious tensile strain in IT wire (see Figure 5.25). This observation is further supported by the results found for the BR4 wire, with a low number of fractures, even below that in IT. The BR4 wire benefits from a high axial stiffness, comparable to that of IT strands and an I_c - strain sensitivity being among the lowest of all tested wires (see Figure 5.7).

The BR1 wire contains more cracks than BR2. This difference is clearly dominated by the larger deflection at 10 kN/m (see Figure 5.25). On the other hand, the ϵ_{irr} for the BR1 wire is about 0.1% higher than in BR2 (see Figure 5.6).

For the I_c under bending load, the lower strain sensitivity of BR1 with respect to BR2 seems to compensate the weaker mechanical properties (see Figure 5.7). In Figure 5.22 the sequence of the relative performance is shown for BR5, BR1 and BR2, in line with the strain sensitivity under compression. The high axial stiffness of BR4 wire combined with its high ϵ_{irr} , not only limits the number of fractures but also yields a relative good performance in combination with low strain sensitivity in spite of the low I_c .

For Figure 5.22 it is noted that the performance of IT1 is lower than expected, which may be related to the larger scattering in properties found in IT strands in general. For other IT samples similar behaviour is observed. It is important to note that this spread in sensitivity for transverse load seems not reflected in the overall performance of full-size ITER samples.

The normalized critical current versus applied stress up to a peak load of 90 MPa in Figure 5.31 illustrates the difference in contact stress sensitivity. The IT wires are significantly more susceptible to contact stress than BR wires although at 20 MPa not much effect is expected.

For contact stress in TARSIS, only IT type of wires showed cracks and although the transport properties under contact stress are clearly superior for BR wire, no clear correlations have been found between wire performance and transverse stiffness but the presence of voids in the non-copper volume may be relevant.

Cycling between almost zero and a peak contact stress of 90 MPa leads to a continuous increase of the deflection for all wires (see Figure 5.30). The increase is limited to several μm , which is in the range of 5% of the overall contact deflection. Notice that it does not saturate with cycling. This may be a source for the observed continuous degradation with load cycling in full-size TF conductors and the first CS conductors since this deformation affects the local bending strain pattern. It is noted that the peak stress level during cycling of 90 MPa in TARSIS is about five times higher than the average maximum stress level in an ITER TF conductor. This means that the main fraction of the strands is not subjected to such high stress levels and only a very small fraction undergoes these severe conditions.

A wealthy amount of data was collected on axial and transverse deformation of wires supporting the next step in refining our quantitative understanding of the observed strain sensitivity in relation to wire internal resistances [111, 112, 113] and filament fracture. This challenge in modelling is not in the scope of this thesis but a short summary is provided of the work reported in [118]. In the previous it was observed that for bending a permanent reduction in I_c is already reached at about 1.5 kN/m due to a permanent plastic deformation, sustaining a strain distribution in the filament bundle. For bronze wires, a reduction was found of 20 to 30% (partly reversible) under a load of 6 kN/m, still without filament fracture. In the case of severe local strand bending, filament fracture in part of the filamentary zone subjected to high tensile strains can occur (see Figure 5.36). At these locations, electric field will develop as filaments start bypassing current through the matrix.

The effect on the current sharing temperature T_{cs} was computed using a detailed strand model [122], between the situation with only the corresponding realistic bending strain and the cases with worst observed crack densities in the CSIO1 and CSJA2 full-size samples [118], for the number of crack events in a wire with BR properties. The observations by FSU established about two crack events per meter wire. For a wire with BR properties in the model, a decrease in T_{cs} of 120 mK is found for the CSJA2 crack type and 40 mK for the CSIO1 crack type.

The influence of filament cracks on the T_{cs} , based on the FSU statistics and the Twente strand model [122], although present, is rather limited. Even for a frequency of crack events, ten times higher than what has been observed in the initial crack count analysis of full-size ITER samples after loading, the decrease in T_{cs} remains limited to 0.2 K. For IT wire this may have the least impact since the bridging between filaments in the same bundle provides a non- or very low-resistive redistribution of the current within the bundle, in spite of the high resistive matrix compared to BR wire [111, 112, 113]. Filament fracture reduces the transport properties but the crack densities found in ITER CS baseline CICC are not sufficient to explain the degradation during cyclic load testing, which could span from 0.5 to 1.0 K for CS BR conductors to about half of that for CS IT conductors. This entails that the dominant mechanism for the observed degradation in ITER Nb₃Sn CICC is attributed to axial and bending strain variations in the strand's filament bundle.

5.4 Conclusion

Most of the ITER TF wires and a couple of CS wires were extensively tested to thoroughly study the impact of axial and transverse strain on the electrical transport properties. It was found that the susceptibility of the critical current for axial compressive strain is higher for IT than for BR wires. The irreversibility strain limit of the wires, derived from the n -value, exhibit a limit above 0.4% for all bronze wires while all IT wires show a spread between zero and 0.4%.

The spread in wire axial tensile strain at an applied stress level of 150 MPa can vary up to a factor 1.6. The strain is always higher for the softer BR wires, in average about 0.2% higher. The strain level at which the wire breaks is higher for BR wire and the difference with IT wire can even reach to 0.5% strain.

For bending, a permanent reduction in critical current is reached at about 1.5 kN/m, the peak bending strain does not return to zero anymore after release of the force, because the wire has reached a permanent plastic deformation, sustaining a strain distribution in the filament bundle. At an irreversible critical current reduction of bronze wire by 10 to 20% in unloaded state and 20 to 30% (partly reversible) under a load of 6 kN/m, still no filament fracture occurs.

Cyclic bending between almost zero and a peak load of 10 kN/m does not lead to a perceptible degradation in critical current and n -value or an increase of the bending deflection. For high levels of bending strain, BR wires show less reduction than IT wires. But importantly, as deformation in CIC conductors is load driven, most of the IT wires are less sensitive to applied bending load.

The bronze wires expose the largest bending deflection, with one exception resulting in an improved relative performance. A good correlation is found between axial stiffness and bending deflection.

For contact stress, the average transverse load in an ITER CICC is below 20 MPa and the permanent reduction in critical current after 90 MPa load is about several percent for bronze wire. Cycling at peak contact stress of 90 MPa leads to a continuous increase of the deflection for all wires of several μm but does not saturate with cycling.

Internal tin wires are significantly more susceptible to contact stress than bronze wires although up to 20 MPa no reduction in transport properties is observed. The normalized n -value versus applied contact stress seems to behave rather similar for all types of wire. No clear correlation has been found between strand performance and transverse stiffness.

Stiffness is a very important strand property to avoid CICC performance degradation. To somewhat lesser extent, the bronze matrix resistance; filament sintering in IT and axial strain sensitivity are relevant. Filament fracture causes a reduction of the transport properties but the crack densities found in ITER CS baseline CICC are by far not sufficient to explain the degradation in the current sharing temperature during cyclic load testing. This implies that the dominant mechanism for ITER Nb₃Sn CICC performance degradation is due to variations in axial and bending strain in the strand's filament bundle, forcing current sharing and reducing the current sharing temperature.

6 Effect of transverse load on full-size cable in conduit conductors

The flexible nature of the cable bundles in the sizeable Cable-In-Conduit-Conductors for ITER comprising more than thousand strands, in combination with a void fraction of around 30% gives scope for cable compression in general and significant local strand deflection. In particular, the transverse stiffness of the Nb₃Sn cables, being subjected to large electromagnetic forces, has an effect on the strand's strain condition, interstrand resistance, coupling loss, cooling and pressure drop along the turns in the windings and are valuable to account for in large magnets as for ITER.

A cryogenic cable press was built to investigate the lifetime issues and changes in the mechanical and electrical properties of full-size ITER conductors. The press is fully automatic and designed to perform tests on CICC's under transverse variable load along for example 40,000 cycles at 4.2 K. Essential design features of the press are presented.

The lifetime characteristics concerning coupling loss, interstrand contact resistance, cable compression, changes in transverse stiffness and mechanical losses are experimentally determined on several prototype ITER Nb₃Sn conductors in the Twente Press and a summary of the results is presented. The non-linear stress-strain characteristics of the cable bundle and its moderate time dependent nature can be considered as a viscoelastic-plastic phenomenon. The evolution of identified cable parameters depends on the peak load, void fraction, strand type, strand coating and the number of load cycles. The dissipated heat by the mechanical energy is not a critical issue for ITER magnet operation but is not negligible, in particular for NbTi conductors. The CICC's show changes in interstrand contact resistance, coupling loss and mechanical properties with cycling. Some of these are quite significant, depending on the specific conductor properties. The most relevant results are summarized and presented.

The content of this chapter is for a large part based on papers [22], [23], [24], [25], [89], [125], [127], [161], [163], [164], [165], [166], [167], [168], [169], [177] and [179].

6.1 Introduction

During magnet operation, the conductors are subjected to severe transverse loading due to the huge electromagnetic forces on the CICC in ITER. These cyclic forces cause a deformation of the cable bundle inside the conduit, which goes along with mechanical, electromagnetic and thermo hydraulic effects [41, 46, 157, 158, 159, 160, 161, 162]. The operation of ITER requires a minimum lifetime of the Central Solenoid conductors of up to 60,000 cycles, associated with ramping up and down current and field in the magnets.

Several Model Coils were built to test and explore the performance of full-size ITER conductors under relevant conditions [41]. At the same time short sample tests were carried out in SULTAN with parametric conductor variations in combination with evaluation of joint concepts [156]. Tests with a very large number of cycles under relevant electromagnetic conditions are not only time consuming but also very costly and are therefore not included in the above mentioned test programs. A cryogenic cable press was built at the University of Twente in order to cover the lack of knowledge concerning long term cycling and to generate specific data that cannot be extracted from electromagnetic tests (see Figure 6.1).

The press is designed for testing the impact of Lorentz forces on a conductor under conditions equivalent to ITER magnet operating conditions.

The evolution of the coupling loss and the associated interstrand contact resistance R_c between various strands and strand bundles are measured for a range of loads and after subsequent number of cycles, starting at virgin condition. From the measured force on the cable and jacket displacement, determining the cable impression, the effective Young's modulus of the cable and mechanical heat generation are determined. Knowledge of the evolution of the R_c is essential for evaluation of current sharing, cable axial voltage-current behaviour in combination with the non-uniformity of the joints [45], the overall heating caused by coupling current loss on top of the mechanical loss and eventually all these effects determine the stability of the CICC.

The mechanical properties in terms of transverse Young's modulus and cable compression are indispensable for the analyses of the impact of strain degradation on the Nb₃Sn transport properties. The strand bending strain for example is a direct function of the transverse cable deformation under load. An estimation of the bending strain amplitude can be extracted from the cable tests in combination with the periodic bending tests on strands with the 'TARSIS' facility. This yields experimental background for a better understanding of the bending strain degradation and possible ways to optimize the conductor design.

Also the helium flow resistance is affected by the so-called 'third-channel' between conduit wall and strand bundle created after compression of the cable and this will

influence the thermo hydraulic properties. The main properties of the press are described together with the developed procedure for sample preparation and testing. Further details on test results in terms of cyclic behaviour of electromagnetic and mechanical properties can be found in several publications [22, 127, 163, 164, 165, 166, 167, 168, 169, 170, 171].

Figure 6.1. Cryogenic Cable Press in the laboratory at the University of Twente.

6.2 Cable press for transverse cyclic loading

6.2.1 General layout

The cryogenic part of the press fits in a cryostat with an inner diameter of 500 mm. Figure 6.2 shows the press, which has a total height of 2.75 m. The force needed to compress the sample is generated by a hydraulic cylinder placed on the cryostat flange. A hydraulic pump unit provides the cylinder with an adjustable oil pressure up to 100 bar. The shaft of the cylinder is connected to the push rod, which is guided through a tube. A wedge is connected to the lower end of the push rod, running at both sides on needle bearings to minimize friction.

The cryostat flange is provided with a sealed feed-through for the support pipe. The bottom side of the support pipe is bolted to the frame to carry the reactive force of the cylinder. The supporting tube contains sleeve bearings spaced at 325 mm to prevent buckling of the driving rod during operation. AISI316 is used for most parts unless otherwise specified in Figure 6.2.

Figure 6.2. Schematic cross-sectional view of the mechanical parts of the press.

6.2.2 L-yoke

The amplification of the hydraulic force will be partly determined by the geometry of the L-yoke, which works like a lever arm. Figure 6.3 shows the forces in the

construction of the press at maximum load condition (320 kN). The required hydraulic force F_{cyl} can be determined by the following equations:

$$F_{cyl} = F_{Bvert} + F_{Offset}, \quad [N] \quad (6.1)$$

$$F_{Bhor} = F_{Bvert} / \tan(8.34^\circ), \quad [N] \quad (6.2)$$

$$\sum M_C = 0: F_A \cdot H_A = F_{Bhor} \cdot V_A + F_{Bvert} \cdot H_B. \quad [Nm] \quad (6.3)$$

Combining Equations 6.2 and 6.3 gives

$$320 \cdot 95 = \left[\frac{F_{Bvert}}{\tan(8.34^\circ)} \right] \cdot 525 + F_{Bvert} \cdot 80, \quad [Nm] \quad (6.4)$$

leading to $F_{Bvert}=8.3$ kN. F_{cyl} will be 8.45 kN for a given $F_{offset}=0.15$ kN. The total gain of the force at full load is $F_A/F_{cyl}=37.9$ times F_{Bvert} .

The wedge amplifies the force F_B/F_{Cyl} 6.77 times F_{Bvert} and the L-yoke amplifies the force by: BC/H_V 5.59 times F_{Bvert} .

Figure 6.3. Vector diagram of the forces applied to the L-yoke of the press.

The ratio between the generated hydraulic force F_{cyl} of the cylinder and the compression force F_A at the position of the sample is 37.9. Thus the cylinder has to generate 8.45 kN to obtain the maximum load on the sample of 320 kN. The back plate and the L-yoke are provided with calibrated strain gauges to measure the local forces.

The L-yoke is bearing-mounted on shaft sleeves (C in Figure 6.3), made of bronze with an inner PTFE/Pb coating. The shaft sleeves have a longitudinal slit to adjust the bearing clearance. The bearing clearance during operation is 0.06 mm and the shaft diameter is 45 mm. For low frequency loads, the sleeve bearing can safely handle a maximum load of 140 N/mm². For unlimited cycling the bearing peak load is specified as 14 N/mm² (data at room temperature). Here the bearing design load amounts to 74 N/mm² and the oscillating angle of the L-yoke is 2.5°. This would correspond to a

bearing lifetime of 100 cycles according to documentation [172]. Up till now the operating loads did not exceed 75% of the design load of the press.

The shaft of the guide block wedge (diameter 22 mm) is bearing mounted on a shaft sleeve made of P30 PTFE sheet. This is a 0.5 mm PTFE/Pb coated glass-fibre reinforced perforated aluminium sheet, rolled to a sleeve. The documentation [173] specifies a static bearing number $C_0=400 \text{ N/mm}^2$ at room temperature. The design surface pressure of the sleeve bearing is 52 N/mm^2 at 4.2 K and operates satisfactory with cycling.

A visual inspection of the sleeves was performed after 100,000 cycles. The PTFE/Pb running surfaces of the sleeves showed no visible wearing. It turns out that PTFE/Pb coated sleeve bearings perform well under cryogenic conditions.

6.2.3 Wedge

The wedge (Figure 6.4) is a critical part of the press because of the needle bearing construction, the pre-press devices, the subdivided concept and the considerable amplification (6.6x) of the hydraulic force. The wedge is connected to the lower end of the driving rod. The wedge with an angle α of 8.34° , has a longitudinal partition, so each half part has an angle of 4.17° .

The wedge is on both sides provided with two countersunk grinded needle raceways, with a width 20.4 mm and depth of 2.7 mm. The needles are enclosed by flat carbon steel cages. The main advantages of needle bearings are the minor built-in space and the ability to handle large loads in comparison to other roller type bearings. A drawback is their susceptibility to misalignment of the running surfaces. This is solved by the split wedge design.

The surfaces of the partition have a cylindrical surface (radius 100 mm), one hollow and the other spherically shaped. This enables the wedge halves to take the most favourable position. This will counterbalance for any misalignments of the running surfaces of frame and L-yoke.

To prevent slipping out of position of the free needle roller strips, the needles require a pre-load. This is achieved by pressing the L-yoke to the frame by a lever arm and a compression spring (see Figure 6.5). The geometry of the mechanism is designed in such a way that the pre-stress force on the needles remains almost constant (within 1%) regardless the positions of wedge and L-yoke.

The load cell shows a load of 180 to 200 N during the downward motion and 100 to 120 N during the upstroke of the wedge in a free running press without a sample inserted.

A calculation of the offset load gives 150 N (neglecting any friction). The hysteresis loop is caused by the friction in the moving parts demonstrating that the friction is quite small.

Figure 6.4. Principle sketch of the self-adjusting split wedge.

Figure 6.5. Principle sketch of the pre-loading device with needle bearings.

6.2.4 Sample preparation and instrumentation

The standard testing procedure is that two conductor specimens are prepared. One sample serves for virgin state AC loss measurement and the other for the cyclic loading test in the press. The first specimen is for measuring the AC loss using helium boil off calorimetry without any modification to the sample, i.e. in virgin condition. The sample length is 400 mm. i.e. about the last stage twist pitch length. The virgin state conductor is also equipped with a pick-up coil and a compensation coil so that the AC loss can be measured calorimetrically and magnetically at the same time. The AC loss results, measured on the virgin sample are used for calibration and fine-tuning of the Cable Press pick-up coil magnetisation results.

The cable press sample is prepared as follows. Both ends of a sample are cut by electric erosion, to a length of ~ 650 mm (see Figure 6.6). At one end the conduit is removed leaving a jacketed section of 400 mm corresponding to the last stage twist pitch length.

Figure 6.6. Bolts at both sides of the conductor jacket to secure a “locked void fraction” before separation of both jacket halves. At one end a section of cable is accessible for connection of strands allowing interstrand contact resistance measurements. Left a CICC with square jacket, at right a CICC with round jacket in a square holder.

Individual strands and strand bundles are selected across the cable section for resistance measurements (see Figure 6.6 right and Figure 6.7 left). The strands are bent in the required shape and attached to the strand support holder (see Figure 6.7 right). Strands not connected for interstrand resistance measurements, are cut away and removed where the conduit starts.

Figure 6.7. Left: selection of strands for interstrand contact resistance measurements in progress. Right: a fully instrumented conductor sample moved out halfway the superconducting dipole magnet bore, the quartz rods and tubes (see Figure 6.8) are inserted when the sample is in its final position.

In order to be able to squeeze the cable, the jacket has to be divided into two halves without affecting the virgin state and void fraction of the cable within the jacket. The milling of the 6 mm longitudinal slits at either side of the jacket is done after the jacket

was secured with stainless steel M4-bolts on each side of the cable (see Figure 6.6). Otherwise the contact resistance between the strands or the void fraction could possibly change due to relaxation of the sample after the partitioning. Thus, the jacket displacement is limited in expansive direction by bolts on each side of the jacket (see Figure 6.6). The cable can be compressed freely, but is blocked in expansive direction to the original value of the void fraction; in the following this is designated as “locked void fraction”.

The conductors, as delivered by the manufacturers, are never perfectly straight. In order to guarantee a uniform load along the conductor, the sample is straightened prior to preparation. It sometimes appeared that after splitting the jacket, the parts become somewhat deformed due to internal stress in the material, probably caused by the shaping during conductor manufacturing. Straightening both the jacket halves after splitting, would guarantee a uniform load. However, this method implies additional cable handling, which may damage the virgin state. The cable expands in diameter when the jacket is removed due to relaxation of the strand bundles and it is impossible to retain the original diameter without severe compression. This actually results into an increased void fraction during testing allowing the cable to expand more than what in reality occurs. A way to mitigate this effect is to machine a thin layer from the outer surfaces of the jacket to create plane parallel surfaces for the load application.

Conductors with a circular conduit are first embedded in a stainless steel support with square outer shape and a round profile inside, exactly fitting the outer circumference of the conductor (see Figure 6.6, right and Figure 6.8). Accordingly the conduit is further prepared for instrumentation (strain gauges, pick-up coils, and displacement sensors, see also Figure 6.8 and Figure 6.9). The displacement of the upper half of the jacket with respect to the lower part during loading (compression and relaxation) is measured by a set-up of displacement sensors based on the use of bending beams with strain gauges at six positions around the conductor jacket halves (see Figure 6.7 and Figure 6.10). The displacement sensors are calibrated using quartz tubes and rods positioned near each sensor on the sample transferring the displacement to conventional micrometers positioned outside the cryostat (see Figure 6.8 and Figure 6.9).

Figure 6.8. Left: instrumentation of the sample with extensometer and quartz tube. Right: cross sectional scheme of circular jacket sample and square holder with LVF bolts at both sides of the conductor jacket, showing the quartz rods for calibration of the displacement measurement with micrometers, performed for each new sample.

Figure 6.9. Left: micrometers at the top of the cryostat, connected to the upper ends of the quartz tubes and rods inside for calibration of the displacement meters with strain gauges. Right: top view of the prepared sample with 12 LVF bolts and 6 strain gauge displacement meters around the sides of the conductor jacket.

The average value of the six sensors is designated as d . For the force F_A , the same procedure is followed by averaging the data from all calibrated strain gauges on the back plate and L-yoke. The force is monitored by a load cell outside the cryostat on top of the hydraulic cylinder. In this way the $d(F_A)$ curves are constructed. The load cell is also used to control the hydraulic unit during automatic cycling.

The contact resistance among strands and strand bundles in various positions in the cable cross section and the effect of the applied force is examined during cycling at various loads. The interstrand resistance is measured with a four-point-method. The connection scheme of the strands is schematically depicted in Figure 6.10 and listed in Table 6.1.

The conductor is surrounded by a dipole excitation coil integrated in the press, which applies an alternating magnetic field. The magnetisation is measured by a set of compensated pick-up coils positioned on the conductor surface. A stationary background field is required to suppress superconductivity in the non-Nb₃Sn materials and avoid possible influences of a diffusion barrier, or superconducting solder coating, on the AC loss and contact resistance measurements.

Figure 6.10. Left: practical layout of selected strands to be connected for resistance measurements. Center: selection of strands from different petals. Right: scheme of the strand selection for intra-petal contact resistance measurements.

Table 6.1. Example of strand combinations selected for intra- and inter-petal contact resistance measurements.

1 st stage (intra-triplet, petal 1)	3 rd stage (intra-petal, petal 1)	5 th stage (inter-petal)
R1-R2	R1-R6	R1-R10, -R11 (petal 2)
R1-R3	R1-R7	R1-R12, -R13 (petal 3)
R2-R3	4 th stage (intra-petal, petal 1)	R1-R14, -R15, -R16 (petal 4)
2 nd stage (intra-petal, petal 1)	R1-R8	R1-R17, -R18 (petal 5)
R1-R4	R1-R9	R1-R19, -R20 (petal 6)
R1-R5		

6.3 Effect of transverse pressure on AC loss and contact resistance

6.3.1 Introduction

Initial experiments with the press were conducted up to 40 cycles on two CSMC conductors (CS1 and CS2) and a TFMC conductor, all containing Nb₃Sn strands, [127, 163, 164, 165, 166, 167]. It was observed that $n\tau$ declines and R_c increases substantially with the number of cycles and it seemed that after roughly 40 cycles saturation in R_c was reached.

It was not feasible at that time to maintain the virgin condition when inserting the conductor into the press after removing the clamps used during the heat treatment. Moreover, it was possible for the cable to expand more than the original void fraction. To achieve a more representative test the jacket relaxation is limited by the LVF method. At the same time, the press was modified to accommodate fully automatic cycling enabling life-time cycling for the first time. A CSMC Nb₃Sn conductor was tested up to 40,000 cycles with a peak load of 650 kN/m at 4.2 K and a void fraction at the time of the CSMC of 36%.

After the CSMC and TFMC program, design optimizations pointed towards a lower void fraction. TFMC type of conductors were tested in the cable press with void fractions of 26.4% (TFMC-A), 29.7% (TFMC-B), and the original 36.6% respectively. The two TFMC conductors with reduced void fraction were prepared according the LVF method. The TFMC conductor sample with the original void fraction of 36.6 % (TFMC-0) was prepared without the LVF-method, leaving the jacket halves unengaged, thus leading to spring back and a somewhat higher initial void fraction compared to the virgin value. The design of the TFMC conductor is described elsewhere [31, 167, 174]. In Table 6.2 the prototype Nb₃Sn CS1, CS2 and TFMC conductors are listed, which all were taken from the Model Coil production. More details of these conductors are given in references [89, 125, 127, 164, 165, 166, 167].

Table 6.2. Full-size prototype ITER Nb₃Sn conductor samples tested in the press.

Identification	CS1 (EU) (CS1.1B)	CS2 (US)	CS2 (JA)	TFMC	CS1 (EU) (CS1.4A)	TFMC-A	TFMC-B
Strand coating	Cr	Cr	Cr	Cr	Cr	Cr	Cr
Peak load [MPa]	650	650	650	650	650	650	650
LVF method	no	no	no	no	yes	yes	yes
Strand diam. [mm]	0.81	0.81	0.82	0.81	0.81	0.81	0.81
Cu:non-Cu	1.5	1.5	1.4	1.45	1.5	1.45	1.45
# Nb ₃ Sn strands	1152	720	720	720	1152	720	720
# Cu strands	0	360	360	360	0	360	360
Cable geometry	3x4x4x4x6	3x3x4x5x6	3x3x4x5x6	3x3x5x4x6	3x4x4x4x6	3x3x5x4x6	3x3x5x4x6
L_o stage 1, [mm]	21*	56*	70*	43	45*	43	43
L_o stage 2	50*	93*	90*	77-85	74*	77-85	77-85
L_o stage 3	100*	147*	150*	142	123*	142	142
L_o stage 4	155*	260*	265*	160-234	160*	160-234	160-234
L_p stage 5	390*	400*	490*	416	380*	416	416
Sub-cable wraps	yes	yes	yes	yes	yes	yes	yes
Void fraction	39	36	36	36.6	36	26.4	29.7
Jacket material	Incoloy 908 tube	Incoloy 908 tube	Incoloy 908 tube	AISI 316 LN	AISI 316 LN	AISI 316 LN	AISI 316 LN

*pitches measured on opened sections at University of Twente.

The evolution of the coupling loss and the associated interstrand resistance R_c between various strands and strand bundles were measured across a range of loads and after subsequent number of cycles, starting at virgin condition. Results are presented and discussed with reference to ITER Model Coil test results.

6.3.2 ITER CSMC CS1 conductor

In section 1.5.3 is explained that the coupling loss of a CIC conductor consists of many different time constants due to the complex cable pattern. This introduces a non-linear behaviour of the coupling loss versus frequency. The AC loss of the CSMC CS1 conductor in the press is determined in a stationary background field of 350 mT and a sinusoidal excitation field with amplitude of 150 mT. The loss versus frequency behaves strongly non-linear below 0.1 Hz but becomes rather linear at higher frequencies. This is characteristic for CICC conductors with a large number of strands. For a first-order characterization, the slope is determined of the linear fit of the data at lowest frequencies. The low frequency data are the most important for magnets in fusion as a measure for the coupling loss. The linear fits to the initial slope and higher frequency range of the AC loss curve of the CS1 conductor sample are shown in Figure 6.11.

The coupling loss time constant $n\tau$ from the initial slope and higher frequency is 196 and 67 ms, respectively. The hysteresis loss is correctly determined for the initial slope method and is 1.01 mJ/cm³, while using the higher frequency fit a loss is found of 2.71 kJ/m³. This shows immediately the significance of a single time constant definition based on the initial slope of the AC loss curve for comparison of different CICC. The measured total volumetric AC loss of conductor sample CS1 versus frequency in the virgin state and after 40,000 cycles with and without 650 kN/m load are compared in Figure 6.12.

Figure 6.11. Total volumetric AC loss in kJ per cycle and per m³ strand material of conductor sample CS1 versus frequency in the virgin state, measured at 4.2 K, 350 mT stationary field and 150 mT amplitude. The dotted lines represent linear fits to the coupling loss slope, depending on the frequency range. The line through the measured data is a 3rd order polynomial.

Figure 6.12. Total volumetric AC loss in kJ per cycle and per m^3 strand material of conductor sample CS1 versus frequency in the virgin state, after 40,000 cycles with and without 650 kN/m load, measured at 4.2 K, 350 mT stationary field and 150 mT amplitude. The lines through the measured data are 3rd order polynomials.

The $n\tau$ of the CS1 conductor in the virgin state amounts to 206 ms. After the first cycle with a peak load of 470 kN/m, the $n\tau$ has decreased significantly to 80 ms at no load and 100 ms at 470 kN/m. It appears that a constant level of coupling loss of some 83 ms is reached already after a few cycles. The $n\tau$ with released load gradually decreases to 20 ms after 40,000 cycles (see Figure 6.13).

Figure 6.13. Evolution of the coupling loss time constant versus number of load cycles for full and zero load of conductor sample CS1, measured at 4.2 K, 350 mT stationary field and 150 mT amplitude.

The behaviour of the inter-strand and inter-bundle contact resistances R_c versus the number of loading cycles from the virgin state to 40,000 cycles are presented in Figure 6.14 (full load) and Table 6.3.

Figure 6.14. Evolution of the R_c (Equation 6.5) of the CS1 conductor from virgin to initial state, subsequently with load up to 40,000 cycles, measured under full load (470 kN/m until 1000 cycles, then 650 kN/m) at 4.2 K and 350 mT stationary magnetic field. IB=inter bundle, IS=interstrand, ne=neighbouring strands or cabling stages (bundles).

Table 6.3. Conductor sample CS1 R_c results in first stage triplet, sub-petal and petal strand bundles under full and zero load, from zero to 40,000 cycles at 4.2 K and 350 mT stationary magnetic field.

R_c [nΩm] for neighbouring strands and bundles					
Load condition	virgin	650 kN/m		0 kN/m	
Stage		max.	final	max.	final
(1 st Stage triplet) IS	11	47	32	169	163
Sub-petal (4 th stage) IB	2.7	17	9	28	25
Petal (5 th stage) IB	25,000	2,000	730	12,000	11,000

The R_c is defined as:

$$R_c = \frac{V \cdot l}{I}, \quad [\Omega m] \quad (6.5)$$

in which V is the voltage, I is the current (50 A) and l is the length of the cable in the conduit where strands and bundles can be in contact.

The maximum in R_c , under full load turns up already between 10 and 100 cycles for the first triplet cabling stage R_c , just as it was found for the CS1, CS2 and TFMC Nb₃Sn Model Coil CICC's tested without the LVF-method [127, 165, 166, 167]. The increase is by about a factor 5 in the triplet R_c when compared to the virgin level, which actually is the minimum value. The narrow spike is caused by switching from a 470 kN/m peak load to 650 kN/m after 1000 cycles. After the increase of the peak load, R_c gradually reduces to the initial trend. Beyond the first maximum at about 10 cycles, the R_c gradually decreases but not more than 10% after 40,000 cycles.

The dependency of the triplet R_c on the applied load at successive stages of the loading history is depicted in Figure 6.15. The error bar in the R_c data is $\pm 5\%$. The inter-bundle R_c of the petal is much larger in virgin state than after cycling, while for the interstrand R_c the behaviour is opposite. The interstrand R_c increases due to disengagement of the strand-to-strand contacts, formed during the heat treatment, while the inter-bundle R_c evolution is controlled by the petal wraps.

Figure 6.15. Interstrand R_c within a triplet versus the applied force per meter with $F=470$ kN/m and accordingly 650 kN/m after a specific numbers of loading cycles at 4.2 K and 350 mT stationary magnetic field.

6.3.3 Comparison with Central Solenoid Model Coil results

The evolution of the coupling loss in the CS1 conductor in the press, presented before the full model coil CSMC was tested, is in very good agreement with the results obtained during the CSMC test campaign [34]. The inner layer of the CSMC with CS1 type of conductor showed an initial $n\tau$ of 280 ms, gradually decreasing to 100 ms after several loading cycles [22]. In addition the CS Insert featuring a conductor layout practically the same as in the CSMC, also subjected to the highest electromagnetic load, was tested for a large number of cycles as well and showed no further change of the coupling loss between 2,000 and 10,000 cycles ($n\tau=52$ ms).

This corresponds quite well to the results obtained in the press, showing a virgin $n\tau$ of 206 ms and monotonous levels of R_c and $n\tau$ after 10 full load cycles with $n\tau=83$ ms. The coupling loss is a function of the applied transverse load. In the press an $n\tau$ of 20 ms is found without load and 83 ms under load after 40,000 cycles. At lower number of cycles the ratio is less. This is in agreement with the CSMC results [34, 126]. The non-linear behaviour of the coupling loss as function of the field ramp rate as depicted in Figure 6.11, considered a consequence of the presence of multiple coupling loss time constants in such a complex cable, was also observed in the Model Coils [34].

6.3.4 Influence of a locked void fraction

The low coupling loss measured on samples prepared without the LVF method is associated with the relatively high void fraction of the sample [125, 167]. The tests provided the first proof that the void fraction is important for the potential decrease of coupling loss with cyclic loading of Nb₃Sn Cr plated CICC's. In addition, they were first signs of strand movement and disengagement in the CICC's. A void fraction above 36% in combination with the CS1 cable pattern and a transverse load of 650 kN/m, results in low coupling loss, close to the level of solely intra strand loss.

For a void fraction of 36% the coupling loss time constant $n\tau$ remains at a level of 83 ms for the CS1, after the initial reduction and does not change significantly anymore with cycling. This is represented in Figure 6.16. Here the contact resistances R_c in the CS2 and CS1 conductor samples with higher void fraction are about one order of magnitude above the loss in the CS1 in the LVF method. Note that the NbTi CICC conductor with Cr plated strands, having the same cable layout and no LVF method applied, but obviously without a reaction heat treatment, reaches much higher levels of R_c . However, after the fast initial increase, the steady reduction spans three orders of magnitude and seems not to saturate after 40,000 cycles.

Figure 6.16. Interstrand contact resistance R_c versus number of cycles for the CS1 conductor sample prepared with and without LVF, a CS2 and a Cr plated NbTi conductor also both without LVF, at 4.2 K and a stationary magnetic field of 350 mT.

6.3.5 Effect of the void fraction

In particular the samples with locked void fraction, CS1, TFMC-A and TFMC-B are relevant for further investigation of the effect of the void fraction on coupling loss and interstrand contact resistance. Figure 6.17 shows the total AC loss per cycle, per unit volume, for the TFMC-A conductor with a void fraction of 26.4%. Again the characteristic early reduction is observed of the coupling loss (under full load) with cycling between initial state and 100 cycles. A further decrease follows gradually towards 40,000 cycles. The coupling loss time constant $n\tau$ of multistage CICC's for

fusion magnets is defined by the initial slope of the loss versus frequency curve. This $n\tau$ represents the loss at lowest frequencies. At moderate frequencies it gives an overestimation, in this case roughly by a factor of two, while for higher frequencies it may even underestimate the loss compared to the classical single time constant model (STC, see Figure 6.17).

Figure 6.17. AC loss versus the frequency in the virgin state, of TFM-A after 100 and 40,000 cycles, measured with a load of 650 kN/m. The dashed line represents the calculated loss from a single time constant model (STC), based on the initial slope of the initial state.

Figure 6.18 shows that the $n\tau$ (determined from the initial slope of the loss versus frequency curve) for TFM-A and TFM-B is significantly higher than for the TFM-0 with 38.7% void fraction. The TFM-0 compares well to the other CICC's with void fractions in the same range. It is observed that the decrease of $n\tau$ with cycling is slowed down in the case of a lower void fraction.

Figure 6.18. Evolution of the coupling loss time constant (from initial slope) in various TF and CS Model Coil conductor samples from initial state to 40,000 cycles and under full load of 650 kN/m, at 4.2 K, 350 mT stationary field and 150 mT amplitude.

This is also illustrated in Figure 6.19, in which the $n\tau$, divided by the number of superconducting strands, is plot against the void fraction. The error bar of the $n\tau$ is ± 0.1 ms. It was shown earlier that as a rule of thumb, the $n\tau$ scales rather well with the number of superconducting strands in CICC's when the sequential cabling twist pitches are not deviating too much from those in the average ITER conductor concept [167]. In both figures it is noticed that the final $n\tau$ in the low void fraction TFMC samples, remains above even the virgin $n\tau$ of CICC's with around 36% void fraction.

Figure 6.19. Coupling loss time constant divided by the number of superconducting strands (initial slope) in virgin, initial state and after 40,000 cycles under full load for the TF and CS Model Coil conductor samples TFMC, CS1 and CS2, versus the void fraction, at 4.2 K, 350 mT stationary field and 150 mT amplitude (lines are guide to the eye).

For the CS1 conductor samples prepared with the LVF method and 35.9% void fraction, the $n\tau$ is after 10 cycles close to the $n\tau$ after 40,000 cycles. For the TFMC-A and TFMC-B samples, the $n\tau$ evolves much more gradually with cycling and is still not saturated after 40,000 cycles [125].

6.3.6 Effect of cycling on contact resistance

The evolution of the intra-triplet (1st cabling stage of three strands) and inter bundle R_c with cyclic loading, is compared for samples TFMC-A and TFMC-B in Figure 6.20. The intra-petal R_c is at very low level due to the high compaction, while the inter-petal R_c is of the usual level as observed during earlier tests of other Nb₃Sn CICC's in the press. In general it shows that the inter-petal R_c hardly depends on the void fraction. The interstrand R_c is about the same for samples TFMC-A and TFMC-B but the sub-petal strand bundle R_c are clearly different. This can be observed more plainly in Figure 6.21 illustrating the strong relation between level and evolution of R_c and compaction for samples TFMC-A, TFMC-B and CS1-LVF. R_c raises fast to a high level for large void fraction while it remains rather low, increasing just slowly, at low void fraction. Figure 6.22 illustrates the effect of the void fraction on the intra-triplet R_c in initial state, after 40 cycles and 40,000 cycles versus the void fraction. Low void fraction corresponds to low R_c but essential is that it remains just rather low with cycling.

Figure 6.20. Inter strand and bundle R_c of the TFM samples A and B versus the number of cycles, $F=650$ kN/m, at 4.2 K (the lines connecting the data are a guide to the eye).

Figure 6.21. Evolution of the inter-sub-petal bundle R_c of the TF and CS1 Model Coil conductor samples with various void fractions from initial state, subsequently with load up to 40,000 cycles, at 4.2 K and 350 mT stationary magnetic field. The CS1 sample is measured under full load with 470 kN/m until 1,000 cycles, then 650 kN/m, causing a reduction in R_c .

Figure 6.22. Intra-triplet R_c of the TF and CS Model Coil conductors versus the initial void fraction, $F=650$ kN/m, at 4.2 K.

6.4 Transverse cable stiffness and mechanical loss

6.4.1 Introduction

The transverse loading by Lorentz forces in a cyclic mode imposes a periodic magnetic load along each strand, but also accumulated load from strands in other layers transferred by the strand-to-strand contacts. These cyclic forces cause a deformation of the cable inside the conduit, which goes along with mechanical effects, besides the electromagnetic and thermo hydraulic influences [41]. Although some studies on mechanical behaviour of cables in conduits were known [175, 176], the cable behaviour for a very large number of cycles under relevant electromagnetic conditions was never investigated. The cable press presented in this chapter is very well equipped for simulating the impact of Lorentz forces on a conductor, equivalent to ITER magnet operating conditions, and studying the mechanical effects.

Because of the cable compression, the helium flow resistance is affected by the so-called ‘third-channel’ between conduit wall and strand bundle created by the compression of the cable and this will influence the thermo hydraulic properties and thus the local stability of the cable as well [126] (see Figure 6.23). The first channel, the central cooling channel, is not affected. The strand bundle changes density from top to bottom in the case of accumulating Lorentz forces while in the press, the volume force causes a uniform increase of density.

Figure 6.23. Cross section of the CS1 conductor (LVF) after 40,000 cycles in the press. The gap at the top (third channel) shows the degree of plastic deformation.

The mechanical properties in terms of transverse Young's modulus and cable compression are indispensable for the analyses of the impact of strain degradation on the Nb₃Sn transport properties of the strands [74, 144].

In addition, the transverse stiffness of the cable is also of importance for the mechanical loss associated with the cable deformation. Since the acceptable overall AC loss and power dissipation is restricted, knowledge of the mechanical loss is also necessary. Both, the transverse stiffness and mechanical loss were experimentally determined on several prototype Nb₃Sn ITER type of conductor samples in the press and an overview of the results is presented.

6.4.2 Cable deformation

The compression of the cable is defined as the measured displacement between both jacket halves (see Figure 6.8 and Figure 6.9). Characteristic hysteresis loops of the displacement versus the applied load following subsequent cycling with a peak load of 650 kN/m are shown in Figure 6.24 for a CS1 conductor with 36% void fraction and prepared following the LVF method.

Figure 6.24. Cable compression (jacket displacement) as a function of applied force after for different number of cycles from 1 to 40,000 for the CS1 conductor sample, $F=470$ and 650 kN/m and 4.2 K (the lower curves are for increasing force).

The transverse loading leads to elastic and plastic deformation of the CS1 cable inside the conduit. Figure 6.24 shows the $d(F)$ curves after various numbers of cycles at both applied peak loads. Already after the first cycle the compression of the cable is about $700\text{ }\mu\text{m}$ on a diameter of 38 mm . The total compression of the bundle goes up to 1.1 mm with a plastic component of less than about 50% . This is in good agreement with the dimensions of the cross section measured after the experiment as depicted in Figure 6.23. The oval shape of the cable is clearly visible when compared with the circular contour of the conduit inner wall. The measured cable width is 37.8 mm , which corresponds to the original diameter, while the height after deformation amounts to 37.2 mm . A plastic compression is left of 0.6 mm .

The hysteresis loops for the displacement versus applied load for a TFMC type of conductor with 29.7% void fraction prepared following the LVF method is displayed in

Figure 6.25. The largest deformation of more than 400 μm is connected to the largest hysteresis loop area and occurs during the first cycle.

Figure 6.25. Cable compression (jacket displacement) as a function of applied force for different number of cycles from 1 to 40,000 for the TFMC Nb_3Sn conductor with 29.7% void fraction, at 4.2 K (the lower curves are for increasing force).

The conductor maximum deformation increases further up to 40,000 cycles at about 800 μm . Between 100 and 10,000 cycles, the shape and size of the hysteresis loops changes only little but the peak displacement at full load still remains increasing. This is well illustrated in Figure 6.26 where the cable deformation at zero ($d\text{-pl}$) and peak ($d\text{-max}$) load is given as a function of the number of cycles for three conductors with void fractions varying from 26% to 36%.

Figure 6.26. Cable compression (jacket displacement) for peak load ($d\text{-max}$) and released load ($d\text{-pl}$, from plastic deformation) as a function of the number of cycles for the CS1, TFMC-A and TFMC-B Nb_3Sn conductor samples with void fractions varying from 26 to 36 % (the peak load is changing for the CS1 conductor after 1000 cycles from 470 kN/m to 650 kN/m).

The largest compression occurs during the first few cycles and after 100 cycles the compression increases very modest while after several thousands of cycles it accelerates again. This behaviour is confirmed by larger series of press sample measurements on ITER TF and CS Nb₃Sn conductors performed later on but these results are outside the scope of this thesis. The CS1, TFMC-A and TFMC-B Nb₃Sn conductor samples were all tested using the LVF preparation method. The evolution of the CS1 conductor is less gradual because the peak load is changing for the CS1 conductor after 1,000 cycles from 470 to 650 kN/m. It appears that the plastic deformation or permanent deflection at zero load is about half the value reached at peak load.

The compression at peak load and permanent deformation after release of the load strongly depend on the initial void fraction. In Figure 6.27 it is demonstrated that the maximum deformation at peak load during the first load and final load after cycling increases more for higher void fraction. The same Figure 6.27 shows the average deformation per strand (or strand layer). The average deformation per strand is defined as the total cable compression divided by the number of strand layers in the cable. The number of layers in the cable is defined as the square root of the total number of strands in the cable. For the highest void fractions, larger than 36%, the LVF preparation method was not applied since it was introduced after testing of these samples.

Figure 6.27. Cable compression (jacket displacement) for cycle 1 and cycle 40,000 and the average final deformation per strand after 40,000 cycles, versus the initial void fraction of the tested CS1, CS2, TFMC, TFMC-A and TFMC-B Model Coil conductors.

The overall elastic modulus in transversal direction E_y defined as

$$E_y = \frac{\sigma_y}{\varepsilon_y}, \quad [\text{Pa}] \quad (6.6)$$

where σ is the stress, E is the elastic modulus, and ε is the strain that occurs under the given stress, similar to Hooke's Law. It can also be reformulated as:

$$E_y = \frac{D \cdot F_y}{A_y \cdot d_y}, \quad [\text{Pa}] \quad (6.7)$$

where D is the cable diameter, A_y is the average longitudinal cable cross section (projected cable area) and F_y is the transversal force (representing stress) and d_y is the absolute cable compaction defined relative to the virgin (unloaded) condition.

The overall elastic modulus versus the applied force of the TFMC-A conductor is presented in Figure 6.28 for different stages of the cycling history. It is observed that the E_y slightly reduces with increasing number of cycles.

Figure 6.29 shows how the overall modulus depends on the initial void fraction and this dependency is presented in Figure 6.30 for three load levels. There is a strong dependency on the void fraction and this is the reason why the first samples, which were all tested without LVF method, the CS1, CS2 and TFMC, all no-LVF, have a small overall modulus.

Figure 6.28. Overall E_y modulus for cable compression versus applied load for different load cycles for the TFMC A, Nb_3Sn conductor at 4.2 K. The symbols are measured data and are connected by lines as a guide for the eye.

The relaxation of the samples being prepared without using the LVF method is not restricted at unloading, which leads to a large deflection in combination with a low stiffness. Figure 6.31 shows that, besides cycling history, also the peak load determining the maximum deformation, influences the modulus. The E_y of the conductor with lowest void fraction reaches a level of more than 4 GPa at a load of 650 kN/m. For the stiffness of the cable at a certain level of stress (and strain), the dynamic elastic modulus can be defined as:

$$\frac{\delta\sigma_y}{\delta\varepsilon_y} = \frac{D \cdot \delta F_y}{A_y \cdot \delta d_y}, \quad [\text{Pa}] \quad (6.8)$$

where σ_y is the transversal stress and ε_y is the transversal strain. In Figure 6.32 it is shown that the $\partial\sigma_y/\partial\varepsilon_y$ of Nb_3Sn conductors progresses with the applied load and approaches a level of 50 GPa at a peak load of 600 kN/m. In Figure 6.33 it is illustrated that the increase of $\partial\sigma_y/\partial\varepsilon_y$ with applied load is not largely dependent of the initial void fraction.

Figure 6.29. Overall E_y modulus for cable compression versus applied load (for a cycle between 10 and 100 cycles) for various Nb_3Sn CICC conductors with different void fractions at 4.2 K (TFMCA 26.4%, TFMCB 29.7%, CS1 35.9%, TFMCA no LVF 38.7%, CS1 no LVF 38.8% and CS2 no LVF 39.1%).

Figure 6.30. Overall E_y modulus for cable compression versus void fraction for different applied loads at 10 to 100 cycles for the tested Nb_3Sn conductors.

Figure 6.31. Overall E_y modulus for cable deformation versus applied load for the CS1 Nb_3Sn conductor prepared with the LVF method, with a peak load of 450 kN/m up to 1,000 cycles and 650 kN/m beyond 1,000 cycles at 4.2 K.

Figure 6.32. Dynamic modulus of cable compression versus applied load up to 40,000 cycles for the TFMC Nb₃Sn conductor with void fraction 26.4% at 4.2 K.

Figure 6.33. Dynamic modulus of cable compression versus applied load after 40,000 cycles for the CS1, TFMC-A and TFMC-B Nb₃Sn conductors with void fractions from 26 to 36% at 4.2 K.

This means that although the overall compression is different and depending on the initial void fraction, the effective stiffness is similar for all these conductors after having passed the initial deformation phase of the first few hundred cycles. This observation correlates well with the change of the mechanical loss per cycle, also saturating after about 100 cycles. The compression of the CS1 sample in the press of 1.1 mm (Figure 6.23 and Figure 6.24) is in very good agreement with the interpretation of the pressure drop found in the Model Coil tests. The pressure drop can be explained by the appearance of a gap of 1.3 mm between conduit and cable caused by the Lorentz forces [126].

In [177] it is stated that the overall modulus of elasticity of the strand is about 1.4 GPa for a load of 100 N per crossing strands. For 33 strand layers in an ITER conductor, a current of 45 A, a magnet field of 12 T and a distance between strand crossings of 6 mm an accumulated peak load of 106 N is reached [129]. Note that the $I \times B$ load of a

full-size conductor with 1152 strands, times 45 A in 12 T, amounts to 620 kN/m. Comparing this to the results obtained on full-size conductors when transversally loaded in the Twente Cryogenic Press [125] (see Figure 6.31), it is noted that the cable transverse stiffness is about half of the value measured on a single strand. In Figure 6.29 the moduli of CS and TF conductors with different void fractions are depicted and we refer here to a conductor with 35.9 % void fraction (CS1), like in Figure 6.31 as well.

Although the strand properties may be somewhat different in the CS and TF conductors, for which other strand types were applied, it appears that roughly the result of the crossing-strands probe matches the results in Figure 6.31 measured on full-size conductors. In section 4.4.2, it was found that the stiffness of the strands at higher uniform stress amounts to 1.2 for BR and 1.5 GPa for IT, respectively. For crossing-strands, the stiffness of the strands peaks at a stress of 20 MPa to about 4.0 GPa and then saturates to 2.5 GPa for BR. For IT it gradually increases to 2.5 GPa towards a transverse stress of 90 MPa. These values are in the same range as found for the PIT strand in section 4.3.2. The low effective stiffness of the cable bundle in the full-size conductor is due to strand bending, causing a reduced transverse stiffness of the cable bundle [125]. Note that a lower void fraction leads to a higher transverse cable stiffness, which is caused by the limited space available for bending as demonstrated by the TEMLOP model and presented further on and in [144].

6.4.3 Mechanical loss

For determining the mechanical loss, the total loss dissipated in one complete loading cycle is computed using

$$Q_M = \oint F_y \cdot dy. \quad [\text{J/cycle}] \quad (6.9)$$

The mechanical loss per unit strand volume and per cycle at a peak load of 650 kN/m for different numbers of cycles on Nb₃Sn conductors is shown in Figure 6.34.

Figure 6.34. Mechanical loss per unit cable length per cycle for cable compression as function of the number of cycles for the CS1 and TFCM model coil Nb₃Sn conductors with void fractions from 26 to 39% at 4.2 K.

The loss during the initial cycles in Nb₃Sn conductors is 200 to 300 mJ/cm³, subsequently decreasing to below 30 mJ/cm³ during the first 100 cycles and remaining then in a range between 20 and 30 mJ/cm³. The mechanical loss hardly depends on the void fraction up to 36% as illustrated in Figure 6.35 although it is affected by using the LVF method.

Figure 6.35. Mechanical loss per unit strand volume per cycle for cable compression as a function of void fraction for the first cycle, cycle 100 and cycle 40,000 for the CS1, CS2, TFMC, TFMC-A and TFMC-B Nb₃Sn conductors with void fractions from 26 to 39% at 4.2 K.

6.4.3.1 Mechanical deformation affecting performance

Already after the first cycle, the deflection of the CS1 cable is about 700 µm on a diameter of 38 mm. When proceeding the total compression reaches 1.1 mm with a plastic component of less than about 50%. The largest compression of a TFMC type of conductor with 29.7% void fraction of more than 400 µm also occurs during the first cycle. The conductor maximum compression increases to even beyond 10,000 cycles, before saturation is reached of 800 µm.

The largest degradation of performance under full load occurs during the first few cycles. According to the results presented in Figure 6.24 and Figure 6.25, no definitive saturation can be accomplished during the following thousands of loading cycles. A continuous further increase of compression is expected. A similar effect was also observed during the crossing strands tests as for example displayed in Figure 5.30, where the compression during cycling with the same peak load continues to increase.

Analogous behaviour is also expected when testing short samples of ITER Nb₃Sn CICC and Model Coils. Characteristic examples of the evolution of the current sharing temperature T_{cs} in ITER TF qualification samples tested in Sultan (CRPP, Villigen), are shown in Figure 6.36. Indeed a sharp drop is observed during the initial cycles and mostly a consistent decrease on further cycling.

Figure 6.36. Examples of the evolution of the current sharing temperature T_{cs} in ITER TF qualification samples and tested in Sultan (CRPP, Villigen), showing a sharp drop during the first few cycles and mostly a consistent decrease along further cycling (courtesy A. Devred, ITER Organization, Dec 2012). For all these samples differences in preparation and one bronze processed strand type was used.

The expectation following the model coil test program (e.g., CS Insert coil tested in 2000) was that the conductor performance could arrive at stable behaviour after a few thousand EM cycles but in fact the T_{cs} kept decreasing with further cycling (Figure 1.19). A similar behaviour was found for practically all full-size TF samples (see Figure 6.37) and the performance is consistent with the strand measurements performed with PACMAN. In Figure 6.38 the good correlation between T_{cs} of Sultan TF samples and I_c at -0.5% compressive strain from PACMAN is nicely demonstrated.

Figure 6.37. Examples of the current sharing temperature T_{cs} in ITER TF internal tin type qualification samples tested in Sultan (CRPP, Villigen), showing a consistent drop along the first 1000 cycles (courtesy A. Devred, ITER Organization, Dec 2012).

The overall cable compression is transferred into local strand deformations like strand-to-strand point and line contacts and periodic bending, leading to a reduction of the critical current and n -value [118, 122, 178]. The dependency between overall compression and void fraction is shown in Figure 6.27. In order to scale the experimental values to an estimated ‘bending and pinching’ deflection of the strands, it is assumed that the press is simulating the so-called ‘accumulated load’ condition. The total compression divided by the square root of the number of strands leads to an approximate deflection per strand of about 33 μm for the CS1 type of conductor [89] while it is limited to 24 μm for TFMC-B and 20 μm for TFMC-A [125].

Figure 6.38. Average current sharing temperature T_{cs} for each conductor type versus average I_c (measured on ITER barrels) and rescaled to -0.5% intrinsic strain using Twente PACMAN data of the strands used in the SULTAN qualification samples (courtesy A. Devred, ITER Organization, Dec 2012).

Along with cycling, the strain distribution in the filaments is broadened permanently and thus irreversible [73], which results into a permanent reduction in performance, even after release of the load (electromagnetic force). This does not mean that filament cracks are imposed but it cannot be excluded either if the deformation exceeds the irreversibility strain limit of the filaments.

In Figure 6.33 it is shown that the $\partial\sigma_y/\partial\varepsilon_y$ of the conductors tested gradually follow the same tendency and can reach up to 50 GPa. This is an indication that the void fraction is not important anymore after a large number of cycles and the compression dynamics may be dominated by the strand-to-strand contact mechanical properties.

In section 4.4.2 for the transverse Young's modulus E_t , the strain is taken as the strand deflection divided by the strand diameter of 0.82 mm. Taking these approximations, the stiffness of the strands at higher stress level amounts to 1.2 GPa for bronze route and 1.5 GPa for internal tin conductors. The dynamic modulus $\delta\sigma/\delta\varepsilon$ at higher stress level is about 2.0 GPa for both bronze route and internal tin conductors. For crossing-strands, the stiffness of the strands saturates to 2.5 GPa towards a stress of 90 MPa. The

dynamic modulus $\delta\sigma/\delta\varepsilon$ at higher stress level is about 3 to 4 GPa for both bronze route and internal tin conductors.

The main difference between the cable compression test and the TARSIS strand test is the cross sectional stress distribution. The load on a strand in the TARSIS crossing-strand set-up is applied only in the vertical plane, in one direction, and the strand can freely deform in the horizontal plane (ignoring the axial direction because of the friction) [73]. The strands in the cable set-up are limited by the surrounding strands in first instance and the whole cable eventually by the jacket wall (see Figure 6.23). This observation can explain the large difference in $\partial\alpha_y/\partial\varepsilon_y$, effectively measured in the strand and cable tests. Moreover, the assessment of the contact area taken as projected strand and cable diameter times loaded length, may also give rise to differences since the real contact area in the case of crossing strands (Figure 4.8) is significantly smaller than the value used for the comparisons.

6.4.3.2 *Correlation between mechanical and electromagnetic losses*

A remarkable feature is the decrease of the coupling loss with cycling in Nb₃Sn CICC's. This phenomenon was observed in short sample tests with electromagnetic load [179], later also established in coils [34, 180] and studied in detail using the Twente Press [22, 55, 165, 166]. However, it is demonstrated that the span of this effect is strongly related to the combination of void fraction and applied transverse force [22, 125]. It is also observed that after the well-known initial decrease of the interstrand coupling loss during the first hundred cycles, the long-term cycling behaviour appears different for NbTi and Nb₃Sn CICC's. The coupling loss of NbTi CICC's can increase again and may, depending on the type of coating, exceed the level of the virgin conductor appreciably [22] while in Nb₃Sn CICC's the coupling loss remains practically constant at the same relatively low level, reached after about one hundred cycles.

The mechanical loss for a full load cycle from zero to peak load, remains below 30 kJ/m³ strand volume, except during the initial loading cycles where the loss is about 200 kJ/m³. The mechanical loss adds up to the electromagnetic eddy current, coupling current, hysteresis and transport losses.

The electromagnetic coupling loss for a partial magnet charging cycle in a Central Solenoid coil with $n\tau=100$ ms, $\Delta B=2$ T, $dB/dt=1.2$ T/s at peak magnet fields, corresponding to the plasma breakdown phase of the ITER CS operating scenario, amounts to some 400 kJ/m³. The mechanical loss representative for a similar magnet field cycle near peak load is about 0.5 kJ/m³ [164, 165] and thus negligible with respect to the total coupling loss production during such a cycle.

A trapezoidal field cycle, for example between zero and 6 T with a ramp rate of 2 T/s on the same conductor, causes a coupling loss of almost 2000 kJ/m³. When the same field sweep is performed with a lower ramp rate of 0.05 T/s, the coupling loss amounts to 50 kJ/m³, which is then still much less than the mechanical loss.

As the electromagnetic hysteresis loss is independent from the ramp rate of the applied field, the loss per cycle for ITER type of Nb₃Sn strands varies from about 100 to 500 kJ/m³ strand volume, for a $\Delta B=6$ T magnetic field sweep around zero. For high current density strands, as applied in the dipole magnet in the EDIPO test facility, the hysteresis loss can reach even 2000 kJ/m³. The mechanical loss per cycle can thus be neglected for most advanced ITER strands. For ITER NbTi strands the electromagnetic hysteresis loss is in the order of magnitude of 100 kJ/m³. A mechanical loss of 20 to 50 kJ/m³ or even during the first operation cycles 100 kJ/m³ can be relevant.

Although mechanical losses are not a critical issue for ITER operation, the heat dissipation associated to only the mechanical loss cannot always be neglected compared to losses from solely electromagnetic origin. In particular, for ITER NbTi CICC conductors or in the case of slow ramp rates the mechanical losses are in the same range as the electromagnetic losses and may be relevant for understanding ITER magnet operation and interpretation of model coil test results.

It is interesting to compare the mechanical loss of the Nb₃Sn conductors in Figure 6.34 to the acoustic emission (AE) analysis performed on the Central Solenoid Model Coil (CSMC). At first the AE signals were considered to be induced by global motion of the cable in the conduit and motion of the entire conductors [181, 182]. However, the number of AE events decreased by a factor of five during the initial five cycles and saturated accordingly for later ramps. Although it is not clear to what extent both methods correlate quantitatively, the results obtained by AE energy correspond quantitatively nicely to the mechanical loss as measured with the Twente Press and demonstrated in Figure 6.34.

6.5 Conclusion

Concerning the evolution with time of the coupling loss and mechanical compression characteristics on similar CIC conductors, there is a good quantitative agreement between the results from the Twente Press and those obtained during the CS model coil test campaign. After the immediate decrease during the first 10 to 100 loading cycles, the level of coupling loss remains practically constant up to at least 40,000 cycles with an $n\tau$ under load of some 80 ms (Model Coils' $n\tau$: 50 to 100 ms).

The decrease of the coupling loss with progressive cyclic loading is less pronounced in the case of lower void fraction in Nb₃Sn CICC's. For a TFMC conductor with 26% void fraction, a virgin $n\tau$ of 470 ms is observed, finally decreasing to some 170 ms but only after 40,000 cycles. At 30% void fraction the virgin $n\tau$ is 360 ms with 115 ms left after cycling. It is thought that the transverse sub-petal bundle R_c mainly determines the evolution in coupling loss rather than the intra-triplet R_c .

The maximum transverse compression at a peak load of 650 kN/m after cycling for Nb₃Sn conductors varies from 640 to 1130 μm when the void fraction increases from 26

to 36%. This average deflection per strand is representative for a peak load of about 20 kN/m on the strands in the accumulated load zone of the ITER CICC's. The average deformation per strand, in terms of transverse compression at a peak load of 650 kN/m, amounts to 33 μm for 36% (CS1), 24 μm for 30% and 20 μm for 26% void fraction.

The stiffness and mechanical loss depend on the peak load, loading history, cable pattern, void fraction and specific strand properties and change with the number of load cycles.

The mechanical loss per unit strand volume per cycle at a peak load of 650 kN/m during the initial cycles in Nb_3Sn conductors is 200 to 300 kJ/m^3 , subsequently decreasing to below 30 kJ/m^3 during the first 100 cycles and remaining then in a range between 20 and 30 kJ/m^3 . The mechanical loss hardly depends on the void fraction.

The largest plastic deformation of the cable and associated critical current degradation, occurs during only the first few cycles and no clear saturation is accomplished during the following thousands of loading cycles.

7 Optimisation of the transverse load on strands in Nb₃Sn CICC

In the years following the Central Solenoid Model Coil tests, the various Nb₃Sn CICC tested for ITER showed a significant degradation in their performance due to the electromagnetic load on the strands and cycling as well. Not only the differences in thermal contraction of the materials in the composite affect the critical current and temperature margin, but in particular the electromagnetic forces cause significant strain in transverse strand contacts and bending strain in the Nb₃Sn layers.

A new model called Transverse Electro-Magnetic Load Optimisation (TEMLOP), has been developed for analyzing the transverse load degradation in Nb₃Sn CICC, based on measured strand and cable properties. It is capable to predict how such degradation can be minimized.

The model uses data describing the behaviour of single (TFMC) strands under periodic bending and contact loads as measured with the TARSIS setup and enables a discrimination in performance reduction per specific load and strand type. In addition, data extracted from transverse load testing of entire CICC sections are crucial input for the model.

The most important conclusion of the model computations is that the observed severe degradation of large CICC can be drastically and straightforwardly improved by for example increasing the pitch length of subsequent cabling stages, in combination with a smaller void fraction. It was for the first time that an increase of the sub cable pitches was proposed though no experimental data were available to confirm the outcome of the TEMLOP model. The model also convincingly shows that strand bending is the dominant mechanism causing degradation. The model gives an accurate description for the mechanical response of strands to a transverse load, from layer to layer in the cable.

The content of this chapter is for a large part based on papers [74], [102], [125], [144], [195] and [216].

7.1 Introduction

The envisaged CICC for ITER comprise more than 1000 strands with a diameter of about 0.8 mm. The conductors have a void fraction v_f of 33% and are cabled by twisting in several stages, thus creating a wavy pattern of strands throughout the cable. The conductors are carrying more than 50 kA in a magnetic field locally exceeding 13 T, causing a severe transverse loading due to Lorentz force. This imposes distributed magnetic loads along each strand, but also cumulated loads from other strands transferred by strand-to-strand contacts. These bending and contact loads on the Nb₃Sn strands affect the critical current and create a periodic strain variation along filaments. Magnitude and periodicity of the periodic strain pattern in combination with the ability of a strand to redistribute the current between the filaments determines the impact on the critical current (I_c) and n -value [122, 129, 183].

Since the first tests of the Central Solenoid Model Coil (CSMC) in Japan, a lot of effort was spent to the understanding of the unexpected severe degradation of the conductors [33], [38], [39], [40], [41], [49], [184], [185], [186], [187], [188], [189], [190], [191], [192], [193], [194], [195], [196], [197] and [198]. Although many papers were published on this subject, they mostly just describe the degradation also giving possible explanations varying from sometimes-severe current non-uniformity to often-severe strand bending. Most of the explanations were supported by some modeling but unfortunately, none of these models was able to provide an adequate solution to the problem although it was suggested that a lower void fraction and shorter cabling pitches could partly confine the degradation [38]. In the meantime, it was proven experimentally that a higher level of local mechanical support of strands can improve the performance significantly [42] and accordingly the ITER conductor design was already slightly modified compared to the CSMC layout [43]. It was assumed that the resistance to degradation increases by using a steel jacket, providing thermal pre-compression in reducing the tensile strain levels associated with strand bending [38, 44]. The void fraction was reduced from 36 to 33% and the non-copper material in the cross section (superconducting Nb₃Sn) was increased by 25%. In addition, newly developed, so-called high- J_c Nb₃Sn strands are being pursued attempting to compensate for the performance loss.

At that time, it seemed accepted that the transverse load inherently led to a severe degradation in the transport properties due to local strand deformation. Some of the analysts in the fusion community indicated that about 30 to 40% of the Nb₃Sn layers in the strands of the cable was breaking [38, 43]. When true it would mean that the cable in conduit concept for large ITER type of conductors was beyond efficient application. In view of this, the development of advanced high- J_c strands would not be effective either, as a significant fraction of the Nb₃Sn material would immediately break in the cable. The European Union ITER Participant Team (EU-PT) tested by middle of 2006 two full-size conductor samples with four types of advanced Nb₃Sn strands. For ITER,

the conductor requirements are fulfilled when the current sharing temperature, measured at the nominal ITER operating conditions is 5.7 K or higher. While the nominal operating current of the ITER TF coils is 68 kA, the maximum magnetic field in the conductor cross-section varies between 10.35 and 11.85 T. The EU samples showed performance much below expectation with regard to strand performance and below the ITER TF conductor design criterion for T_{cs} of 5.7 K [83, 84]. This poor performance was attributed to a higher sensitivity to deformation of the newly developed advanced strands. It was evident that a method to overcome this severe degradation would lead to significant cost savings when leading to use of less superconducting strand or a significant improvement of the ITER magnets performance in terms of operation margin.

The model for Transverse Electro-Magnetic Load Optimisation (TEMLOP) revealed a straightforward and economical method to overcome a significant part of this excessive reduction in performance. The model, describing the mechanical response of individual strands within a cable bundle, clearly shows that applying longer cabling pitches provides a convenient solution against transverse load degradation. Insufficient practical experience with varying cabling pitches, explains why this solution remained hidden so long. It was assumed though, that only shorter cabling pitches could lead to a better performance obviously by shortening the bending beam [38], which is indeed confirmed by the TEMLOP model as well. The model calculations on the cable design are in good agreement with the deformation experiments that were performed on full-size ITER conductors in the Twente Cryogenic Press, as well as with the critical current degradation observed in the CSMC tests [33].

A first essential parameter (deduced from the Press measurements) turns out to be the maximum possible compression of the cable, i.e. the available local space for bending and contact deformation of strands. The strand mechanical properties determined from tests with TARSIS form the second key input for TEMLOP. In section 7.2, a full description of the mechanical model is given and the required parameters to describe the cable layout are adequately identified. Then, in section 7.4, results of calculations with parametric variations in cabling pattern, void fraction and strand stiffness are presented. They are discussed in section 7.5, leading to a number of specific recommendations. The symbols used in the equations are listed in the List of symbols and Table 7.1.

7.2 Numerical model for cable mechanics under transverse load

7.2.1 General model assumptions

The TEMLOP numerical model describes mechanical interactions between strands within the cable, including strand bending, strand cross- and line contacts under influence of electromagnetic force. The plastic deformation of strands is not separately

distinguished although the overall quantitative analysis is improved by accounting for yielding in both, transverse and axial directions, at higher stress levels. This is incorporated in the description for the axial Young's modulus (see section 2.1.7). A way was found to improve the quantitative accuracy of the model. Axial strain variations, other than already included in the bending strain model are not considered. A possible influence of temperature variations and choice of conduit material with different thermal expansion are not considered here. The main input for the TEMLOP-Model is listed in Table 7.1. The input parameters are based on the layout of the CS1 type of conductor from the CSMC (see 0) and [41], while the strand properties are used from the TFMC internal tin strand manufactured by EM (Italy) [124, 129].

In the model, the shape of the cable cross section is simply taken square instead of round and no central channel is present. Although it is not complicated to account for the circular cross-section of the actual cable in the numerical model, preliminary results showed that the influence on the outcome is not significant. Omitting the central channel from the model corresponds to considering it as a fully stiff medium that transfers the reaction force and displacement without any intrusion.

Table 7.1. Input parameters for the TEMLOP-model.

Input parameters strand	
Diameter wire, d_s [μm]	810
Diameter filamentary region, d_f [μm]	660
Bending wavelength, L_w [mm]	6
E modulus strand (axial), E_i [GPa]	29
E modulus strand (transverse), $E_\perp$ or E_{tr} [GPa]	3.3
Input parameters cable	
Number of strands, N_s	1152
Number of layers, N_l	34
Cable void fraction, νf	0.36
Cos θ factor ($\cos^{-1} \theta$)	1.05
Cabling scheme CS Model Coil conductor	3x4x4x4x6
Cabling pitches CS Model Coil conductor [mm]	45x74x123x160x380
Cable-compression @ $F_{\max}$ measured in the cable Press, f_{mc} [mm]	1.134
Magnetic field, B [T]	12

7.2.2 Model for mechanical interaction of strands

The local transverse deformation of the strands in a CICC is composed of a distribution of bending, contact- and homogeneously distributed forces. The local spatial periodic bending, causing a wave-pattern [129, 199] can actually be described by a periodic

repetition of a point force with clamped ends, schematically shown in Figure 3.2 (left). The corresponding bending beam equations are

$$f_{sb} = \frac{F \cdot L_w^3}{192 \cdot E_{\parallel} \cdot I_a}, \quad [\text{m}] \quad (7.1)$$

$$\varepsilon_b = \frac{F \cdot L_w}{8 \cdot E_{\parallel} \cdot W_b}, \quad [-] \quad (7.2)$$

in which f_{sb} is the strand deflection, L_w is the bending wavelength (see Figure 7.1), $E_{\parallel}$ is the Young's modulus in axial direction, I_a is the moment of inertia while W_b is the section factor written as $d_s^3 \cdot \pi / 32$ with d_s is the strand diameter. The force F represents the local accumulated magnetic force on a strand in a given layer of the CICC.

The model for the mechanical interaction between strands is schematically represented in Figure 7.1 where three layers of strands (A,B,C) are depicted to illustrate the deformation for the force increasing in three steps.

Figure 7.1. Schematic model for mechanical interaction between crossing strands in the cable bundle from layer to layer with increasing electromagnetic force pointing from top to bottom.

The first case (Figure 7.1a) corresponds to the virgin situation before electromagnetic loading of the cable bundle. The second scheme in Figure 7.1b shows the situation when the deflection of strand 1 in layer B, characterised by the bending amplitude f_{sb1} , has virtually reached its maximum value and strand 1 is about to touch the surface of strand 2 in the layer C below. This situation constitutes a limiting case for bending with the wavelength characterised by L_{w1} . Strand 1 with the wavy pattern is still only

supported by the crossing strands associated with the wavelength indicated with L_{w1} , i.e. the transverse contact load on the strand crossings remains concentrated on the same number of strand crossings that were already present from zero load, on a distance L_{w1} . In this situation, the bending is fully described by the point force F_1 , the wavelength L_{w1} and the maximum bending amplitude f_{sbm1} . The transverse force is transmitted through the contact points between strand 1 and the two supporting strands 3 and 4.

With further increasing the force (Figure 7.1b), strand 1 with the wavy pattern will make contact with strand 2. Arriving at this stage the periodicity in load contacts increases by a factor two, while the maximum possible deflection f_{sb2} becomes half the initial value and the load F_2 becomes half F_1 . Hence, one can distinguish for a decrease by a factor two of the characteristic bending wavelength from L_{w1} to L_{w2} and a doubling of the number of contact loads. The transverse load, initially a reaction force only supported by the contacts spaced by the original periodicity in crossing strands represented by L_{w1} , becomes also partly distributed now by strand 2 from layer C below. The decrease in wavelength from L_{w1} to L_{w2} has two important effects: it restricts further deflection (see Equations 7.1 and 7.2 and it causes a more homogeneous distribution of the contact stress and point loads.

When the force is further increased by other strands in layer A, (not indicated in the picture) strand 1 will make contact with other strands from the same layer C as to which strand 2 belongs (Figure 7.1c). The result is a further successive decrease of the bending wavelength introducing now the more general description $L_{w,n}$, the point force F_n and the maximum possible deflection $f_{sb,n}$. At the same time a progressive increase of the contact area occurs, which furthermore becomes more homogeneously distributed. Both factors reduce the local peak contact stress.

Next, one can derive the maximum bending amplitude and strand deformation at the crossovers. The peak strain occurs at the strand crossings with the point loads. With reaching the maximum deflection and going from $L_{w,n}$ to $L_{w,n+1}$, the peak strain at the initial point load F_1 will not increase further (may actually decrease) but new maxima are created at other locations corresponding with the position of the F_2 point loads. To obtain the strain value that limits the critical current in a strand, the peak strain along the strand is considered.

7.2.3 Strand and cable deformations

The accumulation of Lorentz force F_n on the strands in a CICC is further explained in Figure 7.2 where the electromagnetic force per meter $I \times B$ increases progressively from the right to the left. At the same time, the local magnetic field decreases also in this direction, due to the combination of the applied field generated in the coil (or background field) and the cable self-field due to the transport current.

The local accumulated peak load in an ITER conductor with 1152 strands is about 20 kN/m, assuming the number of strand layers can be approximated by the square root of the total number of strands (N_s) in a cable [39, 129]

$$F = I_s \cdot B \cdot N_s^{0.5}. \quad [\text{N/m}] \quad (7.3)$$

The most favourable transverse load distribution is as homogeneous as possible, for strands in a cable corresponding to line contacts between parallel strands and avoiding local bending effects. In this case, the transverse peak stress $\sigma_{\max}$ is the local accumulated peak load in an ITER conductor of 20 kN/m on the projected area of a strand cross section d_s

$$\sigma_{\text{hom}} = \frac{I_s \cdot B \cdot N_s^{0.5}}{d_s}. \quad [\text{Pa}] \quad (7.4)$$

Figure 7.2. Magnetic field profile in the cable cross-section in relation to the accumulated load distribution. The red zone indicates the peak stress location while the green zone represents the linearly increasing magnetic field in opposite direction due to the self-field contribution.

The peak transverse stress in the homogeneous case σ_{hom} amounts to about 20 MPa. However, as soon as there is locally sufficient space available for strand bending, the load at the crossovers rises and will exceed the value determined for a homogeneous distribution, since the projected contact area A_c becomes smaller than in the case of an infinite line contact. The projected contact area between two crossing strands depends on the angle between the wires, φ , and on the strand diameter

$$A_c = \frac{d_s^2}{\sin \varphi}. \quad [\text{m}^2] \quad (7.5)$$

Correspondingly, the local transverse stress is multiplied with a contact area geometrical factor k

$$k = \frac{d_s \cdot L_w}{A_c} = \frac{L_w \cdot \sin \varphi}{d_s}. \quad [-] \quad (7.6)$$

Combining (4), (5) and (6) leads to the local peak stress

$$\sigma_{max} = \frac{\sigma_{hom} \cdot L_w \cdot \sin \varphi}{d_s}, \quad [\text{Pa}] \quad (7.7)$$

which can also be written as

$$\sigma_{max} = \frac{I_s \cdot B \cdot N_s^{0.5} \cdot L_w \cdot \sin \varphi}{d_s^2}. \quad [\text{Pa}] \quad (7.8)$$

Apart from the local electromagnetic forces and strand mechanical properties, the associated bending deflection is also determined by the distance between the supporting strands (Equation 7.1). This wavelength is in turn determined by the cabling pattern of the strand bundle and is connected to the number of elements per sub-cable and the twist pitches and finally by the load determining $L_{w(n)}$ (see Figure 7.1).

The actual average wavelength L_w for crossing strands (and bending) is determined from a disassembled full-size ITER CS1 conductor and is 6 mm with a standard deviation of 2 mm, while the crossing angle amounts to 45 degrees. For a so-called SeCRETS-A sub-size conductor with a practically identical cabling scheme, also a wavelength of 6 mm was found [200]. A photograph of the CS1 cable bundle in Figure 1.16 (left) shows clearly the deformation at strand crossings and varying periodicity.

A conductor with longer pitches, a CS2 type of conductor used for the outer module of the CSMC, is shown in Figure 1.16 (right). Here, the average L_w is difficult to determine as the positions of the crossings are less clear but it is definitively longer than 6 mm while the crossing angle φ amounts to about 10 degree. This implies that for a wavelength of 6 mm the accumulated load per strand crossing amounts to about 100 N. For a periodicity of $L_w=6$ mm of the CS1 conductor and a strand diameter of 0.81 mm, the local peak stress can reach a level of about 100 MPa.

Due to the cabling scheme in the ITER CICC (Table 7.1) in several cabling stages with subsequent twist pitches it seems unavoidable to have local stress/strain concentrations at the crossover contacts and bending in between them. The bending deflection itself is restricted in absolute sense by the average free space available in the cable bundle (Figure 7.3). When the cable bundle is represented by a square bundle of strands with N_l layers of strands and each layer containing N_l strands (in this case $N_l=34$), the maximum possible free distance for a strand to move before encountering a neighbouring strand can be derived. The numbering of the strand layers N_y goes from 1 to N_l , starting at the high field (low $I \times B$) side of the conductor.

For a given void fraction v_f , number of strands N_s and strand diameter d_s , the cable width D is calculated with

$$D = \left(\frac{N_s \cdot \pi \cdot d_s^2}{4 \cdot (1 - v_f) \cdot \cos \theta} \right)^{0.5}. \quad [\text{m}] \quad (7.9)$$

The maximum possible deflection available for bending for $N_l=(N_s)^{0.5}=34$ (f_{cbm}) is derived from the cable width D , the strand diameter d_s and the $\cos(\theta)$ factor, correcting

the strand cross-section area for the average angle between the twist pitches in the cabling with respect to the cable axis

$$f_{cbm} = D - N_s^{0.5} \cdot d_s \cdot \cos^{-0.5}(\theta). \quad [\text{m}] \quad (7.10)$$

Figure 7.3. Schematic representation of the cable model for mechanical compression to derive the initial free deflection for bending per strand.

The average $\cos(\theta)$ factor from the CS1 type of conductor in Figure 1.16, with angles for θ between roughly 15 and 20 degrees, is determined at 0.95 [201]. The average angle θ decreases with an increasing cabling pitch length, and is correlated to the cable void fraction. It is assumed that the $\cos(\theta)$ correction factor rises approximately linearly with the increase of the cable twist pitches. At the same time that the characteristic bending wavelength L_w also increases linearly with the twist pitch. As L_w is the variable input parameter for the model, the $\cos^{-1}(\theta)$ correction factor k_θ is defined as

$$\cos^{-1}(\theta) = k_\theta = 1 + \frac{\lambda}{L_w}, \quad [-] \quad (7.11)$$

with $\lambda=3 \cdot 10^{-3}$ m as empirically derived from the CS1 type of conductor for $k_\theta=1.05$ and $L_w=0.006$ m.

For a given void fraction, the space between the strands for maximum possible deflection (through bending) will change with varying wavelength, following Equation 7.11.

The maximum possible average deflection per strand f_{sm} is then calculated from the entire cable f_{cbm} as

$$f_{sbm} = \frac{f_{cbm}}{N_s^{0.5}}. \quad [\text{m}] \quad (7.12)$$

For the calculation of the maximum free deflection per strand the two dimensional free space direction is taken. Allowing for a 2D free space displacement, multiplication of the one-dimensional f_{sbm} by a factor $2^{0.5}$ is required. The real maximum possible deflection of the strands, however, is expected less than for a regular grid of parallel

strands with $\cos(\theta)=1$, as schematically presented in Figure 7.3. In a real cable, the spatial distribution of strands is not that regular at all.

Most of the free space in a cable determining the void fraction, seems concentrated at larger gaps likely between the higher cabling stages but not the final-one and is not available for the majority of the individual strands. This is illustrated by the CS1 cable cross section in Figure 6.23, where larger voids are surrounded by regions with rather highly compacted strands.

The theoretically maximum deflection becomes further restricted with the variation in the wavelengths, the angle of the strands with the surrounding strands and the friction between the strands (causing hysteresis). Using the photographs of conductor cross sections the average available space between strands is estimated and the theoretically maximum deflection per strand f_{sbm} by a factor of 3 and the effective f_{sbm} determined following

$$f_{sbm} = \frac{f_{cbm}}{3 \cdot N^{0.5}} \cdot [m] \quad (7.13)$$

For the CS1 type of cable with a void fraction of 0.36 and 6 mm wavelength, a maximum (average) possible bending deflection per strand f_{sbm} of 41 μm is calculated. This is still without accounting for contact deformation at the strand crossings but this mechanism is built-in further on.

Transverse stress-strain measurements performed with the TARSIS crossing-strands probe on the LMI strand [120, 145] as presented in Figure 7.4 are used for the calculations. It appears that the loading curve is practically without hysteresis when unloading.

Figure 7.4. Stress-strain characteristic of the LMI strand measured in the TARSIS crossing strands probe [120, 145].

The initial deformation requires only a very small load. The assumption here is that the initial strand deformation up to a diameter compression of almost 40 μm (5% strain) already occurs partly during cabling of the conductor, but mostly throughout the final

compacting step applied by swaging after inserting the cable in the conduit. Thereafter a constant E -modulus of 3.3 GPa for the strand transverse modulus of elasticity can be used, a value that matches well with the range reported in section 6.4.3.1.

The available space for bending in the virgin state is determined by the calculated f_{sbm} (Equation 7.13). When the cable is compressed by electromagnetic force (or mechanically in the press), the strands will deform not only by bending but also at the crossings and line contacts. As a result, the available space for bending will be further restricted. This is formulated in the model as

$$f_{sb} = f_{sbm} - f_{sc}, \quad [\text{m}] \quad (7.14)$$

where f_{sc} is the strand deformation per strand crossing and line contact and f_{sb} is the space for bending, decreasing with accumulating electromagnetic load from layer to layer in the cable.

The bending mechanism throughout the layers of the cable, is now described with Equations 7.1, 7.2 and 7.9 and the successive limitation of the possible deflection f_{sb-n} with increasing n for higher orders of L_w , limiting the possible peak strain in the strands with Equation 7.14.

The principle of contact stress evolution in the TEMLOP-model is depicted in Figure 7.1, where strand 1 from layer B is crossing under an angle φ with the strands belonging to layers A and C. By further increasing the load (Figure 7.1b), strand 1 with the wavy pattern will make contact with strand 2. The transverse load, initially a reaction force only supported by the contacts spaced by the original periodicity in crossing strands represented by L_{w1} , becomes also partly distributed now by strand 2 from layer C below.

Important is also the angle φ in determining the contact stress and this angle is directly connected to the cabling pitches (see Figure 1.16). Here it is assumed that the relation between L_w and the crossing angle φ is linear and inversely proportional for larger wavelengths. Using the crossing angle φ and L_w determined for the mentioned CS1 and CS2 type of conductors the following empirical value is found

$$L_w \cdot \sin(\varphi) = 4.3 \cdot 10^{-3}, \quad [\text{m}] \quad (7.15)$$

in which $4.3 \cdot 10^{-3}$ m is the characteristic length L_ϕ . Instead of Equation 7.5, the contact area A_c can now be expressed as

$$A_c = \frac{A_{c\perp} L_w}{L_\phi}, \quad [\text{m}^2] \quad (7.16)$$

where $A_{c\perp}$ is the minimum projected contact area for a strand crossing. Towards shorter wavelengths, the absolute minimum of this contact area is restricted by the square of the strand diameter, d_s . Thus, a hyperbolic description can be applied in the computation of the effective contact area in relation to L_w

$$A_c = d_s^2 \left(\frac{L_w^2}{L_\varphi^2} + 1 \right)^{0.5} \quad [\text{m}^2] \quad (7.17)$$

This contact area is further used in this chapter. The numerical method uses discrete steps for the increase of the contact area from $L_{w(n)}$ to $L_{w(n+1)}$, which also explains the discontinuities in some of the curves shown later on. The A_c increases stepwise from $L_{w(n)}$ to $L_{w(n+1)}$, proportional to n instead of doubling each time the contact area in order to make the strand layer interactions more gradual. This can be considered as a conservative approach with respect to the influence of contact stress.

Also a more gradual increase in A_c can be considered. In the scheme of Figure 7.1, strand 1 from layer B is crossing under a relatively large angle φ with the strands belonging to layers A and C. For a much smaller crossing angle, the crossing strands from layer C nearly form a line support contact for strand 1 from layer B. This is well illustrated by the cabling pattern in Figure 1.16 (right).

The effect of a limited bending deflection, further restricted by the deformation at the strand contacts at increasing load, combined with a progressively homogenisation of the contact load can be formulated as

$$A_c = (L_{wn} - 2 \cdot L_c) \cdot \frac{d_s}{\sin \varphi}, \quad [\text{m}^2] \quad (7.18)$$

in which L_c is the decreasing “characteristic bending length” as indicated in Figure 7.1c and L_l is the increasing length along strand 1 with almost homogenously distributed load: $L_l = L_w - 2 \cdot L_c$. The minimum value of A_c is given by Equation 7.5 (with $\varphi = 45^\circ$ for $L_w = 6 \text{ mm}$) starting in the virgin condition of the cable as represented by Figure 7.1a.

As soon as strand 1 meets strand 2, not only the bending wavelength becomes shorter but also at the same time, A_c becomes larger. The characteristic bending length L_c is derived from the standard bending beam formula for a point load with one clamped end and a supported end at the other side (but obviously also other descriptions can be used):

$$L_c = \left(\frac{48 \sqrt{5} \cdot f_{sb} \cdot E \cdot I}{F} \right)^{1/3} \quad [\text{m}] \quad (7.19)$$

It was shown that using Equation 7.19 leads to qualitatively the same conclusions as when using Equation 7.17.

Equations 7.1-7.17 in combination with the relevant strand parameters like the experimentally determined axial and transversal E -modulus describe the overall mechanical response of the cable to an electromagnetic force.

The void fraction determines the bending deflection limit and its influence is evaluated. L_w is a parameter directly corresponding to the cabling pitches and will be varied to demonstrate the influence on the conductor performance. The conductor performance in

terms of critical current reduction due to bending and contact stress is included by following experimentally determined relations presented hereafter.

7.3 Critical current reduction in strands

7.3.1 Bending strain

In section 3.4.3.1 the effect of electrical resistance between twisted filaments subjected to periodical distributed peak strains on current transfer is discussed. It was identified that, at least for the ITER Model Coil strands used in the CSMC and TFMC, the normalized critical current versus bending strain can be described by the so-called low resistivity regime representing full interfilament current transfer [145] (see Figure 7.5). The critical currents are determined with the I_c criterion of 10 $\mu\text{V/m}$.

Figure 7.5. Measured and normalized I_c versus applied bending strain (with polynomial fit) at 12 T and 4.2 K for the LMI strand used for the TFMC [120, 145] comparison to the computed curves for full and no inter-filament current transfer under bending.

It is noted that the experiments performed on strands that were swaged into a steel tube, see section 3.6 [131], to study the influence of axial thermal pre-compression from the stainless steel conduit on the cable bundle due to the higher coefficient of thermal expansion, did not follow the low transverse resistivity regime [202]. In Figure 7.6, the bronze strand indicated with VAC (from CS1-type), which was used for the inner layer CSMC production, showed that the normalized I_c versus bending strain is plainly below the predicted low-resistivity behaviour.

For the TEMLOP-model, it is assumed that the I_c reduction with increasing bending strain under axial pre-compression applies to the uncompressed strand condition (only strand cool-down strain). This is slightly less severe than the performance of the strand in a steel tube (with imposed axial and radial thermal compression). Therefore, the polynomial fit through the data in Figure 7.5 is taken to account for the bending strain

in the TEMLOP-model. In fact, this assumption and the factor 3 from Equation 7.13 mainly determine the quantitative accuracy of the model, but the qualitative prediction in performance remains unaffected.

For the layers of strands travelling from lowest load to the accumulated peak load, the magnetic field decreases from 12 T to 11 T (see Figure 7.2). As the critical current varies substantially in the cable cross section due to this field variation, this effect is accounted for by calculating the dependency of the critical current at 11 T with Equation 3.9 from section 3.4.3.1 and the strand $I_c(B, \epsilon)$ data. The local critical current reduction is then determined by the local magnetic field inside the cable by a weighted interpolation of the reduction between 11 T and 12 T, assuming a linear field profile in the cable cross-section from layer to layer.

Figure 7.6. Measured and normalized I_c versus applied bending strain (with polynomial fit) at 12 T and 4.2 K for the VAC bronze strand used for the CSMC [202]. Comparison to the computation of the curves for full and no inter-filament current transfer under bending (bare strand and with SS tube).

7.3.2 Contact stress

A similar strategy is followed for the effect of transverse load on strand crossings. Although several experiments were performed [51, 57, 132, 137, 138, 139, 140], there is no analytical expression available that describes the reduction of the critical current under increasing contact load. It is obvious that a polynomial fit through the measured critical current has the best accuracy. Therefore, the reduction of the critical current for the LMI strand measured with the TARSIS probe for crossing strands [120, 145] is used and the results are depicted in Figure 7.7.

Also here the magnetic field increases along the cable cross section from 11 T to 12 T (see Figure 7.2) is accounted for. Similarly to the method applied for strand bending, the local critical current reduction is determined by the local magnetic field by weighted interpolation between the measured reduction at 11 T and 12 T through the cable from layer to layer. Note that for the strands in 11 T magnetic field in the accumulated load region, the critical current is not affected up to 75 MPa stress.

Figure 7.7. Measured and normalized reduction of the critical current versus applied stress for the LMI strand at 11 T and 12 T measured in the TARSIS crossing strands probe.

7.3.3 Integrated model

The overall critical current reduction in the cable is calculated layer by layer from minimum to maximum accumulated load and in a magnetic field changing from 11 T to 12 T. Just as for interfilament current transfer in a strand described by the relations in section 3.4.3.1, we can distinguish non and full current transfer between strands in the cable. For a full current transfer between strands the current is allowed to redistribute and all strand critical currents from layer to layer can be summed to get the average cable critical current reduction. This is reflected by the following Equation

$$\left(\frac{I_c}{I_{c0}}\right)_{cable} = \frac{\sum_1^{N_l} \left[\sum_{Lw1}^{Lwn} \left(\frac{I_c(\epsilon_b, B)}{I_{c0}} \frac{I_c(F_c, B)}{I_{c0}} \right) \right]}{N_l}, \quad [-] \quad (7.20)$$

in which the peak bending strain in the filamentary region ϵ_b is derived from the load in the layer of strands N_l . The reduced critical current, I_c/I_{c0} , depends on the magnetic field and if the interpolated value exceeds 1 when the combination of bending strain and magnetic field is sufficiently low not to reduce the real critical current, a value of 1 is just taken instead. The reduced critical current caused by the transverse contact load F_c , represented by the right term in Equation 7.20, is multiplied with the reduction obtained from the bending strain (left term) assuming that both reductions add up at the strand crossovers. The I_c reduction determined for all layers is averaged to arrive at the reduced I_c of the entire cable, as full current sharing between strands is assumed here.

When the current in the strands is not able to redistribute due to high interstrand contact resistance, the peak strain and the corresponding minimum I_c limits the current for all strands as all strands have to pass the peak strain region within one pitch length of the final cabling stage. Depending on the ratio of consecutive cabling pitches, it can be that the peak strain region is passed with a periodicity larger than the last stage cabling pitch. A high interstrand contact resistance, or in other words, a long current transfer length, further limits the I_c .

In [102] the possible effect of the current transfer length is mentioned for a full size NbTi conductor. The current transfer length between the wrapped final-minus-one cabling elements (petals) amounts to several tens of meters. Without the resistive wraps around the last minus one cabling stages the current transfer length amounts to several meters. This means that for strands inside the last-minus-one cabling stage some redistribution may be allowed when the periodicity is larger than one last stage pitch length. A conservative approach is to choose for no sharing. In our analysis, both limits, full and no current redistribution were examined.

7.4 Results of model computations for an 1152 strands reference CIC conductor

7.4.1 Mechanical response to transverse load

The cable deformation under transverse compression versus the applied electromagnetic $I \times B$ load is calculated for different wavelengths L_w in a reference conductor with 1152 strands and a void fraction of 0.36 to illustrate the effect of systematic variations of most essential properties. The strand properties are from the internal tin LMI strand with $E_{||}=29$ GPa and $E_{\perp}=3.3$ GPa. The results are shown in Figure 7.8. The total cable deflection becomes larger with longer wavelength when the cable space remains the same. As a result the cable becomes stiffer for shorter twist pitch lengths, which is also illustrated by Figure 7.9 where the overall transverse cable modulus of elasticity versus the applied electromagnetic $I \times B$ load is presented for different wavelengths.

Figure 7.8. Calculated cable compressive deflection versus the applied electromagnetic $I \times B$ load for different wavelengths L_w and $v_f=0.36$. Strand properties are from the LMI strand with $E_{||} = 29$ GPa and $E_{\perp} = 3.3$ GPa.

The deflection for bending and contact load is depicted in Figure 7.10. For the total deflection it is assumed that both components can simply be added though this is not trivial. The TEMLOP model gives a total cable compression of about 1.3 mm, with 1.1 mm for bending and 0.2 mm for direct strand contact stress in a cable with a void fraction of 0.36 and $L_w=0.006$ m. The total transverse cable compression saturates for contact and bending stress with increasing $I \times B$ load. After about 400 kN/m, bending reaches its maximum deflection limit while the contact deformation progressively continues. This is very well in agreement with the measured results on crossing strands and contact stress presented in chapter 4 and the result for CS1 cable compression in Figure 6.24.

Figure 7.9. Calculated overall transverse cable modulus of elasticity versus the applied electromagnetic $I \times B$ load for different wavelengths L_w and $vf=0.36$.

Figure 7.10. Calculated transverse cable compressive deflection versus applied electromagnetic ($I \times B$) load for bending, contact stress and both components added and $vf=0.36$.

The deflection from bending and contact load differs from layer to layer through the cable. In Figure 7.11 it is seen how the deflection starts to increase linearly in the layers from high to low magnetic field until the maximum limit is reached in layer 4, coming

in contact with strands in the next layer ($L_w=0.007$ m). Here the contact stress changes rapidly due to the numerical structure of the model, imposing a sudden increase of the contact surface when changing from L_{w1} to L_{w2} . Behind this layer the bending deflection becomes further restricted because the increasing contact stress is compressing further the strand crossovers, at the same time increasing the contact deformation (deforming the strands). At layer 20, the contact stress changes rapidly again due to the sudden increase of the contact surface when changing from L_{w2} to L_{w3} .

Figure 7.11. Calculated bending deflection and strand contact deformation (per strand layer) versus strand layer number, N_i , from high to low field at 750 kN/m load for $L_w=0.007$ m and $\nu_f=0.36$.

In Figure 7.12 the characteristic initial increase of the bending strain from the section characterised by $L_{w1}=0.007$ m is observed together with the rise of the contact stress until reaching contact with the first strand of the layer below. Then the bending strain linked to L_{w1} (almost 0.7%) relaxes and the contact stress increases with only half the initial slope.

However, in the same layer the bending strain connected with L_{w2} starts to increase, reaching a peak strain of about 1.1%, exceeding the maximum strain connected to L_{w1} , but deeper in the conductor and in significantly lower magnetic field. This maximum for L_{w2} bending occurs at layer 20, where the maximum deflection is reached for this wavelength. The peak strain related to L_{w3} remains restricted to below 0.5%. The contact stress reaches its maximum accumulated load at the low field side of the conductor at 40 MPa. Note that L_{w4} has not yet become involved in the loading process.

Figure 7.13 shows what happens with the peak strain and stress for twice the value of L_{w1} to 0.014 m. Now L_{w4} is involved here but the peak strain occurs now in layer 11 instead of layer 23 and is associated to L_{w3} . The peak strain in the cable is now less than 0.6% and the peak stress is reduced to 35 MPa.

The sudden increase of the contact surface between strands from layer to layer is shown in Figure 7.14 for a wavelength of 0.007 m. For this wavelength, the contact surface

remains constant in the first two layers at the high field region while the contact stress increases linearly towards higher load. In the third layer additional strand-to-strand contacts are formed connected to L_{w2} , consequently increasing the contact area. From this layer of, the stress increases with half the initial slope with further increasing contact area at layer 20, associated with L_{w3} where the process repeats.

All formulations and steps applied in the model, describing the local bending strain and contact stress in each layer are essential for an accurate computation of the local critical current.

Figure 7.12. Calculated bending strain and contact stress (per strand layer) versus strand layer number, N_l , from high to low magnetic field at 750 kN/m load for $L_w=0.007$ m and $v_f=0.36$. In the legend the 'e peak-fil reg' is the peak bending strain in the filamentary region or ϵ_{b0} .

Figure 7.13. Calculated bending strain and contact stress (per strand layer) versus strand layer number, N_l , from high to low field at 750 kN/m load for $L_w=0.014$ m and $v_f=0.36$. In the legend the 'e peak-fil reg' is the peak bending strain in the filamentary region or ϵ_{b0} .

Figure 7.14. Calculated interstrand contact stress and contact area versus strand layer number, N_l , from high to low field at 750 kN/m load for $L_w=0.007$ m and $\nu f=0.36$.

7.4.2 Critical current and transverse $I \times B$ load

The reduction of the critical current depends on the applied bending and contact load, which varies from layer to layer, and the local magnetic field. Thus, the I_c reduction for each strand layer number, N_l is calculated from high to low field from zero to 750 kN/m load, in a conductor with a void fraction of 0.36 and a wavelength of 0.006 m. The strand properties are from the LMI strand with $E_{//}=29$ GPa and $E_{\perp}=3.3$ GPa. The results for bending and contact load are shown in Figure 7.15. The curves represent the reduction from purely interstrand contact load distinguished from the reduction from pure bending throughout the layers of strands in the cable. The reduction from strand contact load is practically nil, so almost all reduction is caused by bending.

Figure 7.15. Calculated reduction of the normalized critical current versus strand layer number, N_l , from high to low magnetic field at 750 kN/m load for $L_w=0.006$ m and $\nu f=0.36$. The curves represent the reduction from purely interstrand contact load distinguished from the reduction from pure bending.

The evolution of the critical current reduction from layer to layer is illustrated in Figure 7.16 for strand currents I_s increasing from 5 to 50 A. A strand current of 50 A corresponds to an $I_x B$ load of 750 kN/m. In Figure 7.17 the same is carried out for a wavelength of 0.009 m. The I_c reduction is restricted at the low magnetic field (accumulated load) zone of the cable cross-section because the maximum deflection is reached.

Figure 7.16. Calculated reduction of normalized critical current versus strand layer number, N_l , from high to low field at various strand currents I_s up to 750 kN/m load for $L_w=0.006$ m and $\nu f=0.36$. The curves represent the overall reduction from bending and contact stress.

Figure 7.17 Calculated reduction of the normalized critical current versus strand layer number, N_l , from high to low field at different strand currents I_s up to 750 kN/m load for $L_w=0.009$ m and $\nu f=0.36$. The curves represent the overall reduction from bending and contact stress.

In the high field region in the cable cross-section, bending is responsible for a rapid decrease of the I_c towards the low field region, until the maximum bending deflection is reached for L_{w1} (Figure 7.1). The reduction in this high field region is almost completely attributed to bending with the larger wavelength L_{w1} while the reduction related to L_{w2} becomes significant somewhat deeper in the conductor. It appears that

when the strand has reached the first bending limit (see Figure 7.1b) and just before the strand gains support by the underlying layer of strands, (N_{y+1}), thereby dividing the wavelength L_w by two (L_{w1} to L_{w2}), the bending strain reaches up to 0.8% for an L_{w1} of 6 mm in layer 5. When the strand becomes actually supported by the underlying strand, thereby changing L_{w1} to L_{w2} , the bending strain reaches a maximum of 1.3%. Although this level of strain is extreme, the cable I_c is only moderately affected because this strain occurs in layers N_y with largest accumulated load in the lowest magnetic field.

When the wavelength L_{w1} is increased to 0.009 m (see Figure 7.17), the reduction in I_c becomes less excessive, in particular for higher currents. This is due to the much lower peak bending strain and contact stress. The maximum available space for bending leads to lower bending strain for longer L_w , while at the same time the strands support across longer lengths and on more locations thereby reducing the local peak contact stress.

Averaging the I_c reduction from all layers for successive $I \times B$ loads, leads to the overall cable I_c reduction versus the $I \times B$ load (Equation 7.20). This is summarised in Figure 7.18 for different wavelengths. An impressive improvement in performance is achieved when the wavelength L_w is increased from 6 to 20 mm. The average wavelength L_w determined from the disassembled full-size ITER CS1 conductor in Figure 1.16 (left) is 6 mm with a standard deviation of 2 mm. Most wavelengths L_w are thus within the range of 4 to 8 mm. When the average overall reduction of I_c versus $I \times B$ load is taken as calculated for $L_w = 4, 5, 6, 7$, and 8 mm, the upper curve presented in Figure 7.19 is obtained. The computed $I \times B$ behaviour is in good agreement with measured performance reported on experiments with coils on short sample [33, 38]. However, the curve represents the situation for full current sharing between strands. For comparison, in the same Figure the curve accounting for no current transfer with much stronger degradation is shown as well. All other results presented here are reflecting full interstrand current transfer.

The remarkable improvement in performance, when wavelength L_w is increased from 6 to 20 mm, is clearly illustrated in Figure 7.20 showing the overall reduction of the critical current versus characteristic bending wavelength. Also with a wavelength shorter than 3 mm improved performance is possible, confirming that the given ITER cable design in terms of sub-cable pitches was unfortunately set at the worst possible conductor performance. Application of shorter twist pitches leads to less efficient use of strand, increase of the cable size and likely more strand deformation from CICC manufacture. In the same Figure 7.20, the peak strain in the strands is shown to correlate with the I_c reduction. The peak strain and the peak contact stress in the conductor versus the wavelength are presented in Figure 7.21. Also for the contact stress the same dependency is obtained although there is hardly an effect on the critical current. The influence of the interstrand resistivity, allowing for full current redistribution or none, is depicted in Figure 7.22. It appears that for higher L_w the difference between full and no current transfer becomes less pronounced due to a different balance between stress and strain concentrations across the cable in relation to the self-field profile. This is well illustrated by Figure 7.16, showing a small number of

layers with severe I_c degradation and in Figure 7.17 representing a more balanced distribution of the degradation over the cable cross section.

A moderate decrease of the void fraction also leads to a better performance. This is demonstrated in Figure 7.23 for void fractions of 0.36 and 0.33 in previous and present ITER TF conductor design values, respectively. The influence of the void fraction is significant and can be explained as limiting the available space for bending deflection. The effect of the stiffness of the strand is simulated by comparing results when using the properties of the internal tin strand, used for practically all simulations, with those of bronze strand with a lower axial elastic modulus. The result is shown in Figure 7.24, illustrating that a strand with lower E_{II} is more degraded as it gets stronger deformed by bending loads.

Figure 7.18. Calculated overall reduction of the normalized critical current versus IxB load for different wavelengths from 2 to 20 mm and $v_f=0.36$.

Figure 7.19. Computed average overall reduction of normalized I_c versus IxB load from $L_w = 4, 5, 6, 7$, and 8 mm representing the spread in the measured L_w from the CSI conductor with an average L_w of 6 mm, a standard deviation of 2 mm and $v_f=0.36$. The upper curve accounts for full interstrand current transfer while the lower curve represents no current transfer.

Figure 7.20. Calculated overall reduction of normalized critical current and peak bending strain in the cable cross section versus the characteristic bending wavelength and $vf=0.36$.

Figure 7.21. Calculated peak contact stress and peak bending strain in the cable cross section versus characteristic bending wavelength and $vf=0.36$.

Figure 7.22. Overall reduction of the critical current for full interstrand current transfer (low interstrand resistance, ISR) and no current transfer (high ISR, polynomial fit) in the cable cross section versus the characteristic bending wavelength ($vf=0.36$).

Figure 7.23. Calculated overall reduction of the normalized critical current versus IxB load for conductors with a void fraction of 0.36 and 0.33.

Figure 7.24. Calculated overall reduction of the normalized critical current versus IxB load and $vf=0.36$. The strand properties are from the LMI-EM internal tin strand with $E_{||}=29$ GPa and $E_{\perp}=3.3$ GPa and the VAC bronze strand with $E_{||}=21$ GPa and $E_{\perp}=3.0$ GPa.

7.5 Discussion

7.5.1 Mechanical response of the cable

For the reference CICC analyzed in section 7.4 the TEMLOP model predicts a total cable compression of 1330 μm , with 1070 μm for bending and 250 μm for direct strand contact stress, in a cable with a void fraction of 0.36 and a characteristic wavelength of 6 mm. This is in line with the experimental values obtained with the Cable Press (Figure 6.24). Obviously this is related to the derived limitation in bending deflection f_{sb} in Equation 7.9 and the strand contact deformation adopted from Figure 7.4. The experimental values found for the cable compression and correlated displacement per strand, for three conductors with different void fraction, are shown in Figure 7.25. For

the CS1 type of cable with a void fraction of 0.36 a maximum deflection (f_{cm}) of 1040 μm was found. The measured compression per strand then reaches a maximum amplitude of 34 μm .

For a cable with a void fraction of 0.33, the model predicts a total cable compression of 1040 μm , with 810 μm for bending and 225 μm for direct strand contact stress (L_w of 6 mm). This is in agreement with what is expected by interpolation for the total deflection in Figure 7.25. However, the influence of the void fraction on the cable compression as computed with the model is stronger than observed in the press measurements. This may be due to an underestimation of the total contact deformation as derived from.

Figure 7.25. Displacement under compression of several Model Coil conductors with different void fraction and deduced possible free displacement for bending per strand.

Transverse stress-strain measurements performed with the TARSIS crossing-strands probe on the LMI strand showed that the loading curve is practically without hysteresis when unloading (see Figure 7.4). The low transverse modulus of elasticity of the strands, compared to the modulus in axial direction being one order of magnitude higher, implicitly suggests that the transverse cable bundle compression through contact stress is connected to mostly plastic deformation of strand crossings and line contacts.

The plastic deformation from transverse interstrand contact deformation is primarily determined by the copper stabiliser, practically immediately yielding at 4.2 K, while the axial strand stiffness, linked to bending, is significantly enhanced by the Nb_3Sn layer (see section 2.1.5) [120].

The conductor cross section depicted in Figure 6.23 illustrates the compaction of a cable after test in the Twente Cable Press with peak transverse load after 40,000 cycles. At the upper perimeter of the cable bundle, a permanent plastic deformation of 0.6 mm is clearly visible leaving a gap between cable and inner conduit wall. This 0.6 mm plastic deformation consists of 250 μm representing direct strand contact stress

deformation according to the model and the remaining part can be attributed to plastic deformation from bending. This leaves about 0.5 mm scope for compression with elastic bending after cycling of the conductor. The so-called IxB dependency, as it is often presented in an $\epsilon_{\text{extra}}(IxB)$ plot, is then directly linked to the remaining span for elastic bending. The ϵ_{extra} represents the additional axial strain that is required to match the measured strand performance to the degraded cable performance [33].

As the model is not adapted to comply for plastic deformation and creep, the performance reduction with cycling of the load is not reflected in the results.

The overall modulus of elasticity measured on a CS1 conductor in the Twente Cryogenic Press is shown in Figure 7.26 together with the computed curve for $L_w=6$ mm, showing acceptable agreement.

When comparing the computed deflection of the cable versus the applied load for different wavelengths to the measured cable compression in the Press (see Figure 7.27), it is found that the behaviour of the cable for increasing number of cycles in the experiment can be described by the model as for increasing L_w with constant cable space (void fraction).

The model obviously does not incorporate the effect of plastic deformation and work hardening with cycling, but there is a strong analogy in the characteristics. The computed mechanical response with increasing L_w is linked to a decreasing influence of bending but at the same time accompanied with a growing contact surface. In this sense the measured cable deformation can be understood already during the first cycle, as largely affected by plastic deformation originated from mainly bending. With further cycling of the load on the cable the component of the plastic deformation decays, finally leaving only the component of mainly elastic bending.

Figure 7.26. Measured and computed overall transverse cable modulus of elasticity versus applied electromagnetic IxB load for the CS1 conductor ($vf=0.36$). The strand properties are from the LMI-EM strand with $E_{||}=29$ GPa and $E_{\perp}=3.3$ GPa.

Figure 7.27. Measured and computed overall transverse cable compression versus applied electromagnetic IxB load ($\nu f=0.36$). The experimental results are from the CS1 conductor along cyclic loading while the computed curves are for different L_w . The strand properties for the computation are from the LMI-EM strand with $E_{||}=29$ GPa and $E_{\perp}=3.3$ GPa.

7.5.2 Critical current reduction

A key purpose of the TEMLOP-Model is to verify the possible influence of the cabling pitches on the performance degradation under transverse load. The most favourable transverse load distribution is a homogeneous one, representing almost infinite line contacts between parallel strands. The most important prediction of the model, in terms of optimisation of the conductor performance by increasing the twist pitches, is understood as a homogenisation of the load distribution or in other words, a significant lowering of the local peak strains. This is clearly illustrated in Figure 7.20 and Figure 7.21.

The outcome of the model can be explained intuitively by exploring the limiting cases for L_w . When $L_w \rightarrow \infty$, the angle between the crossing strands goes to zero and the stress reaches to the homogeneously distributed case represented by Equation 7.4. At the same time the bending diminishes as the strands become in fact parallel resulting into minimization of the bending strain. For $L_w \rightarrow 0$, the bending strain also goes to zero as reflected by Equation 7.2 and the contact stress reduces to the homogeneity limit from Equation 7.4 with contact area going to the maximum. With L_w in practice between the extreme values, periodic bending strain and local concentration of contact stresses lead to reduction of I_c .

In Figure 1.16 (left) it is shown that for the CS1 type of conductor, the periodicity in the number of strand crossings is relatively high with a wavelength of 6 mm. The first cabling stage triplet has a twist pitch of 45 mm and the second stage quadruplet has a pitch of 74 mm. In the CS2 type of conductor from Figure 1.16 (right), with much longer twist pitches (about 70 mm and 90 mm respectively) a less frequent periodicity is found with an average wavelength larger than 6 mm. At the same time, a much

smaller crossing angle than the average 45 degrees from the CS1 type of conductor is observed.

In view of the model predictions, concerning the dependency between twist pitch and transverse load distribution along the strands in the conductor, the cabling scheme of the CS2 seems much more advantageous for large conductors with high transverse load [144]. Unfortunately, this type of conductor was only used in the low field region of the CSMC and not examined regarding its temperature margin. The cabling schemes for CICC's and not only for ITER conductors, from which experimental data are available, were kept practically the same over the years. As said before, this is likely the main reason that the influence of cabling parameters on I_c reduction remained unknown for many years.

In section 7.1 it was already stated that part of the analysts in the fusion community postulated that a significant fraction of the Nb₃Sn layer in the strand material of the cable will unavoidably crack or break [38, 43] but that no conclusive evidence was presented. The possibility of filament fracture is supported by experimental work [59] even though no results of an internal inspection of strands from a tested conductor were presented at that time. Although it is not relevant for the analysis with TEMLOP presented here, whether the permanent degradation is due to filament breakage or plastic deformation of strands, the outcome of the model gives an indication in terms of peak bending strain.

Experiments with the TARSIS bending probe with stepwise increase of the load with intermediate unloading, clearly shows irreversible degradation of the I_c and the n -value. In general a permanent degradation in I_c is observed when exceeding an applied peak bending strain of 0.3 to 0.4%. At the same time a permanent deformation of the strand is observed as for zero load the strand deflection never returns to zero after having applied a certain load level. This means that this experiment alone cannot allow to distinguish between plastic deformation and filament breakage. The irreversible strain limit of about 0.4% corresponds more or less to the applied tensile strain that is required for irreversible degradation of the I_c in experiments for strand axial strain testing [85].

With TEMLOP an applied peak bending strain in the Nb₃Sn filaments of about 1.3% is found for the used cabling scheme (Figure 7.20, Figure 7.21 and Figure 1.16). Assuming that a steel conduit would impose an additional axial pre-compression leading to roughly -0.5% average intrinsic axial strain in the filaments after cool-down, bending strain would lead to a net tensile intrinsic peak strain of +0.8%. If in addition, also is taken into account that at the location with peak bending strain the contact stress reaches maxima as well, local filament breakage seems evident for the CS1 design.

7.5.3 Related effects

The most favourable transverse load distribution is obviously a homogeneous one, representing line contact between parallel strands. This pleads for long cabling pitch

lengths but this is in conflict with the restrictions given by the generation of coupling loss according to the state of the art knowledge at that time. The AC loss in terms of interstrand coupling loss is the driving factor for minimizing the cabling pitch lengths together with compelling adequate stiffness to the cable bundle.

For a single loop, the generation of coupling loss increases with the square of the pitch length as long as the contact resistance remains constant. In this sense the increase of the coupling loss is a potential disadvantage when choosing longer pitches to enhance transverse load optimisation. However, for the CS2 type of cabling structure we find a coupling loss time constant in the virgin state of about 100 ms, while for the CS1 type a value twice as high is observed [177].

Take note that a third part of the strands in the CS2 is copper strand instead of superconducting wire as in the CS1-type. The coupling loss is roughly proportional to the amount of superconducting strands for comparable cable geometries. From this it can be expected that an extrapolation of the coupling loss by only taking into account the ratio of twist pitches is by far too conservative. Consequently, a serious drawback from a possible increase of the coupling loss is not expected.

Improvement of the performance is observed for lower void fraction. The transparency of the cable bundle for the coolant (liquid helium under pressure) might be slightly affected but also here a noticeable impact is expected as long as the heat generation remains sufficiently low.

An advantage of longer cabling pitches besides the lower transverse load degradation, is an increase of the $\cos(\theta)$ correction factor leading to higher critical current density, although the effect is modest. When the wavelength is increased from 6 to 20 mm, the $\cos(\theta)$ increases from 0.950 to 0.985, increasing the J_c by 3.5%.

7.5.4 Parametric variations

A few sensitivity studies were performed to explore the influence of strand stiffness and conductor aspect ratio. It appears that in particular the axial stiffness of the strand is important for a higher void fraction (~ 0.36). When compacting to below ~ 0.30 , the performance improves significantly for low stiffness and the strand stiffness becomes less important. Figure 7.28 illustrates this effect for the EM-LMI strand used for the TFMC model coil with $L_w=6$ mm.

Figure 7.29 shows that a decrease of the conductor aspect ratio, when the force is parallel to the height of a rectangular conductor, cause a reduction in performance compared to a square shape ($L_w=7$ mm). An aspect ratio larger than 1 is hardly beneficial. The peak stress increases to values that cause a further decrease in performance. For $L_w=26$ mm the peak stress is still acceptable, also for low aspect ratio.

Figure 7.28. Normalized I_c versus axial E modulus for three different void fractions determined on the TFMC strand.

Figure 7.29. Influence of the conductor aspect ratio on the performance for $L_w = 7$ mm with the peak stress in the cable bundle.

7.5.5 Recommendations

As a first test, the model predictions can be tested experimentally by testing a short sample in for example the SULTAN facility. Two different conductor variations can be tested when incorporated as the two legs in a hairpin sample in Sultan. Two conductors, identical but with a significant difference in the cabling scheme, in particular for the first four stages, should be compared directly on their performance. A cabling scheme with subcable pitches two to three times longer than the ITER TF reference conductor seems appropriate (see Figure 7.20). Application of much shorter pitches can be useful when there is a need for higher void fraction, for example to reduce the pressure drop in a single channel conductor.

Strands with high stiffness (Young's modulus) have to be selected for conductors subjected to the highest $I \times B$ load if the magnet design concept allows for this choice. Strands with a lower modulus can be applied in sections of the magnets with lower transverse load.

Other conductor optimizations are possible as well, like for example changing the aspect ratio of the conductor and decreasing the number of strand layers in the direction of the accumulated load. For ITER however, the project construction schedule does not allow for a drastic change of the conductor and magnet design. With only a change in the cabling twist scheme and void fraction, the margin can be significantly improved according to the TEMLOP model.

7.6 Conclusion

A numerical model was developed for optimizing the strand layout design optimisation of cabled superconductors subjected to a transverse load (TEMLOP model). The first results of computations for an ITER type conductor, based on measured mechanical and electromagnetic properties of an internal tin strand, have led to new insights.

The most important conclusion is that the severe critical current degradation of large CICC's, as designed for ITER, can be overcome by decreasing or increasing the cabling pitches. The principle is based on a more homogeneous distribution of the stresses and strains acting on crossing strands in the cable and achieving significantly moderate the local peak stresses associated with intermediate twist pitches.

The model gives a good description for the mechanical response of strands to a transverse force load, transferred and accumulating from layer to layer in the cable, and highly dependent on the strand stiffness and cable void fraction. The computation results are in good agreement with observed behaviour in transverse stress-strain experiments on cables.

The simulations point out that the degradation is mainly attributed to strand bending, although the transverse contact load on strand crossings and line contacts may locally reach up to 90 MPa.

An important finding is that a lower cable bundle void fraction and higher strand stiffness add to a further improvement of the conductor performance.

Other conductor optimizations are possible as well, for ITER however, the construction schedule does not allow for a drastic change of the conductor and magnet design. With only a change in the cabling twist scheme and void fraction, the operational margin can be significantly improved.

8 Experimental verification of cable pitches and void fraction effect

The main conclusion of the a priori TEMLOP predictions presented in May 2006 and summarized in the previous chapter is that the observed severe degradation in CICC's can be avoided straightforwardly by decreasing or increasing the pitch length in subsequent cabling stages and by reducing the void fraction. These corrective measures provide more support to the strands, sufficiently reduce the strain, and therefore avoid filament cracking at the strand crossover points in the cables. It was the first time that an increase of the cable twist pitches was proposed for improving performance without experimental evidence present. A full-size European prototype TF conductor sample (TFPRO-2), manufactured in autumn 2006, was adapted according the new insight and tested April 2007 in SULTAN for validation of the predictions. The results were outstanding. For the first time a Nb₃Sn CICC conductor achieved the performance expected from the single strand properties, with high n-value and no significant degradation.

Besides the cable properties, the model directly uses measured data from single strands under uni-axial stress and strain, periodic bending and contact loads.

It was experimentally proven that the proposed changes recover the ITER TF conductor operation margin completely. The ITER TF conductor specification was adapted following the TEMLOP prediction and its experimental verification.

The content of this chapter is for a large part based on papers [87], [203], [212], [213], [215], [216] and [217].

8.1 Introduction

The TEMLOP model requires an essential parameter also deduced from the press measurements, which is the maximum possible compression of the cable, i.e. the available space for bending and contact deformation of strands. Also, to couple the parameters temperature, magnetic field and uni-axial strain to the computations, the deviatoric strain model was implemented in TEMLOP. The strand $I_c(B, T, \epsilon)$ scaling parameters for both OST type of strands used in the TFPRO-2 legs are also determined.

The results of the TFPRO-2 cable test sample, with one leg featuring the cable configuration based on an optimization using TEMLOP, are presented. The sample layout and instrumentation are given in Figure 8.1. The conductor sample is a hairpin placed vertically in the SULTAN test facility [204]. It consists of two conductor legs jointed at the bottom and electrically connected at the top to a superconducting transformer for injecting current. A clamping system keeps the two legs parallel, reacting the electromagnetic forces. The peak magnetic field provided by the SULTAN facility applies to about 40 cm of conductor (1% homogeneity). This is somewhat shorter than the final stage cable twist pitch and raised concern that not all strands experience the high field region. This concern has been addressed, for the case of a 45 cm final stage twist pitch, by calculating the integrated electric field along each strand with the JackPot electromagnetic-thermal cable model [29, 30, 205], leading to the conclusion that the current sharing temperature estimate is at most 0.1 K too high compared to coil operating conditions.

The two legs are cooled by a nominal helium mass flow of 3 g/s at 10 bar. The sample instrumentation consists of temperature probes and voltage taps installed on the conductor jacket as shown in Figure 8.1. The main voltage drop is sensed by two pairs of voltage taps, V3V9 and V10V4, located in the high field section at 0.45 m distance, which is equal to the final twist pitch of the tested cables.

The performance assessment of the conductor is based on transport current and magnetic field representative for the operating condition in TF coils, i.e. 68 kA and 10.78 T.

The main test program consists of 0–68 kA current cycling. A measurement is carried out after 1, 3, 10, 200, 400, 600, 800 and 1000 current cycles. Once the plateau of current is established, the sample temperature is ramped up with the facility heater at a rate of about 1 mK/s. Voltage drop and temperature are recorded when the conductor enters the current sharing regime.

The performance is assessed using two criteria:

- evidence of T_{cs} saturation with current cycling,
- $T_{cs} > 5.7$ K for an electric field E_c of 10 μ V/m across the 450 mm voltage taps.

After current cycle 1000 an extra test at lower current and lower electromagnetic loading is performed to explore the conductor performance versus applied transverse loading $I \times B$.

The test is crucial to investigate the influence of the cable pattern on the performance and for validation of the TEMLOP model predictions and ultimately important for the final ITER TF conductor design in achieving an optimum and reliable performance. The strand mechanical properties determined by TARSIS tests are key input for the quantitative comparison with TEMLOP. Some results of simulations with parametric variation in mainly the cabling pattern are presented as well.

Figure 8.1. SULTAN sample layout and instrumentation.

8.2 Results for long pitches

The TFPRO-1, was manufactured with EAS bronze type of strand and for both legs it has the ITER reference-cabling pattern, however, one leg with a void fraction of 33.8%

and the other leg 29.3%. The results are reported elsewhere [87]. The TEMLOP simulations gave a $T_{cs}=6.4$ K for the leg with low void fraction while the leg with higher void fraction shows $T_{cs}=6.1$ K. For both an axial compressive strain was assumed of -0.55%. The leg with lower void fraction showed only slightly better performance ($T_{cs}=6.24$ K) than the leg with higher void fraction ($T_{cs}=6.15$ K) [75]. The critical temperature was not too far from the initial prediction based on strand properties but the n -values of both legs were far below what was expected from the virgin strand properties.

It was also observed that the n -value decreased with cycling during the first load cycles, altogether a clear sign of degradation and independent of the joint resistance non-uniformity. The difference between both EAS1 legs was indeed significant initially but tended to vanish with cycling, indicating that the effect of void fraction for this range was rather limited.

Based on the TEMLOP prediction presented in May 2006, a second SULTAN sample was manufactured in September 2006 intended to demonstrate the advantage of longer subcable pitches in combination with low void fraction. The TFPRO-2 conductor sample comprised advanced internal-tin OST Nb_3Sn strands. One leg was manufactured using OST-I strand and the other leg with OST-II strand, with practically similar strand layout. The strand used to fabricate the OST-I leg was already used in the dipole pre-prototype conductor tested in 2005 [86, 206]. The OST-II leg comprises the same strand as the right leg of the TFAS-1 sample tested in 2005 and 2006 [84]. This conductor showed a T_{cs} after cyclic loading, which was considerably less than the value expected from strand performance. The strand cross sections are depicted in Figure 8.2.

Figure 8.2. Cross sections of OST-I (left) and OST-II (right) wires.

The wire specifications including J_c results at 4.2 K and 12 T as certified by the supplier are listed in Table 8.1. The heat treatment of all wire samples was carried out at the University of Twente under vacuum. Prior to the heat treatment the chrome layer on the wire surface was etched with a 37% solution of hydrochloric acid. The heat treatment schedule was recommended by the supplier and is identical to the one applied to the

full-size TFPRO-2 conductors that were tested in SULTAN [84] in order to avoid uncertainties in the performance comparison between single strand and cable. The two legs of the TFPRO-2 sample are characterized by void fractions of 29.1% (OST-I) and 27.7 % (OST-II). The main difference between both legs is the cabling configuration with ITER reference pattern for the OST-I leg and a long pitches variant for the OST-II leg (see Table 8.1). Note that the OST-I strand has a significantly higher current density than the OST-II strand.

Table 8.1. Properties of the TFPRO-2 sample and input parameters for the TEMLOP model.

Strand type	OST-I	OST-II
Diameter strand, d_s	0.81 mm	
Diameter filamentary region, d_f	0.55 mm	
E modulus strand (axial), E_l	27 GPa	
E modulus strand (transv.), E_t or E_{tr}	3.6 GPa	
Cabling pattern	$((2 + 1 \text{ Cu}) \times 3 \times 5 \times 5 + \text{core}) \times 6$ $(\text{core} = 3 \times 4 \text{ Cu})$	
Number of sc strands, N_s	900	
Number of Cu wires	522	
Layers, N_l	30	
Twist pitch, L_p	45 / 88 / 127 / 245 / 470 mm	116 / 182 / 245 / 415 / 440 mm
Bending wavelength, L_w	7 mm	26 mm
Void fraction, ν_f	29.1%	27.7%
Cos θ factor ($\cos^{-1} \theta$)	1.04	1.01
Axial strain TFPRO-2 test [87]	$\sim -0.45 \%$	-0.45%
Cable space diameter	38.85 mm	38.25 mm
Current, I	0 – 68 kA	
Magnetic field, B	11.2 T	

The measured T_{cs} of 5.7 K for the OST-I leg, containing strands with the best properties, was far below what could be expected from the strand properties, while the OST-II leg showed a T_{cs} of 7.4 K. The electric field versus temperature characteristics for the EU TFPRO-2 sample with OST-I and OST-II strand are presented in Figure 8.3. The degradation with cycling is obvious for the OST-I leg, showing gradually decreasing T_{cs} , while the performance of the OST-II leg is outstanding with no degradation at all and featuring an n -value of about 12.

The T_{cs} evolution during 1000 cycles, shown in Figure 8.3, shows a reduction of about 0.4 K for the OST-I [87]. The long pitches OST-II sample, with relatively weak strand, clearly showed at least 1 K better performance compared to actually all other samples measured till then at 68 kA [75], [217].

Figure 8.3. Measured electric field versus temperature for the EU TFPRO-2 sample with OST-I and OST-II strand, clearly illustrating the severe degradation with cycling of the OST-I leg and the outstanding performance of the OST-II leg (courtesy of N. Mitchell, ITER International Organisation).

In Figure 8.4 the results between the OST-I and II legs are compared regarding the effective longitudinal strain versus the transverse $I \times B$ load. The OST-I leg is clearly susceptible to variations in transverse load while no effect at all is visible for the OST-II leg, adequately illustrating the impressive improvement. The intersection of the strain axis is -0.48% for the OST-II leg and -0.55% for the OST-I leg.

From an axial thermal prestrain point of view there is obvious physical reason for this difference. Part of the difference may be attributed to the degradation of the OST-I leg (at already the first loading cycles). Besides the earlier voltage rise caused by the non-uniform joints (in the order of tenths of a Kelvin), an additional non-uniformity in the current distribution is supplemented by the transverse load degradation, which may not be uniformly distributed.

Figure 8.4. Calculated effective axial strain as a function of the transverse load in terms of $I \times B$ for the two legs in the TFPRO-2 sample [75].

8.3 Critical current reduction in strands

8.3.1 Axial strain

The effect of axial strain and transverse load on strands was measured and simulated for the strands used in the cable samples prepared for performance test in SULTAN for investigating the effect of varying subcable pitches and void fraction. Uni-axial tensile stress strain measurements were performed with the TARSIS probe on OST-I and II strands and the result is shown in Figure 8.5. The yielding modulus, represented by the linear section above 100 MPa, amounts to 27 GPa. The initial modulus is 115 GPa, with similar results for OST-I and II.

Figure 8.5. Tensile axial stress-strain characteristics of the OST-I and II strands measured in the TARSIS probe at 4.2 K.

The I_c versus longitudinal axial strain is determined with the PACMAN spring [85]. The deviatoric strain model based on the measured data of the OST-I and OST-II strands, describes the scaling of field, temperature and strain of the Nb_3Sn strands. A comparison is made for the I_c versus strain in Figure 8.6. The critical current of the OST-I strand measured on two samples is substantially higher than of the OST-II strand. The data points measured by the University of Durham are also added to the graph, illustrating the very good agreement between the results of both laboratories. Figure 8.7, representing the reduced I_c versus strain of both wires, shows that the OST-II strand is more sensitive to strain than the OST-I type.

Three superconducting parameters C_1 , $H_{c2m}^*(0)$, T_{cm}^* and four deformation related parameters C_{a1} , C_{a2} , $\varepsilon_{0,a}$ and ε_m have to be determined experimentally as input for TEMLOP. This can be performed by applying a least square fit of Equation 1.5 to the data set. The results of the best fit to the I_c data obtained below the irreversible strain limit (i.e. about +0.2%) are listed in Table 8.2.

Figure 8.6. Measured I_c versus intrinsic strain (for peak I_c corresponding to zero strain) at 12 T and 4.2 K for the OST-I and OST-II strands. Data points provided by the University of Durham of OST-I are included.

Figure 8.7. Measured normalized I_c versus intrinsic strain (for peak corresponding to zero strain) at 12 T and 4.2 K for the OST-I and OST-II.

Table 8.2. Scaling parameters for the OST strands, as specified in Table 8.1.

Reversible regime, $\epsilon < 0.2\%$			
Parameter	OST-I (#1)	OST-I (#2)	OST-II
C_{a1}	53.30	50.51	53.10
C_{a2}	8.55	8.10	4.65
$\epsilon_{0,a}$ [%]	0.344	0.287	0.136
ϵ_m [%]	-0.066	-0.092	-0.044
$\mu_0 H_{c2m}(0)$ [T]	33.92	33.34	34.05
$T_{cm}(0)$ [K]	16.40	16.32	16.20
C_1 [AT]	19,463	19,979	17,431

The parameters H_{c2m}^* (0) and T_{cm}^* for the three OST strands are quite similar while the deformation related parameters differ substantially. Deviations between the calculated lines and the experimental data points are clearly observed at strain values above +0.2% where irreversibility occurs. The n -value versus I_c is a common way to plot this dependency as shown in Figure 8.8 for OST-I. The n -value for OST-II evolves in a similar manner. The red triangles in the plot represent the n -values measured in the irreversible strain range. A fraction of the Nb₃Sn filaments is then already cracked.

Figure 8.8. Measured n -values versus I_c at various temperatures and magnetic fields for the OST-I strand.

The irreversibility limit can be determined by the method described in section 5.2.2. The n -value is plot versus the intrinsic strain for a stepwise increase of the applied strain and returning to zero applied strain after each increase. Then linear fits are drawn through both straight sections; the intersection of the two lines marks the irreversibility strain limit. The intrinsic irreversibility strain limit for the OST-I strand amounts to +0.25% and +0.16% for the OST-II strand.

The n -value is a very sensitive indicator for filament cracks. With 900 superconducting strands in the cable carrying a current of 68 kA, the current per strand amounts to 75.6 A. In Figure 8.8 the n -value for a strand under axial compression at a current of 75.6 is about 13. This substantiates once again the outstanding performance of the OST-II leg with no degradation at all and featuring an n -value virtually similar to the virgin strand.

8.3.2 Effect of transverse load through bending and contacts

The analyses of the EU TFPRO tests in SULTAN [75, 87] showed that the thermal pre-strain is about -0.45%, a value much less than assumed in the decade before. The extreme high compressive values often assumed in the previous years, were based on observed severe strand degradation and joint resistance and current non-uniformity. The value was reduced considerably throughout the years from initially some -0.9% to less than 0.5% for internal tin type of strands at present. It became routine in the fusion

community to use an ϵ_{extra} term to describe the degradation of the conductor T_{cs} with increasing electromagnetic load, although the initial strand degradation at first load still remained hidden in the ϵ_{th} , just as the early voltage rise caused by joint non-uniformity and likely the overestimation of the axial thermal pre-compression.

A strong indication for this damage was the conductor's n -value decreasing with cycling, less than half the strand value, and always far below 10. The axial thermal pre-strain on the TARSIS bending probe is about the value in the full-size conductor. For the use in TEMLOP, it is better to use the directly measured data instead of using corrections that are not understood.

Although the internal layouts of OST-I and II strands are almost similar (see Figure 8.2), the I_c and the sensitivity for applied load are different. This is illustrated in Figure 8.9 where the normalized I_c is plot versus the peak bending strain. The difference in sensitivity to applied bending load is shown in Figure 8.10. The reduction of the critical current with applied load on crossing strands for both OST strands was measured with the TARSIS probe. The results are shown in Figure 8.11 and the reduction of the n -value in Figure 8.12. The OST-I is hardly sensitive up to 40 MPa while the I_c of the OST-II strand starts decreasing at already 20 MPa.

The n -value, a better indicator for degradation, reflects the sensitivity very well with a sharp decline of n around 20 MPa as shown in Figure 8.12. Here the contact stress is calculated from the projected contact area [207]. In section 7.2.3 it was explained that for a cable with the ITER baseline twist pitches the local peak stress could reach about 100 MPa while for homogeneously distributed load it is about 20 MPa. The strong reduction in n -value for stress higher than 20 MPa for the OST-II strand shows that this strand can only perform well when the transverse load is homogeneously distributed and less than 20 MPa.

Figure 8.9. Measured normalized I_c versus applied peak bending strain with $L_w=5, 7$ and 10 mm (with polynomial fit) at 12 T and 4.2 K for the OST-II strand used for the TFPRO. The curves show the computation for full (LRL, upper curve) and no inter-filament current transfer (HRL, lower curve).

Figure 8.10. Measured normalized I_c versus applied bending load at 12 T and 4.2 K for OST-I and OST-II.

Figure 8.11. Reduction of the normalized I_c versus applied stress for the OST-I and II strands measured in the TARSIS crossing strands probe.

Figure 8.12. Reduction of the normalized n -value versus applied stress for both OST-I and II strands.

Transverse stress-strain measurements were performed with the TARSIS crossing-strands probe on the OST-II strand and are presented in Figure 8.13. The load curve is practically without hysteresis. The initial deformation requires only a very small load. It is thought that the initial strand deformation up to a diameter compression of almost $25\text{ }\mu\text{m}$ occurs partly already during cabling of the conductor, but mostly throughout the final compacting step, after the insertion of the cable in the conduit. This assumption allows a constant value of 3.6 GPa for the strand transverse modulus of elasticity E_t in the model for both OST-I and II strands.

Figure 8.13. Transverse contact stress-strain characteristic of the OST-II strand measured in the TARSIS crossing strands probe.

8.3.3 TEMLOP results for critical current degradation

The cable deformation under transverse compression is calculated for the OST-I leg with $L_w=7\text{ mm}$ and for the OST-II leg $L_w=26\text{ mm}$. For the OST-I leg the TEMLOP-model gives a total cable compression of about $980\text{ }\mu\text{m}$, comprising $180\text{ }\mu\text{m}$ accounting for the direct strand contact stress at peak load. For the OST-II leg, a total cable compression of about $870\text{ }\mu\text{m}$ is found, including only $20\text{ }\mu\text{m}$ for direct strand contact stress. The large difference regarding the contact stress deformation is due to the large dissimilarity in contact area while the initial void fraction is relevant for the overall compression as well.

A peak bending strain for the OST-I conductor is calculated of about 0.7% while for OST-II it remains rather limited to less than 0.3% . This means that a large fraction of the Nb_3Sn filaments in OST-I is cracked, while virtually no cracks are present in OST-II. The contact stress reaches its maximum accumulated load at the low field side of the conductor at 45 MPa for OST-I and less than 10 MPa for OST-II. Figure 8.12 shows that for transverse contact stress, critical current and n -value of the OST-II conductor are reduced already while the OST-I conductor is still at optimum performance.

The reduction of the critical current depends on the applied bending and contact load, as well as the local magnetic field, all varying from layer to layer. Consequently the I_c reduction is calculated for each strand layer number N_l from high to low magnetic field and from zero to 750 kN/m load. The results for bending and contact load are shown in Figure 8.14 for the OST-I leg. The three curves show the calculated reduction by purely interstrand contact load, pure bending throughout the layers of strands in the cable and the total load, respectively. The force interaction between strand layers throughout the cable cross-section is following the principle explained in section 7.2 and each mark in the plot represents the outcome of one layer of strands. The calculated reduction in I_c for the OST-II leg, shown in Figure 8.15, is small and dominantly caused by bending.

Figure 8.14. Calculated normalized I_c versus the strand layer number of the OST-I conductor, N_l , from high to low field at 750 kN/m load for $L_w=0.007$ m and $\nu f=0.291$. The three curves show the reduction by purely interstrand contact load, pure bending, and total load, respectively.

Figure 8.15. Calculated normalized I_c versus strand layer number, N_l , from high to low field at 750 kN/m load for $L_w=0.026$ m and $\nu f=0.277$. The three curves represent the reduction by purely interstrand contact load, pure bending, and the total load, respectively. The curves for bending and total reduction follow the same trace.

Here the curves for bending and total reduction follow the same trace since there is no effect from transverse contact stress. Figure 8.14 shows that the I_c degradation peaks in layers 14-18, about in the middle of the cable cross section and amounts to some 65%. In Figure 8.15 though the peak reduction in I_c is closer to the outer radius and much less. The effect of bending is dominant for both cables, OST-I and II.

The computed cable I_c reduction versus IxB load for contact stress and total reduction is shown in Figure 8.16 for the OST-I conductor. The strong dependency on IxB load should be compared to the effective axial strain versus IxB load as shown in Figure 8.4 for the OST-I conductor. The reduction in critical current with increasing IxB is reflected in the increasing compressive axial strain. A part of this I_c reduction initiated by bending strain and contact stress is irreversible, since it is caused by Nb_3Sn cracks and plastic deformation of the strands, resulting in a permanent change of the strain distribution in the filaments. The I_c reduction is partly reversible as illustrated by the slope of the OST-I data in Figure 8.4. Figure 8.4 and Figure 8.4 illustrate well how effective axial strain and IxB dependency, both terms being used for many years without clear understanding, are quantitatively clarified by the TEMLOP model computations.

The remarkable improvement in the current sharing temperature performance, when the wavelength L_w is increased from 7 mm (ITER reference cabling pattern as applied in OST-I leg) to 26 mm (OST-II leg), is clearly illustrated in Figure 8.17 where the overall reduction of the T_{cs} is pictured versus the characteristic bending wavelength for two extremes; none and full current sharing between strands. In TEMLOP we can implement the ability of full current sharing between strands but this is likely a too optimistic approach since the current sharing length between individual strands is in the range of the bending wavelength L_w , or longer. Particularly when L_w is in the range of severe degradation, 0.002 to 0.01 m, some current sharing between strands may occur.

This essential figure clearly demonstrates that the ITER reference-cabling pattern (Option I [75]) leads to a substantially degraded conductor performance. Choosing intuitively or pragmatically, without analysis, a cable pattern with a wavelength between 0.002 and 0.01 m will lead to a high degradation. This figure is a convincing demonstration of how powerful a simulation of cable behaviour can be and that a not-justified cabling pattern can lead to a serious performance problem in a very expensive machine like ITER.

The T_{cs} for the OST-II conductor amounts to 7.4 K due to the longer twist pitches. This is in agreement with the test result measured in SULTAN, indeed mainly due to the outcome of the thermal pre-strain of -0.45% (at full load). For the reference cabling pattern at $L_w=7$ mm the T_{cs} by TEMLOP computation is 6.0 K, a value in between the full and none interstrand current sharing regimes. For the OST-I leg, a similar characteristic is computed as presented in Figure 8.17 with a T_{cs} at $L_w=26$ mm of 8.3 K. For a thermal pre-strain of -0.60%, a T_{cs} of 7.6 K is found for the long twist pitch case. For the OST-I leg with $L_w=7$ mm, a T_{cs} of more than 7 K is found, a value much higher

than the 5.7 K experimentally obtained. When adopting a thermal pre-strain of -0.60%, a T_{cs} of 6.7 K is found, still notably higher than the measured value.

Figure 8.16. Calculated normalized I_c versus IxB load of the OST-I conductor with ITER reference cable pattern. The curves behave as the case of no interstrand current transfer for I_c reduction by contact stress and for bending and contact stress.

Figure 8.17. Simulated overall reduction of the current sharing temperature T_{cs} versus characteristic bending wavelength for the OST-II leg for none and full current sharing between strands. A polynomial is drawn through the calculated curve in the case of no current transfer regime to smoothen the discretisation effects [216].

8.4 Effect of twist pitch variations on sub-size CICC's

After the successful test of the TFPRO2-OST2 conductor with long twist pitches, a sequence of “long” twist pitches in the early stages of a multi-stage cable was credited in the community to mitigate the performance degradation seen in all early conductors. To assess quantitatively the effect of long and short pitches maintaining constant all

other conductor parameters, a few four stages CICC samples were prepared, with in the first half length long pitches in the first three cable stages and the second half length short pitches. The last stage pitch is 213 mm for both lengths [208].

The so-called PITSAM5 cable comprises Cr plated copper strands and high J_c Nb₃Sn strands with a diameter of 0.81 mm. The test results of the high J_c Nb₃Sn strands are reported in [209].

The PITSAM5 cable contains 48 superconducting strands and 60 Cu strands and was cabled following the 3x3x3x4 scheme. It is contained in a square steel jacket with outer dimensions 12.6 x 12.6 mm² with a void fraction of 30%. The conductor was assembled into a SULTAN hairpin sample where the two branches have long and short pitches, respectively. The twist pitches are listed in Table 8.3, together with those of an earlier tested sample (PITSAM2), having twist pitches in between PITSAM5 legs [210]. For comparison, the PITSAM1 conductor with different aspect ratio and 144 superconducting strands is also included (see Figure 8.18) [210].

Figure 8.18. PITSAM1 conductor with different aspect ratio, 144 superconducting strands, no copper strands [210], and 8 layers of strand in transverse load direction, courtesy A. Vostner.

Table 8.3. Specifications of the PITSAM conductors with a void fraction of 30%.

Sample names	PITSAM1	PITSAM2	PITSAM5	PITSAM5
Sizes in [mm]			Short pitches	Long pitches
Twist pitch	58/95/139/213	58/95/139/213	34/95/139/213	83/140/192/213
Outer dimensions	21.1 x 9.5	12.6 x 12.6	12.6 x 12.6	12.6 x 12.6
Jacket thickness	1.60	1.65	1.75	1.75

The results are gathered in Figure 8.19 in terms of reduced T_{cs} versus the characteristic bending wavelength (according to Equation 8.1 explained further on) for the PITSAM conductors, together with the reduced T_{cs} from both TFPRO2 legs. The T_{cs} of the PITSAM and TFPRO2 legs for zero load condition is determined by the Jackpot cable model for $\epsilon_{axial} = -0.60\%$, taking into account SULTAN's magnetic field profile and the cable self-field, based on the strand properties from [209]. The trend, indicated by the dotted line, is following the TEMLOP predictions as depicted in Figure 7.20. The

degradation of the TFPRO2-OST1 is larger because of the higher number of strand layers in the direction of the electromagnetic force.

Figure 8.19. Reduction of current sharing temperature T_{cs} versus characteristic bending wavelength for the TFPRO2 and PITSAM conductors. The dotted line is a guide to the eye excluding the PITSAM1 result.

PITSAM1 is made with the same strand and twist pattern 58/95/139/213 as PITSAM2, but with 144 Cr plated superconducting strands and no relatively soft copper strands and 3x3x4x4 scheme was manufactured with a different aspect ratio, having outer dimensions 21.1 x 9.5 mm² and also a void fraction of 30%. The performance of PITSAM1 was computed with TEMLOP and the results are shown in Figure 8.20.

Figure 8.20. Reduction of the critical current versus the strand layer number N_l for the PITSAM1 conductor according to the TEMLOP computation. The total reduction of the I_c , $I_{c-red}(tot)$, the I_c reduction from the peak bending strain, $I_{c-red}(strain)$, the I_c reduction from contact stress, $I_{c-red}(stress)$, and the corresponding stress are plot versus the number of strand layers in the direction of load accumulation.

The total reduction of the I_c , $I_{c\text{-red(tot)}}$, the I_c reduction from the peak bending strain, $I_{c\text{-red(strain)}}$, the I_c reduction from contact stress, $I_{c\text{-red(stress)}}$, and the corresponding stress are plot versus the number of strand layers in direction of load accumulation. Since there is no I_c reduction from contact stress, the total I_c reduction is similar to the I_c reduction by bending.

According to TEMLOP there is only a very small reduction in performance up to 8 strand layers where after more degradation commences. According to the test results in SULTAN and the JackPot T_{cs} calculation before loading and assuming -0.60% axial prestrain, a reduction of about 5% of the T_{cs} is expected. This reduction is much less compared to the PITSAM2 conductor with the same twist pitch configuration (T_{cs} reduction about 17% in Figure 8.19) because of the different aspect ratio of PITSAM1 (only 8 layers of strands) and all strands are Nb_3Sn (higher stiffness). For increasing number of strand layers and copper strand inclusion, the I_c degradation worsens quickly according to Figure 8.20.

The main conclusion is when all other conductor parameters (void fraction, strand, geometry) are kept constant, a sequence of long cable pitches is beneficial to mitigate the irreversible, transverse load degradation in an ITER type cable-in-conduit conductor. A long pitch in the first triplet is the main driver to reduce the degradation [208]. The results of the sub-size CICC tests of the PITSAM series yield an additional confirmation regarding of the influence of the pitch length and are in good agreement with the TEMLOP predictions. Following the results presented here adjustments to the ITER TF cable pattern were proposed as explained in the next section.

8.5 Revision of ITER TF cable pattern

For the validation of the model, an experimental verification of the prediction was proposed by testing a short sample in SULTAN [144]. Two legs, identical but with a significant difference in the cabling scheme, had to be compared directly on their performance under identical circumstances. A cabling scheme was proposed with twist pitches at least twice longer for all stages except the final one (see Table 8.1). The last cabling stage pitch comprising wrapped petals, was kept as forced by the joint size.

The result of the SULTAN test provided a full confirmation of the prediction with outstanding results of a conductor manufactured with even a relatively weak strand. It clearly confirmed that the cable pitches optimisation leads to an outstanding improvement thereby eliminating degradation. As predicted by the model, the ITER conductor design, can be adjusted to the proposed cabling pitches scheme 116 / 182 / 245 / 415 / 440 mm and 28% void fraction instead of the inefficient ITER reference scheme 45 / 88 / 127 / 245 / 470 mm and 29% void fraction.

Apart from the electromagnetic load and strand mechanical properties, the associated deflection by bending is determined by the distance between the supporting strands providing the reaction force (see Equation 7.1 and 7.2). This wavelength is in turn determined by the cabling pattern of the strand bundle and is connected to the number of elements per sub-cable, the twist pitches and finally by the load determining $L_{w(n)}$.

The average wavelength L_w present in the real cable was investigated for a few disassembled ITER conductors. For the relation between twist pitch and characteristic bending wavelength an empirical first order dependency is proposed. It appears that the number of strands and twist pitch of the first stage is fairly dominant. The relevance of higher stages gets less important with increasing stage number as long as the increase is maintained proportional. This is supported by the experiments done on the sub-size PITSAM samples described in section 8.4. For this reason only the twist pitch of the first triplet is used, knowing that the second and higher stages are less relevant:

$$L_w = \frac{L_{p1}}{(N_{Lp1})^2} + \frac{L_{p1}^2}{[1m]}, \quad [m] \quad (8.1)$$

where L_{p1} is the first stage twist pitch, N_{Lp1} is the number of strands in the first stage with $N_{Lp1}=3$ for the TFPRO and PITSAM conductors. For the ITER reference cabling pattern applied to TFPRO2 L_w is estimated at 7 mm for the OST-I leg and 26 mm for the OST-II leg.

In general, longer cabling pitch lengths result in an increase of the coupling loss. As a first order approximation, the interstrand coupling loss is proportional to the twist pitch length. However, the relation between the twist pitches of subsequent cabling stages and the effect on the overall cable coupling loss was still unknown at the time the decision for the final TF cable pattern was made. At ITER it was believed that the originally proposed cabling pattern as applied in TFPRO2-OST2 was in conflict with the restrictions given by the generation of coupling loss and therefore not within the acceptable limits set for ITER.

The AC loss in terms of interstrand coupling loss was a driving factor for minimization of the cabling pitch lengths in combination with compellingly more stiffness to the cable bundle and a larger coolant fraction in the cable. Although, based on the CSMC CS2 AC loss results, at that time already it was clear that an extrapolation of the coupling loss by only using the square ratio of the twist pitches would be too conservative [144]. Moreover, the TF magnets generate stationary magnetic fields and are not (fast) ramped during plasma operation.

With regard to the effect of twist pitch combinations on coupling loss, it is interesting to compare some recent prototype CS conductors with the TFPRO2-OST2. The CSIO1 prototype conductors are according the CS reference design (CSIO-Baseline) and a similar cable pattern but with three superconducting strands and Cu:nonCu 1.5 as CSIO-3SC. Two alternative designs, one with long twist pitches (LTP) and one with very short twist pitches (STP) are included as well. All conductors are based on similar strand design with Cr-plating and Cu:nonCu close to 1, except for the CSIO-3SC with

Cu:nonCu 1.5, the specifications are listed in Table 8.4. The AC loss versus frequency as measured in SULTAN after load cycling is shown in Figure 8.21 for all conductors. The coupling loss time constants derived from the initial slope of the loss versus frequency curves are listed in Table 8.4.

Both CSIO1 conductors have low $\pi\tau$'s of 11 and 13 ms, which can be explained by the relatively large void fraction of 33.4%. The CSIO2-LTP conductor with long twist pitches has a ratio close to 1.1 for all subsequent cable stages within the last stage petal, which results in a small $\pi\tau$ of 24 ms, in spite of the low void fraction of 29.1%. The CSIO2-STP cable design with very short twist pitches and a void fraction of 32.4%, selected for the ITER CS conductors because of its good mechanical performance, has the highest coupling loss with an $\pi\tau$ of 150 ms. This is almost twice higher than the TFPRO2-OST2 with very long twist pitches and void fraction of 28%.

The explanation is in the choice of the twist pitches and the combination with the right void fraction for good mechanical performance. The key for minimum coupling loss lays in the choice of a small ratio of subsequent twist pitch lengths, preferably close to 1.1. The CSIO-STP has very short twist pitches but still high twist pitch length ratios, resulting in high coupling loss. The ratio between the fourth and first cable stage is about 8 for the CSIO-STP, while it is about 4 for the TFPRO2-OST2. In addition, the CSIO-STP constitutes a very stiff strand bundle, not able to increase the interstrand contact resistance between strands to a significant extend up to 6,000 cycles. The TFPRO2-OST2 has more scope to relax the contacts and after 1,000 cycles a lower AC loss was reached already. At the same time the mechanical performance of the TFPRO2-OST2 was excellent already.

A next step in CICC optimisation is the combination of the TFPRO2-OST design principle of long pitches and reduced void fraction with the optimized cable pattern for AC loss with a small ratio of subsequent twist pitch lengths, close to 1.1 [211, 212, 213, 214, 215].

However, the motivation for ITER at that time not to accept the TFPRO2-OST2 design for the TF conductor was further minimization of coupling loss. A compromise was requested by ITER for a significant reduction of the twist pitch lengths and at the same time allowing an acceptable marginal degradation. Although the acceptable marginal degradation was not quantified, a threshold for limited Nb₃Sn filament breakage was taken as the critical limit for the TEMLOP analysis.

TEMLOP was then used to calculate the minimum allowable bending wavelength L_w . It was assumed that the functionality of Equation 8.1 was sufficiently reliable to determine the minimum allowable twist pitch length of the first stage. With Equation 8.1, we obtain $L_w=15$ mm and as can be seen in Figure 8.17, some degradation is anticipated [216, 217]. According to the TEMLOP computations, the acceptable T_{cs} degradation is mainly caused by broadening of the filament strain distribution caused by bending and plastic deformation of strands. Nb₃Sn filament breakage is controlled due

to the limited locations with moderate peak bending strain exceeding the strain irreversibility limit .

Table 8.4. Twist pitch schemes and coupling loss time constants for an applied magnetic field amplitude of 0.3 T and 2 T background field on different ITER conductors.

	CSIO1-3SC	CSIO1 Baseline	CSIO2-LTP	CSIO2-STP	TFPRO2-OST2
Strand					
Cu:non-Cu	1.5	1.0	0.95	0.95	1
Cable	(3 sc)	(2sc+1Cu)	(2sc+1Cu)	(2sc+1Cu)	(2sc+1Cu)
Layout	x3x4x4x6	x3x4x4x6	x3x4x4x6	x3x4x4x6	x3x4x4x6
Pitches					
[mm]	45	45	110	22	116
Stage 1	83	83	115	45	182
Stage 2	141	141	127	81	245
Stage 3	252	242	140	159	415
Stage 4	423	423	385	443	440
Stage 5					
νf [%]	33.4	33.4	29.1	32.4	28
$n\tau$ [ms]	11	13	24	150	90

Figure 8.21. Coupling loss versus frequency measured in SULTAN with a sinusoidal magnetic field of 0.3 T amplitude and a background magnetic field of 2 T on ITER conductors with different cable twist pitches after cycling.

Following these principles, the first stage twist pitch length must be above 80 mm. As such, a twist pitch scheme of 80 x 140 x 190 x 300 x 450 mm was accepted as a compromise for the ITER TF conductor, also indicated as Option II. In addition, the void fraction for the final TF CICC design was increased from the 28% in the TFPRO2-OST2 to 30% for the final ITER TF conductor.

8.6 Conclusion

The new TEMLOP model developed for the design optimisation of Nb₃Sn CICC superconductors for solving the problem of degradation by transverse strand-to-strand contact load, predicted a priori that the application of longer or shorter subcable pitches would be greatly beneficial. The prediction has been validated by the results of a SULTAN test on two full-size TF conductors, one with the ITER reference-cabling pattern and the other with longer subcable pitches, proving that there is no noticeable degradation for the one with long pitches and low void fraction.

In addition, tests on sub-size CICC_s confirmed experimentally again the correctness of the prediction and indicated that also a change of the CICC aspect ratio, decreasing the number of strand layers in the direction of the electromagnetic load, is mitigating degradation.

The TFPRO2 leg with long pitches has a T_{cs} of 7.4 K, which is at least a 1 K improvement with respect to the other ITER TF samples tested in SULTAN at that time. The assessment of the effective axial strain is -0.48%, which is a very small value compared to the effective axial strain commonly derived from degraded CICC samples in that period. The n -value, a very sensitive indicator for filament cracks, substantiated the outstanding performance of the OST-II leg with no degradation at all and featuring an n -value virtually similar to the virgin strand.

The model gives a good description for the mechanical response of strands to a transverse load, from layer to layer in the cable, based on the strand stiffness and cable void fraction. The computations are based on the measured mechanical and electromagnetic properties of the strands as determined by periodic bending, contact stress and uni-axial strain tests. The computed T_{cs} based on the TARSIS measurements is mostly higher than what is interpreted from the ITER CICC tests. This is probable partly due to the rudimentary coarseness of the TEMLOP model, hardly being further developed since the first version and not tuned in with experimental results. Another reason is that the cyclic loading may lead to a different result in CICC_s compared to single strand testing. Nevertheless, the work presented here clearly demonstrates that the ITER reference-cabling pattern (Option I) lead to a substantially degraded conductor performance. Choosing intuitively or pragmatically, without analysis, a cable pattern with a wavelength between 0.002 and 0.01 m will lead to a high degradation. The TEMLOP analysis is a convincing demonstration of how powerful a simulation of cable behaviour can be and that a not-justified cabling pattern can lead to a serious performance problem in a very expensive machine like ITER.

Higher strand stiffness is favourable, at least for coils that are subjected to highest transverse loads. A lower void fraction is also needed, not only to avoid a large gap between the inner conduit wall and the cable under applied transverse load but also to block too much strand deflections.

The main conclusion is when all other conductor parameters (void fraction, strand, geometry) are kept constant, a sequence of long cable pitches in the subcables is beneficial to mitigate the irreversible, transverse load degradation in an ITER type cable-in-conduit conductor. The effect is further improved in combination with a lower void fraction. The beneficial effect is caused by a more homogeneous distribution of stress and strain in the cable and significantly moderation of the local peak stresses associated with intermediate twist pitches. The ITER TF conductor specification has been adapted for allowing longer pitches according to the so-called Option II with an 80 mm first stage twist pitch.

The long cabling pitch lengths of the TFPRO2-OST2 cable based on TEMLOP were not accepted by ITER. It was thought that the long pitches were at odds with the restrictions given by the generation of coupling loss. The interstrand coupling loss was a motivation for minimization of the cabling pitch lengths together with giving more stiffness to the cable bundle. It is noteworthy that the selected cable design with short twist pitches for the ITER CS coil, which is a pulsed coil, has higher coupling loss than the TFPRO2-OST2. Also, the TF magnets generate stationary magnetic fields and are not (fast) ramped during plasma operation like the CS coils. The explanation lays in the choice of the twist pitches, their sequence and combination with void fraction.

A next step in CICC optimisation can be the combination of the TFPRO2-OST2 design principle of long pitches and reduced void fraction together with the optimized cable pattern for AC loss.

9 Conclusion

The focus in this thesis is on understanding degradation in transport properties due to transverse electromagnetic load acting on Nb₃Sn strands in ITER type Cable-In-Conduit Conductors and optimisation of the cable design to mitigate the performance degradation putting large-scale magnet projects at risk.

9.1 Axial strain in strands of a CIC conductor

Knowledge of the quantitative effect of Nb₃Sn strand deformation on the transport properties is crucial for understanding Cable-In-Conduit Conductors. The strain condition of the Nb₃Sn filaments inside strands determines the performance of the CIC conductor, not only when subjected to electromagnetic load but also thermal pre-strain due to cooling down from reaction heat treatment at some 950 K to 4 K. For this reason the versatile TARSIS facility for investigating strands with devices for axial and transverse deformations was developed. TARSIS enables measurement of the transport properties, i.e. the voltage-current transition, applied force and deflection in the course of cyclic loading.

The stiffness of the strand restrains the response to applied stress thereby determining the final change in the load strain and so the critical current. Since the mechanical material properties of the strand depend on the manufacturing process, the effective stiffness can vary depending on the process and the fractions of materials in the compound. Bronze route, internal tin and powder-in-tube type of Nb₃Sn wires were tested with good reproducibility at three temperatures, 4.2 , 77 K and room temperature. A straightforward 1D method appeared a very practical and convenient method for computation of stress-strain curves, based on general available material properties for the temperatures mentioned.

At higher stress levels when yielding takes part, a considerably higher strain for the softer bronze route wires is present compared to internal tin and powder-in-tube fabrication methods, respectively. When the average axial compression of the Nb₃Sn filaments is about -0.4%, a level that can be anticipated for CIC conductors with a stainless steel conduit, one can expect significant variations for the different wire processing techniques in performance, depending on the specifics of the load conditions. The spread in strand axial tensile strain at an applied stress level of 150 MPa can vary up to a factor 1.6. The strain is always higher for the softer bronze route wires, by about 0.2%.

Besides the strand's axial stiffness, the change of the strand's transport properties with uni-axial strain variation is an imperative feature. The determination of the critical current characteristics is performed by applying data obtained with the PACMAN tool at across a range of strain, temperature and magnetic field to identify the critical surface. It is demonstrated that determination of the scaling parameters of the deviatoric strain model, a basic set of 30 voltage-current measurements is adequate. Most of the ITER TF strands and a couple of CS strands were extensively tested to study the impact of axial strain on the electrical transport properties. The susceptibility of the critical current for axial compressive strain is higher for internal tin than for bronze route strands.

After application of high axial tensile strain, densely and uniformly spaced cracks were observed in the bronze route strand, while less but more correlated cracks occurred in the internal-tin strand. Crack initiation was observed in the bronze route strand after applied tensile strain of more than 0.8%, while for the internal tin strand cracks were found already in unloaded specimen with further crack growth beyond 0.3% applied strain. In both types of strands the crack density increases and cracks propagate with increasing strain.

For the first time, utilizing metallographic analysis of filaments subjected to strain loading compared with PACMAN measurements, a clear coincidence between the onset of the irreversible I_c degradation and crack growth was shown. The irreversibility strain limits of the strands, derived from their n -value, exhibit a limit above 0.4% for all bronze route strands while all internal tin strands show scattering between zero and 0.4%.

In full-size ITER TF CIC conductor qualification tests, the internal tin strand conductors perform significantly better than the bronze route strand conductors in terms of current sharing temperature. This seems simply consistent with the strand critical current density, given that internal tin strands have a 10 to 35% higher critical current (measured on a barrel with minor compression).

The average current sharing temperature at the end of cyclic loading obtained with internal tin strand conductors is 6.6 K, while 6.1 K with bronze route conductors. The average current sharing temperature drop with cyclic loading by Lorentz force at 1000 cycles is about 0.3 K for internal tin and 0.2 K for bronze route strand conductors. This 'larger degradation' observed on internal tin conductors may also occur because the initial degradation from virgin condition in terms of current sharing temperature is higher for bronze route strand cables.

However, the large difference in critical current between internal tin and bronze route conductors around the peak in the critical current versus strain curve, is largely diminished at a compression around -0.5%. Initial differences in critical current of about 25% can converge to less than 10% when in compression. The higher strand axial stiffness of internal tin strands is an important property protecting CIC conductors for degradation.

9.2 Bending strain in strands of a CIC conductor

The results of strand tests in the unique TARSIS bending test bench were validated satisfactorily against a simple and straightforward experimental method to verify the effect of bending due to electromagnetic load on Nb₃Sn strands. For this, the strand is wound on an ITER critical current test barrel however with extra slots to impose a spatial periodic bending pattern along the two centre turns.

For strands manufactured with different processing techniques, the critical current reduction versus applied bending strain is practically similar for all applied wavelengths in the range from 5 to 10 mm. The critical current reduction is almost equivalent to the so-called “low interfilament resistance model for uniform bending”, although the estimated current transfer length of the strands based on AC loss measurements, ranged from 4 to 21 mm. To date, the detailed relation between filament twist pitch, interfilament resistance and bending periodicity has not been experimentally investigated and further work is recommended. In an experimental manner, this can be accomplished particularly by varying the filament twist pitch for constant bending wavelength, and for quantitative modelling by implementing the strain patterns in the electromagnetic strand model.

The start of I_c -irreversibility for bending is often found in the range of 0.3 to 0.4% applied peak bending strain, at about 1.5 kN/m. The cyclic behaviour in terms of critical current and n -value plainly shows a component representing a permanent reduction and a factor, which is a reversible function of the applied load. The permanent reduction is associated with plastic deformation, yielding of the soft copper stabilizer, sustaining a permanent change of the strain state of the filaments, while the reversible component is linked to the elastic deformation of the strand. Important to notice is that persistent reduction of critical current and n -value does not necessarily indicate filament damage. Clearly, the same behaviour will occur in a CICC and should be taken into account for understanding its performance.

The axial stress-strain response of the strand largely determines spatial periodic bending and thus the transverse load degradation observed in large CICC. Based on the results presented a better strand versus coil performance can be expected for the TFMC model coil with internal tin strand compared to the CSMC model coil inner layer with bronze route strand, which is in agreement with analysis of the model coil results presented by other investigators.

Crucial for conductor modelling is a proper description of the influence of axial compression on the critical current reduction under periodic bending for which specific samples were prepared. A stainless steel tube swaged around the strand imposes thermal contraction. For bare strands in general, the impact of bending on the critical current corresponds closely to the relation proposed for low interfilament electrical resistance. For strands in a steel tube exhibiting a substantial thermal contraction, this relation based on purely axial strain appears too optimistic. The rather complex

condition of bending wavelength, variety of twist pitches, strand strain susceptibility and internal resistance distribution requires a new effort in building a dedicated numerical model for strand behavior. Until the new strand model including the internal strand resistance distribution is available, an empirical scaling is used based on the experimental results obtained with TARSIS on strands in a stainless steel tube.

For strands tested in TARSIS and not subjected to axial pre-compression, no filament fracture occurs in bronze strands at an irreversible critical current reduction of 10 to 20% in unloaded state and 20 to 30 % (partly reversible) under a load of 6 kN/m.

Cyclic bending in TARSIS between almost zero and a peak load of 10 kN/m does not lead to a perceptible degradation in critical current and n -value nor an increase of bending deflection. For high levels of bending strain, bronze route strands show less reduction than internal tin strands, due to their lower strain susceptibility. But importantly, as strand deformation in CIC conductors is load driven, most of the internal tin strands are less sensitive to applied bending load due to their axial stiffness. In general, the bronze route samples expose the largest bending deflection. A good correlation is found between axial stiffness and bending deflection.

9.3 Contact stress on strands in a CIC conductor

Although the average accumulated transverse load under most severe condition does not exceed 20 MPa, the contact stress at strand crossings may locally reach values up to 100 MPa. The effect on the transport properties of Nb₃Sn strands by cycles of spatial periodic loading of identical crossing strands was experimentally investigated with the TARSIS test bench. The device is equipped for direct measurement of strand compression amplitude and applied force with a spatial periodicity in strand crossings of 4.7 mm.

For contact stress, the permanent reduction in critical current after 90 MPa load is low and about few percent for bronze route strand. Cycling at peak contact stress of 90 MPa leads to a continuous increase of the deflection for all strands of several micrometer but it does not saturate. Internal tin strands are significantly more susceptible to contact stress than bronze strands to both transverse contact and homogeneous stress. However, for contact stress between crossing strands the irreversibility for both bronze route and internal tin wires starts to occur already below 20 MPa. The irreversibility stress level for uniform load is not observed for bronze route wire while for the internal tin wire it is reached just below 20 MPa. The susceptibility to irreversible behaviour clearly depends on the strand layout.

The normalized n -value versus applied contact stress seems to behave rather similar for the two types of strand. No clear correlations were found between strand performance and their transverse stiffness. Also here permanent reduction in critical current and n -

value is observed at high stress, associated with plastic deformation of the soft copper stabilizer, causing a permanent change in the strain state of the wire, while the reversible component is linked to the elastic deformation.

It is observed that the absolute value of the stress determines the reduction in critical current and n -value. The spatial distribution seems hardly relevant for the performance degradation, whether a crossing strand periodicity of 4.7 mm is taken or a uniform distribution. This seems in agreement with the results found for periodic bending with different bending wavelengths. So far it is not clear what the relation is between filament twist pitch, interfilament resistance, bending- and contact stress periodicity and further work is recommended. In an experimental matter, particularly by increasing the distance between loading points with crossing strands and in the sense of modelling in the direction of mechanical strand deformation and connecting the imposed strain patterns with the electromagnetic strand model.

The transverse modulus of elasticity $E_{\perp}$, was defined here as the strand deflection divided by the strand diameter. Taking this approximation, the stiffness of the strands at higher uniform stress level ranges between 1 and 2 GPa. The dynamic modulus $\delta\sigma/\delta\epsilon$ at higher stress level is about 2 GPa. For crossing strands contacts, the stiffness of the strands saturates to 2.5 GPa towards a stress of 90 MPa. The dynamic modulus $\delta\sigma/\delta\epsilon$ at higher stress level is about 3 to 4 GPa.

The main difference between the compressive testing of a strand or a cable is the stress distribution in the strand cross section. The load on a strand in the TARSIS crossing strands and uniform load test is applied only in one direction through the vertical plane and the strand deformation is not restricted in the horizontal plane (ignoring the axial direction because of friction). The strands in the cable are constrained by the surrounding strands in first instance and the whole cable eventually by the circular inner jacket wall. This is likely the main explanation for the large difference in $\partial\sigma_y/\partial\epsilon_y$ found between strand and cable. This sets a conservative condition for the strand contact stress deformation in TEMLOP assuming soft strand, while at the same time it may somewhat underestimate the coupled bending deflection.

9.4 Transverse load tests of CIC conductors

When comparing the time evolution of the coupling loss as well as the mechanical performance of similar conductors, a good quantitative agreement is found between the results on short conductor samples in the Twente press and those extracted from the CSMC model coil test campaign. After the immediate decrease during the first few loading cycles, the level of coupling loss remains practical constant up to at least 40,000 cycles with a coupling loss time constant $\pi\tau$ of 80 ms under full load of 650 kN/m. The Model Coils' $\pi\tau$ ranges from 50 to 100 ms. The decrease of the coupling loss with cyclic loading is confined for lower void fraction in Nb₃Sn CIC conductors. A conductor from the TFMC model coil compressed to 26% void fraction yielded a virgin $\pi\tau$ of 470 ms, which gradually decreased to 170 ms, but only after 40,000 cycles. At

30% void fraction the virgin $n\tau$ amounts to 360 ms with 115 ms left after cycling. The transverse sub-petal bundle contact resistance mainly determines the evolution in coupling loss rather than the intra-triplet contact resistance.

In full-size ITER Toroidal Field conductor qualification tests, the AC loss of all samples were measured before and after the DC tests in order to evaluate the effect of electromagnetic and thermal cyclic loading. The AC losses were measured by gas-flow calorimetry in a background magnetic field of 2 T with a sinusoidal current variation at frequencies ranging from 0.1 to 1 Hz. The AC loss of all samples exhibits a reduction after cyclic loading by a factor of 4 to 5 (at 1 Hz). This is similar to what was observed on tests in the Twente press on the same conductors.

The maximum transverse compression at a peak load of 650 kN/m after cycling for Nb₃Sn conductors varies from 640 to 1130 μm when the void fraction increases from 26 to 36%. This average deflection per strand is representative for a peak load of about 20 kN/m on the strands in the accumulated load zone of the ITER CICC's. The corresponding average deformation per strand, in terms of transverse compression at a peak load of 650 kN/m, amounts to 33 μm for 36% (CS1), 24 μm for 30% and 20 μm for 26% void fraction. The stiffness depends on the applied peak load, loading history, cable pattern, void fraction and strand properties and change with the number of load cycles.

Analogous behaviour is observed for short sample testing of ITER Nb₃Sn CICC's and Model Coils. Characteristically a sharp drop is noticed during the initial cycles and mostly a consistent more gradual decrease along further cycling. No definitive saturation can be accomplished during the following thousands of loading cycles. This is in agreement with the continuous further increase of the deflection observed in the Twente press tests and a similar effect is also observed in the crossing strands tests, where the deflection during cycling with the same peak load keeps increasing. Also when testing the CS Insert coil no real stable behavior was reached after a ten thousand load cycles, the T_{cs} kept decreasing with further cycling.

The mechanical loss per unit of strand volume per cycle at a peak load of 650 kN/m during the initial cycles in Nb₃Sn conductors is 200 to 300 mJ/cm³, subsequently decreasing to below 30 mJ/cm³ during the first 100 cycles and remaining then in a range between 20 and 30 mJ/cm³ when further cycling. The mechanical loss hardly depends on the void fraction. The largest plastic deformation of the cable bundle and associated critical current degradation occurs during only the first few cycles and no clear saturation is accomplished during the following thousands of loading cycles.

9.5 TEMLOP transverse load optimisation

TEMLOP models the behavior of cabled superconductors under transverse load taking into account the electromagnetic forces in one direction through the layers of strands. It

is used to optimize the cabling pattern in CIC conductors. The model does not take into account the thermal contraction of the jacket enclosing the cable bundle. The first results of computations for an ITER type conductor immediately led to new insights. As input for TEMLOP were measured mechanical and electromagnetic properties of an internal tin strand and CIC conductors in the Twente press. The model includes a good description for the mechanical response of strands to a transverse load, from layer to layer in the cable, based on the strand stiffness and cable void fraction.

The key conclusion is that decreasing or increasing the cabling pitches, can solve the problem of the severe degradation of large CICC's designed for ITER. The solution is based on providing a more uniform distribution of the stress and strain in the cable and considerably lessening the local peak stress associated with intermediate twist pitches used for the baseline ITER CIC conductor design. The simulations made clear that the degradation is mainly attributed to strand bending, although the transverse contact load on strand crossings and line contacts may locally reach up to 90 MPa.

An important finding is that a lower cable bundle void fraction and higher strand stiffness add to a further improvement of the conductor performance. Other conductor optimizations are possible as well, like for example changing the aspect ratio of the conductor and decreasing the number of strand layers in the direction of the accumulated load. For ITER however, the time schedule did not allow for drastic changes of conductor and magnet design. With only a change in the cabling twist scheme and reduction of void fraction, it became possible to improve the temperature margin significantly in accordance with the TEMLOP model.

The TEMLOP prediction was verified experimentally by testing a short sample in SULTAN. Two legs, identical but with a significant difference in the cabling scheme, in particular for the first four stages, were compared directly regarding their critical current and current sharing temperature performance. A cabling scheme with pitches more than twice longer than the TF baseline conductor, 116 / 182 / 245 / 415 / 440 mm, except for the final stage, and 28% void fraction was selected for the sample called TFPRO2-OST2. The last cabling stage comprising wrapped petals, had to be kept the same because of compatibility with the joint size.

The result of the SULTAN test gave a convincing and complete confirmation of the prediction with outstanding results for a conductor manufactured with even a relatively weak strand. It clearly confirmed that the optimisation leads to improvement of the severe degradation.

In addition, tests on sub-size CIC conductors called PITSAM samples, confirmed experimentally again the correctness of the prediction and indicated that also a change of the CIC conductor aspect ratio, thereby decreasing the number of strand layers in the direction of the electromagnetic load, reduce degradation as well.

The bending wavelength used in TEMLOP is determined by the cabling pattern of the strand bundle and is connected to the number of elements per sub-cable. The actual

average wavelength was investigated for a few disassembled ITER conductors and for the relation between twist pitch and characteristic bending wavelength an empirical first order dependency is proposed. It appeared that the number of strands and twist pitch of the first stage is fairly dominant while the relevance of higher stages gets less important with increasing stage number. This is supported by the experiments done on the sub-size PITSAM samples.

The so-called TFPRO2-OST2 cable sample with long pitches has a current sharing temperature of 7.4 K, which is at least a 1 K improvement with respect to other ITER TF samples tested in SULTAN at that time. The assessment of the effective axial strain is -0.48%, which is a very small value compared to the effective axial strain commonly derived from degraded CICC samples.

The results clearly demonstrated that the ITER reference-cabling pattern (Option I) lead to a substantially degraded conductor performance. Choosing intuitively or pragmatically, without analysis, a cable pattern with a wavelength between 0.002 and 0.010 m leads to high degradation. The TEMLOP model yielded a convincing demonstration of how powerful a simulation of cable behaviour can be and that a not-justified cabling pattern can lead to a serious performance problem in a very expensive machine like ITER.

Since the long cabling pitch lengths based on TEMLOP were at odds with the restrictions given by the generation of coupling loss, the originally proposed cabling pattern as applied in TFPRO2-OST2 was not accepted for ITER. The AC loss in terms of interstrand coupling loss was a driving factor for minimization of the cabling pitch lengths together with compelling more stiffness to the cable bundle.

According to TEMLOP, the minimum allowable pitch length of the first stage, with “acceptable marginal degradation”, has to be kept above 80 mm. According to the TEMLOP computations this degradation is tolerable in the sense that degradation in current sharing temperature will be mainly caused by filament strain distribution broadening and filament damage is expected to be very limited due to the moderated peak bending strains. Arrived at this point the following compromise for the cabling schedule (Option II) as minimum allowable twist pitches for the ITER TF conductor: 80 mm x 140 mm x 190 mm x 300 mm x 450 mm was decided. The void fraction for the final TF CICC design was increased from the 28% of the TFPRO2-OST2 to 30% for Option II.

Clearly, the present Option II TF conductors still suffer from degradation but now at least they all meet ITER requirement of a current sharing temperature higher than 5.8 K after 1000 electromagnetic cycles. The sub-size CIC conductor from the PITSAM series, with the Option II cable pattern also showed a modest degradation but in a full-size TF CIC conductor the effect becomes larger due to the larger number of strand layers and associated accumulated electromagnetic force.

In full-size ITER TF CIC conductor qualification tests, the internal tin strand conductors performed better than the bronze route strand conductors in terms of current sharing temperature. The average current sharing temperature at the end of cyclic loading obtained with internal tin strand conductors is 6.6 K, and 6.1 K with bronze route strand conductors. Strands with high stiffness (Young's modulus) should be selected for conductors subjected to the highest $I \times B$ load if the magnet design concept allows for this choice. Strands with lower modulo can be applied in sections of magnets with lower transverse load.

Later work confirmed that filament fracture reduces the transport properties but the crack densities found in ITER CS baseline CIC conductors are not sufficient to explain the degradation in current sharing temperature during cyclic load testing. This implies that the dominant mechanism for ITER Nb₃Sn CIC conductors is due to variations in axial and bending strain in the strand's filament bundle, forcing current sharing and reducing the current sharing temperature. This is in agreement with the TEMLOP predictions for shorter twist pitches and higher void fraction as selected for the TF Option II cable pattern, with respect to the TFPRO2-OST2 design.

Interestingly, the presently selected cable design for the ITER CS coil has higher loss than the TFPRO2-OST2 conductor with long twist pitches. While the latter one was intended for the TF magnets, which generate stationary magnetic fields and are not (fast) ramped during plasma operation like the CS coils.

The explanation lays in the choice of the twist pitches, their sequence and combination with void fraction. The CSIO-STP has very short twist pitches but still a large twist pitch length ratio, resulting in high coupling loss. The ratio between the fourth and first cable stage is about 8 for the CSIO-STP, while it is about 4 for the TFPRO2-OST2. In addition, the CS-Short Twist Pitch (STP) constitutes a very stiff strand bundle, not able to reduce the interstrand contact conductances between strands to a significant extend up to 6,000 cycles. The TFPRO2-OST2 has more scope to relax the strand contacts and after 1,000 cycles already a lower AC loss than the CS-STP was reached, although for low frequency range, both are similar.

Moving on in CIC conductor optimisation can be the combination of the TFPRO2-OST design principle of long pitches and reduced void fraction together with the optimized cable pattern for AC loss. In addition the conductor cross section can be chosen rectangular to reduce the number of strand layers in the direction of the electromagnetic force.

For exceptionally large-scale projects like ITER it is critical to timely invest in dedicated R&D for superconductors in order to be prepared when the project is started. In the meantime all ITER CIC conductors are in production and are used in the various coils in the magnet system. In the next 10 years the performance of these coils will become clear when ITER is commissioned.

Next steps in the design of CIC conductors concern the specifications of the conductors required for the next generation of fusion and other large-scale magnets using CIC conductors with focus on low cost and small dimensions. The work presented here plainly demonstrates how powerful an integrated approach, combining specific simulation of cable behaviour and small-scale experiments, can serve to reach optimisation of conductor design against low cost.

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Summary

This thesis deals with the understanding of the effects of strand and cable deformation on the performance and optimization of the transport properties of the superconductors used in the magnet system in the thermonuclear reactor ITER. Thermonuclear fusion is one of the alternative sources for generating energy without pollution or greenhouse gases from the process itself. The required fuel is abundantly available and a rather small amount is needed in the reaction.

The ITER project comprises building the world's largest and most advanced experimental tokamak fusion reactor involving one of the largest and most integrated superconducting magnet systems ever built. The machine is expected to demonstrate the feasibility of producing more power from the fusion process than is used to sustain it, not yet been achieved by previous fusion reactors. To generate the high magnetic field needed to confine, shape and control the plasma, the use of superconducting magnets is imperative.

The magnet system in ITER relies on superconducting round wires also named strands, used in so-called Cable-In-Conduit conductors (CICC). The strands on their turn contain thousands of superconducting NbTi or Nb₃Sn filaments embedded in a pure copper or a copper/bronze matrix, respectively. Reliability and stability of the CIC conductors are prerequisite for a machine that eventually will serve for supply of electrical energy to the power grid in the future. Important factors impacting on stability are AC losses and cable performance degradation due to mechanical deformation caused by the enormous electromagnetic forces on the strands. The flexible nature of the cable bundles in the sizeable Cable-In-Conduit-Conductors for ITER comprising more than thousand strands in combination with a void fraction of about 30%, gives scope for cable compression and thus substantial local deflection of strands. In particular the transverse stiffness of the Nb₃Sn cables, when subjected to large electromagnetic forces, has a significant effect on the strand's strain condition, interstrand resistance, coupling loss, cooling and pressure drop along the turns in the windings and are crucial to account for in large magnets as for ITER.

In the years following the Central Solenoid Model Coil tests, the various Nb₃Sn CIC conductor samples tested for ITER showed a substantial degradation in their performance due to the electromagnetic load on the strands and cycling as well. Not only the differences in thermal contraction of the materials in the composite affect the critical current and temperature margin, but also in particular the electromagnetic force

causes significant strain in contacts between crossing strands and bending strain in the Nb₃Sn layers.

For predicting full-size conductor performance, understanding the impact of strand stiffness, transport properties dependence on axial strain and crack initiation is crucial. Axial tensile stress-strain measurements were performed on Nb₃Sn strands manufactured by the internal tin, bronze route and powder-in-tube methods with the especially for this purpose developed TARSIS setup (Test ARrangement for Strain Influence on Strands). With the PACMAN spring, Nb₃Sn strands were characterized at various combinations and values of strain, temperature and magnetic field to identify their critical surfaces. Exceeding the limit of tensile strain irreversibility, leads to cracks in Nb₃Sn filaments. The impact of filament crack distribution on ITER-type Nb₃Sn superconductor strand's transport properties is determined and correlations between crack distribution and transport properties were investigated. This to quantify the distinction between degradation originating from strand and filament deformations leading to strain distribution broadening and from filament cracks.

The effect of spatial periodic bending on transport properties of bronze-, powder in tube- and internal tin- pro-cessed Nb₃Sn strands was measured using dissimilar bending wavelengths. The critical current versus strain sensitivity and the mechanical material properties like effective stiffness of the strand, depend on the manufacturing process. Since the thermal and electromagnetic forces are the drivers for strand bending in CIC conductors, the stiffness of the strands restrains the response to applied stress thereby determining the final change in the strain and so the critical current. The axial strain of Nb₃Sn strands in TARSIS bending probes is due to their thermal contraction while in a CICC the steel conduit imposes an axial contraction. In order to study the effect of the additional axial contraction, Nb₃Sn strands tightly swaged in a steel tube thereby imposing a compressive strain on cooling, were used to simulate experimentally the conditions representative for an ITER CIC conductor.

The effect of a spatial periodic loading by crossing strands and a homogeneously distributed load on the transport properties is compared for a bronze and an internal tin ITER TF Nb₃Sn strand using the TARSIS test rig. Remarkably, it was observed that the absolute value of the stress determines the reduction in critical current and n -value and not the spatial distribution of the load. This conduct is in line with the results found for periodic bending with different bending wavelengths, illustrating that the current transfer length is in the range of the load periodicity.

For bending as well as contact stress, the cyclic loading behavior for critical current and n -value involves a component representing a permanent reduction as well as a factor expressing reversible (elastic) behavior.

For ITER, various companies are involved in strand procurement and all strand types need to be tested, not only regarding their axial $I_c(B, T, \epsilon)$ strain scaling using the PACMAN test rig, but also for transverse loading in the TARSIS rig. It was demonstrated that there is a substantial variation in the strain sensitivity of Nb₃Sn

strands manufactured by various companies using different processing techniques to distinct mechanical loads anticipated in CIC conductors. The experimental results enable discrimination in performance reduction per type of specific load and per type of strand, being in general different for each supplier. Metallo-graphic filament fracture studies were carried out for comparison with electromagnetic and mechanical test results.

The CIC conductors show changes in interstrand contact resistance, coupling loss and mechanical properties with cycling. Some of these are quite significant, depending on the specific conductor properties. A cryogenic cable press was built to investigate the lifetime issues and changes in the mechanical and electrical properties of full-size ITER conductors. The press is fully automatic and designed to perform tests on CIC conductors under transverse variable load along for example 40,000 cycles at 4.2 K. The evolution of identified cable parameters depends on the peak load, void fraction, strand type, strand coating and the number of load cycles. The dissipated heat by the mechanical energy is not a critical issue for ITER magnet operation but is not negligible, in particular for NbTi conductors.

A new model called Transverse Electro-Magnetic Load Optimization (TEMLOP) has been developed for analyzing the transverse load degradation in Nb₃Sn CIC conductors, based on measured strand and cable properties. It is capable to predict how such degradation can be minimized and what parameters are involved to control it. The model uses data describing the behavior of single strands under periodic bending and contact loads as measured with the TARSIS setup and enables a discrimination in performance reduction per specific load and strand type. Furthermore, data extracted from transverse load testing of entire CICC sections in the cryogenic cable press are crucial input for the model. By straightforwardly using the experimental results as input, large uncertainty in mechanical modelling of the composite strands and cables related to plastic deformations was circumvented.

The main conclusion of the a priori TEMLOP predictions presented in May 2006 is that the observed severe degradation in CICC's can be avoided to large extent by decreasing or increasing the pitch length in subsequent cabling stages and by reducing the void fraction. These corrective measures provide more support to the strands, sufficiently reduce the strain, and therefore avoid filament cracking at the strand bending and crossover and points in the cables. It was the first time that an increase of the cable twist pitches was proposed for improving performance without existing experimental evidence. The model also convincingly shows that strand bending is the dominant mechanism causing degradation. Other conductor optimizations are possible as well, like for example changing the aspect ratio of the conductor and decreasing the number of strand layers in the direction of the accumulated load. For ITER however, the time schedule did not allow for drastic changes of conductor and magnet design but other optimization options can be incorporated in next generation conductor designs.

A full-size European prototype TF conductor sample (TFPRO-2), manufactured in autumn 2006, was adapted according the new insight with a first stage twist pitch of

116 mm and tested April 2007 in the SULTAN conductor test facility for validation of the predictions. The results were outstanding. For the first time a Nb₃Sn CIC conductor achieved the performance expected from the single strand properties, with high n -value and no noteworthy degradation.

However, the AC loss in terms of interstrand coupling loss was a driving factor for ITER at that time for restricting the cabling pitch lengths together with compellingly more stiffness to the cable bundle. According to TEMLOP, the minimum allowable pitch length of the first stage, with “acceptable marginal degradation”, had to be kept above 80 mm. According to the TEMLOP computations this degradation is tolerable in the sense that degradation in current sharing temperature will be mainly caused by broadening of the filament strain distribution and filament cracking is expected to be very limited due to the moderated peak bending strains.

As predicted, the present TF conductors following the 80 mm first stage pitch length still suffer from degradation but now at least they all meet the ITER requirement of a current sharing temperature higher than 5.8 K after 1000 electromagnetic cycles.

Later work confirmed that filament fracture cause degradation of the transport properties but that the dominant mechanism for ITER Nb₃Sn TF conductors is due to variations in axial and bending strain in the strand’s filament bundle, forcing current sharing and reducing the current sharing temperature. The work presented here clearly shows how powerful an integrated approach, combining detailed simulation of cable behavior and small-scale experiments, can be and serve to reach optimization of conductor design for large coils against low cost.

Samenvatting (summary in Dutch)

Dit proefschrift gaat over het begrijpen en beschrijven van de effecten die draad- en kabel vervormingen kunnen hebben op de prestaties en optimalisatie van de transport eigenschappen van de supergeleiders voor het magneetsysteem in de experimentele kernfusie reactor ITER. Kernfusie is één van de alternatieve bronnen voor stroomopwekking waarbij geen vervuiling in de vorm van broeikasgassen uit het proces zelf vrijkomt. De benodigde brandstof is overvloedig aanwezig en slechts een kleine hoeveelheid is nodig.

Het ITER-project omvat de bouw van 's werelds grootste en meest geavanceerde experimentele tokamak fusiereactor uitgerust met een van de grootste en meest geïntegreerde supergeleidende magneetsystemen ooit gebouwd. Van de machine wordt verwacht dat het mogelijk is om meer vermogen uit de fusiereactie te onttrekken dan nodig is om de reactie op gang te houden, het geen nog nooit is bereikt in de voorgaande fusiereactoren. De hoge magneetvelden noodzakelijk om het plasma op te wekken, te vormen en te beheersen, maken het gebruik van supergeleidende magneten noodzakelijk.

De geleiders in het magneetsysteem voor ITER zijn samengesteld uit supergeleidende draden in een kabel omgeven door een huls, de zogenaamde Cable-In-Conduit conductors (CICC). De draden zijn op hun beurt weer samengesteld uit duizenden dunne NbTi of Nb₃Sn filamenten ingebed in een matrix bestaand uit respectievelijk koper of een koper/brons combinatie. Betrouwbaarheid en voldoende stabiliteit van de CIC geleiders zijn een belangrijke voorwaarde voor een reactor die uiteindelijk dient voor de levering van elektrische energie aan het net. Belangrijke factoren voor de stabiliteit zijn de wisselstroomverliezen en mogelijke degradatie als gevolg van mechanische vervorming veroorzaakt door de enorme elektromagnetische krachten. De flexibiliteit van de kabelbundels in de forse CIC geleiders voor ITER, met meer dan duizend draden en een helium vulfactor van ongeveer 30%, leidt tot compressie van de kabels met als gevolg aanzienlijke lokale doorbuiging van de draden. Vooral de transversale stijfheid van de Nb₃Sn kabels als ze blootgesteld worden aan grote elektromagnetische krachten, heeft een grote invloed op de vervorming en de elektrische weerstand in de contacten tussen de draden, de geïnduceerde koppelingsverliezen, koeling en het drukverschil over de spoelwikkelingen en is derhalve essentieel voor de stabiliteit van grote magneetspoelen als voor ITER.

In de jaren na de test van de eerste model spoel voor de ITER Central Solenoid, is aangetoond dat in de geteste ITER Nb₃Sn CIC geleiders een aanzienlijke verslechtering

van de stroomdragende eigenschappen optrad ten gevolge van de elektromagnetische krachten, en die verder progressief werd verergerd met het aantal belastingen. Niet alleen de verschillen in de thermische krimp van de materialen in het composiet zijn van invloed op de kritieke stroom en temperatuurmarge, maar vooral ook de elektromagnetische krachten op de draden veroorzaken aanzienlijke vervorming in de dwarscontacten tussen de draden en buigspanningen in de Nb₃Sn lagen.

Om de ITER CICC geleider prestaties goed te kunnen voorspellen is er een beschrijving nodig van de relatie tussen transporteigenschappen en de invloed van draadeigenschappen, kabelbundel stijfheid, rek en buig gedrag en breuk in filamenten. Rekspanningsmetingen in axiale richting zijn uitgevoerd met de speciaal voor dit doel ontwikkelde TARSIS opstelling (Test ARrangement for Strain Influence on Strands), aan Nb₃Sn draden die gemaakt zijn volgens zogenaamde interne-tin, brons route en poeder-in-buis methodes. Met de PACMAN rekopstelling zijn de Nb₃Sn draden gekarakteriseerd met combinaties en variaties van rektoestand, temperatuur en magneetveld om per draad de kritieke waarden vast te leggen. Het overschrijden van de irreversibiliteitsgrens in rek leidt tot interne scheuring en breuk in de Nb₃Sn filamenten. De invloed van de verdeling van de breuken in de filamenten op de transporteigenschappen van ITER Nb₃Sn draden is bepaald en correlaties tussen breukverdelingen en transporteigenschappen zijn onderzocht. Dit is gedaan om onderscheid te kunnen maken tussen degradatie, enerzijds veroorzaakt door draad en filament vervorming wat leidt tot verbreding van de rekverdeling, en anderzijds afkomstig van filamentbreuk.

Het effect van periodieke buiging op de transporteigenschappen van brons route, interne tin en PIT Nb₃Sn draden is gemeten met toepassing van verschillende golflengten. De gevoeligheid van de kritieke stroom als functie van de axiale rek en mechanische materiaaleigenschappen zoals effectieve stijfheid van de draad is sterk afhankelijk van het productieproces. Aangezien de thermische en elektromagnetische krachten de draadvervormingen veroorzaken in een CIC geleider, bepaalt de stijfheid van de draden de respons op de aangelegde kracht en daardoor de rek, en daarmee de kritieke stroom. De axiale rektoestand van Nb₃Sn draden in de TARSIS buigingsopstelling wordt bepaald door thermische krimp na afkoeling, terwijl in een CIC geleider de stalen huls nog eens een extra axiale compressie oplegt. Om het effect daarvan te bestuderen zijn Nb₃Sn draden in een stalen buisje onder voorspanning gebracht en bemeten in de TARSIS opstelling, waarbij de omstandigheden die representatief zijn voor draadbuiding in een ITER CICC worden gesimuleerd.

Het effect van de lokale draadcontacten op de transporteigenschappen door een belasting die periodiek langs de draad en homogeen over de volle lengte wordt aangebracht, is vergeleken in de TARSIS opstelling voor een brons-route Nb₃Sn draad en een interne-tin Nb₃Sn draad voor de ITER TF toroïdale veldspoel. Het is opmerkelijk dat de absolute waarden van de mechanische spanning de vermindering van de kritieke stroom en n -waarde bepalen en niet de ruimtelijke verdeling van de mechanische belasting. Dit gedrag is in lijn met de resultaten die zijn gevonden voor periodieke buiging met verschillende golflengtes. Dit suggereert dat de zogenaamde “current

sharing length”, de afstand die nodig is voor herverdeling van de stroom tussen filamenten, binnen het bereik ligt van de ruimtelijke afstand tussen de belastingpunten.

Voor een cyclisch toenemende belasting bij zowel periodieke buiging als contactdruk, blijkt de degradatie van de kritieke stroom en n -waarde deels gepaard te gaan met een permanente afname (plastische draadvervorming en uiteindelijk filamentbreuk) maar deels ook omkeerbaar (elastisch verend) te zijn.

Voor ITER, zijn verschillende bedrijven betrokken bij de aanbestedingen van de supergeleidende draden en alle soorten draden moeten getest worden. Dit geldt niet alleen met betrekking tot axiale $I_c(B, T, \epsilon)$ karakterisatie met behulp van de PACMAN opstelling, maar ook voor transversale belasting die gemeten wordt in de TARSIS opstelling. Er is aangetoond dat er een aanzienlijke variatie voorkomt in de gevoeligheid van de Nb₃Sn draden vervaardigd door de verschillende bedrijven met behulp van de uiteenlopende technieken met betrekking tot de diverse mechanische belastingen zoals ze optreden in CIC geleiders. De experimentele resultaten maken het mogelijk onderscheid te maken in de prestaties per specifieke belasting en per type draad en laten in het algemeen significante verschillen zien voor elke leverancier. Microscopische studies aan filamentbreuk zijn uitgevoerd en vergeleken met elektromagnetische en mechanische testresultaten om de invloed van filamentbreuk vast te leggen.

De CIC geleiders tonen progressieve veranderingen in de elektrische contactweerstand tussen draden, koppelstroomverliezen en mechanische eigenschappen tijdens een cyclische belasting. Sommige veranderingen zijn significant, afhankelijk van de specifieke geleider eigenschappen. Een cryogene kabelpers is speciaal gebouwd om de problemen en veranderingen van de mechanische en elektrische eigenschappen tijdens de levensduur van de ITER geleiders te onderzoeken. De pers is volledig automatisch en ontworpen om proeven aan CIC geleiders onder transversale variabele belasting uit te voeren tot bijvoorbeeld 40.000 cycli bij 4,2 K. De ontwikkeling van de bemeeten kabelparameters hangt af van de piekbelasting, de draadvolumefractie, soort draad, draadcoating en het aantal belastings-cycli. De vrijgekomen warmte door mechanische energie blijkt niet kritiek voor de ITER magneten, maar is niet volledig te verwaarlozen, in het bijzonder voor de NbTi CIC geleiders.

Een nieuw model genaamd Transverse Electro-Magnetic Load OPTimisation (TEMLOP) is ontwikkeld voor het analyseren van degradatie ten gevolge van de transversale belasting in Nb₃Sn CIC geleiders, op basis van de gemeten draad en kabeleigenschappen. Met het simulatiemodel zijn we in staat om te voorspellen hoe een degradatie zoals die experimenteel is waargenomen, kan worden geminimaliseerd en welke parameters hierbij betrokken zijn en die het proces bepalen. Het model maakt gebruik van de gegevens die het gedrag beschrijven van draden onder periodieke buiging en contactbelasting zoals gemeten met de TARSIS opstelling en maakt een onderscheid in de prestaties per specifieke belasting en draadtype. De resultaten van metingen aan complete CIC geleider secties in de cryogene kabelpers zijn eveneens

cruciaal als invoer voor het model. Door het directe gebruik van de experimentele resultaten als invoer voor het model, kan de onzekerheid in mechanische modelvorming van de ingewikkelde samengestelde draden en kabels met het oog op plastische vervormingen worden omzeild wat uiteindelijk resulteert in een hoge mate van betrouwbaarheid van de voorspelling.

De belangrijkste conclusie van de a priori TEMLOP voorspelling van mei 2006 was dat de waargenomen ernstige degradatie van CIC geleiders in hoge mate kan worden vermeden door de kabel-twistlengte significant te verkleinen of te vergroten en door de helium vulfractie te verminderen. Deze corrigerende maatregelen zorgen voor meer ondersteuning van de draden, afname van de piek vervormingen en voorkomen filamentbreuk bij de contactpunten tussen de draden in de kabel. Het is voor het eerst dat een verlenging van de kabeltwistlengte is voorgesteld om de prestaties te verbeteren zonder directe aanwijzingen op basis van experimentele resultaten. Het model laat ook op overtuigende wijze zien dat buiging van draden het dominante mechanisme voor degradatie is. Andere geleider optimalisaties zijn eveneens mogelijk, zoals bijvoorbeeld het aanpassen van de verhouding van de afmetingen in de geleider doorsnede en het aantal draadlagen in de richting van de geaccumuleerde elektromagnetische kracht te verminderen. Het ITER tijdschema liet echter geen drastische veranderingen van geleider en magneet ontwerp toe, maar deze aanpassingen zijn uiteraard te testen in de volgende generatie ontwerpen van CIC geleiders.

Een Europees prototype ITER TF geleider (TFPRO-2), vervaardigd in de herfst van 2006 is aangepast volgens de nieuwe inzichten met een eerste fase twistlengte van 116 mm en getest in april 2007 in de SULTAN faciliteit om de voorspelling te kunnen bevestigen. De resultaten waren uitstekend. Voor het eerst bereikte een ITER TF Nb₃Sn CIC geleider de prestaties die verwacht mochten worden op basis van de enkele draadeigenschappen, met een ongekend hoge n -waarde en geen merkbare degradatie.

Echter, de wisselstroomverliezen ten gevolge van de koppeling tussen draden in de kabel waren op dat moment voor ITER de bepalende factor voor het beperken van de twistlengtes in de kabel. Vervolgwerk met TEMLOP heeft aangegeven dat de minimaal toegestane twistlengte van de eerste kabelstap boven de 80 mm moet blijven om een "aanvaardbaar marginale degradatie" te bereiken als compromis. Volgens de uitkomsten van TEMLOP is deze degradatie aanvaardbaar in de zin dat deze voornamelijk wordt veroorzaakt door verbreding van de filament rekverdeling en filamentbreuk maar zeer beperkt zal optreden vanwege de gematigde piek buigspanningen.

Zoals voorspeld, als aanvaardbaar compromis, treedt er degradatie op in de huidige ITER TF geleiders als gevolg van de 80 mm eerste fase twistlengte, maar voldoen ze nu allemaal aan de door ITER vereiste "current sharing" temperatuur hoger dan 5,8 K na 1000 elektromagnetische belastingen cycli. Later werk heeft bevestigd dat filamentbreuk de transporteigenschappen van kabels reduceert, maar het dominante mechanisme voor degradatie van de huidige ITER Nb₃Sn TF geleiders is toe te schrijven aan verbreding van de rekverdeling in de filamenten, waardoor stroomherverdeling wordt opgedrongen en de "current sharing temperature" daalt. Het

werk zoals hier beschreven laat duidelijk zien hoe krachtig een geïntegreerde aanpak kan zijn, als een gedetailleerde simulatie van het kabel gedrag en kleinschalige experimenten worden gecombineerd voor de optimalisatie van een geleider ontwerp voor grote magneetspoelen tegen lage kosten.

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Starting in the early nineties with the undertakings of the NET-Team in Garching involving research at the High Current Applications group, lead by Louis van de Klundert, I made my first modest footsteps in the research of superconductors for fusion. The following years the EFDA Close Support Unit in Garching (GE) coordinated the collaboration in Europe on fusion research. During this period the decision was taken to build ITER in Cadarache (F). Shortly after the start of ITER in 2006, it became clear that the ITER Nb₃Sn TF conductors were not able to meet the ITER performance requirements. This released the need to expand the efforts in Twente and to follow an integrated approach based on the development of detailed multi-physics models supported by specific and unique laboratory experiments. I am grateful for the support and credence along many years of Neil Mitchell and Arnaud Devred, both from the ITER Organization.

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