On Measurement and Interpretation of Toughness Behaviour of Carbide Tools

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The actual significance of any definition of toughness behaviour of carbide tools depends on the existence of an interrelation between the quality as defined and the occurrence of chipping and premature failure in cutting. While at present there is no adequate analysis available and the existing classifications do not even provide a qualitative indication for tool choice, one first has to evaluate the behaviour of cemented carbides for simplified load conditions. Where fracture and chipping is a mechanical phenomenon, in the case of tools assisted or even dominated by thermo-mechanical effects, a logical first step for the evaluation of carbide grades would seem to determine the property profile of carbide tools in terms of toughness and resistance to thermal shock, but in such a way that a qualitative interpretation of the cutting conditions can be taken into account. This article deals with a tentative approach for the evaluation of toughness performance of throw-away carbide inserts with the aid of a four point bending test and the diagonal compression test, the latter also being used for measuring the relative resistance to thermal shock.

1. Introduction

Tool life performance of cemented carbide cutting tools is often dominated by chipping and fracture. This in particular applies to milling as a result of the successive interrupted cuts taking place. The spontaneous fracture of the cutting edge and the chipping of the tool faces are both mechanical phenomena, their occurrence being greatly influenced and accelerated by thermo-mechanical activities. In milling the load conditions during entry and exit of the tool are found to be most detrimental to tool life. During entry, the dangerous load conditions will merely be caused by remnants of previous chips which stuck to the tool. Clean tool faces and rounded cutting edges will substantially reduce the risk of chipping and breakage. The exit conditions are even more important. In those cases where the thickness of the chip increases from zero to a given value (up-milling) and depending on the exit angle, negative chip flow may occur¹. This causes tensile stresses in the tool near the rake face and promotes the sticking of chip material to the tool, the latter being detrimental during the next entry into the workpiece material. Thermal cycling is another phenomenon to which milling cutters are subjected². Most harmful to tool life may be the cooling off of the tool faces during the idle period, once the bulk of the tool has been warmed up. This becomes even more dangerous when during the warming up plastic deformation has taken place on the tool-workpiece contact faces. Unfortunately the most wear resistant cemented carbides are highly sensitive to thermal stresses and consequently they will suffer from premature failure by chipping and breakage when used for instance in milling. Both the resistance to chipping and fracturing, referred to as toughness, and the resistance to thermal load are becoming of increasing interest. Now it is common knowledge that for the present prevailing cutting conditions in more than fifty percent of all cases tool life of carbide tools is largely determined by breakage and chipping. The problems which arise in choosing and improving carbide tools can only be solved when both the property profile and the load profile of these tools in operational conditions can accurately be described. This and proper fitting of both profiles can only result from physically based model studies.

It is true that much has been achieved with respect to the thermal and mechanical stress distributions on cutting tools, but the opposite is there where the prediction of toughness behaviour of the "brittle" tool materials is concerned. Concerning the latter, one simply has stuck for too long a time to the classic type of testing methods without noticing that strength and toughness of "brittle" materials like cemented carbides are particularly dependant on the state of stress. Regarding the application of the sophisticated theories of fracture mechanics to cemented carbides no successful results applicable to practical situations are known. As a result of this one has still to use the classic types of definitions which are inadequate to characterize the relevant material properties. Even when disregarding phenomena like impact failure, fatigue and creep, the mechanical problem which has to be solved is very complex and it is therefore not to be expected that a simple solution can be obtained. However, starting from simplified load conditions but taking into account the state of stress it appears to be possible to explain some of the characteristic behaviour of cemented carbide tools.

It would appear that the combined principal stresses rather than the maximum uniaxial stress determines failure and that brittleness is not so much a material property, but rather the materials behaviour depending upon its composition in conjunction with the state of stress induced.

2. A simple property profile of cemented carbides; the introduction of the state of stress and the relative resistance to thermal shock

The state of stress is introduced by the choice of the U.U.S. (ultimate uniaxial strain) as the strength criterion and the isostatic stress component as a conditional parameter. There are a number of experiences which indicate that the U.U.S. i.e.: the value of the maximum strain at the point of failure, rather than any other known definition will satisfy as the criterion for brittle failure. In this respect it should be noted that:

1. Brandes³ has shown that brittle failure occurs, - irrespective of the value of maximum stress - when the maximum elastic strain:

$$\epsilon_1 = \frac{1}{E} \{\sigma_1 - \nu(\sigma_2 + \sigma_3)\} \quad (1)$$

reaches a critical value (U.U.S.).

2. It has been shown by Brandes³ and Shaw⁴ that the U.U.S. depends on the isostatic stress component.
 3. The present author⁵ has shown that for cemented carbides a unique relation exists between the U.U.S. and the structural parameter d_{av}/λ_{av} , the latter being the ratio between the average value of grain size and the average value of the intermediate binder-layer thickness
- $$U.U.S. = A - B \ln(d_{av}/\lambda_{av}) \quad (2)$$
4. Testing concrete at various loading rates, Hatano⁶ concluded that rupture strength varies substantially with the loading rate but that the maximum strain of failure remains unaffected.
 5. The interpretation of results from different fracture tests is possible on the basis of the critical uniaxial strain at failure⁷; e.g. the relation between the U.U.S. and the Charpy un-notched - value is virtually linear for W_{C-Co} composites with $Co > 40\%$.

Fig. 1 Relation between impact strength (Charpy unnotched-value) and the U.U.S.⁵)

For a general triaxial state of elastic stress, the effective stress for failure

$$\sigma_e = \epsilon_1 E = (1 + \nu)\sigma_1 - 3\nu\sigma_i \quad (3)$$

σ_i being the isostatic stress component = $1/3 (\sigma_1 + \sigma_2 + \sigma_3)$. σ_e reaches its critical value σ_{ec} when

$$\sigma_{ec} = \text{U.U.S.} \times E \quad (4)$$

The value of the critical effective stress at failure depends on the value of the isostatic stress component.

This is shown in Fig. 2, giving the experimental results of fracture tests carried out on various carbide grades and using two different test methods viz. the bending test and the diagonal compression test. The latter method will briefly be discussed in the next section.

Fig. 2 The median effective stress at failure vs. the isostatic stress

Assuming a linear relationship between σ_{ec} and σ_i (see Fig. 2), σ_{ec} can be expressed as

$$\sigma_{ec} = \sigma_{e0} - a\sigma_i \quad (5)$$

Eqs. (3) and (5) yield the expression for the maximum uniaxial stress at failure i.e.

$$\sigma_{1c} = \frac{\sigma_{e0} - (a - 3\nu)\sigma_i}{1 + \nu} \quad (6)$$

Since the isostatic stress which commonly occurs in cutting tools is clearly negative (see indicated area in Fig. 2) it is obvious that bending test results alone are not sufficient to predict strength of cutting tools. However, in particular during exit in milling, the σ_i -values will shift into the positive direction and consequently carbide tools may then behave significantly weaker.

The influence of the structural parameter d_{av}/λ_{av} on the U.U.S. has been described and analysed using a continuum-mechanics approach⁵. Referring to Eq. (2), which has been derived from the experimental data given in Fig. 3, A and B are constants. The value of A depends on the type and state of the binder material while B depends on the load characteristic. The value of d_{av}/λ_{av} depends almost exclusively on the percentage of binder material.

Factors such as shape and grain size distribution have no significant influence on it. The value can adequately be calculated with the equation

$$d_{av}/\lambda_{av} = \frac{X_f}{1 - X_f} \quad \text{where } X_f = \sqrt[3]{1-f} \quad (7)$$

and f represents the volume fraction of soft phase.

Fig. 3 The U.U.S. (results from bending tests) vs. the parameter d_{av}/λ_{av} for a number of commercial grades (see Table 3)⁵

It is noticed that no correlation whatsoever exists when the maximum uniaxial stress at failure is being related to d_{av}/λ_{av} . The latter parameter actually describes the constraint to plastic flow in the intermediate binder layers. With $\bar{\sigma}_1$ being the average value of the maximum tensile stress component and $\bar{\sigma}$ being the effective stress for plastic flow, the local plastic constraint factor can be defined as $\bar{\sigma}_1/\bar{\sigma}$. From Ref. 3 it follows that

$$\frac{\bar{\sigma}_1}{\bar{\sigma}} = \ln \frac{k'}{2} + \frac{d_{av}}{\lambda_{av}} = c + \ln \frac{d_{av}}{\lambda_{av}} \quad (8)$$

where k' accounts for porosity, its value being largely dependant on the specific material processing conditions. By combining Eqs. (2) and (8) it is possible to express the U.U.S. directly in terms of the local deviatoric stress

$$U.U.S. = P - B \left(\frac{\bar{\sigma}_1}{\sigma} \right) \quad (9)$$

Regarding the physical significance of this relation it is believed that brittle fracture is predominantly caused by the inability for stress relaxation by plastic deformation in the binder phase, resulting in an increased risk of failure. In conclusion, the mechanical property profile as regards strength and toughness of cemented carbides can be drawn from the following evidence.

1. Strength and toughness of cemented carbides are determined by the U.U.S. (ultimate uniaxial strain).
2. The U.U.S. is uniquely determined by the structural parameter d_{av}/λ_{av} as long as the binder material and the bulk state of stress are not changed. The structural parameter describes the local deviatoric stress in the binder phase and with it the potential plastic flow for stress relaxation, while its value can be derived from the percentage of binder only.
3. The influence of the bulk state of stress on the U.U.S. acts via the isostatic-stress component σ_i , its influence becoming more substantial with increasing value of d_{av}/λ_{av} and hence of $\bar{\sigma}_1/\sigma$ (see Fig. 2).
4. A change in binder material can be described by a difference in offset of the curve in Fig. 3. This means in terms of Eq. (2), that the value of A largely depends on the properties of the binder material.

The composition of this evidence yields the mechanical model depicted in Fig. 4.

Fig. 4 Mechanical model on strength and toughness behaviour of cemented carbides

The arrows in Fig. 2 indicate the rotation and the shifting of the curve as a result of an increase of σ_i or a change in binder material respectively. To complete the toughness behaviour profile, in addition to the mechanical property profile the thermomechanical behaviour has to be brought in. The resistance to thermal shock is closely related to the U.U.S. The relative resistance to thermal shock (R.T.S.) can be written as $U.U.S. \times k/(R\alpha)$ where k is the thermal conductivity, α the coefficient of thermal expansion and R a factor depending on the character and magnitude of the thermal load. This will be dealt with further in the next section.

3. The measurement of the U.U.S. and the R.T.S. of cemented carbide inserts

The main diagonal compression test

The main disadvantages of the standard mechanical test methods such as the uniaxial tensile test when applied to brittle behaving materials like cemented carbide concern the need for careful preparation and handling of the specimens as well as the considerable test-to-test variation which requires a large number of specimens. Another obvious disadvantage is that the shape and size of carbide inserts are not suited for one of the standard tests. Shaw et al. were the first who reported on the disk test as applied to cemented carbide disks. The test is based on the phenomenon that a transverse tensile stress is acting across the loaded diameter of a diametrically loaded disk. Except for the area near the load, where the state of stress is bi-axially compressive, the transverse tensile stress is nearly uniform across the loaded diameter and equals

$$\sigma_{1y} = \frac{2F}{\pi Dt} \quad (10)$$

in which F is the load, D is the diameter and t is the thickness of the disk. Across the loaded diameter a compressive stress σ_{2y} is acting perpendicular to σ_{1y} (see Fig. 5), showing a minimum at the centre of the disk. A diagonally loaded square specimen shows a stress distribution which resembles the one in a diametrically loaded disk. At the centre of the disk where failure is initiated, σ_{1y} can equally be calculated with Eq. (10). Defining in plane stress

$$\sigma_{2y} = -K\sigma_{1y} \quad (11)$$

it follows with the aid of Eqs. (1), (3), (10) and (11) that

$$\sigma_{ec} = (1 + vK) \frac{2F_{max}}{\pi Dt} \quad (12)$$

A more detailed analysis concerning the nature of the stress distribution, the influence of the diagonally opposed (loaded) contact faces on it and the nature and location of fracture initiation is given in Ref. 7. It reveals that $K = 3.6$ and the value of v has to be taken as 0.5 (plastic flow in the binder material at failure).

Measurement of the R.T.S.

When a sudden heat flux is applied to the centre of a specimen which simultaneously is under diagonal compression, the thermal stresses cause the resulting stresses to be maximum at the edge of the heated zone (see Fig. 5). Now the effective stress for failure is defined as

Fig. 5 Transverse stress distribution for a combined mechanical and thermal load

$$\sigma_e = (\sigma_{1F} + \sigma_{1T}) - v(\sigma_{2F} + \sigma_{2T}) \quad (13)$$

The indices F and T refer to the mechanical and the thermal load respectively. For a thin circular disk (for a square, the situation will virtually be the same), being subjected to a temperature change ΔT within a comparatively small central circular zone, the tangential stress σ_{1T} and the radial stress σ_{2T} at the border, just outside the heated zone, can be written as¹

$$\sigma_{1T} = \frac{1}{2}\alpha E \Delta T; \sigma_{2T} = -\frac{1}{2}\alpha E \Delta T \quad (14)$$

Since the primary aim of the present analysis is to find comparable values of the resistance to thermal shock rather than absolute ones, preference is given to the lumped approach given below. Let the thermal load be characterized by a heat flux ϕ per unit area, then

$$\Delta T = \frac{\phi}{k} \quad (15)$$

where k stands for the thermal coefficient of conductivity and ψ is a constant dependent on the geometry of the heated zone; for non-steady conditions dependent on the heating time and the specific heat. With the aid of the Eqs. (10), (12), (14) and (15), Eq. (13) can now be rearranged to yield

$$\sigma_{ec} = \frac{\alpha}{k} \frac{(1 + \nu)E}{2} + \frac{2F_{min}(1 + \nu K)}{\pi D t} \quad (16)$$

where F_{min} corresponds to the additional mechanical load which is needed to fracture ($\sigma_e = \sigma_{ec}$) the specimen, when it is simultaneously subjected to a heated central zone. Without thermal assistance

$$\sigma_{ec} = \frac{2F_{max}(1 + \nu K)}{\pi D t} \quad (17)$$

Eqs. (16) and (17) finally yield the possibility to define the relative resistance to thermal shock

$$R.T.S. = \frac{\sigma_{ec} k}{E R \alpha} = \frac{1}{1 - F_{min}/F_{max}} \quad (18)$$

with $R = \frac{1}{2}\phi\psi(1 + \nu)$, its value depending on the character and magnitude of the thermal load and the specific heat. This means that under standardized conditions of the thermal load, the resistance to thermal shock of different carbide grades relative to each other can be determined once F_{min} and F_{max} are known.

The test equipment and specimen

The test-rig for the diagonal compression test is described in Ref. 7. The standard throw-away type carbide inserts are placed upright, the two diagonally opposed contact faces (ground to a length of 0.2 D) touching two copper shims between the dies of an adapted pillar die set. The average dimensions of the inserts are: original diagonal length $D = 17.7$ mm (effective diagonal length = 0.8 D), thickness $t = 3.16$ mm.

The thermal load is obtained by conducting a few pulses of electric current through the specimen, using molybdenum electrodes which are being clamped on both sides in the centre.

To measure σ_{ec} for positive values of the isostatic stress, the four point bending test was chosen. The testing-device consists of two free swinging yokes, the upper one provided with a fixed ram and the lower one carrying a ball joint which is adjustable under load. The specimen (12x3x1 mm), sliced from carbide inserts by wire EDM and polished to a surface roughness of 0.2 μ m, are clamped between four precision needles: two fixed on to the ram and two suspended by the ball.

4. Test results and evaluation

The merit of the diagonal compression test has been proved in Ref. 7;

a main finding was the unusual small scatter in the test results, indicated by high values of the Weibull slope m ($31 < m < 70$). According to the Weibull extreme value theory, the fraction P_1 of a series of specimen that will fail at a strain $U.U.S._1$ can be estimated from the empirical formula

$$P_1 = 1 - e^{-\left(\frac{U.U.S._1}{U.U.S._0}\right)^m} \quad (19)$$

where $U.U.S._0$ is a constant for the series, known as the characteristic value.

For the present aim six different carbide qualities of one make have been investigated, viz. a P05, P10, P25, P35, P50 and a P25 Special grade. For each of the grades mentioned, 8 specimens have been subjected to successively the four-point bending test, the diagonal compression test and the measurement of the R.T.S. All the results have equally been processed using the Weibull probability method. Table 1 shows a survey of the final results.

The first remark which has to be made is that the previously mentioned m -values are substantially greater than the corresponding values in the fifth row of Table 1. It is believed that the reason for this arises from the fact that the experiments, the results of which are given in Table 1, have been carried out by people who had no previous experience in using the diagonal compression test. Visual monitoring of the specimen is necessary to determine the exact moment of diagonal fracture if no sophisticated recording equipment is available, because the moment of diagonal fracture does not always coincide with total breakage.

GRADE		P05	P10	P25	P25S	P35	P50
% Co		9.1	9.0	9.2	10.1	9.7	16.1
d_{av}/λ_{av}		31	31	31	28	29	17
$E(x10^5 \text{ N/mm}^2)$		5.4	5.2	4.9	5.0	4.7	4.5
$\frac{U.U.S._O^1}{(x10^{-3})}$ <							

1) The figure 0 denotes the Weibull characteristic value corresponding to $P = 0.632$

2) The values are recalculated to a critical loaded volume of 30 mm³

Table 1 Compilation of the results of the different tests

This would seem a reasonable explanation since on the one hand

- the very small scatter in the results of several previous tests proves that fracture of the so-called "brittle" cemented carbides is thoroughly predictable, and on the other hand

- it appears that in the case of four out of six grades, viz. the P25, P25S, P35 and P50 grades, the F_{min} -values show substantially less scatter than the F_{max} -values do (the moment of diagonal rupturing always coincides with one of the moments in which the thermal load is activated).

The reliability of the F_{min} -values follows from the testing of two different series of P25S carbide inserts, the results of which are given in Table 2.

Series	m	F_{min0} (N)	r	F_{min} (N)		
				P=0.99	P=0.5	P=0.01
1	10.7	29000	0.95	33474	28020	18825
2	12.8	27654	0.93	31143	26877	19323

R.T.S. for series 1 = 1.98A
R.T.S. for series 2 = 1.89A
Number of samples per series = 8

Table 2 Repeated measurements of F_{min} (P25S-grade)

For the second test series, the number of thermal loadings before failure has been minimized (for measuring F_{min} , the mechanical load is increased stepwise, while after each step the current is activated). The results reveal that F_{min} is not significantly dependent on the number of the thermal loadings applied.

The results of the four-point bending test are fairly acceptable. The respective values given in Table 1 have been converted to meet the same volume of material subjected to critical stress as has been the case in the diagonal compression test ($\sigma_{ec} \times (\text{volume})^{1/3} = \text{constant}$).

Finally, all the results of Table 1 have been graphically compiled in Figs. 6, 7 and 8. In Fig. 7, the critical effective stress instead of the U.U.S. has been put together with the isostatic stress so that the results of both the diagonal compression test and the bending test can conveniently be arranged by a straight line.

Fig. 6 The U.U.S. vs. d_{av}/λ_{av} in bending and in diagonal compression

Fig. 7 The critical effective stress vs. the isostatic stress

Fig. 8 Graphic representation of the R.T.S. covering the different P-grades

5. Conclusion

Strength and toughness of cemented carbides are predominantly controlled by the type and state of the binder material, the structural parameter d_{av}/λ_{av} and the isostatic-stress component. An adequate toughness criterion would seem to be the U.U.S., the isostatic stress being the conditional parameter. It follows from Fig. 6 that the influence of the isostatic stress becomes more substantial when the value of d_{av}/λ_{av} - and with it the ratio σ_i/σ_c - increases. In other words: for increasing positive values of the isostatic stress, substantial differences in strength between the different carbide grades becomes apparent. In table 1 a survey is given of the different carbide grades used in Fig. 3, the different numbers denoting the different grades.

GRADE	P01		P10		P20		P30		P40		P50		K01			K10		
NUMBER	1	2	3	4	5	6	7	8	9	10	11	12	13					
MAKE	A	A	D	A	A	A	A	A	B	D	A	A	D					

GRADE	K20					K30			K40	M20
NUMBER	14	15	16	17	18	19	20	21	22	23
MAKE	A	B	C	D	D	C	D	D	B	A

Table 3 Survey of the grades used in Fig. 3 (The numbers refer to the specific position in the figure)

With reference to the results of the Figs. 6 and 7, it appears from Table 3 that the strength of the P40-, P50- and the M20-grade (the number 6, 7 and 23 resp.) and also that of the different K30-grades (numbers 19, 20 and 21) is only slightly dependent on the isostatic stress.

It is the authors belief that this is a major reason for these grades to perform best in heavy interrupted cutting, at least when thermo-cycling is not the dominating factor. The results of Fig. 7 suggest that for those σ_1 -values which commonly appear in continuous cutting (shaded area) the different grades tend to behave equally well. But when in interrupted cutting the tool approaches the surface of the work-piece at exit, negative shear occurs causing a 180° change in the friction force on the rake and a considerable drop in pressure on the clearance face. Thus, apart from the fact that the tensile-stress region in the tool near the rake will shift towards the cutting edge, σ_1 will substantially decrease.

A tough cemented carbide tool would seem to be a tool whose strength in terms of the U.U.S. is not isostatic stress dependent.

Toughness may prevail as a property in heavy milling, the reason why the K30-grade is suited for this. For less heavy cuts the resistance to thermal shock prevails, but toughness certainly cannot be neglected. The K20-grade (number 14) offers the advantage of both a reasonable toughness and a high RTS-value (6.8A). The P40-grade (number 6), the toughest of all, is not suited to resist thermal cycling since the measured RTS-value is only 2.7A (the latter R.T.S.-values are taken from Ref. 7).

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