Chapter One

INTRODUCTION

This chapter presents an introduction into the field of micromechanics. A summary is presented of transduction principles that are applied in microactuators. The important class of electrostatic microactuators is described and a survey of micromechanical fabrication techniques is given. Finally the scope and the outline of this thesis are summarised.

1.0 MICROMECHANICS

The advances made by silicon IC technology have led to the application of microelectronic devices and components in a wide range of new areas. The trends for microelectronic systems are characterised by increasing miniaturisation, decreasing costs with respect to function, and increasing complexity and performance. Various transducers such as sensors and actuators are required to enable electronic systems to interface with their environment. Until now transducers have mainly been fabricated using conventional fine mechanics. This is a limiting factor for further system integration with respect to miniaturisation, performance and price. Therefore an increasing demand for new transducers based on IC fabrication and other thin film technologies can be observed.

Micromachining techniques use silicon as a mechanical material due to its good mechanical properties and exploit the highly developed technology of integrated circuits for the fabrication of mechanical structures with very small dimensions. The use of silicon as a base material allows the monolithic integration of mechanical structures with electronic circuitry on the same chip. This enables the fabrication of sophisticated microsystems with totally new types of applications and performance.

Due to fundamental scaling properties, the microworld is dominated by surface forces such as surface tension and friction. Bulk forces like inertia and weight tend to become insignificant in the small domain. In the presentations
"There's plenty of room at the bottom" [1.1] and "Infinitesimal machinery" [1.2], dating from respectively 1967 and 1982, R.P. Feynman described the coming technology of making small things and small scale information. He already anticipated the sacrificial layer method of making silicon micromotors, the use of electrostatic actuation and the importance of friction and stiction [1.3].

Systems with small dimensions have advantages that include speed, accuracy and sensitivity. Improvements with respect to reproducibility and reliability of micromechanical devices are also a direct consequence of the batch processing technique employed in the usual silicon IC manufacturing process. Furthermore, a small system can be built from very expensive materials with desirable properties as the cost of materials scales at the third power with characteristic dimensions. Except for thin film processes, it is quite difficult to determine alternative means of building the structures used in microfabricated actuators that involve well characterised and controlled processes.

Micromechanical devices are used for a wide range of applications covering the field of sensors, actuators and constructive elements. Sensors are normally used for transferring information between the electronic and non-electronic world. Micromechanical sensors are mainly used for the detection of mechanical properties such as pressure, acceleration, force, position or flow. Beyond this, micromechanical structures are used for applications where small size and/or small heat capacity increases the performance of the device with respect to sensitivity, response time, power consumption, etc. Typical examples are chemical sensors, temperature sensors and sensors for the detection of radiation. Up to now, pressure and acceleration sensors are the most advanced micromechanical devices.

Even though miniaturisation of mechanical systems is often compared to the miniaturisation of electronics, one should not assume that all mechanical systems will benefit from miniaturisation. For many mechanical systems such as those in the automotive industry, miniaturisation is not practical. However, even in these cases micromechanical devices for sensing and actuation can often improve the overall performance of the system and are attractive because of the batch processing nature of the technology, which produces devices with a low unit cost. However, it should be noted that the overall costs of a final device are also determined by costs such as application specific packaging, bonding and testing. An implicit advantage of the technology is the ability to make a large number of micromechanical devices on the same substrate. Many applications can benefit from the collective operation of a large number of
Microactuators

For example a silicon optical display using a large array of individually addressable torsional mirrors.

The world market for these micromechanical devices is expected to grow to about 10,000 million ECU in the year 2000 [1.4]. Sensors are dominating the markets, but within this decade actuators will gain importance and are expected to hold about 40% of the market in the year 2000. Micromechanical devices are governing the competitiveness of larger instruments or systems, with a market value of about two orders of magnitude larger, demonstrating the role of micromechanics as a key technology of this decade. During the past decade the need for microactuator technology has been increasingly emphasised, and recent advances in microactuators are transforming the field of solid state transducers into the field of Micro Electro Mechanical Systems (MEMS).

1.1 MICROACTUATORS

In contrast to microsensors, research on microfabricated actuators (microactuators) has been largely neglected. The realisation of micromechanical actuators is generally handicapped by the fact that silicon does not exhibit a direct electromechanical actuation mechanism of itself. Electromechanical signal conversion has to be achieved, therefore, via secondary materials and/or system properties.

Actuators are used for the transformation of non-mechanical input energy into mechanical output energy. Microactuators can be divided into two classes: mechanisms and deformable microstructures. Mechanism-type actuators such as micromotors provide displacement and force through rigid-body motion whereas deformable microstructures such as beams and diaphragms provide displacement and force through mechanical deformations. An important issue, especially related to mechanism type of actuators, is microtribology.

An important step in the progress of microactuator technology is the development of actuation forms that can be implemented with the materials and processing technologies of silicon microelectronics. In addition, the actuation should be powered and controlled electrically in order to be compatible with microelectronics. Despite these stringent requirements, a variety of physical phenomena for microactuator applications have been demonstrated [1.5-1.15].
Microactuators employing electrostatic [1.16-1.90], piezoelectric [1.91-1.103], and magnetic [1.104-1.119] effects have been realised. Also microactuators based on thermal effects such as thermal expansion and bi-metal effects [120-131], thermopneumatic and phase change actuators [1.132-1.141] as well as Shape Memory Alloy actuators [1.142-1.146] have been fabricated. Beside these methods other mechanisms like fluid actuated devices [1.147-1.152], giant magnetostrictive alloy actuators [1.153-1.155], electrochemical actuators [1.156] and mechanochemical actuators [1.157] have also been used. The most commonly used microactuation mechanisms are electromagnetic or thermal. The most notable thermal microactuation methods are bi-metallic, thermopneumatics and SMA and the most important electromagnetic microactuation methods are electrostatic, piezoelectric and magnetic.

The force or torque produced by a specific actuator is equal to its spatial rate of energy conversion. Assuming that equivalent spatial rates can be obtained by the different actuation mechanisms, a comparison of available stored energy provides a reasonable metric for comparison of force and torque. Other factors such as down scaling, speed, power consumption, material issues, fabrication technology, size, integration and application must also be considered when comparing different microactuation methods. Each actuation principle has its own advantages and disadvantages. The choice and the optimisation should be made according to the requirements and applications. Some general considerations will be discussed here which are restricted to thermal and electromagnetic actuators.

Although heating and cooling rates increase with decreasing dimensions, in generally thermal microactuators have a slow response time and a higher power consumption compared to electromagnetic microactuators. Thermal actuation methods are generally more suited for deformable types of microactuators and have been shown to be useful in large deflection and large force actuator applications.

As previously mentioned, important electromagnetic microactuation methods are electrostatic, piezoelectric and magnetic actuation. Magnetic and electrostatic actuation are often employed in microactuators and have been compared by many authors [1.158-1.162]. Although with superconducting magnets very high magnetic fields are feasible, magnetic induction is normally limited to about 1.5 T because of magnetic saturation. The electric breakdown limit in air increases drastically from $3 \times 10^6$ V/m for macroscopic air gaps to $10^8$ for air gaps on the order of a micrometer. As a result the stored energy densities of magnetic and electrostatic actuation are comparable at micron
scales and are of the order of $10^5$ to $10^6$ J/m$^3$.

Efficiency considerations favour electrostatic microactuators over magnetic microactuators. Static excitation of magnetic actuators requires static currents through its windings, leading to persistent conduction losses. However static excitation of electrostatic actuators requires static voltages across gaps which can be essentially lossless. This argument holds as the speed of the actuator increases until reactive power dominates the electrical excitation of the actuator. This should occur at comparable speeds in both types of actuators. Most magnetic actuators contain magnetisable material that will exhibit increasing eddy-current and hysteresis losses as the speed increases.

Typical piezoelectric materials and thin films used in microactuator technology are quartz, zinc oxide (ZnO), lead zirconate titanate (PZT), and polyvinylidene fluoride (PVDF). High energy densities are feasible with PZT. Piezoelectric actuation has been used for deformable types of structures. But piezoelectric motors have also been fabricated that are based on travelling wave ultrasonics, impact drive mechanisms and stick-slip or walking mechanisms where fast vibratory motions are transformed into a slower macroscopic motion.

Generally, the most important factors that have to be considered are the choice of materials and microfabrication techniques. For electrostatic actuators, conductors and insulators are easily available in IC technology, while magnetic or piezoelectric materials are not readily obtainable in integrated circuit processing. Furthermore, piezoelectric materials and soft magnetic materials, hard magnetic materials and magnetic circuit components such as coils are more difficult to implement in micromachining processes. As a result, many electrostatically driven actuators have been developed and realised, and only little actuators have been fabricated employing magnetic or piezoelectric actuation.

Research on microactuators is developing fast and much progress has been made in recent years. One is at the point where micromechanisms are starting to walk [1.50, 1.79, 1.163], swim [1.164] and fly [1.115, 1.165, 1.166].

### 1.2 ELECTROSTATIC MICROACTUATORS

Electrostatic actuators have a long history dating back to the 18th century when several types of electrostatic motors were built [1.167]. In macroscopic devices electrostatic forces are relatively small and high driving voltages are
needed. For this reason, electrostatic actuation is rarely used in macroscopic devices. In contrast, electrostatic actuation is attractive for microactuators as explained earlier [1.158-1.162]. One of the first applications of electrostatic forces in silicon micromechanism was to bend or tilt micro beams for use in mechanical filters, light modulator arrays and electromechanical switches [1.168].

Electrostatic actuation is based upon attractive or repulsive forces between positive and negative charges. In fig. 1 two conducting parallel plates are shown with equal but opposite charges. The vectors indicate the electric forces acting between the upper and the lower plate. These attractive forces tend to align and pull the plates together. A mechanically unrestrained conducting body with net charge, in an electric field created by stationary external conductors each with fixed total charge, can never be in stable equilibrium. Therefore, a mechanically restraining structure is necessary, like a spring or bearing, in order to obtain a stable electrostatic actuator.

![Fig. 1.1 Electric forces induced by offset charge distributions.](image)

Several types of electrostatic actuators can be distinguished by the manner in which the rotor charge distribution is defined. For example: a permanent electret motor has a fixed charge embedded in the rotor. An electric hysteresis motor uses hysteresis in the polarisability of the rotor to maintain the proper phase difference between the rotor and stator charge distributions. An electric induction motor is designed in such a way that the charge distribution is induced in a resistive rotor by the stator charge distribution with the proper spatial phase difference maintained by charge relaxation. The rotor charge distribution can also be induced by the stator charge distribution, but its definition and offset can be obtained by geometrical variations in the stator to rotor gap like, for example, saliencies in the highly conductive rotor. Motors based on this principle are called variable capacitance motors.

Electrostatic actuators can be charge or voltage driven. The driving condition is not irrespective with regard to the actuator behaviour. The voltage is in
many cases the most suitable choice for the independent variable because the electrodes define equipotential surfaces making the voltage independent of position. Constant charge conditions can only be guaranteed at terminal level, i.e. global and not local. Constant voltage conditions at terminal level on the other hand also imply constant voltage conditions locally. The electrostatic force or torque acting on a charged body can be calculated from the spatial derivative of the electrostatic energy at constant charge or from the spatial derivative of the electrostatic co-energy at constant voltage.

A variety of electrostatic microactuators employing arrangements such as parallel plate capacitors, interdigitated finger or comb structures and rotor/stator designs have been fabricated.

1.3 FABRICATION TECHNOLOGIES

The processing technology of IC’s generally consists of a sequence of planar processes modifying the properties of the semiconductor material and the surface topology within a few micron’s depth. The fabrication of most micromechanical devices relies on a three dimensional structuring of the substrate material and or secondary layers deposited on top of the original plane.

Probably the oldest micromachining technique is called bulk micromachining [1.168-1.170]. For bulk micromachining, anisotropic dry and wet etching processes are applied to structure the monocrystalline bulk material. Several silicon etchants, like KOH, EDP and NH$_4$OH, exhibit a dependency of the etch rate on the crystallographic orientation of single crystal silicon. Using silicon dioxide, silicon nitride or metals like e.g. Cr, Au, Ag and Pt as a mask well defined grooves, cavities and mesas bounded by slower etching (111) planes can be etched. Furthermore silicon dopants and electric potential can be used to create etch stop layers. Together with wafer-to-wafer bonding techniques, like anodic bonding and direct bonding, several wafers can be stacked together which results in an increased versatility of the devices that can be fabricated [1.171, 1.172].

In addition to bulk micromachining, surface micromachining techniques have been developed [1.173-1.179]. In surface micromachining structural parts are embedded in layers of a sacrificial material during the fabrication process. The sacrificial material is then etched or dissolved in a chemical etchant that does not attack the structural parts. Polysilicon surface micromachining using
doped or undoped polysilicon as the structural material and silicon dioxide or PSG as the sacrificial layer is the most widely used surface micromachining technique.

Another important fabrication method is LIGA [1.180-1.183]. LIGA techniques are based on X-ray exposure and development of thick resist layers, and subsequent electroplating of metal layers to form the micromechanical structures. After stripping of the resist either a final metal structure or a metal mould insert for subsequent replication processes like injection moulding is obtained. With LIGA techniques very high aspect ratio structures can be obtained with thicknesses ranging up to about one mm and lateral dimensions down to the submicron range.

Besides LIGA other moulding techniques based on UV exposure emerged which are characterised by a lower aspect ratio of around 10, a structural thickness of up to about 100 µm and less dimensional control [1.184-1.189].

Dry fabrication techniques like SCREAM [1.190, 1.191], SIMPLE [1.192] and BSM [1.194] have also been used to fabricate micromechanical structures. These are based upon patterning of the silicon wafer by deep anisotropic etching of silicon, subsequent sidewall passivation and release of the structures by dry isotropic underetching of the silicon wafer.

Combinations of previous techniques are also possible. For example sacrificial layers are also employed in the LIGA technique and recently 3D surface micromachining techniques (HEXSIL) have also been demonstrated. Here small and deep etching grooves in the silicon wafer are used as a mold for the sacrificial layer and are subsequently filled with the structural material [1.194]. After removal of the sacrificial layer 3D structures are obtained and the silicon mold can be recycled for further use. Other examples are a combination of bonding and deep dry etching techniques [1.195, 1.196], the dissolved wafer process where dry anisotropic etching, highly boron doped etch stop techniques, bonding, and wet etching are employed [1.197, 1.198] and DEEMO where deep dry etching of silicon is employed to fabricate a mould for multiple replications [1.199].

1.4 OUTLINE OF THIS THESIS

Conventional microstructures, such as cantilever beams, bridges and diaphragms, are able to move only a limited amount of micrometers perpendicular to the plane of the substrate. This restrained travel in one degree
of freedom has restricted existing microactuators to small motion applications. A flexible microactuator technology requires structures that have unrestrained motion in at least one degree of freedom. Surface micromachining techniques provide this possibility. At the onset of this work only bulk micromachining was used at the MESA Research Institute. A surface micromachining technology needed to be developed and implemented within the existing cleanroom facilities. Polysilicon surface micromachining is the best documented surface micromachining technique to date and has been used in this work. Two central issues in surface micromachining are: the understanding and control of the mechanical properties of microstructural films and the release of the microstructure, for example by wet etching, followed by the drying and surface passivation of the microstructure. Another important fabrication aspect is dimensional control of fabricated structures. The fabrication of electrostatic actuators demands small gaps and compliant or high aspect ratio polysilicon microstructures. This requires anisotropic etching techniques of polysilicon. It was therefore necessary to develop a dry anisotropic etching process based on fluorine chemistry. The resulting surface micromachining technology has been used to fabricate a variety of electrostatically driven actuators.

Current microactuation techniques provide either large motion or high force and torque. For many practical applications both characteristics are needed. In order to extend the possibilities of electrostatic actuators emphasis was given to the design and development of electrostatic actuators that provide relatively large forces and displacements.

In this first chapter a general introduction to micromachining technology and microactuators has been given. Chapters 2-4 deal with technological aspects and in chapters 5-9 emphasis is given to device design and performance of different electrostatic actuators. General conclusions are drawn in chapter 10. Chapter 2 presents an overview of surface micromachining technology with emphasis on polysilicon surface micromachining. The basic fabrication steps are described and material requirements are discussed. Methods to determine the mechanical properties of thin films are summarised. The control of polysilicon mechanical properties, etch characteristics of silicon dioxide in hydrofluoric acid and techniques to prevent stiction of surface micromachined structures are described. Examples of basic surface micromachining processes and some special techniques are given.

In chapter 3 the mechanisms causing stiction of polysilicon structures fabricated by surface micromachining techniques are investigated. A simplified
model is used to show that capillary forces are responsible for bringing 
micromechanical structures into contact with the underlying substrate. 
Experimental results obtained from drying experiments are given and 
compared with the theory. Measured adhesion energies of sticking 
microbridges after drying are compared with various adhesion mechanisms in 
order to reveal the origin of the forces.

Chapter 4 describes a study of reactive ion etching of silicon, using 
$\text{SF}_6/\text{O}_2/\text{CHF}_3$, plasmas in an RF parallel plate system. Surface response 
methodology is used to examine etch rate, selectivity, anisotropy and self-bias voltage 
as a function of $\text{SF}_6$ flow, $\text{O}_2$ flow, $\text{CHF}_3$ flow, pressure and the RF 
power. The effects of the variables on the measured responses are discussed. 
Examples of anisotropic etching of high aspect ratio structures with smooth etch surfaces are given.

In chapter 5 the design and performance of a curved electrode actuator is 
presented. Its operation is based on the deformation of a movable 
micromechanical structure which is deflected by electrostatic forces along a 
fixed curved electrode. The behaviour of this type of actuator is studied by 
using cantilever beam structures. A theoretical description of the static 
behaviour of a deformable cantilever beam that is forced into contact with a 
rigid structure by means of electrostatic forces is given. Modelling of the static 
behaviour is done by a simplified model based on energy methods and by 3D 
coupled electromechanical simulations using CoSolve-EM. Experiments are 
performed in order to verify theoretical results.

Chapter 6 presents the design, fabrication and experimental results of lateral 
comb-drive actuators for large displacements at low driving voltages. Several 
suspension designs are compared with respect to large deflection behaviour. 
The electromechanical behaviour of comb-drive actuators is considered with 
respect to large deflection behaviour. Static and dynamic properties are 
determined experimentally and are compared with theory.

Chapter 7 presents the design, modelling, fabrication and first performance 
characteristics of electrostatically driven axial-gap polysilicon wobble motors. 
Aspects like the gear ratio, torque generation, excitation schemes and torque 
coverage, normal forces, friction, rotor kinetics and dynamical behaviour are 
addressed. The motor performance is characterised by gear ratio measurements 
and measuring starting and stopping voltages.

In chapter 8 a surface micromachining process is presented that has been 
used for the fabrication of electrostatic microactuators which are connected 
with each other and linked to other movable microstructures by integrated gear
linkages. The gear linkages consist of rotational and linear gear structures and the electrostatic microactuators include curved electrode actuators, comb drive actuators and axial gap wobble motors. It is a first step towards mechanical power transmission in micromechanical systems.

In chapter 9 basic design issues and a fabrication process based on surface micromachining techniques for electrostatically driven vacuum encapsulated polysilicon resonators are presented. Experimental results, including an admittance plot of the one-port resonator and a plot indicating the dependence of the Q-factor on the resonator geometry and ambient pressure are presented.

Finally in chapter 10 general conclusions are drawn and suggestions for further research are given.

REFERENCES


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Introduction


Introduction
In this chapter, an overview is presented of surface micromachining technology. Emphasis is given to polysilicon surface micromachining using silicon dioxide as a sacrificial layer that is etched in hydrofluoric acid solutions to free the polysilicon structural parts. The basic fabrication steps are described and material requirements are discussed. Control of polysilicon mechanical properties, etch characteristics of silicon dioxide in hydrofluoric acid and techniques to prevent stiction of surface micromachined structures after drying from rinsing liquids are described. Examples of elementary surface micromachined components are given.

2.0 INTRODUCTION

Nowadays a large variety of micromechanical structures can be fabricated using thin film techniques. The fabrication of these micromechanical structures is mainly based upon three fabrication methods; bulk micromachining, LIGA techniques and surface micromachining. In bulk micromachining, structures are fabricated by etching and bonding techniques of silicon wafers [2.1-2.3]. LIGA techniques are based on X-ray exposure and development of thick resist layers, and subsequent electroplating of metal layers to form the micromechanical structures [2.4-2.6].

In surface micromachining structural parts are embedded in layers of sacrificial material during the fabrication process [2.7-2.9]. These layers are usually deposited onto a silicon substrate. The sacrificial material is then etched or dissolved in a chemical etchant resulting in freely movable structures. This process requires a compatible set of structural materials, sacrificial layers and chemical etchants. Mechanical structures with more than one degree of freedom can be built by surface micromachining using one sided processing without the need for complicated assembling techniques [2.10-2.15].
However, the thin film materials must possess the desired mechanical properties for the application in mind, e.g. a controllable residual stress and stress gradient which are of primary importance. The sacrificial layer and the structural material deposition and processing techniques must be compatible with each other. The etchant should have a high etch selectivity with respect to the structural layer in order to prevent chemical attack during sacrificial layer etching. The etch rate should also be high enough to release the structural layer in reasonable etch times and the etch process should not leave etch residue behind.

In an electromechanical system the structures are used for transduction purposes of different quantities that are generally linked to the electrical domain. In addition to technological and mechanical requirements, this also calls for desired properties in the electrical domain. Based on IC-technology several thin film materials, processes and techniques have been developed in order to fulfil these demands. For example, good conductive properties or a high breakdown voltage and good insulating properties are needed in electrostatic transducers. In this case, polysilicon is often used as a conductive mechanical structure after heavily doping with boron or phosphorus. Silicon dioxide, silicon nitride and air gaps are generally used as insulating layers.

The sacrificial layer etch technique was first demonstrated by Nathanson [2.16] in 1967 with the fabrication of resonant gate transistors that employed free standing gold beams. The fabrication of free standing structures from polycrystalline silicon using a silicon dioxide sacrificial layer that was removed in hydrofluoric acid was introduced by Howe [2.17]. Since then, surface micromachining techniques have been developed, and greatly improved, resulting in a large variety of structural materials and sacrificial layer combinations [2.18-2.36]. However, most of the work has been focused on the polysilicon and silicon dioxide combination using HF solutions to etch the silicon dioxide.

Polysilicon surface micromachining is the best documented surface micromachining technique. It closely meets many of the requirements described above, and the materials and the etchants are IC compatible and can be integrated with IC processes [2.37-2.43]. The emphasis of this chapter will therefore be on polysilicon surface micromachining techniques. The basic fabrication steps are shown in fig. 2.1.

The first step is the deposition and subsequent patterning of the silicon dioxide sacrificial layer. This is followed by the deposition and patterning of the polysilicon structural layer. Next the sacrificial layer is selectively etched in a
Deposition and patterning of sacrificial layer

Deposition and patterning of structural layer

Wet chemical etching of sacrificial layer

Rinsing

Drying

Fig. 2.1 Basic surface micromachining steps. a) Deposition and patterning of the silicon dioxide sacrificial layer. b) Deposition and patterning of the polysilicon structural layer. c) Sacrificial layer etching in a hydrofluoric acid solution. d) Rinsing steps. e) Drying procedure.
fluvoric acid solution. This is followed by rinsing procedures to remove the fluvoric acid and the last step is drying from the rinse liquid.

In the next paragraphs, key steps in polysilicon surface micromachining techniques will be discussed. First, the characterisation of mechanical properties of thin films will be addressed. The next part deals with control of residual strain in polysilicon thin films. Then the sacrificial layer etch process will be described in more detail. This is followed by a discussion of methods to prevent unwanted adhesion problems, stiction, that can occur after drying of the mechanical structures. Finally a number of basic surface micromachining processes will be presented that have been used for successful fabrication of elementary components and some special techniques will be addressed.

2.1 CHARACTERISATION OF POLYSILICON MECHANICAL PROPERTIES

A critical factor in the fabrication of surface micromachined structures is the control and determination of the mechanical properties of the structural material. Design of micromechanical structures requires knowledge of the material properties such as Young’s modulus $E_y$, Poisson’s ratio $\nu$, residual stress and tensile strength. Improper mechanical properties, especially residual stress, can lead to warpage, buckling, unwanted deflections, and fracture that result in dimensional limitations, unwanted operation and device failure. Compressive stress can easily cause mechanical microstructures such as bridges and diaphragms to buckle (see fig. 2.2). Free standing and one-sided clamped structures like cantilevers will curl if normal stress gradients relax, when the beam is released after removal of the sacrificial layer (see fig. 2.3). Cracking occurs from excessive internal stresses and blistering and peeling can result from poor adhesion to the substrate.

For isotropic materials under a bi-axial state of stress the average internal strain $\varepsilon_0$ in the plane of the film is related to the in-plane stress $\sigma_0=\sigma_x=\sigma_y$ by the relation:

$$\varepsilon_0 = \frac{\sigma_0 (1-\nu)}{E_y}$$  \hspace{1cm} (2.1)

using Hooke’s law and neglecting stresses in the direction of the thickness of the film $h$, the average stress in the thin film $\sigma_0$ is:

$$\sigma_0 = \frac{1}{h} \int_{-\frac{h}{2}}^{\frac{h}{2}} \sigma_{0(z)} \, dz$$  \hspace{1cm} (2.2)
Fig. 2.2 SEM photograph of free standing microbridges. The long microbridges are buckled because of compressive residual stress. The cantilever beams bend downward because of a negative stress gradient which also results in stiction problems.

Fig. 2.3 SEM photograph of free standing beams. The cantilever beams are curved upward by a positive stress gradient across the beam thickness. The microbridges are straight because of an average tensile stress in the polysilicon film whereas the circular membranes even show fracture as a result of excessive tensile stress.
2.1.1 Measurement techniques

A variety of different load to deflection techniques and approaches have been used in order to determine thin film mechanical properties. Many measurement techniques have been used to determine residual stress, Young’s modulus and fracture strength. Experimental determination of other properties, for example shear modulus and Poisson’s ratio, are rare.

Tensilometers:

Ideally one would like to measure the stress-strain diagram in order to determine mechanical properties like Young’s modulus and yield strength. For thin films microtensilometers have been used to perform this measurement. However the small thickness of thin films makes this approach difficult and accurate measurements can only be made by dedicated instruments [2.44-2.46].

Wafer curvature:

One of the easiest approaches to measure the residual stress is the measurement of the wafer curvature of a silicon wafer covered at one side with a layer of the thin film under investigation [2.47-2.49]. This approach does not require patterning and etching of the film or substrate. The residual bi-axial stress $\sigma_0$ can be found from:

$$\sigma_0 = \frac{E_s}{1 - \nu_s} \cdot \frac{t_s^2}{6 t_f R}$$  \hspace{1cm} (2.3)

where $E_s$ is the Young’s modulus of the substrate material, $\nu_s$ is the Poisson’s ratio of the substrate material, $t_s$ is the thickness of the substrate, $t_f$ is the thickness of the thin film, and $R$ is the radius of wafer curvature.

Bulge test:

A method to determine Young’s modulus, residual stress and fracture strain is the bulge test [2.50-2.56]. This technique is based upon the measurement of the load-deflection characteristic of a membrane by applying a pressure. By least-square fitting of the measured load-deflection characteristic both $E/I-\nu$ and $\sigma_0$ can be determined. The membrane is constructed from the thin film material or consists of a composition of a mechanical carrier and the thin film material under investigation. Circular, square and rectangular membranes have been used for this technique. The load-deflection relationship is given by:
where $P$ is the applied pressure, $h$ is the membrane thickness, $2a$ is the length or diameter of the membrane and $\delta$ is the deflection at the center of the membrane. The constants $C_1$ and $C_2$ are dependent on the membrane shape and have been determined by Pan [2.56] using FEM analysis for circular and square membranes. The values of $C_1$ and $C_2$ are respectively 4.0 and 2.67 for circular membranes and 3.41 and 1.37 for square membranes, in case the Poisson’s ratio is equal to 0.25.

**Surface profiler techniques:**

Concentrated load-deflection measurements of micromechanical structures have also been performed. Loading of microfabricated structures by a stylus type surface profiler [2.57], cantilever beam bending experiments using a nanointender [2.58, 2.59] or micromanipulator [2.60] and loading of membranes by a surface profiler [2.61] have been used to extract thin film mechanical properties.

**Extraction from resonance data:**

Resonant microstructures can be used to determine the residual strain and the Young’s modulus by using the resonant frequency data of beams [2.52-2.66]. The expression for the natural frequencies of clamped-clamped beams with rectangular cross sections and a large width $b$, under plane strain conditions ($\varepsilon_y=0, b>5h$), can be written as:

$$\omega_n = \frac{\alpha_n^2}{\sqrt{12}} \sqrt{\frac{\gamma [1-\nu^2]}{\rho}} \frac{h}{l} \sqrt{1 + \frac{\gamma_n \varepsilon_0 (1-\nu^2) (l/h)^2}{1 + \frac{\gamma_n \varepsilon_0 (1-\nu^2) (l/h)^2}} (2.5)}$$

where $\alpha_n$ and $\gamma_n$ are mode shape constants that are respectively 4.73 and 0.295 for the fundamental mode, $\rho$ is the density of the beam material, $h$ is the beam thickness, $l$ is the beam length and $\varepsilon_0$ is the residual strain.

A linear fit of $(\omega_n l)^2$ versus $(h/l)^2$ gives the Young’s modulus from the slope of the straight line, and the residual strain from the zero offset. The residual strain can also be found from the ratio of higher overtones to the fundamental frequency.
Diagnostic test structures:

A number of micromechanical diagnostic test structures have been developed in order to determine thin film mechanical properties. This approach has the advantage that their fabrication is complementary to the fabrication and development of other microstructures and is suited to create standard "drop-in" mechanical test structures, analogous to transistor test structures used for extraction of electrical device parameters.

An array of microbridges can be used to measure compressive strain [2.67]. A doubly clamped beam under plane stress conditions ($\sigma_y=0$) will buckle if its length exceeds a critical length $L_{cr}$:

$$L_{cr} = \sqrt{\frac{\pi^2 h^2}{3 \varepsilon_0}}$$  \hspace{1cm} (2.6)

Under bi-axial stress conditions a square plate, clamped at all four edges, will buckle if the length exceeds the critical length $a_{cr}$ given by:

$$a_{cr} = \sqrt{\frac{4 \pi^2 h^2}{9 (1+v) \varepsilon_0}}$$  \hspace{1cm} (2.7)

The strain is deduced by determining the smallest geometry for which buckling occurs. Buckling can be readily determined using a phase contrast microscope. Using arrays of ring shaped structures [2.68] or diamond-like structures [2.69] that convert tensile residual strain into compressive forces, it is also possible to measure tensile strains.

Cantilever beams and spiral structures [2.70-2.72] can be used to measure strain gradients across the beam thickness. Variation in film stress with thickness results in an internal moment per unit width $M_0$ equal to:

$$M_0 = \int_{-\frac{b}{2}}^{b} \sigma_{x(z)} z \, dz$$  \hspace{1cm} (2.8)

where $z$ is taken as the distance from the center of the film. After release, the internal stress relaxes and the structural film will curl with a radius of curvature $R$:

$$R = \frac{E_y h^3}{12 M_0}$$  \hspace{1cm} (2.9)
Passive T-shaped structures with a wide, long center stem and thinner deformable cross [2.73], bridges with an intermediate lateral displacement that rotate a long pointer [2.74-2.76] and strain diagnostics with a bent beam deformation multiplier [2.77] have been used to determine the residual strain.

Furthermore, active micromechanical test structures have been fabricated. Comb drive structures have been used to determine Young’s modulus and residual strain [2.78, 2.79]. The pull-in voltage of cantilever beams, microbridges and membranes can be used to determine the residual stress, Poisson’s ratio and Young’s modulus [2.80, 2.81].

Diagnostic test structures have also been used to determine the fracture strength of thin films. Examples are spiral springs [2.11], bridge-slider structures [2.82] and cantilever beams with an end ring [2.83] which have been externally loaded by mechanical probes until fracture occurred. Surface micromachined cleaving structures [2.84] and test structures consisting of a narrow center beam, loaded by wider beams which are under tensile stress can be used for in-situ fracture stress measurement [2.85, 2.86].

2.1.2 Residual stress in polysilicon

The residual stress in polysilicon can be controlled by varying the deposition variables, by annealing procedures and by doping. Stresses in as-deposited, undoped polysilicon change from several hundred MPa tensile to several hundred MPa compressive over a small deposition temperature range with corresponding changes in thin film microstructure. Consequently as-deposited low stress polysilicon is difficult to obtain without doping or annealing. Annealing at high temperatures or doping and subsequent annealing reduces the magnitude of stresses in polysilicon films to small values, regardless of the initial stress state, film microstructure and substrate. At moderate annealing temperatures, a tensile stress develops in initially amorphous films which decays to low values at high annealing temperatures. Films deposited in a polycrystalline form exhibit an initial compressive stress that also decays to low values with increasing annealing temperature. A more detailed discussion about residual stress will be given in the section on control of polysilicon residual strain.

2.1.3 Young's Modulus of polysilicon

The Young’s modulus of polysilicon can be calculated theoretically from well-known mono-crystalline properties by averaging over the grain
orientations in the film [2.87]. For all classes of cubic crystals, Young’s modulus $E_y$ can be expressed as a function of orientation:

$$\frac{1}{E_y} = s_{11} - 2 \left( s_{11} - s_{12} - \frac{1}{2} s_{44} \right) \left( l_1^2 l_2^2 + l_2^2 l_3^2 + l_3^2 l_1^2 \right)$$

(2.10)

where $s_{ij}$ represents elastic compliance, and $l_i$ are the directional cosines of the orientation with respect to the $\langle 100 \rangle$ axes. The shear modulus $G$ is given by:

$$\frac{1}{G} = s_{44} - \left( 2 s_{11} - 2 s_{12} - s_{44} \right) \left( l_1^2 m_1^2 + l_2^2 m_2^2 + l_3^2 m_3^2 \right)$$

(2.11)

where $m_i$ are the directional cosines in the orthogonal direction $m$ under consideration with respect to the $\langle 100 \rangle$ axes. If both the Young’s modulus and the shear modulus are known, the Poisson’s ratio can be calculated from:

$$\nu = \frac{E_y}{2G} - 1$$

(2.12)

For a $\{111\}$ oriented film, irrespective for all orthogonal directions $l$ within $\{111\}$ planes, the value inside the second bracket of eq. (2.10) is constant at 1/4. Therefore, the in-plane Young’s modulus and shear modulus are constant for crystallites oriented with $\{111\}$ planes parallel to the substrate surface.

For silicon, the elastic compliances are $s_{11} = 0.768 \times 10^{-11}$ Pa$^{-1}$, $s_{12} = -0.214 \times 10^{-11}$ Pa$^{-1}$ and $s_{44} = 1.256 \times 10^{-11}$ Pa$^{-1}$. Assuming that effects of grain boundaries are negligible this gives: $E_y = 169$ GPa, $G = 67$ GPa, and $\nu = 0.262$ for in plane directions of $\{111\}$ oriented thin polysilicon films [2.88].

Guckel calculated a theoretical Young’s modulus for polysilicon with randomly oriented grains and reported a theoretical Young’s modulus of 161 GPa, a shear modulus of 65.9 GPa and a Poisson’s ratio of 0.226 [2.89].

Another theoretical calculation of the Young’s modulus of polysilicon and amorphous silicon thin films, based on the combination of grain and grain boundary effects as well as the dependence of crystalline orientations was presented by Guo [2.90]. They showed that the Young’s modulus is a function of grain size and boundary elastic constant and varies between 150 to 170 GPa for films with $\{110\}$, $\{111\}$ and $\{311\}$ orientations and a grain size of 80 nm.

An overview of experimentally determined Young’s moduli is shown in table 2.1. There exists a large variation in measured Young’s moduli of polysilicon which ranges from 130 GPa to 190 GPa. This is suggested to result from differences in processing conditions, impurities, annealing processes and
doping that affect thin film properties like grain size, grain boundary, crystal orientations, etc. which in their turn change the Young’s modulus. Because of the anisotropic nature of polysilicon, the direction in which the load is applied, i.e. the direction in which the Young's modulus is measured, is also important.

<table>
<thead>
<tr>
<th>E [GPa]</th>
<th>Ref. #</th>
</tr>
</thead>
<tbody>
<tr>
<td>175±15</td>
<td>[2.42]</td>
</tr>
<tr>
<td>160</td>
<td>[2.47]</td>
</tr>
<tr>
<td>123</td>
<td>[2.53]</td>
</tr>
<tr>
<td>151</td>
<td>[2.61]</td>
</tr>
<tr>
<td>174</td>
<td>[2.70]</td>
</tr>
<tr>
<td>175±21</td>
<td>[2.75]</td>
</tr>
<tr>
<td>190</td>
<td>[2.78]</td>
</tr>
<tr>
<td>168±7 (undoped)</td>
<td>[2.79]</td>
</tr>
<tr>
<td>130-160 (in-situ P-doped)</td>
<td></td>
</tr>
<tr>
<td>149-162 (600-1100°C ann.)</td>
<td>[2.93]</td>
</tr>
<tr>
<td>140</td>
<td>[2.96]</td>
</tr>
<tr>
<td>150</td>
<td>[2.157]</td>
</tr>
<tr>
<td>170±20 (B doped)</td>
<td>[this thesis]</td>
</tr>
</tbody>
</table>

*Table 2.1 Overview of measured Young’s moduli of polysilicon thin films.*

2.1.4 Fracture strain of polysilicon

The fracture strain of polysilicon has been determined by Fan using a spiral spring resulting in a value of 1.7 % [2.11]. Koskinen et al measured the fracture strain of polysilicon fibers using a microtensilometer [2.46]. They found a tensile strength between 2.8 and 3.4 GPa (1.6-1.9 %), depending on grain size and found that all fibers failed in a brittle fashion. Employing a bridge-slider structure the fracture strain of polysilicon was found to be 1.72±0.09 % for unannealed samples and 0.93±0.04 % for samples annealed at 1000 °C for one hour [2.82]. A value of 2.84±0.09 GPa (≈ 1.7 %) was extracted for the fracture stress of undoped polysilicon from in-situ fracture structures [2.86]. Doping with boron, showed no significant effect, doping with arsenic showed minor effect and phosphorus doping lead to a 25 % reduction in the fracture stress. A 17 % decrease of the fracture stress was observed for a 100 % increase in the

* See Section 6.4.1 of Chapter 6, page 144.
etching time and no difference was observed for samples etched in concentrated HF or BHF. Most of the reported fracture strains of polysilicon are close to the fracture strain of single crystal silicon which is measured to be 2.6 % [2.91].

2.2 CONTROL OF POLYSILICON RESIDUAL STRESS

Residual stress results from a mismatch in thermal expansion between the film and the substrate (thermal stress), accentuated by depositions performed at elevated temperatures, and from effects of grain growth and deposition which "freeze" atoms in positions other than at zero-stress lattice points (intrinsic stress). The detailed physical mechanisms behind this intrinsic stress generation are not fully known. The effect of process conditions, annealing and doping on the residual stress of polysilicon will be discussed.

2.2.1 As deposited films

Polysilicon is usually deposited by Low Pressure Chemical Vapour Deposition (LPCVD) from pyrolyses of silane (SiH\(_4\)) at temperatures around 600 °C and pressures of several hundred mTorr. The residual stress varies significantly with processing conditions, especially with deposition temperature and silane pressure. The general dependency of residual stress, texture and crystal orientation is shown in fig. 2.4 and will be discussed below.

A transition between the deposition of amorphous and polycrystalline silicon generally occurs around 580 °C for operating pressures at about 200 mTorr and shifts to higher values with increasing pressure up to 680 °C when the deposition takes place at atmospheric pressure [2.92]. Below this temperature little surface diffusion exists and amorphous silicon films are grown that exhibit a large compressive stress [2.93, 2.94]. LPCVD silicon films deposited near the crystallisation temperature transform from the amorphous to the crystalline state during deposition resulting in equi-axed grains. These films generally have crystal orientations with {110}, {111}, {112} and {113} components [2.88, 2.95-2.101]. The transformation from amorphous to crystalline silicon is coupled with a volume contraction inducing large tensile stresses [2.98-2.102]. It has been suggested that the tensile strain is associated with the presence of the {112} and {113} orientations. The magnitude of tension in the film has been found to decrease towards the
substrate [2.98, 2.100, 2.101]. Therefore polysilicon structures grown under these conditions will exhibit a positive stress gradient and curl upward after release.

Deposition temperatures above 630 °C produce films that nucleate at the substrate surface creating a strong \{110\} texture, with columnar grains extending through the film thickness and a region with smaller grains at the film/substrate interface [2.97-2.101]. At increasing temperatures up to 700 °C, \{100\} texture becomes dominant with grains that also exhibit a columnar structure [2.95-2.101]. Films deposited in the temperature range from 560 °C to 630 °C at pressures below 100 mTorr also have been found to show a preferred \{110\} orientation [2.103].

Because the amorphous to crystalline transition does not occur in these columnar films, the tensile component disappears and films are under compressive stress which decreases with increasing deposition temperature. For films deposited at 650 °C the magnitude of compression increases towards the substrate, again resulting in a positive stress gradient [2.98, 2.100, 2.101].

Between the temperature region where tensile films develop and the higher temperature region where compressive films are formed, a region has been found where stress, structure and texture vary with wafer position in the boat, even though temperature is constant [2.97-2.100]. This region, like the
amorphous to crystalline transition temperature, is pressure dependent and lies around a temperature of about 615 °C.

It should be avoided because of unpredictable stresses that range from tensile to compressive from the front to the end of the boat. The transition along the boat has been suggested to be due to silane depletion or increased hydrogen partial pressure [2.98, 2.99].

Impurities, like oxygen that is difficult to control in polysilicon, can impede surface migration during the deposition. As a result the structure will be less ordered than in high purity material. Small amounts of carbon can also change the grain size, the film structure, and the amount of stress in the film [2.92]. Polysilicon films have been shown to exhibit a surface layer of approximately 0.1 µm thick that is 50-100 MPa more compressive than adjacent sub-surface layers [2.101]. It is suggested that this compression arises from oxidation and oxygen entrapment that occurs when the wafers are exposed to atmosphere at process temperatures during unloading as well as subsequent annealing steps. Oxidation, which occurs preferentially at grain boundaries, is a recognised source of compression in polysilicon films.

2.2.2 Annealing:

Films deposited at lower temperatures in an amorphous form are unstable and crystallise readily during annealing at moderate temperatures and can crystallise during long heat treatments at temperatures as low as 600 °C [2.74, 2.89, 2.92, 2.104]. Initially compressive amorphous films develop equi-axed grain structures after annealing at high temperatures as a result of grain growth and primary recrystallization. Recrystallization refers to nucleation and growth of new grains within an already crystalline film and is usually driven by the reduction of bulk defect energies and results in new grains with low defect densities which grow at the expense of high defect density old grains. It has been found that the elementary process of polysilicon grain growth process is attributed to silicon self-diffusion across the grain boundaries. Films with orientations in the {110}, {111}, {112} and {113} direction have been found after annealing [2.101,2.104]. The accompanying volume contraction changes the direction of the stress from compressive to tensile. The tensile stress is initially higher for moderate annealing temperatures and decays to a low value with increasing annealing temperature [2.94].

Undoped films deposited in a polycrystalline form are more stable on annealing at moderate temperatures and high temperature annealing is needed before changes occur in grain orientations. The as deposited
compressive residual stress generally decreases to low values with increasing annealing temperature [2.103-2.105]. During annealing of initially \{110\} oriented films, an increasing \{331\} orientation develops with increasing temperature up to 1000°C. Above 1100 °C the \{110\} texture decreases significantly and the \{311\} and \{111\} orientation increase [2.104]. Rapid thermal annealing at high temperature also produces low residual stress polysilicon films [2.39, 2.106].

2.2.3 Doping:

Doping and subsequent thermal activation of polysilicon films also reduces the residual stress. Diffusion of phosphorus in polysilicon at elevated temperatures produces low stress polysilicon films with negligible stress gradient regardless of the original stress state or microstructure of the thin film [2.70, 2.101, 2.107]. Residual strain of implanted films is dependent on dopant types, implantation dose and subsequent annealing temperature. Phosphorus, arsenic and boron implantation followed by an annealing step has been found to result in stress reversal of initially tensile films resulting in compressive stresses [2.108, 2.109]. For initial compressive films boron and phosphorus implantation and annealing was found to result in low strain films [2.110, 2.111]. Wada and Nishimatsu showed that P-implanted polysilicon films undergo grain growth at temperatures as low as 800 °C [2.112]. The rate of grain growth was shown to increase in phosphorus doped films with concentrations above 4.10^{20} \text{cm}^{-3} attributed to the enhancement of the silicon self-diffusion in the presence of phosphorus. Grain growth was found to depend strongly on the phosphorous concentration that acts as a catalyst to recrystallization. Mei et al confirmed this result for phosphorus-doped films and demonstrated that the rate of grain growth is not a function of boron content in boron doped films [2.113]. It has been observed that stress gradients are related directly to phosphorus doping profile and can be minimised by a flat doping profile [2.72]. The properties of in situ phosphorus doped polysilicon films after annealing show an overall agreement with undoped polysilicon films where an increasing phosphorus content tends to decrease residual stress after annealing because of the increased silicon self diffusivity [2.114]. Also, in situ doping results in a uniform dopant distribution resulting in excellent control of strain gradients.

In this thesis solid source boron indiffusion has been used to produce boron doped polysilicon layers. The effect of solid source boron indiffusion on residual stress is shown in fig. 2.5 for films deposited initially in amorphous
Fig. 2.5 Residual stress of 2 µm thick polycrystalline thin films after solid source boron indiffusion for one hour in N₂ as a function of diffusion temperature for polycrystalline films deposited at 250 mTorr, a silane flow of 50 sccm and a temperature of 590 °C and 630 °C.

(590 °C) and polycrystalline state (630 °C) at a pressure of 250 mTorr and a silane flow of 50 sccm. After boron doping the stress of the initially amorphous films grown at 590 °C decays to small values. For the films grown at 630 °C a stress reversal from compressive to tensile occurs between 1000 and 1050 °C after boron indiffusion. In the strongly {110} oriented films, deposited at 630 °C, an increasing {111} orientation with increasing diffusion temperature is observed by x-ray diffraction. This suggests that this stress reversal is induced by recrystallisation effects and is related to the development of the {111} orientation and not a result of boron doping itself.

2.3 SACRIFICIAL LAYER ETCHING OF SILICON DIOXIDE IN HF SOLUTIONS

Another important process step in surface micromachining is the etching of the sacrificial layer to release the microstructure. For the polysilicon/silicon oxide combination hydrofluoric acid is used for sacrificial layer etching. This takes advantage of the isotropic nature and high selectivity of wet etching to
release overlying structural films. It is important to model and understand this sacrificial layer etching process in order to be able to predict etching times a priori instead of simply extrapolating initial etch rates and using trial and error to fully release microstructures because the etch process is destructive.

2.3.1 Chemical reaction

A heterogeneous reaction model for the etching of sacrificial silicon dioxide layers, as presented by D.J. Monk [2.115,2.116] is shown in fig. 2.6.

The etching can be divided into seven steps: 1 Mass transfer of the reactant by diffusion from the bulk to the external etch opening; 2 Diffusion of the reactant from the etch opening through the etch channels to the immediate vicinity of the internal surface; 3 Adsorption of the reactant; 4 The reaction at the surface; 5 Desorption of the products from the surface; 6 Diffusion of the products from the interior of the etch channels; 7 Subsequent mass transfer of the products from the etch opening to the bulk fluid.

![Diagram](image)

**Fig. 2.6** A model for the etching of sacrificial silicon dioxide layers in micromachining.

Hydrofluoric acid (HF) is a weak acid, therefore several hydrogen fluoride species are present in the solution. Silicon dioxide (SiO₂) is used in micromachining as an amorphous film which etches isotropically.

Etching is believed to occur in two elementary reactions. First the protons break up the siloxane bonds to form silanol species on the surface. Then the fluorinated species attack the silicon in the silanol nucleophilically to form SiF₄ (g) which is soluble in water and forms H₂SiF₆ (aq). Any fluorinated species in the hydrofluoric acid may supply the fluorine for this reaction.
Silicon dioxide etching is, therefore, much more complicated than the overall reaction that is usually reported:

\[
\text{SiO}_2 + 6 \text{HF} \rightarrow \text{H}_2\text{SiF}_6 + 2 \text{H}_2\text{O} \quad (2.13)
\]

### 2.3.2 Etch rate models

Sacrificial layer etching has been modelled by D.J. Monk by a combination of the diffusion of reactant to the etch surface and the chemical reaction at that surface using respectively Deal-Grove and non-first order models [2.117, 2.118], and by J. Liu by using a first and second order combined model [2.119]. In the Deal-Grove (D-G) model, the concentration in the channel is assumed to be linear. If the rate of chemical reaction is assumed to be linearly proportional to the etching front concentration the solution of the D-G model for the first order chemical reaction model is:

\[
\delta = -\frac{D}{k} + \sqrt{\left(\frac{D}{k}\right)^2 + \frac{D C_b \text{MW}_{\text{SiO}_2}}{3 \rho_{\text{SiO}_2}} t} \quad (2.14)
\]

where \( \delta \) is the etch length, \( D \) is the diffusion constant, \( k \) is the first-order reaction rate coefficient, \( C_b \) is the bulk HF concentration, \( \text{MW}_{\text{SiO}_2} \) is the molecular weight of SiO\(_2\), \( \rho_{\text{SiO}_2} \) is the density of SiO\(_2\) and \( t \) is the etch time. For specific concentrations this model and non-first-order models fit etching data well. A more universal model which predicts accurately the etching length vs. time over a wide range of HF concentrations has been obtained by using a combined first and second order model which is a specific case of the Freundlich adsorption isotherm reaction-rate model [2.119]. This results in the following first order differential equation for the etch length:

\[
\frac{d\delta}{dt} = \frac{\text{MW}_{\text{HF}}}{4 \rho_{\text{SiO}_2} k_2} \left(\frac{D}{\delta}\right)^2 \left[1 + b\left(\frac{\delta}{D}\right) - \varphi\right] \quad (2.15)
\]

with

\[
b = k_1 + 2 C_b k_2 \\
\varphi = \sqrt{1 + 2b \left(\frac{\delta}{D}\right) + k_1^2 \left(\frac{\delta}{D}\right)^2} \quad (2.16)
\]
where $MW_{HF}$ is the molecular weight of HF, $k_1$ is the linear reaction rate constant and $k_2$ is the quadratic reaction rate constant. This equation can be solved numerically. However instead of searching for solutions of the form $\delta=f(t)$, one can integrate this equation from $\delta=0$ at $t=0$ to $\delta$ at time $t$ to find:

$$t = D \left( \phi + 2k_1^2 \left( \frac{\delta}{D} \right) + b \left( k_1^2 \left( \frac{\delta}{D} \right)^2 - 1 \right) \right) \frac{1}{8aC_bk_1^2k_2(k_1+C_bk_2)} - \frac{D}{2ak_1^2} \log \frac{\phi + k_1 \phi}{2(k_1 + C_bk_2)} \quad (2.17)$$

where

$$\phi = k_1 + 2C_bk_2 + k_1^2 \left( \frac{\delta}{D} \right) \quad (2.18)$$

The coefficients $k_1$, $k_2$ and $D$ can be determined by least square fitting of experimental data of the etching process. This has been done for etching of PSG with a phosphorus concentration of 6 wt.% in HF solutions at room temperature. The bulk concentration $C_b$ in the HF solutions was varied from 3.6 to 49 wt.%. The following values have been found; $k_1=1.2 \times 10^{-4}$ cm/sec, $k_2=6.5 \times 10^{-2}$ cm$^4$/mole/sec and $D=1.6 \times 10^{-5}$ cm$^2$/sec. The theoretical etch length is shown in fig. 2.7 as a function of the etch time.

![Fig. 2.7 Modelled sacrificial layer etch length as a function of etch time at different HF concentrations.](image)
This model was shown to be in excellent agreement with experimental data. At low concentrations, the etching is dominated by a first order chemical reaction. The etching reactions become enhanced in higher concentration hydrofluoric acid solutions. This supports the hypothesis that the etching of silicon dioxide in hydrofluoric acid is a two-step reaction. An overall reaction that consists of two elementary reactions would have a reaction order of two. At low HF concentrations, less of the reactants, H+ for the first reaction and HF or HF2- for the second reaction are present. At high concentrations the etching is dominated by a second order chemical reaction.

Diffusion limitations can be observed after an etch length of approximately 200 µm. This is generally more than the total etch length needed to free micromechanical structures. So, in most cases the total etch time to release a structure can be determined by a linear extrapolation of the etch rate.

Increasing the phosphorus content in the PSG films, increasing the HF concentration, and the addition of HCl to HF solutions increases the initial etch rate of silicon dioxide sacrificial layers [2.120, 2.121]. Buffered HF and surfactant buffered HF did not show to enhance the etching of silicon dioxide sacrificial layers. Etch channel width, structural material choice and stress condition, and applied stress did also not show to affect the sacrificial layer etch rate appreciably. However it was found that thinner sacrificial layers are etched somewhat slower than thicker ones for thicknesses ranging from 0.24 µm to 1.7 µm. Other useful HF solutions are mixtures like HF and glycerol [2.122] or NH4F and acetic acid [2.123] which etch silicon dioxides without attacking aluminum layers.

Besides undoped oxides and PSG also BPSG as a sacrificial layer has been reported in order to be able to obtain reflow at low temperatures for planarization purposes [2.124].

### 2.3.3 Attack of polysilicon

Reported values of single crystal silicon etch rates in HF solutions are smaller than 1 Å/min [2.125, 2.126]. Etch selectivity between silicon oxides and silicon is therefore very large. However degradation of thin polysilicon in HF has been reported by Lober, Walker and Monk [2.127-2.129]. As-deposited, tensile, about 0.1 µm thick polysilicon films on PSG crack upon exposure to HF [2.114]. The HF most likely attacks the polysilicon at its grain boundaries, which may undergo sufficient oxidation upon removal from the reactor into the ambient environment. Thin unannealed and annealed polysilicon on LTO
and PSG show blistering at different attack rates. As-deposited compressive films (650 °C) do not appear to be affected by HF. Phosphorus has been shown to play a critical role in the susceptibility of polysilicon films to both stress-corrosion cracking and HF penetration. Residual stress and fracture strain were found to decrease, and the Young’s modulus was found to increase for undoped polysilicon films exposed for two hours to solutions with increasing HF concentration [2.128].

2.4 METHODS TO PREVENT STICION

After etching the sacrificial layers in an HF-solution, specific drying procedures are generally required to prevent free structures from sticking to the underlying substrate. Standard drying procedures such as spin drying or air drying produce capillary forces during drying that cause "pull-down" of the thin film structures [2.130-2.133] (see chapter 3). Once physical contact has been made, other forces like van der Waals forces, electrostatic forces, hydrogen bridging, solid bridging and chemical reactions come into play resulting in permanent attachment of the structures to the substrate [2.130-2.139]. Stiction can be prevented or reduced in several ways which can roughly be divided into two groups. First there are methods based upon the prevention of physical contact between the structures and the substrate during the drying process. This can be done by avoiding the attractive meniscus forces that bring the structures in contact with the substrate during the drying process, e.g. by freeze drying [2.140-2.143], critical point drying [2.144], dry etching techniques [2.145-2.149] or by rapid heating of rinse liquids [2.150]. Also temporary increasing the mechanical stiffness of the structures by breakaway supports [2.151], fusible supports [2.152] or stiff membranes that are patterned by dry etching afterwards has been applied [2.153]. Another interesting but barely investigated method for sacrificial layer etching of silicon dioxide without stiction is vapour HF etching [2.127, 2.154]. This technique is commonly used in IC technology for wafer cleaning. The boiling point of HF is 19.54 °C and the temperature of etch samples has to be elevated in order to prevent condensation of HF and water vapour. A general problem resulting from vapour HF etching is the formation of hydrated silica particles as a result of reverse gas phase reactions. Very high yields can be obtained and very large structures have been released without sticking problems [2.154].
All these methods have the disadvantage that stiction can still occur if the structures are brought into contact with the substrate afterward by external forces like large accelerations or static charges [2.143].

Methods that directly reduce the adhesional forces do not have this disadvantage. Adhesional forces can be reduced by minimising the surface energy, for instance by surface treatments [2.134] or by using hydrophobic surfaces [2.155], and by reduction of the real contact area by using stand-off bumps [2.107,2.156] or by increasing the surface roughness [2.157, 2.158]. These methods result in a permanent reduction of the adhesional forces.

Another method that has been presented is to release the structures after stiction has occurred by electromagnetic pulses that lift current carrying structures out of the substrate [2.159].

An elegant way to prevent the in process sticking problem is freeze drying. The freeze drying method has been developed as a drying method for biological specimens by Boyde and Wood in 1969 [2.160]. Guckel [2.140] first implemented this technique for the sacrificial layer etching of surface micromachined structures. In general, during freeze drying a final rinse liquid is frozen by lowering the temperature below its melting point after which the solid is sublimated by lowering the pressure. A P-T diagram showing the freeze drying as well as the super critical drying trajectory is given in fig. 2.8.

![P-T diagram](image)

*Fig. 2.8 Sketch of the P-T diagram for a pure substance showing the sublimation process and the triple point $T_p$, and the supercritical drying process and the critical point $CP$.*

A fast freeze drying technique that does not require vacuum equipment but can be performed at atmospheric pressures has been used to fabricate the structures that are presented in this thesis. Cyclohexane which freezes at about 6.7°C is used as the final rinse agent. Freezing and subsequent sublimation is
Methods to prevent stiction

readily accomplished by placing the substrate under a nitrogen flow on a regulated Peltier element at a temperature below the freezing point as shown in fig. 2.9. The total time for the freeze-sublimation process depends on the geometry of the sample and typically takes a few minutes.

![Schematic of the experimental freeze-drying set-up.](image)

*Fig. 2.9 Schematic of the experimental freeze-drying set-up.*

Details of the rinse-freeze-sublimation procedure are as follows: After sacrificial layer etching in an HF-solution a dilution rinse in DI-water is performed to remove the etchant. Isopropyl alcohol (IPA) is added to keep the hydrophobic wafer surface wet after removal from the solution. The wafers are placed in another beaker containing IPA and are then placed in cyclohexane, the final rinse agent. The IPA serves as an intermediate mixing agent. After rinsing the wafer in cyclohexane, they are placed upon a Peltier element, which has already been cooled down to -10 °C and is located under a glass hood. A nitrogen flow aids the sublimation process by removing cyclohexane vapours, and prevents condensation of atmospheric water. After the sublimation is completed, the Peltier element is raised to room temperature. No residues have been observed by optical microscopy and SEM. It was found that temperatures of about -10 °C and high nitrogen flows of about several tens of litres/min produce a very high yield for low strain microbridges and cantilever beams with aspect ratios as high as respectively 2000 and 1000 at gap spacings of 2 μm (see fig. 2.10).
2.5 BASIC SURFACE MICROMACHINING PROCESSES

A number of surface micromachining processes will be described that have been used for successful fabrication of elementary components. These components are generally part of more complicated structures and mechanisms. These basic processes (or modifications of them) can be used in conjunction with structural materials other than polysilicon or can be combined together with other designs and processes in order to fabricate more complex micromechanisms.

The fabrication of free standing beams, cantilevers and membranes is based upon the fabrication process that has already been given in the introduction of this chapter. The structures can be used for movements in directions that are perpendicular or lateral to the wafer surface, for example comb drive structures [2.107]. Rotating structures can be fabricated by a center-pin or flange-bearing process [2.11, 2.14]. These fabrication schemes are also useful for the fabrication of sliding structures by extension of the center bearing [2.11]. Structures that rotate out of the wafer plane can be fabricated by a hinge fabrication process.
An elegant way to create sealed cavities is the reactive sealing process that is based on surface micromachining techniques [2.7, 2.143, 2.161]. The fabrication steps of these processes will be briefly described below.

### 2.5.1 Center-pin process

The basic center-pin process uses two polysilicon and two silicon dioxide depositions as well as four lithography steps. Fig. 2.11 shows cross-sectional views of the design during the fabrication process. After a blanket deposition of silicon dioxide, the bushing moulds are time etched to the desired depth in the oxide. After a blanket deposition of polysilicon, the rotor is patterned. The bushings are formed automatically. After a second blanket deposition of silicon dioxide, the bearing anchor is opened. After a second blanket deposition of polysilicon, the center pin bearing is defined. At this point the sacrificial layers can be dissolved in hydrofluoric acid to release the rotor. The center pin process has the capability of producing self-aligned or non-self-aligned bearings.

### 2.5.2 Flange-bearing process

The basic flange bearing process uses two polysilicon and two silicon dioxide depositions as well as four lithography steps (see fig. 2.12). The overall process is similar to the center-pin process and uses only minor modifications. For the same number of film depositions, the flange bearing process provides more flexibility in design as compared with the center-pin process. After a blanket deposition of silicon dioxide, a blanket layer of polysilicon is deposited. The rotor or slider is patterned into the polysilicon layer. At this point the oxide at the inner (or outer or both) sides is under etched. After a second blanket deposition of silicon dioxide, the bearing anchor is opened. After a second deposition of polysilicon, the bearing is defined. Note that the flange in the bearing forms automatically and the bearing is self-aligned. After release the rotor rests on the bearing flange.

### 2.5.3 Hinge fabrication process

In fig. 2.13 a surface micromachining process is shown that allows structures to be rotated out of the plane of the wafer after fabrication. The structures rotate about hinges whose axes are in the plane of the wafer. The hinge fabrication process starts with the deposition and patterning of a first sacrificial layer. Then a structural polysilicon layer is deposited and patterned and a second sacrificial
Fig. 2.11 The center-pin process.
Fig. 2.12 The flange-bearing process.
layer is deposited and patterned. Anchors are etched and a second structural layer is deposited and patterned. An anneal step is used to remove polysilicon residual strain and dope the polysilicon layers when doped oxides are used as the sacrificial layers. Sacrificial layer etching in an HF solution frees the structures. Different types of hinges can be produced like a pin hinge, that hinges a piece of structural silicon to the substrate, and a scissor hinge, connecting two released pieces together.

2.5.4 Reactive sealing

The reactive sealing technique as developed by Guckel and Burns [2.7] can be used to create sealed cavities. These cavities may contain low pressures inside, depending on the sealing technique. Sealed cavities are made by defining thin regions of the sacrificial layer at the perimeter of the structure (see fig. 2.14). After removal of the sacrificial layer thin film deposition methods can be used to seal the narrow openings. The cavity pressure after sealing is a function of the processing conditions like pressure and temperature, and subsequent (out)diffusions and chemical reactions. The reactive sealing process starts with the deposition and patterning of a first sacrificial layer. Then a thin sacrificial layer is deposited and patterned to define anchors for the polysilicon cap structure. The next step is deposition of polysilicon that is subsequently patterned to form the cap structure. The sacrificial layers are etched which is followed by rinsing and drying procedures. Now the reactive sealing process will close the cavity. Typical processes that have been used for sealing are thermal oxidation, LPCVD of polysilicon and LPCVD of silicon nitride.

2.5.5 Special Techniques

Besides these basic fabrication processes some special techniques have been used. By adding an anisotropic etch step in the sacrificial layer highly compliant lateral suspensions using sidewall beams can be fabricated [2.162].

The attack of underlying oxide through defects in polysilicon grain boundaries in case of thin polysilicon films can be used to create permeable polysilicon etch-access windows [2.163] that are useful for fast removal of sacrificial layers and encapsulation purposes. With a combination of these two techniques it is possible to realise hollow polysilicon beams [2.164].

Deep anisotropic etching of the substrate can be used to create high aspect ratio milli-scale polysilicon structures by trench refill with the sacrificial and polysilicon layer [2.165]. Finally resistive cutting, welding and heating has been applied to attach, release and reshape polysilicon structures [2.166, 2.167].
Fig. 2.13 Hinge fabrication process.
Fig. 2.14 Reactive sealing process.
2.6 CONCLUSIONS

Surface micromachining has extended the possibilities and applications of micromachining techniques. Mechanical structures with more than one degree of freedom can be realised with this technique. Polysilicon surface micromachining is based upon the selective removal of sacrificial layers from a multilayer sandwich of patterned thin films consisting of sacrificial silicon oxide layers and structural polysilicon thin films. The residual stress in polysilicon thin films can be controlled and most of the mechanical properties of polysilicon films have been characterised. The silicon oxide sacrificial layer etch process has been modelled and methods to prevent stiction have been developed. A variety of fabrication processes has been presented and the possibility to integrate electronics has been demonstrated. The feasibility of surface micromachining has already been illustrated by a large number of devices and applications. A wide variety of structures have been realised with applications in mechanical test structures, resonators, sensors, actuators, mechanical interconnections and electronics. This makes polysilicon surface micromachining a well developed and versatile technology. Further development and improvement of materials, fabrication techniques and processes will certainly lead to improved or new devices and applications of micromachining techniques.

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Chapter Three

STICTION OF SURFACE MICROMACHINED STRUCTURES AFTER RINSING AND DRYING

In this chapter the mechanisms causing stiction of polysilicon structures fabricated by surface micromachining techniques are investigated. It is found that during drying from rinse liquids attractive dynamic capillary forces are responsible for bringing micromechanical structures into contact with the underlying substrate. Measured adhesion energies of sticking microbridges after complete drying indicate that van der Waals forces are responsible for the stiction of hydrophobic surfaces and that hydrogen bridging is an additional adhesion mechanism for hydrophilic surfaces.

3.0 INTRODUCTION

A notorious problem of surface micromachined structures using the sacrificial layer etching technique is the permanent attachment of slender structures to the underlying substrate after drying. This phenomenon is known as sticking or stiction [3.1-3.6]. It is assumed that during drying attractive capillary forces bring micromechanical structures into contact with the substrate. After complete drying the structures remain stuck to the substrate. It has been suggested that etch residues [3.1, 3.2], electrostatic forces [3.3] and condensation of water between the structures [3.4, 3.5] may be responsible for sticking. An understanding of the sticking phenomena is important for both static (beams) and dynamic (rotors) applications of surface micromachined structures with respect to technological realisation and tribological aspects.

In this chapter a model is presented which describes the pull-in behaviour of beams during drying from liquids, characterised by a pull-in length defined as the maximum length beyond which beams are forced into physical contact with the underlying substrate. Surface micromachined polysilicon microbridges have been fabricated and the pull-in length has been measured.

After drying, beams with a length greater than the so called *detachment length* remain stuck to the substrate. A relation between the detachment length of microbridges and the corresponding adhesion energy is given. The resulting bond strength has been determined from measured detachment lengths and is correlated to possible adhesion mechanisms.

### 3.1 PULL-IN BY CAPILLARY FORCES

During drying, the surface tension of the diminishing liquid induces an attractive capillary force which is inversely proportional to the gap spacing between the beam and the substrate, see fig. 3.1.

The restoring force consists of the bending and stretching stiffness of the beam. An increase in the deflection of the beam results in a decrease of the gap spacing and thus an increase in the capillary force. If the restoring force is not able to counterbalance the capillary force, the deflection does not reach an equilibrium position and increases until physical contact with the substrate is made. This behaviour is similar to the electrostatic attraction between two conducting surfaces as a result of an applied d.c. voltage as described in [3.7] and can be modelled in a similar way. In the following analysis, the process is assumed to be quasi-static.

The differential equation for the static deflection $w(x)$ of a prismatic wide beam subjected to an axial ($x$) load $N$ and a transverse ($z$) capillary pressure $q$ is given by:

$$
\frac{\hat{E} I}{\partial x^4} \frac{\partial^4 w(x)}{\partial x^4} \cdot N \frac{\partial^2 w(x)}{\partial x^2} = q = \gamma (\cos \theta_1 + \cos \theta_2) b \frac{d - w(x)}{d - w(x)}
$$

(3.1)
where \( \hat{E} = E_y/(1-\nu^2) \) is the plate modulus and \( E_y \) and \( \nu \) are the Young’s modulus and Poisson’s ratio of the beam material, respectively, \( I = bh^3/12 \) is the second moment of inertia, \( \gamma \) is the surface tension of the liquid, \( \theta_1 \) is the contact angle between the liquid and the beam, \( \theta_2 \) is the contact angle between the liquid and the substrate, \( d \) is the zero displacement gap spacing, \( b \) the beam width and \( h \) is the beam thickness.

An approximate solution can be obtained using the Rayleigh-Ritz method. A deflection profile is assumed which satisfies the geometric boundary conditions (zero displacement and rotation at the supports). The total potential energy \( \Pi \) can be expressed as:

\[
\Pi = U_{bending} + U_{stretching} + U_{surface\ tension} \quad (3.2)
\]

where \( U_{bending} \), \( U_{stretching} \) are the strain energy terms due to bending and stretching respectively and \( U_{surface\ tension} \) is the potential energy due to the surface tension or capillary force of the fluid, given by:

\[
U_{bending} = \frac{1}{2} \int_0^l \hat{E} I \left( \frac{d^2 w(x)}{dx^2} \right)^2 dx \quad (3.3)
\]

\[
U_{stretching} = \frac{1}{2} \int_0^l N \left( \frac{dw(x)}{dx} \right)^2 dx \quad (3.4)
\]

\[
U_{surface\ tension} = - \int_0^l \int_0^{\theta_2} \frac{\gamma (cos\theta_1 + cos\theta_2) b}{(d-w)} dw \ dx \quad (3.5)
\]

where \( l \) is the beam length. Trigonometric functions are very convenient in solving problems of clamped-clamped beams. An appropriate deflection profile is:

\[
w(x) = a \left( 1 - cos \frac{2\pi x}{l} \right) \quad (3.6)
\]

where \( a \) is a coefficient that has to be determined. For a system at equilibrium, the first derivative of the total energy with respect to \( a \) is equal to zero. For a stable equilibrium the total potential energy must be at a (local) minimum,
implying that the second derivative with respect to \( a \) has to be positive definite. The transition to an unstable system occurs when the second derivative equals zero. Solving these equations simultaneously yields the following expression for the pull-in length \( L_{PI} \) and the corresponding value for \( a \):

\[
L_{PI} = \pi^2 C \frac{d^2}{N + \left( \frac{8 EI}{C d^2} + N^2 \right)^{1/2}}^2
\]

where

\[
C = \frac{0.1549}{\gamma (\cos \theta_1 + \cos \theta_2) b}\]

and \( a = 0.2950 d \)

The effect of geometry and of the residual strain \( \varepsilon_0 = N/Ebh \) on the pull-in length is shown in fig. 2. For the condition that \( 8EI/Cd^2 >> N^2 \), the influence of the residual strain on the pull-in length can be neglected and expression (3.7) becomes:

\[
L_{PI} = 1.059 \left( \frac{8EI}{\gamma (\cos \theta_1 + \cos \theta_2) b} \right)^{1/4} d^{2.5} h^{3/4}
\]

Note that the bracketed expression on the right hand side of equation (3.8) gives the pull-in length in case the problem is simplified and the beam is modelled as a spring-mass system. The spring stiffness \( k \) for this special case is given by \( k = 384EI/l^3 \) and the active area is equal to the beam surface \( bl \).

![Fig. 3.2](image-url) The influence of the mechanical strain on the pull-in length \( L_{PI} \) for different beam thicknesses and gap spacings normalised on the pull-in length at zero strain.
3.2 DETACHMENT LENGTH

If the beam length exceeds the pull-in length physical contact with the substrate will be made during the drying of the liquid. Physical contact between the released microstructures and the underlying substrate induces an adhesive bond. If the strength of the adhesive bond exceeds the mechanical ‘pull-off force’, the structures remain permanently stuck to the substrate. The bonding strength of sticking beams can be determined from their detachment length as presented by Mastrangelo et al [3.5] for cantilever beams. It is evident, that the detachment length is always equal to or larger than the pull-in length, since sticking only occurs after physical contact has been made.

Using an array of beams with increasing length the detachment length is equal to the length where a transition occurs from sticking beams to free standing beams. The relation between the detachment length and the surface energy is derived as follows. The stored elastic energy in doubly clamped beams again consists of a bending and stretching term, see eqns. (3.3) and (3.4). The energy of adhesion is stored in the segment of the beam that is in physical contact with the substrate.

![Fig. 3.3 Sketch of a polysilicon beam adhering to the substrate.](image)

At equilibrium the total energy of the system is minimised with respect to \( s \) (see fig. 3.3) and an expression for the bondstrength \( \gamma_s \) can be found. In this case an appropriate deflection profile of the beam is:

\[
 w(x) = \frac{1}{2} d \left( 1 - \cos \frac{\pi x}{s} \right) \tag{3.9}
\]

where \( s \) is the region near the support of the beam that is not sticking. We find for the total energy of this system:

\[
 \Pi = \frac{\pi^4 \bar{E} I d^2}{8 s^3} + \frac{\pi^2 N d^2}{8 s} - \gamma_s (l-2s)b \tag{3.10}
\]
Equilibrium is found by setting \( dU_{\text{total}}/ds = 0 \), yielding the value for \( s \) at which the energy is minimal. The energy has a single equilibrium point if \( s < l/2 \) and no equilibrium point if \( s > l/2 \), i.e. the beam is sticking to the substrate in the former case and is free standing in the latter case. At the transition from sticking to free standing beams, \( s \) is equal to \( l/2 \) giving the following relation between the bondstrength \( \gamma_s \) and the detachment length \( L_{\text{det}} \):

\[
\gamma_s = \frac{\pi^4 \bar{E} h^3 d^2}{4 L_{\text{det}}^4} + \frac{\pi^2 \epsilon_0 \bar{E} h d^2}{4 L_{\text{det}}^2}
\]

The influence of the residual strain \( \epsilon_0 \), both compressive and tensile, on the detachment length is very small when \( \epsilon_0 L_{\text{det}}^2/\pi^2 h^2 << 1 \). In this case the second term on the right hand side of equation (3.11) can be neglected.

### 3.3 ADHESION OF SOLIDS

The bondstrength can be determined by measuring the detachment length of samples with different combinations of gap spacing and beam thickness. From the measured value of the bondstrength, as determined from the detachment length, information about the adhesion mechanisms that are present can be deduced.

The contents of this section is based upon the adhesion theories described in references [3.8-3.12]. Interactions between solids which bring about adhesion can be classified as follows [3.8]. Firstly there are long-range attractive interactions, including capillary forces, van der Waals forces and electrostatic forces, which establish the adhesive contact area. Secondly there are short range interactions caused by hydrogen bonds, chemical bonds and metallic bonds. Interfacial interactions based on diffusion and alloying are generally very slow at room temperature and will not be considered.

After HF etching, the hydrophobic silicon surface consists mainly of Si-H bonds which are chemically stable. The silicon surface shows a logarithmic growth rate up to roughly half a monolayer equivalent of silicon oxide during exposure to water or air. This growth rate increases after 3 hrs exposure to water and after nearly one week exposure to air [3.13, 3.14]. On further exposure the surface becomes hydrophilic. Because the surfaces are contaminated, strong ionic interactions between silicon surface atoms are screened out. Chemical reactions between surface hydroxyl groups only start to play a role at elevated temperatures [3.15].
For comparison, a system consisting of two solids, each one having the form of an infinitely extended half space, will be considered. The bonding energy per unit area due to the most relevant adhesion mechanisms is discussed below. Figure 3.4 shows the bond strength for each mechanism as a function of the separation.

Fig. 3.4 Bonding energy versus separation. The capillary energy was calculated for water using a surface tension of 73 mN/m and a contact angle of zero degrees with a reference distance $\delta_r$ of 2 $\mu$m. For the electrostatic energy a voltage difference of 0.5 V was used and for the van der Waals energy a Hamaker's constant of silicon in air equal to $27 \times 10^{-20}$ J has been used.

van der Waals forces:

Van der Waals forces result from the interaction between instantaneous dipole moment of atoms and can be described by the Lifshitz theory [3.8, 3.9]. The expression for the van der Waals energy is given by the following equation for separations in the non-retarded regime ($\delta < 20$ nm):

$$E_{vdW} = \frac{H}{12.\pi.\delta^2}$$  \hspace{1cm} (3.12)

where $H$ is the Hamaker's constant and $\delta$ is the separation distance.
The surface energy due to van der Waals forces between two solid surfaces in contact can be predicted by:

\[
\gamma_s = \frac{1}{2} E_{\text{vdW}} = \frac{H}{24.\pi D_0^2}
\]

where \(D_0\) is a cut-off distance of about 0.2 nm [3.9]. This yields a surface energy for silicon of 90 mJ/m\(^2\) using a Hamaker's constant of silicon in air of 27.10\(^{-20}\) J [3.8].

**Capillary forces:**

The energy as a result of capillary forces is given by:

\[
E_{\text{capillary}} = \int_{\delta}^{\delta_r} \frac{\gamma (\cos \theta_1 + \cos \theta_2)}{\delta} d\delta
\]

where \(\gamma\) is the surface tension of the liquid and \(\theta_1\) and \(\theta_2\) are the contact angles of the liquid with the two surfaces. As opposed to the other mechanisms, the reference distance \(\delta_r\) of the capillary energy cannot be chosen infinite but it is readily shown that, under conditions of meniscus forces, the capillary energy exceeds all other adhesive components (see fig. 3.4). When two hydrophilic surfaces are in contact or close to being in contact in a humid environment, water will capillary condense in the small gap between them until the meniscus radius reaches the Kelvin radius as determined by the Kelvin equation [3.9].

**Electrostatic forces:**

Electrostatic forces can arise from Coulomb attraction between charged objects or from the contact potential between two surfaces caused by differences in the local energy states and electron work functions. Its energy is given by:

\[
E_{\text{el}} = \frac{\varepsilon_{\text{air}} U^2}{2\delta}
\]

where \(U\) is the potential difference between the surfaces separated by an air gap with permittivity \(\varepsilon_{\text{air}}\). At small separations electrostatic pressures equal van der Waals pressures only for extremely large surface charge densities (> \(10^{13}\) elementary charges per cm\(^2\)) and large contact-potential differences (> 0.5 V) and are unlikely ever to exceed them [3.8], see fig. 3.4.
Hydrogen bridging:

Hydrophilic silicon surfaces contain a large number of hydroxyl groups. The hydroxyl groups can form strong hydrogen bonds as the separation between both surfaces becomes small. The density of hydrogen-bonding sites is given by the number of silanol groups and is found to be $5.0 \pm 0.5$ per nm$^2$ for fully hydrated silica surfaces [3.16]. The strength of most hydrogen-bonds lie between 10 and 40 kJ/mol [3.9], thus yielding adhesion energies between 0.1 and 0.3 J/m$^2$ for these surfaces.

Other aspects:

Besides the mentioned bonding mechanisms Alley et al [3.2] suggested that polysilicon structures stick together due to a silicon oxide residue, left behind after a chemical reaction of the silicon surface with water. In this case an intermediate material is introduced between the silicon substrate surface and the polysilicon beam surface. The adhesive strength of solid bridging is high [3.17] but difficult to estimate and is dependent on the type and amount of residue material. In addition, all attractive forces except the meniscus forces decrease rapidly with increasing distance. In practice, the surfaces exhibit a surface roughness which strongly influences the real contact area and nominal separation [3.18]. The surface roughness has a significant effect on the adhesion of elastic solids [3.19] and small increases of the surface roughness are sufficient to reduce the adhesion to very small values.

3.4 EXPERIMENTAL

3.4.1 Fabrication

Test structures consisting of an array of doubly clamped undoped polysilicon beams have been fabricated. First, a PECVD silicon oxide layer was grown on a n-type (100) silicon wafer ($5-10$ Ωcm) from a 2 percent SiH$_4$ in N$_2$ gas mixture and N$_2$O gas at a pressure of 650 mTorr and a temperature of 300 °C. This layer was patterned using BHF to form the beam anchors. Next, an undoped polysilicon layer was grown by LPCVD from SiH$_4$ at a pressure of 250 mTorr and a temperature of 590 °C. The wafers were then annealed at 1100 °C in N$_2$ for one hour to obtain a low residual stress (typically on the order of 10 ppm). The polysilicon layer was patterned by RIE using a SF$_6$ plasma to form the beams with lengths ranging from 10 to 200 µm in steps of 1 µm and widths of 20 and 50 µm. The polysilicon and silicon oxide thickness ranged from 0.5 to 2 µm in steps of 0.5 µm resulting in a 4X4 matrix of the gap spacing $d$ and the
beam thickness $h$. Samples were cleaned prior to sacrificial layer etching. Sacrificial layer etching was done in a 50 % HF solution for 25 minutes. After this, different treatments have been used. Special care was taken to keep the wafers wet and as clean as possible. Dilution rinsing for 15 minutes in DI water and drying, yields hydrophobic wafers dried from $\text{H}_2\text{O}$. Adding isopropylalcohol (IPA) to the water keeps the wafers wet after removal from the solution. Next they were rinsed in IPA for 15 minutes and withdrawn from the liquid. This procedure gives hydrophobic wafers dried from IPA. A set of hydrophilic wafers was also produced by adding nitric acid after DI dilution rinsing. At a concentration of 70 % nitric acid the solution was heated to 100 °C for one hour to render the silicon surface hydrophilic. After dilution rinsing for 15 minutes in DI water a set of hydrophilic wafers dried from $\text{H}_2\text{O}$ was obtained. The same procedure followed by an IPA rinse as described above gives hydrophilic wafers dried from IPA.

### 3.4.2 Measurements

**Pull-in length:**

To verify the model for the pull-in length, hydrophobic and hydrophilic wafers dried from IPA have been used. Wet samples were placed under a microscope connected to a video camera system. The drying process has been recorded and pull-in was made visible using interference contrast during slow replay. The shortest beam showing pull-in due to the surface tension represents the pull-in length which has been determined in this way. The results are shown in fig. 3.5.

The contact angle of a droplet of IPA on a hydrophobic or hydrophilic silicon surface was measured to be smaller than 5 degrees. The measured contact angles of water on hydrophobic silicon and water on hydrophilic silicon were $70\pm5$ and $5\pm5$ degrees, respectively.

**Detachment length:**

After drying, not all the beams that pulled-in during drying deflect upward again but some remain permanently attached to the substrate. The transition from sticking to free standing beams after drying, the detachment length, has also been determined by interference contrast microscopy. For the detachment length experiments samples have been dried from two commonly used rinsing liquids, namely $\text{H}_2\text{O}$ and IPA, to see whether or not the rinse liquid has an effect on the detachment length. Drying from these solutions was done for hydrophobic and hydrophilic samples. The results of these measurements are shown in fig. 3.6.
Fig. 3.5 Measured pull-in length as a function of \([d^2h^3]^{1/4}\) for polysilicon beams dried from IPA. Measurements were performed on two separate fabrication runs, each resulting in a 4x4 matrix of polysilicon beams with a gap spacing \(d\) and a beam thickness \(h\).

Fig. 3.6 Detachment length as a function of \([d^2h^3]^{1/4}\) for hydrophobic and hydrophilic samples dried from water (H\(_2\)O) and isopropanol (IPA).
Fig. 3.7 Photographs showing the transition from free standing to sticking beams which is clearly visible by interference patterns.
Interference contrast photographs of some test samples are shown in fig. 3.7. The residual strain of the polysilicon layer was measured by ring and buckled beam structures and was typically found to be on the order of 10 ppm. For the dimensions of the samples in this paper and the measured residual strain, the conditions to neglect the influence of the residual strain are generally satisfied (see previous section).

The detachment length showed a large spread, especially the hydrophobic samples dried from water. Often long beams are free standing while shorter beams are sticking. Disturbances because of local surface asperities, dust particles, etc. strongly reduce adhesion resulting in errors that lead to an increase of the detachment length. In our experiments, the detachment length was determined from the shortest sticking beams (i.e. giving the highest bonding strength). From the detachment length the surface energy can be calculated by eq. (3.11). The slope of the curves in fig. 3.7 is equal to \( \pi^4 E / 4 \gamma_s \)^{1/4}. The corresponding surface energies for hydrophobic and hydrophilic samples are listed in table 3.1.

<table>
<thead>
<tr>
<th>liquid</th>
<th>Hydrophobic [J/m²]</th>
<th>Hydrophilic [J/m²]</th>
</tr>
</thead>
<tbody>
<tr>
<td>H₂O</td>
<td>0.05 ± 0.05</td>
<td>0.26 ± 0.1</td>
</tr>
<tr>
<td>IPA</td>
<td>0.10 ± 0.05</td>
<td>0.24 ± 0.1</td>
</tr>
</tbody>
</table>

Table 3.1 Surface energy of the different samples calculated from formula (11). A value of 166 GPa was used for the plate modulus \( E \). Residual strain is on the order of 10 ppm and can be neglected in our experiments.

The errors in the surface energies are worst case values and are mainly determined by the error in the slopes of the curve fits.

Because the roughness is important with respect to the adhesion properties, as discussed before, the surface roughness was measured by Atomic Force Microscopy. The root mean square values of the surface roughness of the bottom side of the microbridges varied from about 1 to 3 nm, depending on the thickness of the silicon oxide and polysilicon layer. The surface roughness of the silicon substrate after sacrificial layer etching was 0.2 nm. Measurement examples are shown in fig. 3.8. These values are very small and imply that the measured surface energies are only weakly influenced by the surface roughness of the samples. Some samples were inspected by SEM to see if etch residues were left behind after the etching and rinsing procedures, but only smooth and
Fig. 3.8 AFM scanning images of a silicon wafer and the bottom side of a polysilicon beam. Top figure: silicon wafer surface with RMS surface roughness of 0.2 nm. Bottom figure: bottom surface of polysilicon beam with RMS surface roughness of 3.1 nm, the beam thickness is 1.67 μm and silicon dioxide thickness before release was 1.69 μm.
clean surfaces were observed. Fig. 3.9 shows a SEM photograph of the disappearing gap spacing between a sticking beam and the substrate.

![SEM Photograph of the disappearing gap spacing between a sticking beam and the substrate.](image)

**Fig. 3.9** SEM Photograph of the disappearing gap spacing between a sticking beam and the substrate.

### 3.5 DISCUSSION

From fig. 3.5 it can be seen that the measured pull-in length shows a linear fit with the parameter \( \left( \frac{d^2 h^3}{1} \right)^{1/4} \) as predicted by the theory. However, the slope of the curve is 41 \( \mu \text{m}^{-1/4} \), which is lower than the theoretical value of 79 \( \mu \text{m}^{-1/4} \), calculated from eq. (3.8) by using a plate modulus \( \hat{E} \) of 166 GPa, a surface tension \( \gamma \) for IPA of 21.7 mN/m [3.20] and zero degrees contact angles \( \theta_1 \) and \( \theta_2 \). The influence of the step-up boundary of the fabricated microbeams or other assumptions for the deflection profile have been considered and only lead to small variations in the pull-in length and are thus unlikely to cause the discrepancy. The difference might be due to the non-equilibrium situation that exists during drying. The value used for the surface tension is only valid for IPA at thermodynamical equilibrium. During drying however, the liquid is not in equilibrium. Dynamic effects such as spreading may lead to an increase of the capillary forces. In case of spreading, the difference can be enormous, and is about 60 mN/m for organic liquids on oxides and 300 mN/m for water on metallic oxides [3.21]. In our situation the increase would be 270 mN/m as determined from the pull-in measurements.
The measured surface energies of sticking beams (table 3.1) can be compared with theoretically calculated values to determine which adhesion mechanisms are present. After the rinsing and drying procedures, as described above, no residues have been observed by microscopic- or SEM inspection indicating that adhesion by solid bridging is not present. From table 3.1 one can see that the rinsing liquids which have been used hardly effect the adhesion energy and that it is mainly determined by the surface properties (i.e. hydrophobic or hydrophilic) of the materials which have been used. Although meniscus forces play an important role in the initiation of the physical contact between the beam and the substrate, the measured bonding energies are too small to be associated with meniscus forces from a thin liquid film, remaining after drying or initiated by microcapillary effects. Chemical bonding is also precluded for the same reason. As already discussed, electrostatic forces are unlikely to exceed van der Waals forces at small separations and are disregarded as a possible cause for stiction. This leaves van der Waals forces and hydrogen bonding as the possible causes for stiction.

The measured surface energies indicate that van der Waals forces are responsible for the hydrophobic bonded surfaces and hydrogen bridging is an additional bond mechanism for the hydrophilic bonded surfaces. The values for the surface energy show a good correlation with hydrophobic and hydrophilic wafer bonding experiments [3.15]. In this case energies are found of 0.04 to 0.1 J/m² for hydrophobic wafers and 0.14 to 0.2 J/m² for hydrophilic wafers bonded at room temperature.

In summary, stiction is accomplished in a two step process. First a temporary physical contact between surface micromachined structures and the substrate is induced by dynamic capillary forces during the drying of the rinse liquid. After this, permanent attachment is caused by van der Waals forces and hydrogen bridges which are induced by the small separation distance during this contact.

The maximum free standing length, the detachment length, is a function of the adhesion energy of the beam to the substrate surface and the mechanical stiffness of the structures. The maximum free standing length can easily be increased by increasing the beam thickness and the sacrificial layer thickness and using larger tensile residual strains.

Stiction can be prevented or reduced in several ways which has already been discussed in the previous chapter. The methods are based upon the prevention of physical contact between the structures and the substrate during the fabrication process and on the reduction of the adhesional forces.
3.6 CONCLUSIONS

It has been shown that during the drying process, attractive capillary forces are responsible for bringing micromechanical structures into contact with the substrate. A model for the surface tension forces during drying has been developed and applied to doubly clamped beams. For these beams a pull-in length exists beyond which the beams are forced into contact with the underlying substrate. Measurements of the pull-in length are in qualitative agreement with the theory. The measured values of the pull-in length are somewhat lower than predicted by the theory. This is probably due to dynamical effects during the drying process.

After complete drying, structures remain stuck to the substrate. The measured adhesion energy of the resultant bond has been determined using the detachment length of the beams after complete drying. The resulting bonding strength's were independent of the used rinsing liquids, water and isopropanol. The adhesion energy ranged from 0.05 to 0.10 J/m² for hydrophobic silicon surfaces and 0.24 to 0.26 J/m² for hydrophilic silicon surfaces. These energies indicate that van der Waals forces are responsible for the hydrophobic surfaces and hydrogen bridging is the dominant bond mechanism in case of the hydrophilic surfaces. These results are in agreement with wafer bonding experiments.

REFERENCES

Chapter Four

ANISOTROPIC REACTIVE ION ETCHING OF SILICON USING SF6/ O2/ CHF3 GAS MIXTURES

Reactive ion etching of silicon, using SF6/O2/CHF3, plasmas in an RF parallel plate system has been studied. Etching behaviour was found to be a function of loading, the cathode material and the mask material. Good results with respect to reproducability and uniformity have been obtained by using silicon as the cathode material and silicon dioxide as the masking material for mask designs where most of the surface is etched. Etch rate, selectivity, anisotropy and self-bias voltage have been examined as a function of SF6 flow, O2 flow, CHF3 flow, pressure and the RF power, using response surface methodology, in order to optimise anisotropic etching conditions. The effect of the variables on the measured responses is discussed. The anisotropic etch mechanism is based on ion-enhanced inhibitor etching. SF6 provides the reactive neutral etching species, O2 supplies the inhibitor film forming species and SF6 and CHF3 generate ion species that suppress the formation of the inhibitor film at horizontal surfaces. Anisotropic etching of high aspect ratio structures with smooth etch surfaces has been achieved. The technique is applied to the fabrication of three dimensional micromechanical structures.

4.0 INTRODUCTION

Dry anisotropic etching of silicon is an important technology for the fabrication of micromechanical devices. Dry etch characteristics are not constrained by crystal planes as in the case of wet anisotropic etching for example by KOH solutions. This has the advantage that not only single crystalline-silicon but also poly-crystalline silicon and amorphous-silicon can be used for the fabrication of three-dimensional micromechanical structures. Dry etching techniques can be utilized to etch arbitrarily shaped mask designs. This is especially usefull for the fabrication of electrostatically driven microactuators that often exhibit complex shapes and require small gap sizes with high aspect ratio's.

Dry anisotropic etching of silicon has been achieved with Cl and Br containing gas mixtures such as SF6-CBrF3 [4.1], SF6-C2Cl3F3 [4.2] and SF6-C2ClF5 [4.3, 4.4]. Etching of silicon with SF6 at very low temperatures [4.5] or at

very low pressures [4.6] can also be used to produce anisotropic etch profiles. Furthermore SF$_6$/O$_2$ gas mixtures [4.7-4.14] were found to anisotropically etch silicon. The last technique has the advantage of being a fluorine based etch chemistry that can be used in common reactive ion etch systems. Generally, however, rough etch surfaces are produced that make the process less useful.

In this study the objective was to optimise the anisotropic etching of silicon by SF$_6$-O$_2$ plasmas and produce smooth etch surfaces by response surface methodology, using the etch system described below. This was accomplished by the addition of CHF$_3$ to the SF$_6$/O$_2$ plasma. The process is applied in the fabrication of micromechanical structures with high aspect ratio's.

4.1 EXPERIMENTAL

4.1.1 Equipment

Etching experiments were performed in an Electrotech, Plasmafab 310-340 twin deposition/etch system. The RIE part consists of a parallel-plate system with a RF generator operating at 13.56 MHz and an automatic RF matching network. The pumping system consists of a turbo-pump in series with a rotary-pump. The spacing between the electrodes is fixed at 8.0 cm. The chamber walls consist of aluminium and the powered electrode has a diameter of 19 cm and is covered with a titanium dioxide coated, aluminium (styros) plate. The temperature of the lower electrode can be controlled from 10 to 60 ºC, by backside heating or cooling, using a temperature controlled oil-bath system. The flow rates of the gases were maintained with standard mass flow controllers. The pressure during processing is monitored with a capacitive manometer and controlled automatically with a throttle valve.

4.1.2 Initial experiments

Loading:

Initial experiments showed that the etch behaviour of silicon in SF$_6$/O$_2$ gas mixtures is loading dependent with regard to macroloading (i.e. the amount of wafers) and with regard to microloading (i.e. with regard to the pattern density on the wafers). This effect has been investigated by several researchers [15-18]. For our purpose we are interested in positive mask pattern designs that are equally distributed over the wafer, and where most of the wafer surface will be etched. The loading was kept constant at one 3" wafer per run in our experiments.
**Cathode material:**

The etching behaviour is also depending on the cathode material [4.15]. Three cathode materials; styros, graphite and silicon have been examined. The graphite and silicon electrode cover the styros electrode that is normally present in the reactor. Styros is a non-consumable material in contrast to graphite and silicon that are etched in fluorine based plasmas. With the styros cathode, the etch rate at the wafer edge was twice as high compared to the etch rate at the center of the wafer. This non-uniformity is a result of gradients in local reactant concentrations and is controlled by the relative etch rates of the wafer vs. the cathode material [4.15]. The uniformity in case of the graphite cathode was measured to be about 20 percent. The use of the consumable silicon cathode resulted in a uniformity of a few percent across the wafer. In this case the silicon etching rate as well as the selectivity to silicon dioxide were found to be lower than in case of the other cathode materials. Because of the good uniformity, the silicon cathode was used for further experiments.

**Mask material:**

During initial experiments, it was observed that etching is also affected by the mask material. It has been suggested that the mask material may act as a catalyst for SF$_6$ to generate fluorine [4.19, 4.20], thereby affecting the process conditions. For this reason a non catalytic mask material is preferred. In our experiments silicon dioxide has been used as the etch mask material.

**CHF$_3$ addition:**

Anisotropic etching with SF$_6$/O$_2$ plasmas normally produces rough etch surfaces. The silicon etch surfaces sometimes showed a black color as a result of large surface roughness (black silicon). This phenomenon has also been reported by other investigators [4.7, 4.9, 4.11]. It was found that the addition of CHF$_3$ to the SF$_6$/O$_2$ plasma produced smooth etch surfaces. Therefore this gas mixture has been used in our experiments.

4.1.3 **Sample preparation**

In all of the experiments, the samples prepared were 3" diam., <100> oriented, p-type (5-10 Ωcm) silicon wafers. A 3000 Å thick SiO$_2$ layer was grown by wet oxidation at 1000 °C. This layer was patterned by lithography and RIE using CHF$_3$ gas with a flow of 10 sccm, a process pressure of 20 mTorr, and an RF power of 50 Watt. The temperature of the oil bath, that controls the
temperature of the lower electrode, was set at 25 °C. After the etching of the 
SiO₂ layer the resist was stripped by oxygen ashing. Before the etching 
experiments the etching chamber was manually cleaned with ethanol and by 
an oxygen plasma cleaning step. Before each experiment the wafer was given 
an HF-dip (HF:H₂O = 1:100) for one minute to remove the native oxide layer. 
The gas flows and process pressure were allowed to stabilize for five minutes 
before etching was performed.

4.1.4 Experimental design

The characteristics of the etch process are explored using a statistical 
experimental design [4.21, 4.22] and are modelled empirically by response 
surface methodology. In general a design should be chosen that will support a 
full quadratic model, which includes linear terms, two factor interactions, and 
quadratic terms for curvature. The general form of the full quadratic model is:

\[ Y = b_0 + \sum_{i=1}^{f} b_i X_i + \sum_{i=1}^{f} b_{ii} X_i^2 + \sum_{i=1}^{f} \sum_{j=2}^{f} b_{ij} X_i X_j \]  

(4.1)

where the \( X_i \) represent the independent input variables (i.e., process 
parameters), the \( b_i \) are the coefficients for the linear terms, the \( b_{ii} \) are the 
coefficients for the quadratic terms, and the \( b_{ij} \) are the coefficients for the cross 
terms. A more elucidated discussion of surface response methodology is given 
in Appendix A.

For the optimisation of the RIE process the SF₆ flow, the O₂ flow, the CHF₃ 
flow, the pressure and the RF power have been chosen as the process variables. 
These process variables have been varied within the limits of our etching 
system. The temperature of the oil-bath that controls the cathode temperature 
was set at 25 °C. No special clamping or temperature control of the wafer was 
used. The loading was constant at one 3” wafer. A central composite rotatable 
second order design was used for the experiment. The monitored responses are 
the self-bias voltage, the silicon etch rate, the uniformity, the etch surface 
roughness, the selectivity and the anisotropy. The normalized experimental 
design information is given in table 4.1 whereas table 4.2 shows the actual 
parameter settings. The order of performing the experimental trials was 
randomized to minimize the effect of any systematic error.
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</table>

*Table 4.1* Nonrandomized experimental design for five normalized parameters.
Table 4.2 Variable settings used for the experimental design.

### 4.1.5 Data acquisition

The thickness of the SiO₂ layer was measured by ellipsometry before and after the etching process to determine the etch rate of this layer. A correction was made for the HF-dip which was measured to remove about 50 Å of the oxide layer before the experiments. After etching, the etch depth of the silicon was measured with a Dektak surface profiler. The result was corrected for the remaining thickness of the oxide layer. The etch depth was measured at the center and at four points at 1 cm from the edge of the wafer to obtain an indication of the uniformity of the etch process. The selectivity was found from the ratio of the silicon etch depth and the difference in the SiO₂ thickness before and after the experiment. To determine the anisotropy, the samples were broken and their cross-section was examined by SEM. Several different etch profiles have been obtained. Depending on the process conditions not only mask undercut, but outward sloped profiles have also been observed. The anisotropy is defined by:

\[
A = 1 - \frac{V}{H}
\]

(4.2)

where \( H \) is the etch depth and \( V \) is the maximal undercut of the mask or, in case of outward sloped profiles, the lateral extension of the sidewall as shown in fig. 4.1. This definition of the anisotropy does not give information whether the anisotropy is due to mask undercut or to outward sloped profiles. However the expression in equation (4.2) is generally used and therefore preferred for a comparison with results from other workers. A value of 1 represents a perfect vertical sidewall with no mask undercut.
The roughness of the etch surface was only regarded qualitatively by means of the visual reflectivity of the silicon surface after etching, and by examining the etch surface of the cross-sectional SEM photographs.

In our experiments post structures and lines and spacings of 1, 2 and 5 µm have been used to determine the etch rate and anisotropy for etch depth of about 2 µm. At these dimensions no feature size dependent etch effects have been noticed. However effects like RIE lag and charging, may affect etch results at larger etch depth's [4.17].

4.2 RESULTS AND DISCUSSION

The responses of the factorial design are shown in table 4.3. The independent input variables in formula (4.1) have been normalized as follows:

\[ X_i = \frac{\phi_i - \bar{\phi}_i}{\sigma_i}, \]

Where \( X_i \) is the normalized value for the variable setting \( \phi_i \), \( \bar{\phi}_i \) is the centerpoint value of that variable and \( \sigma_i \) is the step of the variable that has been used in the factorial design (see table 4.2).

The responses of the self-bias voltage, the silicon etch rate, the selectivity, and the anisotropy are fitted by the quadratic model (formula 4.1). The regression coefficients \( b_i, b_{ij}, b_{ij} \) and the fit coefficient \( R^2 \) for the quadratic model are shown in table 4.4. Graphical representation of the responses and SEM photographs of sample cross sections are shown in fig. 4.2-4.8. In the graphs all parameters except for the specific parameters being varied are fixed at the center point of the design.
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<td>9.4</td>
<td>0.960</td>
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*Table 4.3* Responses of the factorial design. *In case of outward sloped etch profiles the anisotropy is marked by an asterisk.*
<table>
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<th>Coef.</th>
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<th>Anisotropy</th>
<th>DCB</th>
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**Table 4.4** Fitted coefficients of the quadratic model for the responses. The $s$ value at the bottom of each column is an estimate of the standard deviation for that response. The $R^2$ index indicates the agreement between model and data. A value of 1.00 denotes an ideal fit.
4.2.1 Etch Rate

In fig. 2 the effect of the SF$_6$ and O$_2$ flow, and the effect of the pressure and RF power, on the etch rate of silicon are shown. The silicon etch rate shows a quadratic dependency on the SF$_6$ flow, the O$_2$ flow, the process pressure and the RF power. The rate is independent of the CHF$_3$ flow. The accuracy of the model is indicated by the squared multiple $R^2$ which is 0.99 and represents a good fit.

To understand the etch behaviour, some background of the etch mechanism is needed. It is well known that plasma etching of silicon with fluorinated compounds is primarily due to free fluorine [4.23]. The dissociation of SF$_6$ is assumed to involve electron impact dissociation reactions of the form:

$$e + SF_x g \rightarrow SF_x - 1 g + F g + e (x = 3-6)$$  (4.4)

The etching of silicon occurs by a reaction with F atoms. The overall stochiometry of etching by atomic F is [4.24]:

$$Si + 4 F \rightarrow SiF_4$$  (4.5)

The etch rate $R_{Si}$ can be written as:

$$R_{Si} = k \beta_{Si} n_F$$  (4.6)

where $k$ is a constant, $\beta_{Si}$ is the reaction probability of fluorine at the surface and $n_F$ is the density of fluorine atoms. Higher SF$_6$ flows will increase the fluorine concentration in the gas mixture, resulting in higher etch rates. At high flows, the fluorine concentration can decrease due to higher convective losses (see section on total flow rate) and the etch rate will start to decrease.

The influence of oxygen addition to SF$_6$ plasmas has been investigated by several authors [4.11-4.14, 4.25-4.27]. With an SF$_6$-O$_2$ mixture in the absence of silicon, the final reaction products are F$_2$, SOF$_4$, and SO$_2$F$_2$. When Si is etched, SiF$_4$ is the only stable silicon-containing etch product and SOF$_2$ is formed in oxygen-poor mixtures. It was found that oxygen additions drastically increase the conversion of the feed gas, evidently by reacting with fluorosulfur radicals and thus preventing their recombination with fluorine to reform SF$_6$ [4.12].

$$SF_x + F \rightarrow SF_x - 1 (x = 5-1)$$  (4.7)
Fig. 4.2 Surface plots showing the silicon etch rate as a function of the SF$_6$ and O$_2$ flow, and as a function of the process pressure and the RF power. All parameters except for the specific parameters being varied are fixed at the center point of the design.
This leads to a net increase of F atoms. As the feed is made more O₂ rich, SO₂F₂ increases with respect to SOF₄ while the F-atom concentration first increases, reaches a maximum, and then decreases. These results closely parallel analogous trends in the carbon-based CF₄-O₂ system [4.24].

When silicon is exposed to the discharge, a significant change in the product composition is observed. SOF₂ is formed in oxygen-poor mixtures, SiF₄ appears, and the concentration of molecular fluorine is depressed [4.13]. As the quantity of oxygen is increased, the etch rate goes through a maximum and subsequently decreases.

In the presence of oxygen, oxygen species compete with F for active surface sites. This has been explained by a quantitative model which takes oxygen adsorption into account to relate the etch rate to the fluorine concentration for silicon and SiO₂ etching in CF₄-O₂ plasmas [4.24]. The decrease of the reaction probability was found to be:

\[ \beta' = \frac{\beta}{1 + C n_O / n_F} \]  

(4.8)

where \( \beta' \) is the reaction probability in the presence of oxygen, \( n_O \) is the density of oxygen atoms and C is a constant. Maneschijn [4.28] showed that this model also represents the data for SF₆-O₂ gas mixtures quite well.

Thus at higher O₂ flows the etch rate will be depressed as a result of the competition of oxygen atoms with fluorine atoms for chemisorption on the silicon surface.

As was pointed out by Tzeng [4.11], increasing the RF power leads to a significant increase of the atomic fluorine concentration while the oxygen concentration increases only slightly. This explains the increased silicon etching rate with increasing RF power.

At low RF power the etch rate decreases with increasing pressure, while at high RF power the etch rate first increases, reaches a maximum and finally decreases with increasing pressure. These results agree with the experiments of Kopalidis [4.12]. By measuring the effect of pressure on the discharge composition of SF₆-O₂ plasmas they found that the main effect of increasing pressure is to enhance the oxyfluoride production rates, thus reducing the recombination reactions. In accordance with our results, the self-bias was found to decrease strongly with increasing pressure. Assuming that ion bombardment contributes significantly to the overall etching rate (the synergistic effect of ion bombardment and chemical etching), the decrease in
Fig. 4.3 Surface plots showing the selectivity as a function of the SF$_6$ and the O$_2$ flow, and as a function of the process pressure and the RF power.
the etch rate with increasing pressure can be explained by the decrease in ion bombardment at higher pressures.

At high RF power the initial increase of the etch rate with increasing pressure may be the result of an increasing F concentration and ion density that leads to a larger ion flux towards the substrate.

4.2.2 Selectivity

The selectivity linearly increases with the SF$_6$ flow and linearly decreases with the O$_2$ flow and the RF power, and only slightly decreases with increasing CHF$_3$ flow. It shows a quadratic dependancy on the pressure. Surface plots showing the selectivity as a function of the SF$_6$ and the O$_2$ flow, and as a function of the process pressure and the RF power are shown in fig. 4.3. The $R^2$ index indicates a good model fit.

The selectivity is the quotient of the etch rates of silicon and silicon dioxide. The effect of the process parameters on the silicon etch rate have been discussed. The etch rate of SiO$_2$ is to a large extent due to direct etching by reactive ions. It was reported that the etch rate of SiO$_2$ in a CHF$_3$-O$_2$ plasma was found to follow the ion density and to be fairly independant of the plasma chemistry under most experimental conditions [4.29]. Ion bombardment increases with increasing RF power and with decreasing pressure. This results in a decrease of the SiO$_2$ etch rate with increasing RF power and an increase of the SiO$_2$ etch rate with increasing pressure. Assuming that the SiO$_2$ etch rate in this process is also fairly independent of the plasma chemistry, this leads to an increase of the selectivity with increasing SF$_6$ flow and increasing pressure (more chemical etching) and a decrease of the selectivity with increasing RF power and O$_2$ flow and CHF$_3$ flow (more physical etching).

4.2.3 Anisotropy

Responses of the anisotropy are shown in fig. 4.4. The anisotropy shows a linear increase with the O$_2$ flow. It is quadratically dependent on the process pressure, the RF power and the SF$_6$ flow. Furthermore the anisotropy shows a weak quadratic dependency on the CHF$_3$ flow. Again the fit between the data and the model is good.

Fig. 4.5 shows a selection of the sidewall profiles, which have been obtained in our experiments. These photographs clearly demonstrate that it is difficult to obtain a straightforward definition of the anisotropy. Depending on process conditions profiles may vary from isotropic to outward sloped profiles.
\textbf{Fig. 4.4} Surface plots showing the anisotropy as a function of the SF$_6$ and O$_2$ flow, and as a function of the process pressure and the RF power.
Fig. 4.5 Sidewall profiles obtained under different process conditions. The parameter settings are shown in Table 1 and 2 for: a) fact. nr. 24, b) fact. nr. 27, c) fact. nr. 13 and d) fact. nr. 6.
Note that at high pressures and high O$_2$ flows the value of the anisotropy, as
defined before, may be lower because of outward sloped etch profiles.

Auger Electron Spectroscopy (AES) data, taken within 15 minutes after the
etch process, from the sidewall and bottom etch surface of a center point run is
shown in fig. 4.6. Approximately equal amounts of sulfur and carbon were
detected on both surfaces. Sulfur is most likely a product of the SF$_6$ gas and
carbon may result from the CHF$_3$ gas or from organic contamination. The
presence of fluorine was not observed. However, this may be due to electron
stimulated desorption of fluorine atoms as a result of the measurement.
Oxygen is present on both surfaces. The amount of oxygen on the sidewall
surface is much higher than the amount of oxygen on the bottom surface. This
clearly indicates a sidewall passivation effect by oxygen species, as proposed by
Zhang [4.14]. On the horizontal silicon surfaces, adsorption of oxygen residue
can be readily attacked through the physical bombardment of active ion species
like SF$_x^+$ and CF$_x^+$. While on the the vertical Si surfaces (sidewalls), the
removal of sidewall material is not significant because of the less directional
kinetic energy in this orientation. These results are in agreement with other
investigations [4.7, 4.30, 4.31].

Surface and interfacial residue films formed on polycrystalline silicon and
silicon dioxide by reactive ion etching with SF$_6$-10% O$_2$ at 100 mTorr have been
investigated using x-ray photoemission spectroscopy [4.30]. Composition and
chemistry at the surface were found to be variations of SiO$_x$F$_y$. The thickness
varied from 7 to 13 Å with a decreasing etch rate. Silicon surfaces etched in
CF$_4$-O$_2$ plasma have been characterised with the use of in situ x-ray
photoemission spectroscopy [4.31]. It was found that a SiO$_x$F$_y$ layer on
elemental silicon was formed under all conditions. For oxygen percentages
greater than 5% in the feed gas, the oxygen content of the film and the film
thickness increased, whereas the fluorine content of the film decreased.

These observations may explain the increase of the anisotropy with
increasing O$_2$ flow and the decrease of the anisotropy with increasing SF$_6$ flow
by affecting the formation of the sidewall passivation layer. At high O$_2$ flows
and low SF$_6$ flows, profiles are positively tapered (outward sloped) and the
anisotropy shows an initial increase with the SF$_6$ flow. In this region, the
formation of the passivation layer is very pronounced and outward sloped etch
profiles are obtained that may be the result of an orientation dependant etch
rate. Increasing the SF$_6$ flow at high O$_2$ flows, increases the F concentration,
thereby reducing the formation of the passivation layer on outward sloped
profiles which leads to a more vertical etch profile that has a higher anisotropy.
Fig. 4.6 AES data of the sidewall and bottom etch surface after etching of a center point run.
It has been found that increasing the pressure increases the atomic fluorine concentration and decreases the ion bombardment [4.12]. Both effects lead to a reduction of the anisotropy in agreement with our results at high pressures. At low pressures, however, the anisotropy increases with increasing pressure until it reaches a maximum. This may be explained by a low silicon surface coverage by oxygen atoms. The surface coverage is a function of the pressure and increases with increasing pressure. Apparently the oxygen concentration at low pressures is too small to result in a surface coverage that is high enough to form a stable passivation layer.

The RF power gives an initial increase, reaches a maximum and then results in a decrease of the anisotropy with increasing RF power. Increasing the RF power increases the F concentration as well as the ion bombardment on the horizontal surface. At low RF power it seems that the effect of the increase in ion bombardment is stronger than the effect of the increasing F concentration, giving a net increase in the anisotropy. At higher RF power the increase in the F concentration probably becomes dominant and reduces the formation of the passivation layer. This leads to a decrease in the anisotropy with increasing RF power.

Adding CHF$_3$ to the SF$_6$-O$_2$ plasma may influence the O to F concentration. In CHF$_3$-O$_2$ plasmas the major reaction products are CF$_x$ species, CO, CO$_2$ and COF$_2$ [4.31]. These reactions will lower the O atom concentration. Since the selectivity (i.e the SiO$_2$ etch rate) is only slightly affected by the CHF$_3$ addition it is suggested that the formation of the passivation layer is suppressed by an additional competition of oxygen with CF$_x$ species on the surface. This effect is assumed to be more pronounced at the horizontal surfaces because the etch mechanism of silicon oxide, by CF$_x$ species, is ion enhanced where ions are the main reactants in the etch reaction [4.29]. For high CHF$_3$ flows the reduction of the O atom concentration and passivation layer formation eventually result in more isotropic etching profiles (see fig. 4.8).

4.2.4 Total gas flow

It should be noted that the total gas flow in the experiments is not constant and changes, depending on the SF$_6$, O$_2$ and CHF$_3$ gas flow settings, from a minimum value of 32 sccm to a maximum value of 72 sccm. As a result, the residence time of the gases is not constant. Longer residence times will lead to a higher conversion of the etch gases into lower molecular weight products. The etch rate dependency as a function of the total flow rate for SF$_6$/O$_2$ gas
mixture has been studied by Tandon, and Kopalidis [4.8, 4.32]. At low pressures an increase in total flow rate is accompanied by an increase in the dissociation of SF$_X$ molecules and radicals. The etch rate is limited by the supply of fluorine atoms while at high flow rate, the etch rate decreases due to convective losses. At high pressure and low flow rate, etchant production is at its maximum and increasing flow rates causes the fluorine concentration to decrease due to higher convective losses (active species are pumped away before they have an opportunity to react).

4.2.5 Uniformity

As shown in table 4.4 the uniformity does not show a large variation when the parameter settings are changed. Only a few points per wafer have been used to determine the uniformity leading to a relatively large standard error. The average uniformity of the etch rate is 3.5 percent with a standard deviation of 1.4 percent. A small increase is observed for high SF$_6$ flows and high pressures. The uniformity is mainly a function of loading and cathode material as discussed in the section on initial experiments.

4.2.6 Self-Bias Voltage

The bias voltage is quadratically dependent on the SF$_6$ flow, the RF power and the pressure. It increases linearly with the O$_2$ flow and the CHF$_3$ flow. The model fit of the experimental values is very good as indicated by the $R^2$ index which equals 0.99. The results are presented graphically in fig. 4.7.

SF$_6$ is used as a gaseous insulator because of its electronegativity. Increasing the SF$_6$ flow makes the discharge more electronegative due to a lower ratio of electrons to positive ions and the self-bias voltage decreases.

The decrease of the DC bias voltage with pressure is a result of a decrease in the electron energy as the pressure is increased [4.12]. The increase of the DC Bias with increasing O$_2$ flow and CHF$_3$ flow, is a consequence of a shift of the electron energy distribution to higher values [4.12].

4.2.7 Etch surface roughness

It was observed that the etch surface roughness showed a correlation to wafer cleanliness. A cleaning step, followed by an HF-dip were sufficient to reduce the etch surface roughness in this case, indicating that etch residue from previous steps or native oxide could be responsible for this effect. In spite of
Fig. 4.7 Surface plots showing the bias voltage as a function of the SF$_6$ and the O$_2$ flow, and as a function of the process pressure and the RF power.
Fig. 4.8 SEM photographs showing the influence of the CHF$_3$ addition with respect to the surface roughness and etch profile. All parameter settings except for the CHF$_3$ flow are set at the center point, a) 2 sccm CHF$_3$; b) 12 sccm CHF$_3$; c) 22 sccm CHF$_3$. 
these precautions rough etch surfaces have been observed after the etching process and were found to be a function of the etch parameters. This indicates that rough etch surfaces are also generated by the etching process itself. At high pressures and high O\textsubscript{2} flows, in the anisotropic etching regime, the etch surface roughness increases. The addition of CHF\textsubscript{3} to an SF\textsubscript{6}/O\textsubscript{2} gas mixture improves the etch surface quality, as shown in figure 4.8. When no CHF\textsubscript{3} is added, the etch surface roughness, in the anisotropic etch regime, is high as a result of "micrograss". The addition of CHF\textsubscript{3} results in smooth surfaces, only slightly affecting the anisotropy. At high CHF\textsubscript{3} flows, the anisotropy will be lost and more isotropic etching is obtained.

It is suggested that surface roughness is the result of silicon oxide micromasking. It has been shown that during etching in SF\textsubscript{6} plasmas, large amounts of Si particles are generated [4,33]. When oxygen is added to SF\textsubscript{6} plasmas these particles may also contain silicon oxide. Redeposition of these particles on the etch surfaces results in micromasking leading to the development of surface roughness (micrograss) in case of anisotropic etching. Another possibility is that the surface roughness results from variations in thickness of the oxyfluoride layer on the horizontal silicon surfaces.

The presence of CF\textsubscript{x} species in the plasma may suppress the formation and/or oxidation of the particles that are generated by the plasma or reduce their masking effect and suppresses the formation of the passivation layer on the horizontal surface by chemical and physical attack. Note that CF\textsubscript{x} species may not only be produced by the etch gas but also by photoresist or reactor components like a graphite cathode.

In summary it can be concluded that the anisotropic etch mechanism is based upon an ion-enhanced inhibitor etching process. This mechanism requires three ingredients: 1 reactive neutral species, 2 inhibitor film forming

![Fig. 4.9 Schematic drawing of the SF\textsubscript{6}, O\textsubscript{2}, CHF\textsubscript{3} etch process.](image-url)
species, and 3 vertical ion flux to the substrate to prevent growth or etch the inhibitor film at the horizontal surfaces. These mechanisms can be more or less controlled independently by the three etch gases. SF$_6$ produces the F radicals for the chemical etching of the silicon. O$_2$ creates the O radicals to passivate the silicon surface by silicon oxide species. CHF$_3$ produces CF$_x$ ions that, in addition to SF$_x$ ions, suppress the formation of the passivation layer at horizontal surfaces. The etch process is schematically shown in fig. 4.9.

### 4.3 APPLICATIONS

The process parameters optimized for high anisotropy are very useful for the fabrication of deep trenches and micromechanical structures. The optimized process parameter settings result in an anisotropy of 0.98, an etch rate of 0.5 $\mu$m/min, a selectivity with SiO$_2$ of 10 and a smooth etch surface. Not only monocrystalline silicon but also LPCVD polysilicon and sputtered silicon films have been used to fabricate micromechanical structures. In fig. 4.10 and 4.11 SEM photographs are shown that clearly demonstrate the usefulness of the RIE process for high aspect ratio structures. The selectivity of the silicon dioxide mask limits the etch depth. This problem can be solved by using metal etch masks. For instance with a chromium etch mask very high selectivities (>500) have been obtained, whereas etch characteristics are only slightly affected. Applications of this etch process, with respect to deep trench etching and other mask materials, has been presented elsewhere [4.34, 4.35].

### 4.4 CONCLUSIONS

Reactive ion etching using SF$_6$, O$_2$, CHF$_3$ gas mixtures for the anisotropic etching of silicon has been investigated. The etching behaviour was found to be affected by loading, the mask material and the cathode material. Reproducable and uniform results have been obtained by using a silicon cathode and a silicon dioxide mask. Surface response methodology was used to characterize etch rate, mask selectivity, bias voltage and anisotropy as a function of the RF power, the process pressure, the SF$_6$ flow, the O$_2$ flow and the CHF$_3$ flow in order to optimise anisotropic etching conditions. The effect of several variables on the measured responses has been discussed. The addition of CHF$_3$ can be used to produce smooth etch surfaces in the anisotropic regime and is useful for a fine tuning of the anisotropy.
**Fig. 4.10** SEM photograph showing 1, 2 and 5 µm lines and spacings etched to a depth of plusminus 10 µm. For the 1 and 2 µm structures the effect of RIE lag is clearly visible.

**Fig. 4.11** SEM photograph of a stator-rotor structure of an electrostatic micromotor. Diameter of the rotor is 100 µm, etch depth is 13 µm and the rotor-stator gap spacing is 2 µm.
AES measurements indicate that anisotropic etching results from sidewall passivation by silicon oxide species. The anisotropic etch mechanism in SF\textsubscript{6}, O\textsubscript{2}, CHF\textsubscript{3} plasmas is based on ion-enhanced inhibitor etching. SF\textsubscript{6} provides the reactive neutral etching species, in the form of F atoms. O\textsubscript{2} supplies the inhibitor film forming species that passivate the surface with a SiO\textsubscript{x}F\textsubscript{y} layer. SF\textsubscript{6} and CHF\textsubscript{3} generate ion species, SF\textsubscript{x+} and CF\textsubscript{x+} respectively, that suppress the formation the inhibitor film at horizontal surfaces.

The fabrication of structures with aspect ratios of about 10 has been demonstrated. The process is applied to deep trench etching and fabrication of high aspect ratio structures used in micromachining.

REFERENCES


Anisotropic reactive ion etching of silicon
Chapter Five

ELECTROSTATIC CURVED ELECTRODE ACTUATORS*

The design and performance of an actuator design that is based on the deformation of a movable micromechanical structure, which is deflected by electrostatic forces along a fixed curved electrode, is presented. The behaviour of this type of actuators is studied by using cantilever beam structures. A theoretical description of the static behaviour of a deformable cantilever beam that is forced into contact with a rigid structure by means of electrostatic forces is given. Modelling of the static behaviour was done by a simplified model based on energy methods and by 3D coupled electromechanical simulations using CoSolve-EM. The shape of the curved electrode was found to have a strong influence on the actuator performance. The models are in qualitative agreement with each other and predict stable actuator behaviour when the beam deflection becomes constrained by the curved electrode geometry before electrostatic pull-in can occur. Actuators that generate displacements in a direction lateral to the wafer surface were fabricated by polysilicon surface micromachining techniques. Experiments were performed in order to verify theoretical results. Relative large displacements and forces can be generated by curved electrode actuators. Depending on the design, or as a result of geometrical imperfections, instable regions in the deflection behaviour are present.

5.0 INTRODUCTION

Electrostatic actuation is very attractive for micro-electro-mechanical systems because of good its scaling properties to small dimensions, high energy densities and its relative ease of fabrication. However, electrostatic actuators which are able to generate relatively large displacements and large forces are difficult to design as a result of a geometric discrepancy. Large displacement actuators (e.g. comb drive structures) require displacements perpendicular to the major field lines, leading to small forces. Large force actuators (e.g. parallel plate structures) require small gaps and a displacement in the direction of the major field lines, thus implying small displacements. Several actuator designs

Electrostatic curved electrode actuators have been reported employing curved structures in order to generate large displacement and large forces. A curved electrode has been applied in microactuators for aligning optical fibers [5.1]. Actuators have been presented where a large vertical displacement is obtained by a S-shaped film sandwiched between planar electrodes [5.2]. Another design employs a deformed membrane which is pulled against a glass plate by electrostatic forces [5.3]. Active joints that employ a bend beam electrode that is pulled against a rigid counter electrode have also been proposed [5.4]. Furthermore a distributed electrostatic microactuator using wave-like electrodes [5.5] and an electrostatic moving wedge actuator for use in a microrelay [5.6] have been presented. All these actuators use curved structures with a specific shape that are deflected by electrostatic forces towards a counter electrode and generate displacements that are normal to the wafer surface.

In order to investigate the basic phenomena of these actuators, the static behaviour of cantilever beam structures that are deformed by electrostatic forces along curved electrodes has been studied. Special attention has been given to the effect of the electrode curvature on the static behaviour of the actuators. The dynamic properties of comparable structures have been presented elsewhere [5.7]. Devices, that generate displacements in a direction lateral to the wafer surface, have been fabricated by polysilicon surface micromachining techniques. Experimental data obtained from these structures is compared with theoretical results.

5.1 DESIGN

The design of curved electrode actuators includes a movable electrode structure, a cantilever beam, and a mechanically fixed curved counter electrode as shown in fig. 5.1. The gap distance between both electrodes is small near the clamped edge of the beam and increases with a position along the length of the beam. When a voltage is applied across the gap, an electrostatic force is created that will deform the beam along the outline of the curved electrode. The displacement is parallel to the wafer surface. To prevent short circuiting between the beam and the curved electrodes, electrical insulation is required e.g. by applying a dielectric layer between the structures or by using stand-off bumper structures that prevent physical contact of the electrodes.

For the shape of the curved electrode simple polynomials have been used that are normalised to a maximum tip deflection of the cantilever beam.
As will be shown in the next section the performance of these actuators is dependent on the electrode curvature and can become unstable after the so-called pull-in voltage. Although this work is focused on cantilever beam structures, similar actuators can be fabricated by using microbridges or membranes.

![Curved Electrode Beam](image)

**Fig. 5.1** Schematic view of the curved electrode actuator.

### 5.2 ENERGY MODEL

#### 5.2.1 Unloaded and unconstrained static behaviour

Because the gap spacing is small with respect to the electrode length it is assumed that the electric field lines cross the gap only in the y-direction, i.e. a parallel plate approximation.

When a d.c. polarisation voltage is applied between the capacitor electrodes, an electrostatic force is developed that is inversely proportional to the square of the gap spacing. This makes the force dependent on the deflection, leading to non-linear behaviour. The static deflection \( w(x) \) of a prismatic beam with a transverse pressure \( q(x, V) \) can be described by the following non-linear differential equation of equilibrium:

\[
E_y I \frac{d^4 w(x)}{dx^4} = q(x, V) = \frac{1}{2} \frac{\varepsilon_0 h V^2}{d} \left[ \frac{d}{\varepsilon_r + s(x) w(x)} \right]^2
\]

where \( q(x, V) \) denotes the static electrostatic force per unit beam length as a function of the position \( x \) and the drive voltage \( V \), \( E_y I \) is the bending stiffness, \( d \) is the thickness of the insulator, \( \varepsilon_0 \) the dielectric constant in air, \( h \) the width of the beam, \( \varepsilon_r \) is the dielectric constant of the insulator and \( s(x) \) the shape of the beam.
Electrostatic curved electrode actuators

electrode as a function of the position \( x \). An analytical closed-form solution of the above equation cannot be found and numerical solutions or a simplified model based on the method of total potential energy using small deflection theory has to be applied e.g. the Rayleigh-Ritz method, were an approximate solution to the differential equation is constructed in the form of admissible trial functions \([5.8, 5.9]\). The total potential energy, denoted by \( \Pi \), can be expressed as:

\[
\Pi = U_b + V_{el}
\]

(5.2)

where \( U_b \) and \( V_{el} \) are the strain energy term of bending and the potential energy of the electrostatic force \( q(x,V) \) given by:

\[
U_b = \frac{1}{2} \int_0^L E I \left( \frac{d^2 w(x)}{dx^2} \right)^2 dx
\]

(5.3)

and

\[
V_{el} = -\frac{1}{2} \int_0^L \frac{ \varepsilon_0 h V^2 }{ \varepsilon_r + s(x) - w(x) } dx
\]

(5.4)

The deflection profile of a uniformly loaded cantilever beam has been used for the admissible trial function:

\[
\tilde{w}(x) = c \ g(x) = c \ x^2 \ (6 \ L^2 - 4 \ L \ x + x^2)
\]

(5.5)

where \( c \) is a constant that has to be determined. The applied shape of the curved electrodes have been simple polynomials, described by the following expression:

\[
s(x) = \delta_{max} \left( \frac{x}{L} \right)^n
\]

(5.6)

where \( \delta_{max} \) is the maximum gap distance of the curved electrode and \( n \) is the polynomial order of the curve, \( n \geq 0 \).
The system is in equilibrium when the first variation of the total potential energy with respect to the constant $c$ equals zero. By solving this equation the unknown $c$ can be found giving the approximate deflection profile. Whether this equilibrium is stable or unstable is determined by the second variation of the potential energy with respect to $c$. At the transition from a stable to an unstable equilibrium the first and the second derivative of the potential energy with respect to $c$ are zero. Solving these equations simultaneously yields the pull-in voltage $V_{PI}$ of the cantilever and an implicit expression for the constant $c_{PI}$ at pull-in:

$$V_{PI}^2 = \frac{E_y I}{\varepsilon_0 h} \int_0^L \frac{\left( \frac{d^2 g(x)}{dx^2} \right)^2}{\frac{g(x)^2}{\left( \frac{d}{\varepsilon_r + s(x) - c_{PI} g(x)} \right)^3} dx} (5.7)$$

$$\left( \frac{c_{PI} g(x)^2}{\left( \frac{d}{\varepsilon_r + s(x) - c_{PI} g(x)} \right)^3} dx \right) = \int_0^L \frac{g(x) dx}{\left( \frac{d}{\varepsilon_r + s(x) - c_{PI} g(x)} \right)^2} (5.8)$$

The calculated pull-in voltages for different polynomial orders obtained by this energy method are listed in table 5.1.

<table>
<thead>
<tr>
<th>$n$</th>
<th>$V_{PI}$ [V]</th>
<th>$c_{PI}$ [$\times 10^7$ m$^{-3}$]</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>141.7</td>
<td>7.63</td>
</tr>
<tr>
<td>0.5</td>
<td>115.2</td>
<td>7.66</td>
</tr>
<tr>
<td>1.0</td>
<td>86.2</td>
<td>7.56</td>
</tr>
<tr>
<td>1.5</td>
<td>59.5</td>
<td>6.88</td>
</tr>
<tr>
<td>2.0</td>
<td>40.0</td>
<td>5.54</td>
</tr>
</tbody>
</table>

Table 5.1 Calculated pull-in voltages from the energy model, for polysilicon ($E_y=150$ GPa) cantilever beams ($h*t*L=5*2*500$ µm) with a minimum gap spacing $d$ of 2 µm and a maximum deflection $\delta_{max}$ of 30 µm. A relative dielectric constant of the insulator $\varepsilon_r$ equal to 1 has been used.
Equations (5.7) and (5.8) give little insight into the effect of different parameters. Some aspects will be discussed here. Increasing the polynomial order $n$ of the electrode curve decreases the pull-in voltage, while the maximum displacement just before pull-in stays about the same. Thus, by using curved electrodes the pull-in voltage can be lowered significantly resulting in large amplitude motion at lower driving voltages as compared to a parallel plate structure ($n=0$). At voltages above the pull-in voltage the displacement cannot be controlled because of unstable behaviour. The maximum tip displacement at pull-in is independent of the beam properties and only depends on the gap geometry. The tip displacement at pull-in, calculated from the examples in table 5.1, is about one third of the maximum gap spacing $\delta_{\text{max}}$ which is comparable to the stable to unstable deflection boundary for a lumped parallel-plate spring model [5.10]. The pull-in voltage strongly decreases with a decreasing initial gap spacing at the clamped edge of the beam or an increasing dielectric constant of the insulating layer between the electrodes.

5.2.2 Constrained static behaviour

The pull-in voltage decreases with increasing polynomial order. However for polynomial orders above two it was found that the deflection profile of the beam becomes constrained by the geometry of the curved electrode before the pull-in voltage is reached. In this situation, the model needs to be adjusted. In addition, a force $P$, that is acting on the tip of the beam, has been added in order to perform external work. In addition to the electrostatic forces, this force will deform the beam in that part where the beam is not in contact with the curved electrode.

Fig. 5.2 Sketch of the constrained beam deflection model with external loading force $P$.

It is assumed that the beam will partly be in contact with the curved electrode and will partly be free standing; and is clamped at point $a$, as sketched in fig. 5.2. The problem will have a variable boundary condition with respect to
the free standing length of the beam. Therefore the system has to be divided in two regions. From the clamped edge of the beam to point \( a \), the beam is assumed to be in physical contact with the curved electrode and the deflection profile \( w(x) \) will be equal to the shape of the electrode \( s(x) \). The distance between the electrodes is equal to \( d \), the thickness of the insulator. Beyond point \( a \), the beam is free and deflected by electrostatic forces and the external force.

The expressions for the strain energy of bending and the potential energy of the electrostatic force become:

\[
U_b = \frac{1}{2} \int_0^a E_y I \left[ \frac{d^2 s_x}{dx^2} \right]^2 dx + \frac{1}{2} \int_a^L E_y I \left[ \frac{d^2 w(x)}{dx^2} \right]^2 dx
\]  

(5.9)

and

\[
V_{el} = -\frac{1}{2} \int_0^a \frac{\varepsilon_r \varepsilon_0 h V^2}{d} dx - \frac{1}{2} \int_a^L \frac{\varepsilon_0 h V^2}{\varepsilon_r + s(x) - w(x)} dx
\]  

(5.10)

An additional term has to be added to the total potential energy, given in expression (5.2). This term is the work from the external force acting on the tip of the beam, given by:

\[
V_P = P w(L)
\]  

(5.11)

The admissible trial function of the deflection profile of the cantilever beam is now also dependent on the contact distance \( a \) and bending from force \( P \):

\[
w(x) = s(x)
\]  

for \( 0 < x \leq a \)  

(5.12)

\[
w(x) = c(x-a)^2 \left[ 6(L-a)^2 - 4(L-a)(x-a) + (x-a)^2 \right] - \frac{P (x-a)^2 \left[ 3(L-a) - (x-a) \right]}{6 EI} + \left[ s(x) \right]_{x=a} + \frac{dx}{dx} \left[ s(x) \right]_{x=a} \frac{x-a}{x-a}
\]  

for \( a < x < L \)  

(5.13)

This system, is in equilibrium when the first variation of the potential energy with respect to the contact distance \( a \) equals zero and the first variation of the potential energy with respect to the constant \( c \) equals zero. Solving both
equations simultaneously by numerical iteration gives the values of $a$ and $c$ at a certain driving voltage.

The tip deflection versus driving voltage for unloaded designs with a polynomial order ranging from two to four is shown in fig. 5.3. For designs with a polynomial order above 2, a stable behaviour up to the maximum tip deflection is found. This is the result of the constrained beam deflection which makes the beam zip along the curved electrode.

![Graph showing tip deflection versus driving voltage](image)

**Fig. 5.3** Modelled tip deflection as a function of the applied voltage for several polynomial orders $n$ of the curved electrode. When the polynomial order is larger than 2 the behaviour becomes stable up to the maximum tip deflection. Otherwise a pull-in voltage exists. Variable settings that have been used are: $E_y=150$ GPa, $h=5$ µm, $t=2$ µm, $L=500$ µm, $d=2$ µm, $\delta_{\text{max}}=30$ µm and $\varepsilon_r=1$.

### 5.2.3 Force generation

The force generated by the actuator is a function of the displacement of the tip. It can also be found by numerical iteration from the first variation of the potential energy, with respect to the contact distance $a$ and the constant $c$, that equals zero. The external force needed to keep the tip fixed at a certain position is shown in fig. 5.4 for different tip positions. Actuator dimensions are given in the figures and the effect of the dielectric constant of the insulating layer between the electrodes is also shown. Forces are typically a few µN for this example but increase with decreasing gap distance and increasing dielectric
constant of the insulating layer. The deformation of the beam is illustrated in fig. 5.5. In this case the tip is fixed at zero deflection. When the actuator is loaded by displacement dependent forces like e.g. a spring, the problem can be solved by substitution of the force to displacement relation.

![Figure 5.4](image1)

**Fig. 5.4** Theoretical force generation of a quadratic order electrode curvature as a function of driving voltage. The tip deflection is fixed at 0, 10, and 20 µm. The effect of the relative dielectric constant of the insulating layer is also shown. Unless given in the graph, variable settings equal to fig. 5.3 have been used.

![Figure 5.5](image2)

**Fig. 5.5** Deflection profiles of the cantilever beam in fig. 5.4 for several driving voltages when the tip is constrained at zero deflection.
5.3 3D COUPLED ELECTROMECHANICAL SIMULATIONS

In order to study 3D effects like fringing fields and the effect of a groundplane below the actuator, which will be present after fabrication by surface micromachining techniques, simulations have been performed by CoSolve-EM.

CoSolve-EM is a software package that is capable of doing self-consistent electromechanical analysis of complex three-dimensional structures [5.11]. The approach is based on a relaxation scheme combining a fast multipole-accelerated scheme for the electrostatic analysis (FASTCAP) with a standard finite-element method for the mechanical system analysis (ABAQUS). An example of a geometric model of the actuator is shown in fig. 5.6. The models consist of a movable cantilever beam, a groundplane and different types of curved electrodes.

![Fig. 5.6 Geometric model used in the CoSolve-EM simulations.](image)

In the model, $x$ is along the length of the beam, $y$ is in a direction normal to the groundplane and the principal motion of the beam is in $z$-direction towards the curved electrode. Because the cantilever beam contacts the curved electrode, an interface had to be inserted between the electrodes using CoSolve-EM. Because of this contact problem the levitation in $z$-direction of the beam
had to be suppressed in order to obtain proper convergence [5.11]. The modelled tip deflection to driving voltage behaviour for quadratic, cubic and fourth order curvatures with an initial gap distance of 1 µm are shown together with results from the numerical energy model in fig. 5.7.

![Graph showing tip deflection vs voltage for n=2, n=3, n=4, CoSolve-EM, and Energy Model](image)

**Fig. 5.7** Results of the unloaded tip deflection versus voltage behaviour for the energy model using a parallel plate approximation and the 3D finite element model in the presence of the groundplane. The dielectric constant of the spacer is 1, and a Young’s modulus $E_y$ of 150 GPa, a Poisson’s ratio $\nu$ of 0.3, beam dimensions $(h^*t^*L)$ of 5*2*500 µm, a minimal gap spacing $d$ of 2 µm, and maximum gap spacing $\delta_{\text{max}}$ of 30 µm have been used in the calculations.

It can be concluded that the results from the numerical energy model and the 3D coupled electro-mechanics are qualitatively in good agreement with each other. The presence of the groundplane reduces the tip deflection and increases the pull-in voltage. Simulations without a groundplane showed to increase the tip deflection of the beam because of electrostatic fringing fields compared to the parallel plate approximation that was used in the energy model.

Actuators have been fabricated that employ polysilicon bumper structures instead of a continuous insulating layer between the electrodes to simplify the fabrication process. The static behaviour of these complex structures can easily be simulated by CoSolve-EM by attaching interface nodes at the corresponding bumper positions.
5.4 FABRICATION

A one mask fabrication process has been developed where electrical insulation between the electrodes is obtained by polysilicon bumper structures or by a sidewall silicon nitride layer.

The fabrication starts with a (100) p-type 3" silicon wafer. The first step is wet thermal oxidation at 1150 °C to obtain a 2 µm thick SiO2 sacrificial layer. Next a 5 µm thick polysilicon layer is grown by LPCVD at a temperature of 590 °C, a pressure of 250 mTorr and a silane flow of 50 sccm. This polysilicon layer is heavily doped with boron by deposition of a BSG layer and indiffusion at 1150 °C for 3 hours. This yields a sheet resistance of about 4.5 Ω/□ and also results in a small residual strain and strain gradient of the polysilicon layer. After boron indiffusion the BSG layer is stripped in a buffered HF-solution. A 0.6 µm thick PECVD SiO2 layer is grown that serves as an etch mask for the polysilicon. After patterning this SiO2 layer by RIE using CHF3 gas, the polysilicon is anisotropically etched using a SF6, O2, CHF3 gas mixture. After a cleaning step the sacrificial layer is etched in a buffered HF solution for 30 minutes. This releases thin beams but leaves larger structures attached to the substrate. Drying is done by means of a special freeze drying method to prevent stiction of free structures to the substrate [5.12]. Finally a 1 µm thick aluminum layer is evaporated for backside contact. The final result for bumper designs is shown in fig. 5.8.

By introducing a deposition of LPCVD stress reduced silicon nitride after anisotropically etching the polysilicon and a subsequent anisotropic RIE step in CHF3 gas, silicon nitride sidewall layers are obtained that act as a continuous insulating layer between the electrodes. Examples of devices having silicon nitride sidewall insulation are shown in fig. 5.9.

5.5 EXPERIMENTAL RESULTS AND DISCUSSION

5.5.1 Experimental Set-up:

The tip deflection as a function of the applied driving voltage has been measured for different electrode curves using an experimental set-up that consisted of a probe station with a microscope and a digital voltage supply. The beam electrode and the substrate are connected to ground potential while the
Experimental results and discussion

Fig. 5.8 a) SEM photograph of curved electrode actuator with stand-off bumpers.

Fig. 5.8 b) Close-up of the free standing actuator tip and the stand-off bumper structures.
Fig. 5.9 a) SEM photograph of curved electrode actuator with a silicon nitride sidewall insulation.

Fig. 5.9 b) SEM photograph of a microgripper employing curved electrode structures for actuation.
curved electrode is connected to a positive voltage. Deflections and dimensions have been measured using a micrometer eyepiece.

5.5.2 Pull-in voltage of instable bumper designs:

To determine the pull-in voltage, the driving voltage was slowly increased until the beam deflection became unstable. The results are shown fig. 5.10 and the pull-in voltages are listed in table 5.2. The calculated pull-in voltages as obtained from the energy model are smaller than the measured values. The difference is increasing with a decreasing order of the electrode curvature. This effect can be explained by three dimensional electrostatic field effects.

Electrostatic fringing fields will increase the electrostatic forces and will result in lower pull-in voltages than calculated by our simple parallel plate approximation. However, the presence of the groundplane gives rise to an unbalanced electrostatic field distribution [5.13]. This effect reduces the electrostatic forces, especially for large gaps, and induces a levitation of the surface micromachined structure. The pull-in voltage will increase by the presence of the groundplane and this effect will be stronger for increasing gap distances. Therefore it is difficult to give accurate quantitative predictions of the pull-in voltage by a simple one-dimensional model and makes 3D simulations necessary.

Note that the pull-in voltage is very sensitive to the thickness of the cantilever beam and the initial gap spacing. For instance a change in beam thickness of 0.1 µm results in a change of about 10 percent in the pull-in voltage of a second order polynomial design.

<table>
<thead>
<tr>
<th>order</th>
<th>Pull-in Voltage V_{PI} [V]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Cantilever</td>
</tr>
<tr>
<td></td>
<td>Exp.</td>
</tr>
<tr>
<td>0</td>
<td>223</td>
</tr>
<tr>
<td>1.0</td>
<td>98</td>
</tr>
<tr>
<td>2.0</td>
<td>40</td>
</tr>
</tbody>
</table>

*Table 5.2* Pull-in voltage of polysilicon (E_y=150 GPa) cantilever beams using bumper structures with a maximum deflection δ_{max} of 30 µm. The measured thickness of the poly layer is 4.6 µm (width of the beam), the thickness of the sacr. layer is 1.6 µm. The thickness t of the different beams is 1.6, 1.7 and 1.8 µm and the initial gap is 2.2, 3.2 and 3.3 µm for the 0,1 and 2 order polynomial designs respectively.
Fig. 5.10 Measured tip deflection versus driving voltage for unstable electrode profiles. The polynomial orders are 2, 1 and 0. Measured dimensions are given in table 5.2.

Fig. 5.11 Measurement of the hysteresis effect for second order polynomial designs. Beam length and width are respectively 500 µm and 4.6 µm. Beam thickness and initial gap spacing are 1.6, 3.3 µm and 1.7, 4.3 µm respectively.
The behaviour of the actuators show a hysteresis effect for increasing and decreasing voltages. This has been studied in more detail for the second order polynomial electrode and the experimental results are shown in fig. 5.11. The origin of the hysteresis comes from a difference in the electrostatic field distribution between a beam before and after pull-in. Once the beam has collapsed, the electrostatic forces strongly increase as a result of the decreased gap spacing and a large decrease in the driving voltage is needed before bending forces overcome the electrostatic forces again.

5.5.3 Constrained bumper designs:

When the polynomial order becomes larger than 2 the deflection of the beam becomes constrained by the geometry of the curved electrode as discussed earlier. The measured deflection behaviour of a third and fourth order polynomial curvature design with bumper structures at every 50 µm is shown in fig. 5.12 for beams with a length of 500 µm. The application of bumpers results in a stepwise behaviour where the cantilever beam reaches an instable point from bumper to bumper. In fig. 5.12 also simulated results, as obtained from CoSolve-EM, are shown for the third order design. The model is in fair agreement with the data.

![Graph](image_url)

**Fig. 5.12** Experimental results of curved electrodes with stand off bumper structures where the beam deflection becomes constrained by the electrode geometry (bumpers). The polynomial orders are 3 and 4. Also the results of a CoSolve-EM simulation for a cubic electrode design with a gap spacing of 3.1 µm employing bumper structures spaced apart at every 50 µm and 2 µm away from the curved electrode are shown.
The CoSolve model is systematically somewhat overestimating the electrostatic force on the beam. This is probably a result from the suppression of the levitation of the beam or a too small theoretical Young’s modulus.

5.5.4 Constrained sidewall insulator designs:

An example of the measurement results from an actuator with continuous sidewall insulation is shown in fig. 5.13. In contrast to our theoretical results, devices with a continuous sidewall insulator did not show completely stable behaviour. Fabricated devices show a behaviour that is more or less identical to the constrained bumper designs. Steplike instable and stable regions are also observed. Furthermore, a large variation in behaviour of identical designs has been observed.

![Graph](image.png)

**Fig. 5.13** Example of the measured tip deflection as function of the driving voltage for a fourth order design with a silicon nitride sidewall layer.

It is suggested that these results are caused by imperfections at the sidewall surfaces such as surface asperities, and entrapped particles and residues between the electrodes after the fabrication process. A close up of a beam with silicon nitride sidewall insulation is shown in fig. 5.14. Small protrusions strongly affect the behaviour of the actuators and prevent the movable beam from smoothly zipping along the curved electrode and act like small bumpers which leads to instabilities. For successful constrained designs smooth
electrodes surfaces are required. This may be difficult to realise by the proposed fabrication process and is, in general, a fabrication problem for this type of actuators.

![Fig. 4.14 Close-up SEM top-view of a cantilever beam with silicon nitride sidewall passivation. The presence of relative large surface roughness, residue and particles is suggested to induce instabilities in the actuator behaviour.](image)

5.6 CONCLUSIONS

An electrostatic actuator design has been presented where a deformable mechanical structure is bent around a fixed curved electrode by means of electrostatic forces. Such a design is attractive because relatively large deflections and force generation can be obtained.

For the shape of the curved electrode simple polynomials have been used. A theory based upon energy methods is presented to describe the static behaviour of the actuator. Furthermore 3D coupled electromechanical simulations using CoSolve-EM have been performed. The results from both models are in qualitative agreement with each other. When the beam deflection is not constrained by the curved electrode geometry, instable behaviour occurs at a certain pull-in voltage and a hysteresis exists to release the structure after pull-in. For polynomial designs, with an order above two, it was found that the
beam deflection becomes constrained by the curved electrode geometry before pull-in occurs. Our models predict completely stable behaviour in this situation.

Curved electrode actuators have been fabricated from polysilicon by surface micromachining techniques using a one-mask process. Electric insulation has been realised by stand off bumper structures between the movable beam and the fixed electrode or by a silicon nitride sidewall layer. Measurements of non-constrained beam deflections show that the qualitative behaviour of the energy model is in agreement with theory but that the pull-in voltages are higher than theoretically predicted. This effect is a result of the presence of a groundplane as shown in 3D coupled electromechanical simulations.

Constrained designs employing bumper structures show a stepwise behaviour as a result of a number of stable positions at the bumper positions. The static behaviour of these designs has been modelled by CoSolve-Em and was found to be in fair agreement with experimental data.

Experimental data of samples with a continuous sidewall insulator did not show stable behaviour up to maximal deflection in contrast to our theoretical results. It is suggested that this is caused by imperfections at the sidewall surfaces, as a result of the fabrication process, which prevent the movable beam from smoothly zipping along the curved electrode and act like small bumpers leading to local instabilities.

In practice, curved electrode structures are therefore mainly suited for bistable actuator applications such as microswitches, microgrippers, microvalves and micro pumps.

5.7 REFERENCES


References


Electrostatic curved electrode actuators
COMB-DRIVE ACTUATORS FOR LARGE DISPLACEMENTS*

The design, fabrication and experimental results of lateral comb-drive actuators for large displacements at low driving voltages is presented. A comparison of several suspension designs is given, and the lateral large deflection behaviour of clamped-clamped beams and a folded flexure design is modelled. An expression for the axial spring constant of folded flexure designs including bending effects from lateral displacements, which reduce the axial stiffness, is also derived. The maximum deflection that can be obtained by comb-drive actuators is bounded by electromechanical side-instability. Expressions for the side-instability voltage and the resulting displacement at side-instability are given. The electromechanical behaviour around the resonance frequency is described by an equivalent electric circuit. Devices are fabricated by polysilicon surface micromachining techniques using a one mask fabrication process. Static and dynamic properties are determined experimentally and are compared with theory. Static properties are determined by displacement-to-voltage, capacitance-to-voltage and pull-in voltage measurements. Using a one-port approach, dynamic properties are extracted from measured admittance plots. Typical actuator characteristics are deflections of about 30 µm at driving voltages around 20 V, a resonance frequency around 1.6 kHz and a quality factor of approximately 3.

6.0 INTRODUCTION

Comb drive actuators consist of two interdigitated finger structures, where one comb is fixed and the other one is connected to a compliant suspension. Applying a voltage difference between the comb structures will result in a deflection of the movable comb structure by electrostatic forces. Comb drive actuators have been used as resonators [6.1-6.3], electromechanical filters [6.4], optical shutters [6.5-6.7], microgrippers [6.8] and voltmeters [6.9]. They have also been used as the driving element in e.g. vibromotors [6.10] and micromechanical gears [6.11].

Voltage controlled comb drive actuators exert a lateral electrostatic force which is independent of position making them attractive for micro positioning applications like, for instance, xy-microstages [6.12-6.14]. Together

* submitted to J. Micromech. Microeng.
with nanotools like microtips, integrated systems can be fabricated with applications in scanning microscopy and data storage [6.15, 6.16]. In most devices actuator deflections have been limited to a few microns but in many cases micropositioning over larger distances is attractive. For this reason the design and fabrication of large displacement comb drive actuators is investigated.

A suspension that is compliant in the aimed direction of displacement and stiff in the orthogonal directions is required. Electrostatic forces increase with decreasing gap spacing and increasing number of comb fingers. However, due to fabrication processes, dimensions are limited by minimum feature size constraining e.g. the minimum beam width and gap spacing. These and other design constraints are discussed in order to obtain large-displacement, low-driving-voltage comb-drive actuators.

### 6.1 SPRING DESIGNS

#### 6.1.1 Introduction

Different types of spring designs have been applied in comb drive actuators [6.1, 6.12, 6.17]. In this section the following spring designs will be discussed; clamped-clamped beams, a crab-leg flexure and the folded beam flexure. In most cases, it is desirable to have a structure which is very compliant in one direction while being very stiff in the orthogonal directions. This can be expressed as a stiffness ratio. Most polysilicon micromechanical flexures constrain motion to a rectilinear direction, and are created from straight beams. When a concentrated load is applied the linear spring constant is defined as:

\[
k_i = \frac{F_i}{\delta_i}
\]  

(6.1)

where \( F_i \) and \( \delta_i \) are respectively the force and the deflection in the \( i \)-direction. The lateral spring constants and stiffness ratio of a clamped-clamped beam, a crab-leg flexure and a folded flexure design will be discussed. The analysis assumes that there is no residual stress present in the spring structures.

#### 6.1.2 Clamped-clamped beam

A clamped-clamped beam with rectangular cross section is shown in fig. 6.1. A concentrated force is applied to the center of the shuttle. The axial displacement along the \( x \)-axis can be found directly from Hooke's law and the
lateral displacement along the y-axis is obtained from small deflection theory [6.18].

![Diagram of a clamped-clamped beam with labels: anchor, shuttle, L, X, Y, b]

**Fig. 6.1 Clamped-clamped beam.**

The spring constants for a concentrated force in x- and y-direction are given by:

\[
\begin{align*}
    k_x &= \frac{2 \, E_y \, h \, b}{L} \\
    k_y &= \frac{2 \, E_y \, h \, b^3}{L^3}
\end{align*}
\]  \hspace{1cm} (6.2)  \hspace{1cm} (6.3)

where \( E_y \) is the Young’s modulus, \( h \) is the beam width, \( b \) is the beam thickness and \( L \) is the length of one beam segment. The stiffness ratio is:

\[
\frac{k_x}{k_y} = \left( \frac{L}{b} \right)^2
\]  \hspace{1cm} (6.4)

The stiffness ratio of a clamped-clamped beam can be very high. For example, the stiffness ratio is \( 1.25 \times 10^5 \) for a beam with a length of 500 µm and a width of 2 µm. For large displacements, however, extensional axial forces develop in the beam that result in a non-linear force-to-displacement relation. In this case non-linear effects have to be included that strongly increase the stiffness of the beam with increasing deflection. This spring design is therefore not suitable for large deflections. However, it is useful in applications that require measurement of axial forces; for instance in order to determine residual stresses or measure externally applied axial stresses as in sensing applications.

A derivation of the large deflection behaviour of clamped-clamped beams can be found in [6.19, Appendix B]. The center deflection for a concentrated load \( P \) at the center can be found by simultaneously solving the next equations:

\[
P_y = \frac{E_y \, I \, b}{L^3} \, \sqrt{8/3} \, u^3 \left( \frac{3}{2} - \frac{1}{2} \, \text{Tanh}^2 u - \frac{3}{2} \, \frac{\text{Tanh} \, u}{u} \right)^{\frac{1}{2}}
\]  \hspace{1cm} (6.5)
\[ \delta_y = b \sqrt{\frac{2}{3}} \left( u - \frac{1}{2} \tanh u \right) \left( \frac{3}{2} - \frac{1}{2} \tanh^2 u - \frac{3}{2} \frac{\tanh u}{u} \right)^{1/2} \]  
\hspace{1cm} (6.6)

with

\[ u = \sqrt{\frac{N}{E_I}} \frac{L}{2} \]  
\hspace{1cm} (6.7)

where \( N \) is the normal force that develops in the beam as a result of the applied force and \( I \) is the second moment of inertia of the beam. The normalised deflection to force behaviour is shown in fig. 6.2. This graph shows that the small deflection theory is valid for deflections up to about a quarter of the beam thickness. For larger deflections non-linear theory has to be used.

![Graph showing normalised deflection to force behaviour.](image)

**Fig. 6.2** Normalised deflection of a clamped-clamped beam with dimensions \((h \times b \times 2L)\) of \(5 \times 2 \times 1000 \ \mu m\) under a force \(P\) at the center.

### 6.1.3 Crab-leg flexure

In order to reduce the extensional axial forces a crab-leg flexure can be used. A sketch of a crab-leg flexure is shown in fig. 6.3. The thigh segment has a second moment of inertia \(I_1\) and a length \(L_1\); the shin segment has a second moment of inertia \(I_2\) and a length \(L_2\). The spring constants, as a result of a concentrated force on the shuttle, in \(x\)- and \(y\)-direction are given by:
Fig. 6.3 Crab-leg flexure.

\[
\begin{align*}
  k_x &= \frac{12 E_y I_2}{L_2^3} \left( \frac{L_1 I_2 + 2 L_2 I_1}{2 L_1 I_2 + L_2 I_1} \right) \\
  k_y &= \frac{24 E_y I_1}{L_1^3} \left( \frac{L_1 I_2 + L_2 I_1}{L_1 I_2 + 4 L_2 I_1} \right)
\end{align*}
\]

(6.8) 

(6.9)

When the shin and thigh have the same width and thickness, the stiffness ratio can be obtained from:

\[
\frac{k_x}{k_y} = \frac{1}{2} \frac{L_1^3}{L_2^3} \left( \frac{L_1^2 + 6 L_1 L_2 + 8 L_2^2}{2 L_1^2 + 3 L_1 L_2 + L_2^2} \right)
\]

(6.10)

Although this design increases the linear deflection region to a certain extent, a large reduction of the stiffness ratio is introduced. A design that has a shin length of 50 µm and thigh dimensions equal to our previous clamped-clamped beam example, has a stiffness ratio that is already several orders of magnitude smaller. A flexure design that is less susceptible to a decrease in the stiffness ratio is the folded flexure design [1].

6.1.4 Folded flexure

A sketch of the folded beam design is shown in fig. 6.4. The beams are anchored near the center and the trusses allow expansion or contraction of the beams along the x-axis. The length of the inner and outer beams is identical otherwise problems may arise because the structure is statically indeterminate.

Assuming rigid trusses, the spring constant of the folded flexure design in axial and lateral direction can be found from:

\[
\begin{align*}
  k_x &= \frac{2 E_y h b}{L} \\
  k_y &= \frac{2 E_y h b^3}{L^3}
\end{align*}
\]

(6.11) 

(6.12)
Fig. 6.4 Folded flexure design.

and the stiffness ratio is:

$$\frac{k_x}{k_y} = \left(\frac{L}{b}\right)^2$$  \hspace{1cm} (6.13)

The folded flexure design strongly reduces the development of axial forces and exhibits a much larger linear deflection range. The stiffness ratio for small deflections is equal to the stiffness ratio of a clamped-clamped beam. This design is therefore very suitable for large deflection actuators. For a more extensive analysis of folded beams, including the effect of compliant trusses, the reader is referred to [6.20] and [6.21]. The large deflection behaviour of a folded flexure design can be obtained by considering the folded flexure as four folded beams in parallel. Each folded beam is a combination of two clamped-guided beams connected in series. By solving the elastica for a clamped-guided beam the large deflection behaviour of the folded flexure can be described by the following set of equations:

$$\delta_y = 8 \sqrt{\frac{E_y I}{P}} \int_{\phi_1}^{\pi/2} \frac{\left(2 p^2 \sin \phi - 1\right) d\phi}{\sqrt{1 - p^2 \sin^2 \phi}}$$  \hspace{1cm} (6.14)

where \( p \) can be found from numerical iteration and solving of:

$$\frac{1}{4} L \sqrt{\frac{P}{E_y I}} = \int_{\phi_1}^{\pi/2} \frac{d\phi}{\sqrt{1 - p^2 \sin^2 \phi}}$$

$$\phi_1 = \text{ArcSin}\sqrt{2 p^2 - 1}$$  \hspace{1cm} (6.15)

A detailed derivation of these formulas is given in appendix C. In these equations \( p \) and \( \phi \) result from a change in variables to bring the equations in
the standard form of elliptic integrals. The normalised deflection to force behaviour of a folded flexure and cantilever beam is shown in fig. 6.5.

\[ \frac{\delta_y}{L} \]

**Fig. 6.5** Normalised large deflection behaviour of a beam with a length 2L that is clamped at one side and guided along the x-axis at the other end and loaded at its center by a concentrated load P. Assuming rigid trusses the behaviour of such a beam resembles the deflection behaviour of a folded flexure. The deflection of a cantilever beam with a length L loaded at the tip is also shown.

The small deflection theory is valid for lateral deflections up to approximately 10 percent of the beam length, which is considerably larger than in case of the clamped-clamped beam.

Another important effect that has to be considered is that the stiffness of the folded beam flexure in the x-direction decreases with increasing displacement in the y-direction. Thus, expressions (6.2), (6.8) and (6.11) are, in fact, upper limits of the axial spring constants at zero lateral deflection which decrease with increasing lateral deflection. In order to include lateral deflection effects in the axial spring constant of the folded flexure the problem is simplified. Because of the folded beam symmetry only one beam segment is considered (see fig. 6.6).

It is assumed that the beam has an initial deflection given by the function \( w_{0(x)} \). The known admissible trial function of the initial deflection profile is:

\[ w_{0(x)} = a x^2(6L - 4x) \]  

(6.16)
The function of the deflection is approximated by the following admissible trial function:

$$w_{o}(x) = bx^2(6L - 4x)$$  \hspace{1cm} (6.17)

where $b$ is an unknown constant. The total potential energy $\Pi$ for the beam is:

$$\Pi = \frac{1}{2} E_y I \int_0^L \left[ \frac{d^2(w - w_0)}{dx^2} \right]^2 dx - P \Delta$$ \hspace{1cm} (6.18)

where $P$ is the axially applied load. The contraction of the beam $\Delta$ can be expressed as [6.22]:

$$\Delta = \frac{1}{2} \int_0^L \left( \frac{dw}{dx} \right)^2 dx - \frac{1}{2} \int_0^L \left( \frac{dw_0}{dx} \right)^2 dx$$ \hspace{1cm} (6.19)

After extremisation of the potential energy with respect to $b$, this contraction is given by:

$$\Delta = \frac{3 \delta_{beam}^2}{5L} \left( \frac{1}{1 - \frac{P}{P_{cr}}} - 1 \right)$$ \hspace{1cm} (6.20)

where $\delta_{beam}$ is the deflection of the initial beam curvature and the buckling load $P_{cr}$ is:

$$P_{cr} = \frac{10EI}{L^2}$$ \hspace{1cm} (6.21)
In case of forces that are much smaller than the critical load \( P_{cr} \) (i.e. \( P/P_{cr} \ll 1 \)), the axial deflection of a beam segment can be found from:

\[
\Delta = \frac{3 P L \delta_{\text{nom}}^2}{25 E_y I} \tag{6.22}
\]

The spring constant for the folded beam flexure as a result of lateral deflections can be found by combining forces and deflections of the beam segments resulting in:

\[
k_x = \frac{200 E_y I}{3 L \delta_y^2} \tag{6.23}
\]

From this expression, it can easily be seen that the spring constant in the \( x \)-direction decreases with increasing beam displacement in the \( y \)-direction. As a result the stiffness ratio also decreases with increasing lateral beam deflection. The total spring constant is a series connection of the lateral displacement dependant spring constant in eq. (6.23) and the axial spring constant resulting from Hooke’s law (eq. 6.11).

### 6.2 ELECTROMECHANICAL BEHAVIOUR

#### 6.2.1 Static behaviour

In the comb actuator a movable set (rotor) and a stationary set (stator) of comb fingers are engaged. A ground plane is located under the comb fingers which is normally connected to the same potential as the rotor in order to prevent electrostatic pull-down forces to the substrate. An engaged pair of fingers, being one cell, of a comb actuator is shown in fig. 6.7. To simplify modelling the electrostatic field between the rotor and stator is approximated by a one dimensional parallel plate model between the engaged parts of the comb fingers. Therefore 3D effects like fringing fields, comb finger end effects and the groundplane levitation effect are neglected [6.23]. In our case these effects will lead to lateral electrostatic forces underestimated by about 5 %, as will be discussed later.

**Lateral deflection**

The capacitance between the stator and the rotor can be expressed as:

\[
C = \frac{2 n \varepsilon_0 h (y+y_0)}{d} \tag{24}
\]
where \( n \) is the number of fingers, \( \varepsilon_0 \) is the dielectric constant in air, \( h \) is the height of the comb fingers, \( y_0 \) is the initial comb finger overlap, \( y \) is the comb displacement and \( d \) is the gap spacing between the fingers. The lateral electrostatic force in the \( y \)-direction is equal to the negative derivative of the electrostatic co-energy with respect to \( y \):

\[
F_{el} = \frac{1}{2} \frac{\partial C}{\partial y} V^2 = \frac{n \varepsilon_0 h}{d} V^2
\]

(6.25)

where \( V \) is the applied voltage between the stator and the rotor. This force is acting on the spring to which the rotor is connected resulting in a deflection:

\[
y = \frac{n \varepsilon_0 h}{k_y d} V^2
\]

(6.26)

**Side instability**

Besides electrostatic forces along the \( y \)-axis, electrostatic forces pulling the stator and rotor fingers together are also present. The electrostatic force in the axial direction is:

\[
F_{el} = \frac{n \varepsilon_0 h (y+y_0)}{2 (d-x)^2} V^2 - \frac{n \varepsilon_0 h (y+y_0)}{2 (d+x)^2} V^2
\]

(6.27)
The forces on both sides of the comb fingers normally cancel each other. However, when the first derivative of the electrostatic force with respect to \( x \) becomes larger than the restoring spring constant in the \( x \)-direction a side-instability of the comb drive is introduced. Hence stable comb operation is bounded by:

\[
k_x > \left[ \frac{\partial F}{\partial x} \right]_{x \to 0} = \frac{2 n \varepsilon_0 h (y+y_0)}{d^3} V^2
\]  

(6.28)

When the driving voltage exceeds the so-called side-instability voltage \( V_{SI} \) the comb drive becomes unstable leading to side sticking of the rotor and stator fingers. By combining eq. (6.26) and (6.28) the voltage at which side-instability occurs can be expressed as:

\[
V_{SI}^2 = \frac{d^2 k_y}{2 \varepsilon_0 h n} \left\{ \sqrt{2 \frac{k_x}{k_y} + \frac{y_0^2}{d^2} - \frac{y_0}{d} \right\}
\]

(6.29)

By neglecting the second term in the root (when \( k_x \gg k_y \)), the maximum deflection \( y_{SI} \) that can be obtained before pull-in will occur is:

\[
y_{SI} = d \sqrt{\frac{k_x}{2 k_y} - \frac{y_0}{2}}
\]

(30)

From this equation it can be seen that the side-instability voltage and maximum deflection are proportional to the gap spacing and increase with the spring stiffness ratio. This shows that comb drive structures with small gap spacings are more susceptible to side instability [6.24] and that spring designs with a large stiffness ratio are preferred for large deflection comb drive actuators. Other sources of instability may result from sideways pull-in of compliant fingers themselves and pull-in by the finger front ends when the stator and rotor are almost completely engaged [6.25]. Instabilities have to be avoided by proper design of the comb structure and flexure; and supply together with maximum deflection, driving voltage and minimum feature size, the main constraints on comb actuator design. Large deflection comb drive actuators at low driving voltages should employ compliant springs with a high stiffness ratio and a large amount of comb fingers. In our comb drive design a folded flexure has been used which is connected at the center to a beam that is able to hold a large number of comb fingers.
6.2.2 Dynamic behaviour

Equation of motion

Using lumped elements the differential equation of motion is given by:

\[ M_{eq} \ddot{y} + \alpha \dot{y} + k_y y = F_e = \frac{n \varepsilon_0 h}{d} \left [ V_p + v(t) \right ]^2 \]  

(6.31)

where \( M_{eq} \) is the equivalent mass of the comb drive actuator, \( \alpha \) is the equivalent viscous drag parameter and \( V_p \) is the d.c. polarisation voltage and \( v(t) \) the a.c. voltage.

The fundamental frequency

The fundamental frequency of the structure can be obtained from Rayleigh's quotient [6.22]. Using the static deflection profile for the approximate fundamental mode shape function of the beams yields the following expression for the fundamental resonance frequency \( \omega_0 \):

\[ \omega_0 = \sqrt{\frac{k_y}{M_{\text{shuttle}} + \frac{1}{2} M_{\text{truss}} + \frac{96}{35} M_{\text{beam}}}} \]  

(6.32)

where the denominator of the fraction under the root sign resembles the equivalent mass \( M_{eq} \) in which \( M_{\text{shuttle}} \) is the mass of the shuttle, \( M_{\text{truss}} \) is the mass of a single truss and \( M_{\text{beam}} \) is the mass of a single beam. The resonant frequency is ideally independent of \( V_p \) and \( v(t) \).

Electric Admittance - Equivalent Circuit

If the comb drive is operated in a one-port configuration, the dynamic behaviour can be described by the electric admittance \( Y(j\omega) \). The voltage is chosen as the independent variable because the electrodes define equipotential surfaces making the voltage independent of position. The constitutive equations of the electrostatic transducer, describing interactions between the mechanical and electrical variables can be expressed as:

\[
\begin{bmatrix}
\frac{dq}{dt} \\
\frac{dF}{dt}
\end{bmatrix} =
\begin{bmatrix}
\frac{2 n \varepsilon_0 h (y_0 + y_\infty)}{d} & \frac{2 n \varepsilon_0 h V_p}{d} \\
- \frac{2 n \varepsilon_0 h V_p}{d} & k_y
\end{bmatrix}
\begin{bmatrix}
\frac{du}{dt} \\
\frac{dy}{dt}
\end{bmatrix}
\]  

(6.33 a,b)
where \( dq \) is the change in charge on the electrodes, \( dF \) is the change in electrostatic force, \( du \) is the change in voltage, \( dy \) is the change in displacement and \( y_{00} \) denotes the static deflection caused by the polarisation voltage \( V_p \). Combining eq. (6.31) and (6.33a) the admittance is equal to:

\[
Y(j\omega) = \frac{i}{V} = j\omega C_0 + \frac{j\omega C_1}{H(j\omega)}
\]

(6.34)

with

\[
C_0 = \frac{2 n \varepsilon_0 h (y_0 + y_{00})}{d}
\]

(6.35)

\[
C_1 = \frac{4 n^2 \varepsilon_0^2 h^2 u_0^2}{k_y d^2}
\]

(6.36)

\[
H(j\omega) = \left( \frac{j\omega}{\omega_0} \right)^2 + \frac{j\omega}{Q \omega_0} + 1
\]

(6.37)

where \( C_0 \) and \( C_1 \) denote the static and motional capacitance, respectively, and \( Q \) denotes the quality factor. An equivalent electrical circuit has been used to describe the electromechanical behaviour of the comb drive actuator around the resonance frequency. This is shown in fig. 6.8.

![Equivalent circuit representation of the electrostatic comb drive actuator including parasitic loads](image)

**Fig. 6.8** Equivalent circuit representation of the electrostatic comb drive actuator including parasitic loads. for \( C_1 \) see expr. (6.36), \( L_1 = \frac{1}{\omega_0^2 c_1} \), \( R_1 = \frac{1}{Q \sqrt{1 / c_1}} \), \( C_p = \) parasitic capacitance, \( R_p = \) parallel resistance, and \( R_s = \) series resistance.

The corresponding values of resistors, capacitors and inductors can be determined by parameter extraction from measured polar admittance characteristics [6.26].
6.3 FABRICATION

Fabrication starts with a (100) p-type 3" silicon wafer. The first step is wet thermal oxidation at 1150 °C to obtain a 2 µm thick SiO₂ layer. Next a 5 µm thick polysilicon layer is grown by LPCVD at a temperature of 590 °C, a pressure of 250 mTorr and a silane flow of 50 sccm. This polysilicon layer is heavily doped with boron by solid source indiffusion at 1100 °C for 3 hours. This yields a sheet resistance of about 4.5 Ω/□ and also results in a small residual strain and strain gradient of the polysilicon layer. After boron indiffusion the BSG layer is stripped in a buffered HF-solution. A 0.6 µm thick PECVD silicon oxide layer is grown that serves as an etch mask for the polysilicon. After patterning the silicon oxide by RIE using CHF₃ gas, the polysilicon is anisotropically etched using a SF₆, O₂, CHF₃ gas mixture. After a cleaning step the sacrificial layer is etched for 50 minutes in a buffered HF solution. Drying is done by means of a cyclohexane freeze drying method to prevent stiction of free structures to the substrate. Finally a 1 µm thick aluminium layer is evaporated for backside contact. The final result is shown in fig. 6.9-6.12.

6.4 MEASUREMENTS

In the experiments, both the substrate and the rotor electrode were connected to ground potential while the stator electrode was connected to a positive driving voltage. From static measurements, flexure spring constants have been determined and the Young’s modulus of the boron doped polysilicon lateral to the wafer surface has been calculated. From the dynamic measurements the resonance frequency and quality factor are extracted using the equivalent circuit parameters.

6.4.1 Extraction of Young’s modulus

The displacement as a function of the driving voltage was measured using a microscope while applying a d.c. voltage between the rotor and the stator/substrate electrodes. Fig. 6.13 shows the results for two crab-leg flexure designs. As expected the deflection is proportional to the voltage squared. If the amount of comb fingers, finger thickness and gap spacing are known, the spring constant of the beams can be calculated from the slope of the deflection-
**Fig. 6.9** SEM photographs of fabricated comb drive actuators. The total length of the actuator is about 1 mm.

**Fig. 6.10** Close-up view showing the comb finger array, the movable shuttle, the beam anchors and free standing beams. A skeleton structure has been used to enhance sacrificial layer etching.
Fig. 6.11 Close-up view of comb fingers of comb structure deflected by electrostatic forces.

Fig. 6.12 Close-up view of comb fingers, truss and deformed beams of a deflected comb structure.
Measurements

Fig. 6.13 Measured deflection as a function of the driving voltage for two crab-leg flexure designs. The comb consisted of 30 fingers with a gap spacing $d$ of 1.6 and 2.6 $\mu$m. The crab-leg structure consisted of a thigh segment with a length $L_1$ of 500 $\mu$m and a shin with a length $L_2$ of 50 $\mu$m. Measured beam width $h$ was 4.7 $\mu$m and the beam thickness $b$ was 1.4 $\mu$m.

to-voltage curves. The lateral Young's modulus of the polysilicon layer can be extracted from known beam dimensions using the measured spring constant. This yields a lateral Young's modulus of 160±20 GPa for the polysilicon in our fabrication process. The large uncertainty in this value is mainly a result from the measurement error in the beam thickness. The Young's modulus is underestimated by a small factor because the electrostatic forces are somewhat larger as a result of fringing fields which have not be taken into account in our model. To obtain an indication of 3D effects, approximate expressions as derived by Johnson and Warne that include fringing fields and groundplane effects have been used [6.27]. For comb fingers with a height of 5 $\mu$m, a width of 2 $\mu$m, a gap spacing of 2 $\mu$m and a groundplane that is located 2 $\mu$m below the fingers, their expression yields an electrostatic force of: $F_d = 2.6225 \epsilon_0 V^2$. This approximated 3D force is about 5 % larger than the parallel plate approximation. After correction for 3D effects a final value of 170±20 GPa for the lateral Young's modulus has been calculated. This value lies well within the range of reported values [6.1, 6.28]. It is in good agreement with a theoretically calculated value of 164 GPa, which has been obtained by averaging of well known mono-crystalline properties, over the measured grain orientations in our polysilicon thin films.
The measured deflection to voltage behaviour of the folded flexure actuator designs is shown in fig. 6.14. The number of comb fingers in these designs has been increased resulting in lower driving voltages. Deflections of about 30 µm are obtained at driving voltages around 20 Volts.

### 6.4.2 Pull-in measurements

Another way to determine the static behaviour is by measuring the capacitance of the comb structure as a function of the d.c. bias voltage. Capacitance to voltage measurements have been performed using a HP4194A impedance analyser. The change in capacitance of some folded beam designs is shown in fig. 6.15. When the pull-in voltage is reached the comb drive becomes unstable and the capacitance suddenly changes as shown in the figure.

The measured pull-in voltage and deflection at pull-in for different folded flexure designs is summarised in table 6.1.

<table>
<thead>
<tr>
<th>Design</th>
<th>fingers</th>
<th>( V_{SI} ) [V]</th>
<th>( \delta ) [µm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>C2-2</td>
<td>136</td>
<td>20.0</td>
<td>39.9</td>
</tr>
<tr>
<td>C2-3</td>
<td>105</td>
<td>22.8</td>
<td>39.9</td>
</tr>
<tr>
<td>C2-4</td>
<td>88</td>
<td>24.3</td>
<td>39.8</td>
</tr>
<tr>
<td>C2-5</td>
<td>74</td>
<td>26.1</td>
<td>39.7</td>
</tr>
</tbody>
</table>

*Table 6.1* Measured pull-in voltage and deflection at pull-in for different comb drive designs. The initial finger overlap \( y_0 \) is 4.6 µm, the gap spacing \( d \) is 2.2 µm, the height \( h \) of the structures is 4.8 µm and the thickness \( b \) of the beams is 1.5 µm.

The spring constants of the beam in the \( x \)- and \( y \)-direction can be determined from these measurements; the measured spring constants \( k_x \) and \( k_y \) are respectively 17 N/m and 0.029 N/m. The theoretical values using measured dimensions and Young’s modulus, are determined from eq. (6.23) and (6.12), giving respectively \( k_x = 19.2 \) N/m and \( k_y = 0.044 \) N/m.

The measured spring constant along the \( y \)-axis is somewhat lower than the calculated value. This could result from the fact that the trusses are not rigid, as assumed in the theoretical model, or by a non-rectangular cross section of the beams as a result of underetching. The measured spring constant along the \( x \)-axis is in good agreement with the calculated value. A spring constant as obtained directly from Hooke’s law (eq. 6.11), would result in a large overestimation of the spring constant in the \( x \)-direction yielding a theoretical value for \( k_x \) that is equal to almost 9000.
Fig. 6.14 Measured deflection as a function of the driving voltage for several folded flexure designs. Design C2-2, C2-4 employed comb structures with respectively 136 and 88 fingers. The gap spacing $d$ equals 2.2 $\mu$m and the length of the beams in the folded flexure is 500 $\mu$m. The beam width $h$ is 4.8 $\mu$m and the beam thickness $b$ is 1.5 $\mu$m.

Fig. 6.15 Capacitance change of folded beam comb drive designs as a function of the driving voltage squared. At a certain voltage pull-in of the comb structure occurs because of side instability. Design C2-2 and C2-3 employed comb structures with respectively 136 and 105 fingers. Other parameters have already been given in fig. 6.13.
6.4.3 Frequency response

The admittance of the comb actuators was also measured using the HP 4194A impedance analyser. Fig. 6.16 shows the measured and modelled polar admittance plot of a comb drive actuator around the resonance frequency. Neglecting the series resistance $R_s$, which has a small contribution compared to the other admittances, the following parameter values have been extracted from a best fit of the measured response: $C_0 + C_p = 414.5 \text{ fF}$, $C_1 = 145.6 \text{ fF}$, $L_1 = 68.4 \text{ kH}$, $R_1 = 205 \text{ M}\Omega$ and $R_p = 17.6 \text{ G}\Omega$. This gives a resonance frequency $f \approx 1.60 \text{ kHz}$ and a quality factor $Q \approx 3.34$.

![Fig. 6.16 Measured and calculated admittance of comb actuator C2-2, having a beam length of 500 $\mu$m, a beam height of 4.8 $\mu$m, a beam thickness of 2 $\mu$m, a gap spacing of 2.2 $\mu$m and 136 comb fingers. The d.c. polarization voltage was equal to 15 V and the a.c. drive voltage was equal to 0.5V. The dashed line is a theoretical curve using extracted parameter values: $C_0 + C_p = 414.5 \text{ fF}$, $C_1 = 145.6 \text{ fF}$, $L_1 = 68.4 \text{ kH}$, $R_1 = 205 \text{ M}\Omega$, $R_p = 17.6 \text{ G}\Omega$.](image)

6.5 POSITION CONTROL

An important topic for further work is position control. In micropositioning, position feedback is desirable in order to reduce external disturbances and improve system response time and accuracy. In xy-stages, for example, springs are mechanically coupled and deflections in one direction may cause small but unwanted deflections in the orthogonal directions. A first study on feedback
control of comb drive actuators has been done [6.29, 6.30]. The position can easily be obtained by measuring the capacitance of the comb drive itself. The capacitance of the comb-drive can be measured by the superposition of a high frequency measurement signal to the driving signal. The measurement frequency needs to be much higher than the bandwidth of the system. In order to study control aspects, it is convenient to consider the comb drive actuator as a fourth order system in order to obtain information about feedback limitations. The system is generally underdamped; to increase the system damping (stability) it is necessary to use e.g. a P(I)D controller. Important aspects are that single comb drives can only be one-sided controlled and that the maximum driving voltage is limited by side-instability of the comb drive which put constrains on feedback control.

6.6 CONCLUSIONS

A large deflection, low driving voltage comb drive actuator has been designed and fabricated by a one mask fabrication process using boron doped polysilicon as the structural material and silicon dioxide for the sacrificial spacer. The lateral Young’s modulus of the polysilicon layer has been obtained from static displacement-to-voltage measurements and turned out to be approximately 170 GPa. Theoretical lateral spring constants of clamped-clamped, crab-leg and folded beam flexure designs have been determined and were investigated on their large deflection behaviour. Actuator behaviour is limited by electromechanical side instability and expressions for the side-instability voltage and deflection are given. A one-port approach using electrostatic excitation and detection to generate and measure dynamical properties has been used. The electromechanical behaviour around the resonance frequency was described by an equivalent electric circuit. The modelled lateral spring constant of the folded beam design was somewhat larger than experimentally determined values. This is likely to be a result of compliant trusses in our fabricated design in contrast to rigid trusses that have been assumed in our theoretical model, or by a non-rectangular cross section of the beams as a result of underetching. The theoretical axial spring constant of a folded beam which includes a reduction in spring constant with increasing lateral deflection was found to be in good agreement with experimental results in contrast to theoretical spring constants simply determined from Hooke’s law. The electromechanical behaviour can be well described by the presented equivalent circuit which was used to extract the resonance frequency and
Comb-drive actuators for large displacements

quality factor from admittance measurements. An approach towards position control and feedback was briefly discussed.

REFERENCES


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Chapter Seven

AN ELECTROSTATIC LOWER STATOR AXIAL GAP POLYSILICON WOBBLE MOTOR*

This chapter presents the design, modelling, fabrication and first performance characteristics of electrostatically driven axial-gap polysilicon wobble motors. Aspects such as the gear ratio, torque generation, excitation schemes and torque coverage, normal forces, friction, rotor kinetics and dynamical behaviour are addressed. The fabrication is based on a four mask process using polysilicon surface micromachining techniques. Three to twelve stator pole wobble motor designs are realised with rotor radii of 50 and 100 micrometer. Motors are operated successfully at driving voltages of only a few Volts and are capable of generating torque's in the nNm range at high electrostatic fields. The motor performance is characterised by gear ratio measurements and measuring starting and stopping voltages. The motor design and fabrication process are suited to the integration of gear linkages with respect to mechanical power transmission.

7.0 INTRODUCTION

Initial micromechanical motor design and fabrication attempts have been axial-gap architectures because of large estimated driving torques [7.1-7.4]. These designs, however, suffered from instabilities in tilting, to a lesser extent in vertical pertubations, and fabrication complexity that finally led to the development of radial-gap or side-drive micromotors [7.5-7.8]. Salient-pole and wobble side-driven micromotors have successfully been fabricated.

Although the performance of these micromotors is still improving, they do exhibit some drawbacks. The rotor to stator overlap is small, resulting in small driving torque's. Typically a few pNm for salient pole micromotors and a few tens of pNm for wobble motors. This is merely the result of the rotor and stator thickness that is generally only a few microns. Furthermore, it is difficult to implement the radial gap design into a system with respect to mechanical power transmission. In outer stator designs, the stator completely surrounds

the rotor making it difficult to transfer the mechanical energy to another structure when using planar fabrication techniques. While for inner stator designs, it is difficult to make the electrical stator connections [7.9, 7.10].

A number of different side-driven wobble motors have been investigated [7.9-7.13]. Wobble motors are able to generate a larger driving torque's compared to salient pole micromotors because of their gear ratio. The gear ratio increases the driving torque at the cost of a decreasing angular speed. Also, in wobble motors friction is expected to be lower because of a rolling motion instead of sliding.

A lower stator axial gap wobble motor design probably can solve some of the limitations of the radial-gap or side-drive design. This design was first presented by Paratte [7.14-7.16]. The tilting-, vertical- and radial rotor instabilities of this lower stator axial gap design are constrained by the bearing and stator geometry. The larger rotor to stator overlap results in a larger torque generation and successful drive of a gear train has already been realised by a hybrid design based on electroplating and assembling techniques [7.17].

In this paper the design, modelling, a fabrication process based on polysilicon surface micromachining techniques and first performance characteristics of electrostatically driven axial-gap lower-stator polysilicon micromotors is presented. The motor design and fabrication process lend itself for on-chip micromotor driven micro mechanical systems.

### 7.1 OPERATION PRINCIPLE

The motor is sketched in fig. 7.1. The rotor is resting in its centre at a pin- or ball bearing. When a voltage is applied between the rotor and one of the stator poles, the rotor will be pulled down towards a contact point at the angular centre of the excited stator pole.
Switching to other stator poles will move this contact point around and the rotor is forced to roll at its outer radius resulting in a rocking motion.

Because of a difference in radius between the rotor and the resulting contact point circle, the rotor will be rotated by a small angle after one sequential activation of all stator poles. By proper commutation of the charge distribution on the stator electrodes and the rotor, continuous motion of the rotor can be achieved. The ratio between the angular speed of the stator poles and the rotor is called the gear ratio \( n \).

7.2 THEORETICAL MODEL

7.2.1 Gear Ratio

For small rocking angles, in the absence of rotor slip and mechanical deformations, the harmonic reduction ratio of the angular velocity between the stator and rotor is given by the nominal gear ratio. The nominal gear ratio is dependent on motor dimensions and can be found from the ratio of the rotor radius and the difference between the rotor radius and the contact point radius. The radius of the resulting contact point circle \( R_c \) can be found from:

\[
R_c = \sqrt{R^2 + d^2} + (h_b - d) \sin \theta
\]  

where \( R \) is the rotor radius, \( d \) is the axial gap distance at the rotor centre, \( h_b \) is the height of the bearing pin and \( \theta \) is the angle between the rotor and the stator surface which is approximately equal to \( d/R \) (see fig. 7.2).

![Fig. 7.2 Schematic cross sectional view of the rotor.](image)

This yields the following expression for the nominal gear ratio:
\[ n_0 = \frac{R}{R - R_c} \approx \frac{2R^2}{2d h_b - d^2} \quad (7.2) \]

Note that the gear ratio is negative when \( R_c \) is larger than \( R \) which means that the rotor rotates in a direction opposite to the excitation of the stator poles.

### 7.2.2 Torque Generation

The tilt angle \( \theta \) of the rotor is very small. Therefore the electrostatic field can be assumed to be vertical. Furthermore, fringing fields have been neglected and the rotor is assumed to be a rigid disk. An analytical model of the torque generation based on these assumptions has been shown to be in good agreement with FEM simulations for other axial gap motors [7.18]. The axial gap spacing \( g \) between the substrate and the rotor is equal to:

\[ g = \left[ R - r \cos(\varphi - \alpha) \right] \sin \theta \approx d \left[ 1 - \frac{r}{R} \cos(\varphi - \alpha) \right] \quad (7.3) \]

where \( r \) is the radius, \( \varphi \) is a variable for the angle and \( \alpha \) is the contact point angle of the rotor. The electrostatic co-energy \( E'_e \) of an excited stator pole can be found by integration over the excited stator region from angle \( \varphi_1 \) to \( \varphi_2 \) and radius \( R_i \) to \( R_o \) yielding:

\[ E'_e = -\frac{1}{2} CV^2 = -\frac{1}{2} \int_{\varphi_1}^{\varphi_2} \int_{R_i}^{R_o} \frac{\varepsilon_0 r dr d\varphi}{g + \frac{d_{ins}}{\varepsilon_r}} V^2 \quad (7.4) \]

where \( \varepsilon_0 \) is the dielectric constant in air, \( d_{ins} \) is the thickness of the dielectric insulator between the stator and rotor, \( \varepsilon_r \) is the relative dielectric constant of this layer and \( V \) is the applied voltage. In the absence of rotor slip, the torque generated by the motor \( \tau_{motor} \) can be found from the negative derivative of the electrostatic co-energy with respect to the rotor angle \( \alpha \):

\[ \tau_{motor} = \frac{1}{2} \frac{dC}{d\alpha} V^2 \quad (7.5) \]

An example of the single phase torque as function of the rotor angle \( \alpha \) is shown in fig. 7.3 for several stator pole designs. Dimensional values are given in the figure caption. The torque generation can be in the range of nNm at electrostatic fields of \( 10^8 \) V/m. The maximum torque is generated when the
rotor contact point is located at one of the edges of the excited stator pole. The driving torque can be increased by increasing the angular width of the excited stator poles up to $\pi$ radians, by reducing the air gap spacing and the thickness of the dielectric layer between the rotor and the stator, by increasing the relative dielectric constants and by increasing the rotor diameter. The torque scales proportional to the third power with motor radius when the gear ratio and electric field strength are kept constant.

![Graph](image.png)

*Fig. 7.3* Single phase torque of a 3, 4, 6 and 12 stator pole design as function of the rotor angle. The driving voltage is equal to 100 Volts. The rotor radius $R$ is 100 $\mu$m, the axial gap distance $d$ is 2 $\mu$m, the thickness of the dielectric layer $d_{\text{ins}}$ between the stator and rotor is 0.2 $\mu$m and has a relative dielectric constant of 7.5. The stator outer radius is equal to the rotor radius ($R_o=R$) and the inner stator radius $R_i$ is equal to half the rotor radius (50 $\mu$m).

### 7.2.3 Excitation Schemes and Torque Coverage

The stator needs a minimum of three poles in order to generate a driving torque. A stator with two poles with an angle of $\pi$ radians will only result in a rocking motion but does not turn the rotor. To ensure a one directional rotation, a power supply with at least three independent phases is required. Below the independent phases are represented by alphabetical symbols, given in bold capitals when activated, and normal format when deactivated. The total amount of alphabetic symbols equals the number of stator poles. The overall torque generation $\tau_{\text{coverage}}$ for different driving schemes or excitation
patterns can be found from the enveloping curve of excited stator poles (see figs. 7.4 and 7.5). The average torque $\tau_{av}$ can be found from:

$$\tau_{av} = \frac{1}{2\pi} \int_0^{2\pi} \tau_{\text{coverage}} \, d\alpha$$

(7.6)

and the normalised torque ripple $\tau_{\text{ripple}}$ is:

$$\tau_{\text{ripple}} = \frac{\tau_{\text{max}} - \tau_{\text{min}}}{\tau_{av}}$$

(7.7)

The output power of the motor can be found from:

$$P_{\text{motor}} = \tau_{av} \omega_{\text{rotor}}$$

(7.8)

where $\omega_{\text{rotor}}$ is the angular speed of the rotor.

Open loop

The motor is operated in open-loop drive in our experiments. The simplest case for motor operation is a sequential excitation of the different stator poles which is called "single-pole open-loop excitation" (e.g. $\text{A}bcd$, $\text{aB}cd$, $\text{abCd}$, $\text{abcD}$ etc. for a four stator pole design). In this case the rotor rotates till it reaches the zero torque position at the angular center of the excited stator pole (when friction is neglected). Switching to the adjacent stator pole results in an inefficient position with respect to torque generation. This can be improved when using a "double-pole open-loop excitation". In this case two adjacent stator poles are excited simultaneously and in the next step this couple is rotated by one stator pole (e.g. $\text{AB}cd$, $\text{aB}Cd$, $\text{abCD}$, $\text{AbcD}$ etc. ). Now the starting position of the rotor is always at the edge of an excited stator couple where the torque generation is highest. The torque coverage of a four stator pole design in case of single-pole open-loop and double-pole open-loop excitation is shown in fig. 7.4.

Closed loop

Closed loop control requires a feedback of the rotor position in order to switch to the next stator pole at the right moment. Feedback of the rotor position is not implemented at this point. In case of closed loop control the torque ripple decreases with an increasing number of stator poles. However the maximum torque also decreases because the capacitor surface of
Fig. 7.4 Torque coverage in case of open-loop single-pole and double-pole excitation for a four pole stator design with dimensions and variables as given in fig. 7.3. The average torque in case of single and double pole excitation is respectively 305 and 204 pNm.

Fig. 7.5 Torque coverage in case of closed-loop single-pole and double-pole excitation for a four pole stator design with dimensions and variables as given in fig. 7.3. The average torque for the single pole excitation is 313 pNm the average torque for the double pole excitation is 350 pNm.
one stator pole decreases with an increasing number of poles. By simultaneously exciting a group of adjacent stator poles the maximum driving torque can be increased again. The torque generation in case of ”single-pole closed-loop" and "double-pole closed-loop excitation" is shown in fig. 7.5 for a four stator pole design.

These single and grouped pole excitation schemes require a power supply with a number of independent phases that equals the number of stator poles. This may be a problem in case of a large number of stator poles which can be overcome by a parallel connection of stator poles. However, the simultaneous excitation of multiple stator groups will somewhat reduce the torque generation.

7.2.4 Normal Forces and Friction

If the friction torque balances the electrostatic torque $\tau_{\text{motor}}$ generated by the motor, rotor slip is avoided, resulting in a pure rolling motion. In fig. 7.6 forces and torques are shown when a small part $dA$ of the stator is excited.

The axial electrostatic force $dF_{\text{Nel}}$ is balanced by normal reaction forces at the rotor contact point $dF_{\text{Ncontact}}$ and the center bearing $dF_{\text{Nbearing}}$. The electrostatic force induces a torque $d\tau=dF_{\text{Nel}}d\alpha$ that can be divided in a component $d\tau_{\text{ang}}$ in the angular direction and a component $d\tau_{\text{rad}}$ in the radial direction. The normal bearing force $F_{\text{Nbearing}}$ and normal contact point force $F_{\text{Ncontact}}$ can be found from the angular torque balance and the force balance in the normal $z$-direction by integration of the electrostatic forces over the excited region. This
results in the following expressions for the normal force at the contact point and the normal force at the center bearing:

\[
F_{N\text{contact}} = -\frac{1}{2} \varepsilon_0 V^2 \int_{\phi_1}^{\phi_2} \int_{R_i}^{R_o} \frac{r^2 \cos(\phi - \alpha)}{R \left( g + \frac{d_0}{\varepsilon_r} \right)} \, dr \, d\phi \\
(7.9)
\]

\[
F_{N\text{bearing}} = -\frac{1}{2} \varepsilon_0 V^2 \int_{\phi_1}^{\phi_2} \int_{R_i}^{R_o} \frac{r \left[ R - r \cos(\phi - \alpha) \right]}{R \left( g + \frac{d_0}{\varepsilon_r} \right)} \, dr \, d\phi \\
(7.10)
\]

The radial torque \( \tau_{rad} \) can be divided into a component that provides the rocking motion and a component in the z-direction (perpendicular to the stator surface) that provides the driving torque of the motor (see fig. 7.7):

\[
\tau_{\text{motor}} = -\frac{1}{2} \varepsilon_0 \sin \theta V^2 \int_{\phi_1}^{\phi_2} \int_{R_i}^{R_o} \frac{r^2 \sin(\phi - \alpha)}{\left( g + \frac{d_0}{\varepsilon_r} \right)^2} \, dr \, d\phi \\
(7.11)
\]

This expression is equal to eq. (7.5) that was derived earlier. The normal force at the contact point \( F_{N\text{contact}} \) and the normal force at the centerpoint \( F_{N\text{bearing}} \) are shown in fig. 7.8 for a four stator pole design as function of the contact point angle.

Fig. 7.7 Decomposition of radial torque into motor torque and rocking torque.
Sliding and rolling friction results from two bodies that are respectively sliding or rolling against each other. The frictional forces are generally dependent on the normal forces between the two bodies. A pure rolling motion exists when the sliding frictional torque is larger than the electrostatic torque on the rotor. For the motor this no-slip condition is given by:

$$\text{sgn}(\alpha) \left[ F_{\text{contact}} R \mu_{\text{contact}} + F_{\text{bearing}} r_{\text{bearing}} \mu_{\text{bearing}} \right] - \tau_{\text{motor}} \geq 0 \quad (7.12)$$

where $\mu_{\text{contact}}$ and $\mu_{\text{bearing}}$ are the sliding frictional coefficients at respectively the contact point and the bearing.

Fig. 7.8 shows that the normal force at the contact point is very large around the region of the excited stator pole. It even changes sign when the contact point reaches a position at the opposite side of the excited stator pole because the rotor wants to flip over. In this region the frictional torque as a result of the normal force at the contact point will be zero. However, there is still a frictional torque in this region as a result of the normal force at the bearing. Provided the bearing radius and sliding frictional coefficient at the bearing...
are large enough (for example: $R_{\text{bearing}}=10 \, \mu m$ and $\mu_{\text{bearing}}=0.3$) the sliding frictional torque is still in the same range as the motive torque in this region. An appropriate choice of excitation scheme can be used to sustain continuous large normal forces that will prevent rotor slip. For example, an excitation scheme where the step angle is smaller than the totally excited angular region, like in the double pole excitation, will always operate at large normal forces. During normal operation our model suggests that the motor will always work under no-slip conditions.

### 7.2.5 Kinetic behaviour

The motion of a point on the rotor is a superposition of three movements. 1: a rotation of the rotor around its center, 2: a circular translation of the center itself and 3: the rocking motion of the rotor. The position of a fixed point on the rotor can be described by:

$$
\mathbf{r} = 
\begin{bmatrix}
R_M \cos \alpha + r \cos \left( \frac{\alpha}{n} + \varphi \right) \\
R_M \sin \alpha + r \sin \left( \frac{\alpha}{n} + \varphi \right) \\
d \left[ 1 - \cos \left( \alpha \frac{n-1}{n} + \varphi \right) \right]
\end{bmatrix} e_x + 
\begin{bmatrix}
R_M \cos \alpha + r \cos \left( \frac{\alpha}{n} + \varphi \right) \\
R_M \sin \alpha + r \sin \left( \frac{\alpha}{n} + \varphi \right) \\
d \left[ 1 - \cos \left( \alpha \frac{n-1}{n} + \varphi \right) \right]
\end{bmatrix} e_y + 
\begin{bmatrix}
R_M \sin \alpha + r \sin \left( \frac{\alpha}{n} + \varphi \right) \\
R_M \cos \alpha + r \cos \left( \frac{\alpha}{n} + \varphi \right) \\
\end{bmatrix} e_z
$$

(7.13)

where the radius of the center point rotation $R_M$ is given by (see fig. 7.2):

$$
R_M = \frac{h_b d}{R}
$$

(7.14)

The kinetic behaviour is complex and the motion is related to a cycloid. The movement of a point at the outer radius of the motor is shown in fig. 7.9. As a result of the circular translation of the center, a point at the outer radius of the motor even moves in the opposite direction during short time intervals of a wobble cycle. This effect, which is also present in side driven wobble motors, is exaggerated in this figure by using a small gear ratio and may have consequences for certain applications of wobble motors.

### 7.2.6 Dynamic behaviour

The velocity of a point on the rotor can be found from the time derivative of the position. From this, the kinetic energy $T$ of the rotor can be obtained:

$$
T = \frac{\pi}{2} \rho h R_M^2 R^2 \alpha^2 + \frac{\pi}{4} \frac{1}{n^2} \rho h R^4 \alpha^2 + \frac{\pi}{8} \left( \frac{n-1}{n} \right)^2 \rho h d^2 R^2 \alpha^2
$$

(7.15)
Fig. 7.9 3D-, top- and side-view of the movement of a fixed point at the rotor outer radius in arbitrary units. A small gear ratio of about 11 has been used to clearly show the behaviour.
The first term on the right is due to the motion of the center of the rotor. The second term is a result of the rotation of the rotor and the last term is caused by the rocking motion of the rotor. From Hamilton's principle \([7.19]\), using the expressions for the electrostatic co-energy and kinetic energy as given in eqs. (7.4) and (7.15) respectively, the equation of motion for the rotor can be found. When additional terms are added to account for damping mechanisms this yields:

\[
J \alpha + C_v \alpha + \text{sgn}(\alpha)\left(C_{d1} + C_{d2} V^2\right) = \tau_{\text{motive}(\alpha)} \quad (7.16)
\]

where \(J\) is the rotational inertia of the rotor, related to the contact point angle, given by:

\[
J = \pi \rho \ h R^2 \left(R_m^2 + \frac{R^2}{2n^2} + \left(\frac{n-1}{n}\right)^2 \frac{d^2}{4}\right) \quad (7.17)
\]

The rotational inertia of the rotor is mainly determined by its inertia about the rocking axes which is represented by the last term on the right hand side of eq. (7.17). The \(\alpha\) terms in eq. (7.16) have been added to include damping mechanisms. As proposed by Tai [7.20] and Bart [7.21] in case of side-driven micromotors, the next damping mechanisms have been used: \(C_v\) is the coefficient of viscous drag, \(C_{d1}\) is a constant coulombic friction term which results from constant normal forces like adhesion forces and gravity and \(C_{d2}\) is a voltage dependant coulombic friction term resulting from electrostatic normal forces as shown before and \(\text{sgn}(\alpha)\) is the sign function of angular velocity which is +1 if \(\alpha > 0\) and -1 if \(\alpha < 0\). For small oscillations around an equilibrium position, the differential equation (7.16) can be linearised. In that case the natural oscillating frequency of the rotor \(\omega_N\) can be found from:

\[
\omega_N = \sqrt{\frac{\frac{d\tau}{d\alpha}}{J_{\text{tot}}}}_{\alpha=0} \quad (7.18)
\]

### 7.2.7 Viscous Damping

Because of the surrounding gas a damping torque will develop as a result of viscous drag. The viscous drag torque can be found by solving the Navier-Stokes equations. Because of the complex motion of the rotor this problem cannot be solved easily. Furthermore, realised rotor structures exhibit slots as a
result of the fabrication process. A 3-dimensional FEM fluid analysis of the problem is therefore required. To obtain an indication of the drag torque a strongly simplified approximation has been used. The rotor is assumed to be a thin rigid disk and the contributions due to the edges are ignored. The gas is treated as a continuous medium and is assumed to behave as a Newtonian fluid under laminar flow. The viscous drag forces are assumed to be mainly due to squeeze film damping in the small gap between the rotor and the stator surface as a result of the rocking motion of the rotor. With these assumptions the problem simplifies to a thin inclined rigid disk that is rotating over a horizontal surface where only the drag forces in the gap between the disk and the surface are considered. The rotational axis of the disk is perpendicular to the surface and the disk rotates with an angular speed equal to that of the contact point. The shear force $dF_{\text{shear}}$ of a small part $dA$ at the bottom of the disk is given by:

$$dF_{\text{shear}} = \frac{\mu \, \omega \, r}{8} \, dA \quad (7.19)$$

where $\mu$ is the viscosity of air and $\omega$ is the angular speed of the disk. This infinitesimal shear force results in a frictional torque $\tau_{\text{viscous}}$ that can be found by integrating $dF_{\text{shear}} \cdot r$ over the rotor surface:

$$\tau_{\text{viscous}} = \int_0^{R} \int_0^{2\pi} \frac{\mu \, \omega \, r^3}{d \left(1 - \frac{r}{R} \cos \phi \right)} \, dr \, d\phi = \frac{4 \, \pi \, \mu \, R^4 \, \omega}{3 \, d} \quad (7.20)$$

In case of a slider bearing a comparable approximation was found to be somewhat lower but accurate within a factor of two with a solution from the Navier-Stokes equations [7.22]. Therefore, the expression above is considered to be a reasonable estimate of the viscous drag torque on the rotor.

### 7.3 Fabrication

Micromotor fabrication is based on a four mask process using polysilicon surface micromachining techniques which is schematically shown in fig. 7.10. The fabrication starts with a (100) p-type 3” silicon wafer. The first step is the deposition of a 1 µm thick stress reduced silicongrnitride layer ($\text{Si}_x\text{N}_y$) by LPCVD
from a 70 sccm SiCl$_2$H$_2$ and a 18 sccm NH$_3$ flow at 850 °C and a pressure of 200 mTorr. The next step is the deposition of a 0.5 µm thick polysilicon layer, grown by LPCVD at a temperature of 590 °C, a pressure of 250 mTorr and a silane flow of 50 sccm. This polysilicon layer is heavily doped with boron by solid source drive-in diffusion for one hour at 1150 °C. This yields a sheet resistance of about 70 Ω/□. After boron diffusion the BSG layer is stripped in a buffered HF-solution. After patterning the doped polysilicon layer forms the stator poles (fig. 7.10 a).

Again a 0.5 µm thick stress reduced LPCVD silicon nitride layer is deposited that serves as an insulator between the stator and rotor. Contact windows are opened in this Si$_x$N$_y$ layer by RIE in a CHF$_3$/O$_2$ gas mixture in order to make contact with the stator poles later on (fig. 7.10 b).

Now a 2 µm SiO$_2$ layer is grown by PECVD from a SiH$_4$/N$_2$O gas mixture at 300 °C, a pressure of 650 mTorr and RF power of 60 Watt (fig. 7.10 c). This layer defines the gap between the rotor and stator. The ball bearing is now formed by one lithography step and dry etching of the SiO$_2$, the Si$_x$N$_y$ sandwich layer and dry isotropic underetching of the silicon wafer. This is done by RIE the SiO$_2$ layer using CHF$_3$ at a pressure of 20 mTorr and a RF power of 50 Watt and the Si$_x$N$_y$ layer by RIE using a CHF$_3$/O$_2$ gas mixture at a pressure of 10 mTorr and a RF power of 75 Watt. The resist layer is removed by O$_2$ plasma ashing and the silicon is underetched by dry isotropic etching in a SF$_6$/N$_2$ gas mixture at 100 mTorr and 50 Watt (fig. 7.10 d). Next a 1 µm thick SiO$_2$ layer is deposited by LPCVD from TEOS at 700 °C and a pressure of 400 mTorr (fig. 7.10e). The bearing clearance is defined by this layer.

Now the rotor is constructed. This starts with the deposition of a 2 µm thick LPCVD polySi layer that is also doped by diffusion as previously described. The anneal step also reduces the residual stress of the polySi layer. After stripping the BSG layer in BHF a sheet resistivity of about 6 Ω/□ is obtained. The cross section of a ball bearing like groove at this point is shown in fig. 11.

The motor operation requires a rigid rotor design with respect to vertical deflections in order to prevent rotor deformation by axial electrostatic forces and stiction problems between the rotor and stator surface. In order to increase the stiffness of the rotor a 6 µm thick amorphous silicon layer is deposited by sputtering in Ar. This layer is annealed in a N$_2$ atmosphere at 450 °C for 1 hour to reduce residual stress [7.23]. A 0.6 µm thick PECVD silicon oxide layer is deposited that serves as an etch mask for the silicon sandwich layer. After patterning the silicon oxide by RIE using CHF$_3$ gas, the polysilicon is anisotropically etched using a SF$_6$, O$_2$, CHF$_3$ gas mixture (fig. 7.10f) [7.24].
Fig. 7.10 Processing sequence of the wobble motor. More details are given in the text. (a) LPCVD of SiN, LPCVD and etching of polySi, (b), LPCVD of SiN and contact window etching (c), PECVD of SiO$_2$ (d), RIE of SiO$_2$ and SiN and plasma etching of silicon wafer (e), LPCVD of SiO$_2$ (f), LPCVD of polySi, sputtering of Si and anisotropic etching (g) Sacrificial layer etching and backside metallization.
**Fig. 7.11** SEM photograph of the cross section of a ball bearing like groove. The good step coverage of the TEOS silicon dioxide- and polysilicon layer is clearly visible.

**Fig. 7.12** SEM photograph of an electrostatic axial gap wobble motor with four stator poles. The rotor diameter is 200 µm and rotor thickness is 8 µm.
The accumulated layers at the backside are stripped by dry etching, followed by a standard cleaning procedure. The sacrificial layers are etched in an HF (50%) solution for 37.5 min., DI rinsed and spin dried. The last step is the evaporation of a 1 µm Al backside metallization layer (fig. 7.10g). The final result is shown in fig. 7.12.

A suitable center pin bearing for the rotor is the flange bearing design [7.25]. This, however, requires lithography over the patterned rotor structure. In order to prevent problems with photoresist step coverage a ball bearing design has been used. The ball bearing is, however, not self-aligned like the flange bearing design. But alignment within 1 µm should be possible and is not considered to affect the motor performance strongly. At this point no experimental or theoretical optimisation in order to minimise the rotor thickness has been done. For sufficiently thin rotors or good photoresist step coverage a flange bearing design for these type of motors can also be realised. Note that the ball bearing design can also be realised with LIGA or other moulding and electroplating techniques.

7.4 EXPERIMENTAL

In order to verify micromotor static and dynamic models, it is necessary to measure the torque and rotor transient response as it moves from one stator pole to the next. Unfortunately, measurement of the generated torque and detailed experimental measurement of rotor step transient is difficult for wobble micromotors, since torque measurements require mechanical power transmission and the rotor displacement associated with a step transient is very small. Instead the characteristic gear ratio of a wobble motor can be easily measured [7.25-7.29]. The gear ratio of a wobble motor is defined as the ratio of the electrical excitation frequency to the rotor rotational frequency. Starting and stopping voltages can also be determined from which frictional torque's can be extracted. Starting voltages are measured by increasing the driving voltage and observing at what voltage a motor starts to rotate. Similarly, the stopping voltages are measured by decreasing the driving voltage and observing at what voltage the motor operation fails. The motor lifetime was defined as the time to failure in which a motor could be operated at a fixed driving voltage.

To operate the electrostatic motors a programmable power supply which controls the driving schemes has been realised. The driving frequency of the
square wave signal of four independent output phases can be varied between 15 Hz and 10 kHz using the internal clock generator. Higher driving frequencies can be obtained by using an external clock generator. The amplitude of the driving voltage can be set between 0 and 100 V. The power supply is connected to the bonding pads of the stator poles using a probe station. The movements of the motor are recorded using a microscopic set-up that includes a camera and video recorder. Rotational speeds can easily be determined by video replay, from which the gear ratio is calculated. Single pole excitation schemes have been used in the measurements unless otherwise stated. Stator designs with a larger number of poles than the number of power supply phases are driven by symmetrically skipping the additional stator poles. Measurements have been performed in air under semi-cleanroom conditions after storage periods of up to several months.

7.5 RESULTS AND DISCUSSION

Table 7.1 shows measured starting and stopping voltages for motors with radii of 100 and 50 µm and four stator poles using a single-pole open-loop excitation at a frequency of 100 Hz. The effect of the driving frequency, the driving voltage and operating time on the gear ratio of the wobble motors has been measured. The results are shown in figs. 7.13 to 7.15. Experimentally determined gear ratios varied between 130 and 200 for the 50 µm rotor radius designs, and between 600 and 1000 for the 100 µm rotor radius designs. The large variation is due to the high sensitivity of the gear ratio to variations in the rotor radius as a result of underetching, to variations in the gap spacing and to changes in the bearing height because of etch and thin film non-uniformities.

7.5.1 Starting and stopping voltages

The static and dynamic frictional torque of the motor can be derived from the measured starting and stopping voltages (see table 7.1). The gap spacing of these samples was measured to be 2.3 µm, and the thickness of the silicon nitride layer was 0.46 µm. The motor will stop when the frictional torque equals the generated torque. The smallest torque will occur at the transition point of the individually excited stator poles. For the four stator pole design this transition point is located at approximately -0.2 radians from the angular center of the stator poles. From this the static friction is calculated to be equal to
<table>
<thead>
<tr>
<th>Sample #</th>
<th>Radius $[\mu m]$</th>
<th>Starting Voltage $[V]$</th>
<th>Stopping Voltage $[V]$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>100</td>
<td>6.0</td>
<td>4.4</td>
</tr>
<tr>
<td>2</td>
<td>100</td>
<td>8.1</td>
<td>5.9</td>
</tr>
<tr>
<td>3</td>
<td>100</td>
<td>8.1</td>
<td>5.1</td>
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*Table 7.1* Measured starting and stopping voltages of several motors with a four pole stator design and rotor diameters of 50 and 100 $\mu$m. A single-pole open-loop excitation with a square-wave voltage signal at 100 Hz was used.

![Graph showing gear ratio as a function of excitation frequency](image)

*Fig. 7.13* Gear ratio as a function of the excitation frequency, measured for three different micromotors.
**Fig. 7.14** Measured gear ratio as a function of the driving voltage for two samples. Data has been fitted by the formula given in eq. (7.21).

**Fig. 7.15** Gear ratio versus operation time in millions of wobble cycles for two motors. Both motors have been operated at 15 Volts with a driving frequency of 500 Hz and 100 Hz for respectively sample F and G. Total operation time until motor failure was 3.5 hours for sample F and 6.5 hours for sample G.
0.71 and 0.48 pNm for the four stator pole motors, with rotor radii of respectively 50 and 100 micron. In the same way the average torque to overcome the dynamic friction is equal to 0.43 and 0.25 pNm for the small and large four stator pole motors respectively. The larger motors have smaller frictional torque's in contrast to their larger dimensions. This is a somewhat unexpected result. A possible reason may be the larger rocking angle of the small motors that leads to more friction at the ball bearing. The individual frictional forces at the contact point and the bearing of the rotors cannot, therefore, be extracted from these measurements. Rotor designs with identical rotor diameters but different bearings radii are expected to be more suited for extracting the frictional forces at the contact point and the bearing. The measured frictional torques are somewhat lower compared to their side-driven counterpart [7.25].

7.5.2 Frequency dependency of the gear ratio

The gear ratio as a function of the driving frequency has been measured for three different motors and is shown in fig. 7.13. Measurements have been performed up to a driving frequency of 10 kHz which is the maximum frequency of the internal clock generator in our power supply. At 10 kHz the angular speed of four pole stator motor designs with a radius of 100 µm is equal to about 150 rpm. Furthermore, gear ratios at higher frequencies are difficult to determine by our video system. In this case a stroboscope has to be implemented in our experimental set-up.

Some change in the gear ratio of the motors is present when the driving frequency is varied. However the behaviour is not the same for the different motors and the variations show little correlation with the driving frequency. The overall picture seems to be that the gear ratio is more or less independent of the driving frequency up to a frequency of 10 kHz.

7.5.3 Voltage dependency of the gear ratio

The voltage dependency of the gear ratio is shown in fig. 7.14. The gear ratio increases with increasing driving voltage. This behaviour is opposite to the behaviour of side driven wobble motors where the gear ratio decreases with increasing driving voltage as a result of a reduction in slip. It was observed that the gear ratio decreased somewhat when, instead of a double pole excitation, a single pole excitation scheme was used. As rotor slip leads to an increase in the gear ratio and double pole excitation will always operate at large normal forces
which exclude rotor slip, another effect must be responsible. Furthermore, our model predicts that the motors will normally operate under no-slip conditions. It is suggested that the axial electrostatic field will result in a deformation of the rotor structure. As illustrated in fig. 7.16 this will result in a small decrease of the contact point radius which results in an increase of the gear ratio. Motors that are excited over a larger region will exhibit a larger deformation explaining the increase in gear ratio when double pole excitation is used.

![Fig. 7.16 Sketch of rotor deformation by axial electrostatic forces. The rotor deformation results in a decrease of the contact point radius.](image)

The axial electrostatic forces are quadratically dependent on the applied voltage. Therefore the measured dependency has been fitted by the following equation:

\[ n = n_0 + \gamma V^2 \]  

(7.21)

As shown in fig. 7.14 the experimental data can be fitted well by this equation. A change in gear ratio from 800 to 1500 is related to a change in the contact point radius of only 0.05 %. Additional measurements on rotors with different stiffnesses or coupled electromechanical simulations of the rotor deformation are necessary to verify our suggestion.

7.5.4 Time dependence of the gear ratio

The gear ratio-to-operation time dependency of two motors is shown in fig. 7.15. The gear ratio first increases, then decreases and then remains constant with increasing operation time until motor failure occurs. The initial increase and subsequent decrease in the gear ratio with operation time is expected to result from wear at the ball bearing and the surfaces at the contact circle and is caused by a run-in period. At the end of this period when the surface asperities
have worn off, the gear ratio remains constant till it stops rotating at the set voltage. The change in gear ratio, as a result of wear, can be expressed by:

\[
n \approx n_0 \left[ 1 + \frac{2 h_b \Delta h_b}{d^2 - 2 d h_b} - \frac{2 (h_b - d) \Delta d}{d^2 - 2 d h_b} \right]
\]  

(7.22)

where \( \Delta h_b \) is the change in bearing length as a result of wear and \( \Delta d \) is the wear between the stator surface and the rotor surface at the contact point. Wear at the ball bearing will result in a reduction of the bearing length \( \Delta h_b \) and a subsequent change in the axial gap distance. From eq. (7.22) it can be seen that this will lead to an increase of the gear ratio. Wear at the contact point circle, from surface roughness asperities at the bottom of the rotor and the stator surface, will result in an increase of the gap spacing with \( \Delta d \). This results in a decrease of the gear ratio.

The time to motor failure showed large variations for the two process runs that have been done up to now and ranged from several ten thousands up to several millions of wobble cycles for different samples. As in side-driven wobble motors motor failure may be caused by wear particles [7.29]. At this point no SEM inspection of operated motors has been done. More work on wear is needed and other bearing designs, as well as other bearing materials, may lead to extended motor lifetime.

7.5.5 Dynamic behaviour

Measurements on the transient response of the rotor have not been performed. However, from measured and estimated parameters a theoretical prediction of the transient response can be given by using the equation of motion given in eq. (7.16). The rotational inertia for a rotor with a radius of 100 \( \mu \)m is calculated using eq. (7.17) from which a value of \( 8 \times 10^{-22} \) kgm\(^2\) is obtained. For an air viscosity of \( 1.83 \times 10^{-5} \) kg/ms, the viscous damping constant \( C_v \) is estimated by eq. (7.20) to be about \( 3 \times 10^{-15} \) Nms. Measurements on side driven wobble motors showed that coulombic friction is dominated by the voltage dependant term (i.e. \( C_{d1} \times V^2 \)). From the measured dynamic frictional torque the value of \( C_{d2} \) is calculated to be \( 9 \times 10^{-15} \) Nm/V\(^2\) for the micromotors with a radius of 100 \( \mu \)m. The generated torque can be calculated for a given driving voltage from eq. (7.11). For ease of numerical computation, the function \( \pi/2 \arctan(\alpha/10^{-4}) \) has been used in place of \( \text{sgn}(\alpha) \). The transient
response of a four stator pole motor in case of a single-pole excitation is shown in fig. 7.17.

The response time is strongly dependent on the driving voltage. Because of coulombic friction the rotor does not reach the zero torque position of the excited stator pole but stops at an angle that is dependent on the driving torque. For decreasing driving voltages this stopping angle approaches about -0.2 radians as discussed before in the section on starting and stopping voltages.

![Graph showing transient response](image-url)

**Fig. 7.17** Transient response of a four stator pole design with single pole excitation for a driving voltage of 10, 30 and 100 Volts. Starting angle is $-\pi/2$ radians. $R = 100 \, \mu m$, $R_1 = 50 \, \mu m$, $R_0 = 100 \, \mu m$, $d = 2.3 \, \mu m$, $d_{ins} = 0.46 \, \mu m$, $\varepsilon_r = 7.5$, $\phi_1 = -\pi/4 \, rad$, $\phi_2 = \pi/4 \, rad$, $J = 2.0 \times 10^{-21} \, kgm/s^2$, $C_v = 4.1 \times 10^{-18} \, Nms$, $C_{d1} = 0 \, Nm$ and $C_{d2} = 9 \times 10^{-15} \, Nm/V^2$.

### 7.6 CONCLUSIONS

The design and fabrication of an electrostatically driven lower-stator axial-gap wobble motor has been presented. In contrast to side drive motors, the stator poles are located underneath the rotor instead of surrounding the rotor sides. This results in a higher torque generation and easily accessible rotor structures that are suited to mechanical power transmission to other structures that can be fabricated on the same chip.
Based on small tilt angles, a rigid disk rotor and simplified electrostatic fields, a theoretical model describing the static and dynamic behaviour of the motor has been given. The torque generation is in the range of nNm at high electrostatic fields. The torque coverage for different excitation schemes in the case of both open-loop excitation and closed-loop excitation, which requires position feedback, is discussed. Because of large normal forces, generated by the axial electrostatic field, the motors can be easily operated in no-slip conditions at all times. The kinetic behaviour is complex and related to a cycloid. A fixed point at the rotor even moves opposite to the rotational direction of the rotor during small time intervals of a wobble cycle. This may have consequences for certain applications. Based on side-driven micromotor results an equation of motion for the rotor has been derived that includes rotational rotor inertia, viscous drag and coulombic frictional terms. The rotational inertia of the rotor is mainly determined by its inertia around the rocking axes.

Fabrication is based on a four mask process using polysilicon surface micromachining techniques. Silicon nitride has been used for electrical insulation between the rotor and stator poles and silicon oxides were used as sacrificial layers that have been removed in an HF solution. The rotor and stator poles have been constructed from doped polysilicon. In order to increase the stiffness of the rotor, it is made from polysilicon and a thick sputtered amorphous silicon film. A new ball bearing design, which is not self-aligned, has been used for the rotor in order to avoid photoresist step coverage problems.

Motors have been successfully operated at driving voltages of a few Volts. Motor performance has been characterised by measuring the gear ratio and start and stop voltage measurements using single pole, open-loop excitations with square-wave voltage signals. Although some variation in gear data has been observed for different motors, the gear ratio seems to be independent of the driving frequency up to a maximum measured frequency of 10 kHz. The gear ratio was found to be strongly dependent on the driving voltage. It is suggested that this is caused by the mechanical deformation of the rotor that results from the axial electrostatic forces. The gear ratio was also found to be dependent on operation time showing an initial increase and decrease of the gear ratio after which it remained constant until motor failure. This behaviour is suggested to result from wear at the ball bearing and the surfaces at the contact point circle. Motor lifetimes varied between a few ten thousand wobble cycles to some millions of wobble cycles for two different process runs. This resembles operation times ranging from a few minutes to several hours at
rotor speeds between a few and several hundred rpm. A theoretical prediction of the transient response of the motor has been given using measured and estimated calculated parameters in the equation of motion.

The presented theoretical and experimental results can be used as a framework for further development of axial-gap micromotors. Wear and motor lifetime should be especially investigated as well as other bearing designs and bearing materials in order to improve motor performance.

**REFERENCES**


In this chapter a surface micromachining process is presented which has been used to fabricate electrostatic microactuators that are interconnected with each other and linked to other movable microstructures by integrated gear linkages. The gear linkages consist of rotational and linear gear structures and the electrostatic microactuators include curved electrode actuators, comb drive actuators and axial gap wobble motors. The micromechanical structures are constructed from polysilicon. Silicon dioxide was used as a sacrificial layer and silicon nitride was used for electrical insulation. A cyclohexane freeze drying technique was used to prevent problems with stiction. The actuators, loaded with various mechanisms, were successfully driven by electrostatic actuation. The work is a first step towards mechanical power transmission in micromechanical systems.

8.0 INTRODUCTION

With improving performance and fabrication techniques, micromechanical actuators are merging into the field of mechanical power transmission for driving purposes. It has been previously shown that multi-level linkages can be fabricated by dissolved wafer, bond and assembly techniques [8.1]. Two silicon wafers are successively processed, aligned and bonded to a glass wafer, and then dissolved in EDP to free micromechanical mechanisms. Other examples of assembled devices that transfer mechanical power are magnetic micromotors where Permalloy and PMMA parts are fabricated with sacrificial LIGA techniques which are subsequently assembled with submicron tolerances [8.2]. Axial gap wobble motor have also been fabricated from electroplated nickel rotors that were coupled to miniature pinion and gear trains using assembling techniques [8.3].

Devices where mechanical linkages have been integrated with actuator

fabrication are vibromotors, where a slider is driven by oblique impact of resonant comb structures [8.4], and a comb-drive based microengine where the linear motion of comb structures is converted into a rotary motion of an output gear by connecting rods [8.5]. These latter processes have the advantage of batch fabrication without the need for assembly of multiple wafers requiring alignments, bonding etc., or the addition of other separately fabricated parts.

In this paper a fabrication process is presented where different types of surface micromachined electrostatic actuators are fabricated in one process. These are interconnected and linked to other microstructures using gear mechanisms without the need for assembling techniques. In our previous work several electrostatic actuators that can generate relatively large forces, torques and displacements have been developed and fabricated using surface micromachining techniques. Examples are bi-stable curved electrode actuators [8.6], linear comb drive actuators [8.7] and axial gap wobble motors [8.8]. Mechanisms include gear trains, linear gear racks, sliders and spring structures. First experimental results have shown operational actuators and functional micromechanisms. For example electrically powered micromotors drive gear trains and gear racks. In the latter case a transformation of rotational into linear motion is obtained.

The fabrication process can be used to study and optimise properties of micromechanical gear mechanisms and measure output force or torque of electrostatic actuators. Examples are devices for measurements on static and dynamic friction of gear linkages and measurement of micromotor output torque.

8.1 DESIGN ISSUES
8.1.1 General design issues

Since the fabrication of the comb drive actuators and curved electrode actuators is relatively simple and can be done in a one mask fabrication process, [8.6, 8.7] this process has been merged into the fabrication process of the axial gap wobble motor [8.8]. The micromechanical structures are constructed from polysilicon. Silicon dioxide has been used as a sacrificial layer and silicon nitride was used for electrical insulation. A cyclohexane freeze drying technique is used to prevent problems with stiction.

The operation principle of a lower stator axial gap wobble motor is based on
an inclined rotor that is supported at its center and rolling on its outer radius. Excitation of stator poles results in an axial rocking motion of the rotor. This rocking motion is transformed into a rotational motion because of a small difference in the radius of the rotor and its resultant contact point circle at the stator poles. In contrast to polysilicon side drive motors, where stator poles are surrounding the rotor, the stator poles of the axial gap wobble motor are located underneath the rotor. This results in a large torque generation because of a large rotor to stator overlap and a readily accessible rotor for mechanical power take off. For a more detailed description of the axial gap wobble motors reference is made to [8.8].

Because the rotor of the axial-gap wobble motor makes an axial rocking motion and is rolling on its outer radius, some adjustments are necessary when external gear teeth are added. By an additional etch step a circular reduction is created in the rotor structure. Now, the rotor will roll on this circular reduction and can have an arbitrary outline. The rotor and gears can rotate and rock in axial directions using a ball bearing while for linear movements a slider bearing was applied using slots that are etched into the silicon wafer. The motor rocking motion places a constraint on the thickness of the gears and therefore the gear thickness should be larger than two times the thickness of the sacrificial layer below it. In our fabrication process a sacrificial layer thickness of about 2 \( \mu m \) and a gear thickness of about 6 \( \mu m \) was used.

### 8.1.2 Tooth design

Gears are an efficient way to transmit mechanical power at almost any speed ratio desired. Parallel axis gears transmit power with greater efficiency than any other form of gearing. The most common type of gear is the spur gear which has teeth on the outside of a cylinder which are parallel to the cylinder axis. This type of gear can be fabricated relatively easy using anisotropic etching techniques.

A conjugate tooth shape is needed in order to transform motion with constant angular velocity. Involute and cycloidal tooth shapes are conjugate tooth profiles. However, cycloid gearing requires center distances to be maintained in order to obtain conjugate action, where involute profiles maintain conjugate action even with variations in gear center distances [8.9].

The gear ratio is a constant and can be found from the ratio of gear teeth numbers. It can be applied to all types of gears with conjugate and non-conjugate gear tooth surfaces and corresponds to the ratio between the gear
revolutions. Only in cases of conjugate tooth profiles is the velocity ratio equal to the gear ratio. If not, the gears transform rotation with a varying instantaneous velocity ratio.

In this first approach no optimisation has been done on gear design. Because of limitations in our present mask layout program and pattern generator, gear teeth with a trapezoidal shape have been used. This will negatively affect the performance of our gears. However, at this stage the main goal is the development of a fabrication process for integrated electrostatic actuators and gear linkages; and to demonstrate the feasibility of these micromechanical gear linkages in order to transfer mechanical power.

In our design two different gears have been used. The largest gear has 36 teeth and a base radius of 100 µm which also forms the rotor of electrostatic axial gap wobble motors. Smaller gears have 18 teeth and a base radius of 50 µm. The total depth of the gear teeth is 11 µm, the thickness is 6 µm and a pressure angle of 20 degrees has been used. In order to ensure proper patterning and etching a minimum clearance of 2 µm between engaged gear teeth and a bearing clearance of 1 µm has been used. As a result, gear backlash will be relatively large.

8.2 FRICTION AND WEAR

Although some work has been done on aspects like friction, wear and lubrication of micromechanisms [8.10-8.19], many issues with regard to tribological properties, design and operation of microsystems are unexplored and need to be investigated. Frictional effects should be minimised by a proper bearing design and a suitable gear tooth profile set resulting in a lateral, pure rolling motion between gear teeth. The use of low friction materials [8.15, 8.17, 8.18] and lubricants [8.20-8.23] seems to be promising applications. However, one should bear in mind that the operation of axial gap wobble motors is based upon friction between the rotor and stator surface.

The lifetime of gears and micromotors is limited by wear [8.8, 8.10, 8.11]. Friction and wear studies using specimen-on disc [8.15, 8.19] have been performed and indicate that diamond-like carbon is an attractive material with respect to friction and wear properties. The wear mechanism of brittle materials like DLC, SiO₂, Si₃N₄ and SCS was found to be dominated by asperity fracture, and wear of polysilicon is dominated by asperity deformation [8.19]. In general wear has been found to follow macroscopic theory, where
materials of highest hardness show the lowest wear rates, and wear rate is dependent on contact pressure.

A problem related to the axial gap wobble motor, is that the rocking motion of the rotor gives rise to axial motions between the rotor teeth and the teeth of the gear structure that is driven. This will induce additional frictional forces. The normal force at the tooth surfaces is roughly equal to the excess motor torque divided by the rotor radius. The torque of a motor with a radius of 100 µm can theoretically be in the range of nNm, at high electrostatic fields [8.8], leading to normal forces in the 10 µN range. The axial frictional force resulting from the rocking motion is a fraction of this normal force after multiplication by the frictional coefficient of the materials in contact (e.g. 0.3-0.5). This axial frictional force is small compared to the axial electrostatic forces of the motor which are in the range of mN. Therefore friction between gears as a result of the rocking motion is not expected to strongly affect motor torque and performance.

8.3 FABRICATION PROCESS

The fabrication of electrostatic axial gap micromotors, comb drive actuators and curved electrode actuators together with integrated gear mechanisms is based on a seven mask process using polysilicon surface micromachining techniques. The process steps are illustrated in fig. 8.1. Starting material is a (100) p-type 3" silicon wafer. First a 1 µm thick stress reduced silicon nitride layer is deposited by LPCVD. This is followed by the deposition of a 0.5 µm thick LPCVD polysilicon layer. This polysilicon layer is subsequently doped with boron by solid source indiffusion for one hour at 1100 °C. After boron indiffusion the BSG layer is stripped in a buffered HF-solution and the polysilicon is patterned to form the stator poles of the motor and ground planes of electrostatic actuators (mask #1, fig. 8.1a). Now a second stress reduced LPCVD silicon nitride layer with a thickness of 0.5 µm is deposited which serves as an insulation layer. In this Si_xN_y layer, contact windows are etched by RIE in a CHF_3/O_2 gas mixture in order to make electrical contact to the stator poles and ground planes (mask #2, fig. 8.1b). Next a SiO_2 layer is grown by PECVD with a thickness of 2 µm which defines the air gap spacing between the stator and the rotor of the micromotor and partly defines the sacrificial layer of the structures (fig. 8.1c). Local reductions are etched in this
Fig. 8.1 (a), (b), (c), (d), (e), (f), (g), (h), (i) Processing sequence for electrostatic actuators with integrated gear linkages.
SiO₂ layer using a BHF solution to define the circular region on which the rotors will roll (mask #3, fig. 8.1d).

The ball bearings and slider slots are formed by RIE etching of the SiO₂ layer using a CHF₃ plasma, RIE etching of the SiₓNᵧ layers using a CHF₃/O₂ gas mixture and dry isotropic underetching of the silicon wafer in a SF₆/N₂ gas mixture (mask #4, fig. 8.1e). After this a SiO₂ layer with conformal step coverage is grown by LPCVD from TEOS to define the bearing spacing of the rotor structures (fig. 8.1f). In the silicon dioxide sacrificial layer anchors for the electrostatic actuators are patterned by etching in BHF (mask #5, fig. 8.1g). Now a 6 µm thick LPCVD polysilicon layer is deposited that is also doped by boron indiffusion as described before. The boron indiffusion also reduces the residual stress of the polysilicon layer. After stripping the BSG layer in BHF a resistivity of about 3 Ω square is obtained. A 1 µm thick PECVD silicon oxide layer is grown that serves as an etch mask for the polysilicon layer and prevents boron outdiffusion in the subsequent annealing step at 1100 °C for 3 hours in order to reduce stress gradients as a result of the one sided diffusion step. Backside layers are stripped by dry etching. The silicon oxide is patterned by RIE using CHF₃ gas and the polysilicon is anisotropically etched using a SF₆, O₂, CHF₃ gas mixture [8.16] (mask #6, fig. 8.1h). In order to remove the polysilicon from the slots in which the sliders have to move, an isotropic etch step in a SF₆/N₂ gas mixture is needed in addition to the anisotropic etch while other areas are protected by photoresist (mask #7). After a thorough cleaning procedure the sacrificial layers are etched in concentrated HF for 50 min. This is followed by dilution rinsing in DI water, rinsing in isopropanol and rinsing in cyclohexane while preventing the wafers from drying. Now freeze drying is used to remove the cyclohexane at a temperature of -10 °C under a high N₂ flow in order to prevent stiction problems [8.17]. The last step is evaporation of a 1 µm thick aluminum backside layer (fig. 8.1i). The final result is shown in fig. 8.2 to 8.10.

8.4 EXPERIMENTAL RESULTS

First experiments have shown that the micromotors can successfully drive gear trains and slider gear racks resulting in, respectively, torque leverage, and rotational to linear transformation. Up to now no integrated micromechanisms that are driven by electrostatic micromotors have been reported.
Fig. 8.2 SEM photograph of an axial gap wobble motor with gear teeth driving a smaller gear wheel. The rotor and the smaller gear have a base radius of respectively 100 μm and 50 μm.

Fig. 8.3 Close-up of the gear teeth. The gear teeth have a thickness of 6 μm, a depth of 11 μm and a diametrical pitch of 18.5 μm.
Fig. 8.4 SEM photograph of an axial gap wobble motor connected to a gear rack resulting in transformation of the rotational into linear motion.

Fig. 8.5 Close-up of the gear rack. The slider is able to move on its bearing along slots that are etched into the wafer.
Fig. 8.6 Cross section of a linear slider showing the slots and bearing that guide its motion.

Fig. 8.7 Curved electrode actuator for fixing gear position.
Fig. 8.8 Motors linked to a gear rack suspended by folded flexures of comb drive actuators. Such structures could be used to measure output torque and motor dynamics.

Fig. 8.9 Close up view of comb drive structure connected to axial gap wobble motors from fig. 8.8.
Fig. 8.10 XY-stage fabricated in the same process run.

Fig. 8.11 Close up of the center of the XY stage from fig. 8.10.
Comb drive structures and curved electrode actuators in conjunction with micromechanical mechanisms have also been driven electrostatically.

Variations in operation characteristics of gears have been observed which are expected to result from the non-conjugate gear teeth profile that has been used. Problems such as gear backlash and impact were also noticed which were caused by etching- and bearing clearances. Furthermore frictional problems were encountered with large slider structures (up to 2 mm long). Long sliders could be moved manually by probe needles but did not slide by electrical powering micromotors. It is suggested that stress gradients clamp long slider structures in their bearing slots resulting in frictional forces which are larger than the output force of the motors. Further experimental work is needed to obtain more quantitative results.

8.5 APPLICATIONS

Specially designed devices with gear linkages can be used to investigate tribological properties and system design aspects of micromechanical gear systems. The fabricated devices will be used to explore essential features of these issues.

For instance, measurements on gear trains that are driven by comb drive structures using a gear rack can be used to obtain information on frictional properties of the gear teeth and bearing. In the same way the frictional properties of sliders can be investigated. Gear linkages can also be used to determine the torque output of electrostatic micromotors by, for example, loading the motor with a spring and measuring the deflection to voltage characteristic.

Future applications of integrated actuator systems can be found in areas like micropositioning, micromanipulation (i.e. pick and place), scanning probe microscopy, information storage and micro robotics.

8.6 CONCLUSIONS

A micromachining process for the fabrication of electrostatic microactuators which are linked with each other and connected to other movable microstructures by integrated gear linkages has been presented. The fabrication is based on polysilicon surface micromachining and sacrificial layer etching
techniques. First experimental results show that electrically powered actuators successfully drive various micromechanisms. The work is a first step towards mechanical power transmission in micromechanical systems. Mechanical power transmission of microactuators may strongly increase the number of useful applications and may lead to new possibilities for microelectromechanical systems.

REFERENCES


References


Chapter Nine

ELECTROSTATICALLY DRIVEN VACUUM ENCAPSULATED POLYSILICON RESONATORS*

Basic design issues and a fabrication process based on surface micromachining techniques for electrostatically driven vacuum encapsulated polysilicon resonators are presented. A novel freeze drying method, that does not require vacuum equipment, is presented. Reactive sealing with LPCVD silicon nitride is used to create the evacuated cavity, resulting in cavity pressures close to the deposition pressure. Design issues regarding choice of materials, technology and lay-out are discussed. First experimental results, including an admittance plot of the one-port resonator and a plot indicating the dependence of the $Q$-factor on the resonator geometry and ambient pressure are presented.

9.0 INTRODUCTION

Resonant sensors are very attractive for the precise measurement of mechanical quantities such as pressure and force. They offer high sensitivity and resolution together with a semi-digital output, i.e., a frequency [9.1-9.4]. The central part of the sensor is a vibrating mechanical element called the resonator, which provides the frequency(shift) output.

Surface micromachined resonators, that are operated in a flexural mode of vibration, have been used by several research groups in the past [9.5-9.7]. Without taking special precautions, these resonators suffer from small signal responses as a result of a low mechanical quality factor. Lowering of the quality factor is a direct consequence of the introduction of a second surface in close proximity to the resonator surface. This leads to so-called "squeeze film" damping which can be observed for the normal vibrational motion of two closely spaced surfaces [9.5, 9.8]. Squeeze film damping becomes significant if the gap spacing is small compared to the lateral dimensions of the resonator. Evacuating the surroundings would eliminate the above effect. At the same time, unwanted environmental effects such as changes in the density of the  

surrounding medium, humidity and dust are eliminated as well. Thus, the resonator must be placed inside a vacuum housing. This was realized in a very elegant way by Ikeda et al in 1988 [9.9, 9.10]. They fabricated single crystalline silicon resonators housed locally in on-chip vacuum shells using a self aligned selective epitaxial technology in combination with selective anisotropic etching and hydrogen evacuation techniques. The same concept, but based on a different fabrication technology, has been presented by Guckel et al in 1990 [9.11, 9.12]. They employed the polysilicon/silicon dioxide sacrificial layer etching technique to realize a similar structure.

This chapter deals with design and processing issues of electrostatically driven vacuum encapsulated polysilicon resonators. A discussion of the resonator performance, including a description of the theoretical model is given in [9.13]. The fabrication process is based upon the polysilicon/silicon dioxide sacrificial layer etch technique. A novel freeze drying procedure, which, as opposed to earlier reports, offers enhanced speed and does not require any vacuum equipment, is presented. For the excitation and detection of the vibration, the emphasis is on a single element or one-port approach [9.14].

9.1 DESIGN ISSUES

9.1.1 Basic Structure

The basic structure of the resonator is shown in fig. 9.1. The resonator consists of a prismatic beam with a rectangular cross section and is housed in an evacuated cavity. From an electric viewpoint, the structure defines a four-terminal device. Both the one- [9.6, 9.14] and two-port approach [9.5, 9.7, 9.10] can be used for excitation and detection. Details of the electric driving scheme are presented in reference [13].

Electrostatic excitation and detection has been employed [9.3-9.7]. This is attractive because of its simplicity and compatibility with micromachining- and IC-technologies. The simple structure makes the mechanical properties, and in particular the residual stress, much easier to control. Also, the scheme does not suffer from disturbing frequency shifts due to thermally-induced axial loads. Moreover, no additional transducer materials are required that would be necessary for some other drive and detection schemes, such as piezoelectric transduction. Thus, the electrostatic scheme provides an excellent way
9.1.2 The Resonator

The static and dynamic characteristics of the resonator are determined by its in-plane geometry and by its characteristic dimensions perpendicular to the plane [9.4, 9.13]. Flexural beam resonators were designed with lengths of 210, 310, 410 and 510 µm and widths of 25, 50 and 100 µm. The beam thickness is approximately 1.5 µm. Furthermore, a beam-to-substrate gap spacing of approximately 1.2 µm and a beam-to-cap gap spacing of 1.5 µm were chosen. These dimensions lead to static capacitances (excluding any parasitics) in the range from 0.03-0.3 pF.

Parasitic loads, which are mainly capacitive, have to be kept very low or have to be compensated for in order to be able to detect the resonance [9.14]. The on-chip parasitic loads are mainly determined by the area of the bonding pads and the interconnecting wires. Thus, wire lengths were kept as short as possible and a relatively small bonding pad area of 150 × 150 µm² was used.

Chips, measuring 6×6 mm², including 12 different resonant structures were designed. As a minimum feature size, a rather conservative value of 5 µm was used. Figure 9.2 shows a typical layout of the resonator. A total number of ten masks is needed for the fabrication.

"Drop-in's" of electric and mechanical diagnostics were used for characterization. They include checks for alignment accuracy, structures to measure the sheet- and contact resistances and further, ring- and buckled beam structures to determine the residual strain [9.15].
9.1.3 The Sealing Cap

The mechanical stiffness of the sealing cap must be large enough to prevent excessively large deflections caused by the internal vacuum, which may lead to physical contact between the beam and the cap or the substrate. Thus, the upper limit for the cap width is determined by a given maximum deflection. The maximum deflection \( w_o \) of a clamped rectangular plate with lateral dimensions \( b \times a \) can be approximated as [9.16]:

\[
w_o = \frac{0.00260 \Delta p a^4}{D} \quad \text{for} \quad b > 2a
\]  

(9.1)

where \( \Delta p \) is the difference in pressure of the cavity interior and the ambient and \( D \) is the flexural rigidity of the plate. The coefficient 0.00260 depends on the length over width ratio \( b/a \) and is within two percent accuracy if this ratio is larger than 2. For a cap thickness of 2.5 \( \mu m \) this leads to an upper limit of the
width of approximately 150 µm if the maximum cap deflection should not exceed 0.5 µm. Finite element analyses of a more realistic cap structure have indicated an increase of the maximum deflection of 20-30% due to small corrugations that are a result of the fabrication process (e.g. see fig. 9.5h). This leads to a smaller value for the upper limit of the width. The cap thickness used is approximately 2.6 µm, which is sufficient to keep the inward deflection after sealing below 0.5 µm for 100 µm wide beams.

9.2 TECHNOLOGY AND MATERIALS

9.2.1 Surface Micro Machining

Surface micromachining is used to fabricate the polysilicon resonant structures. The technology relies on the construction of mechanical parts from a thin film material in combination with a sacrificial layer thin film which is used as a spacer between the structural parts [9.5, 9.17, 9.18]. After selective chemical removal of the sacrificial layer, freely movable members remain. Surface micromachining offers small dimensions and a good dimensional control, especially in the thickness direction. Furthermore, a very elegant way to fabricate evacuated cavities by means of surface micromachining techniques has recently been presented [9.17, 9.19].

9.3.2 Materials

Taking the planar silicon thin film technology as a starting point, the aim is to develop an "all silicon device". This provides a good thermal match with the silicon substrate and further, IC compatible processing. Fabrication is based on the polysilicon/silicon dioxide sacrificial layer technique.

In order to obtain a high sensitivity of the frequency per unit applied strain, i.e., the gauge factor, slender beams are required with a low residual strain [9.4]. A tensile strain is desired to minimize the chance for buckling. A tensile strain in the range of 10-100 ppm is considered to be optimal. The residual stress of polysilicon can be well controlled provided that proper deposition conditions and anneal cycles are used [9.15, 9.19-9.21]. Polysilicon with a low tensile strain can be obtained by low pressure chemical vapour deposition (LPCVD) from silane (SiH₄) at processing temperatures close to the transition temperature from amorphous to (poly)crystalline layers (≈580 °C [9.21]). Thermal annealing in nitrogen converts the as-deposited built-in compressive strain into a controllable tensile strain depending on the annealing temperature [9.15].
The electrodes of the driving capacitor are defined by locally doping the silicon substrate, the polysilicon beam and the cap. In order to prevent doping of unwanted places, undoped oxides must be used as the sacrificial layer material. Undoped low frequency (50 kHz) plasma enhanced chemical vapour deposited (PECVD) oxides, provide an acceptable compromise between a good step coverage and a high sacrificial layer etch rate. Doping is done by ion implantation of boron. Boron results in lower sheet resistivities in polysilicon than phosphorous and shows less segregation at the grain boundaries [9.22]. Boron implantation may however affect the mechanical properties of the polysilicon layer. Electrical insulation of the cap, the resonator and the substrate, is achieved using an separating layer of LPCVD silicon nitride. The layer also serves as the anchor material for the different polysilicon layers as indicated in fig. 9.3. The capacitance and the conductivity between the different terminals provide a first indication about the integrity of the insulating layers and whether the beams are free standing or not.

![Schematic diagram](image)

**Fig. 9.3** Schematic of the electrical insulation between the resonant beam, the substrate and the cap using silicon nitride anchors; (a) prior to and (b) following the sacrificial layer etch.

### 9.2.3 Sticking and Freeze Drying

After etching the sacrificial layers in a undiluted (49%) HF-solution, a specific drying procedure is required to prevent sticking of the free structural members to the substrate. Standard drying procedures such as dry spinning or air drying produce surface tension forces which cause “pull-down” of the thin
film structures. Once contact has been made, other forces like van der Waals forces, electrostatic forces, hydrogen bridging, and chemical reactions come into play resulting in permanent attachment of the structures to the substrate [9.23-9.25]. An elegant way to solve this so called sticking problem is "freeze drying". The freeze drying method has been developed as a drying method for biological specimens by Boyde and Wood in 1969 [9.26]. Guckel et al. [9.24] and later Takeshima et al. [9.27] implemented this technique for sacrificial layer etching of surface micromachined structures. They describe a process whereby a final rinse agent is frozen and subsequently sublimated in a few hours under vacuum conditions.

In this thesis a new and faster freeze drying technique is presented. It does not require any vacuum equipment but instead can be performed under atmospheric conditions. Cyclohexane which freezes at about 7°C is used as the final rinse agent. Freezing and subsequent sublimation is readily accomplished by placing the substrate under a nitrogen flow on a regulated peltier element with a temperature below the freezing point. The total time for the freeze-sublimation process depends on the geometry of the sample and typically takes 5-15 minutes for the structures described in this chapter.

Details of the cyclohexane rinse-freeze-sublimation procedure are given in chapter 2.

9.2.4 Reactive Sealing

The reactive sealing technique as developed by Burns and Guckel [9.17, 9.19] is an elegant way to create vacuum-sealed cavities. It exploits the difference in geometry between the small etch channels and the larger cavities to close off the latter by growing a thin film in the channels. Good step coverage is favourable for good sealing properties but also results in the deposition of a thin layer inside the cavity before the etching channels are closed.

In the case of the electrostatic driving scheme, a material with good insulating properties must be chosen. The deposition of electrically conductive material inside the cavity creates a miniature Faraday cage around the beam prohibiting the build-up of an electric field. The field is essential for electrostatic excitation and detection. Furthermore, it is preferable to use a material with a tensile residual strain in order to minimize the chance for buckling of the resonator. LPCVD of silicon nitride was chosen as the reactive sealing process.
The residual pressure inside the cavity after sealing is important with respect to the attainable quality factors [9.3, 9.4, 9.28]. The cavity pressure after sealing is a function of the processing pressure and temperature, the gas flows, the reaction products and subsequent (out)diffusions. The overall chemical reaction for the deposition of silicon nitride from dichlorosilane (DCS) and a surplus of ammonia (NH₃) is [9.29]:

\[
3 \text{Si}_2\text{Cl}_2\text{H}_2 + (4 + x) \text{NH}_3 \rightarrow \text{Si}_3\text{N}_4 \downarrow + 6 \text{HCl} \uparrow + 6 \text{H}_2 \uparrow + x \text{NH}_3 \uparrow
\]  

(9.2)

The reaction above clearly indicates that the number of gaseous molecules increases. This means that the cavity pressure will be higher than the deposition pressure whence the etch channels are closed off. According to Boyle Gay-Lussac’s law for an ideal gas, the final cavity pressure \(p_{\text{cavity}}\) can be expressed as:

\[
p_{\text{cavity}} = p_{\text{process}} \frac{V_{\text{unsealed}}}{V_{\text{sealed}}} \frac{T_o}{T_{\text{process}}} \frac{n_{\text{right}}}{n_{\text{left}}}
\]

(9.3)

where \(T_o\) and \(T_{\text{process}}\) denote room temperature and the processing temperature, respectively, \(V_{\text{unsealed}}/V_{\text{sealed}}\) denotes the ratio of the volume of the cavity prior to and after sealing, \(p_{\text{process}}\) denotes the processing pressure and \(n_{\text{right}}/n_{\text{left}}\) denotes the mole ratio of gaseous products and reactants.

To get an idea of the final cavity pressures, a DCS to NH₃ flow ratio of 3:4 (i.e., \(x=0\)) and no subsequent reactions and diffusions after sealing are assumed. For the reaction given by eq. (9.2) the mole ratio equals 12/7. The ratio \(V_{\text{unsealed}}/V_{\text{sealed}}\) results in a slight increase of the sealing pressure and is due to a volume reduction as a result of the inward deflection of the sealing cap. This deflection is generally undesirable and is therefore kept small, resulting in a volume ratio close to unity. For a processing pressure of 200 mTorr (0.26 mbar) and a deposition temperature of 800 °C, eq. (9.3) predicts a sealing pressure of about 100 mTorr (0.13 mbar). A more dedicated process performed at a pressure of 20 mTorr, a temperature of 850 °C and the same gas flow ratio as indicated above leads to a sealing pressure of approximately 10 mTorr, but exhibits a lower deposition rate. By using a flow ratio DCS:NH₃ = 1:3, a surplus of ammonia results (i.e., \(x=5\)) and the mole ratio is equal to 17/12. This leads to a lowering of the final cavity pressure from 100 mTorr to 83 mTorr. Extending the surplus to infinity, results in a final cavity pressure of 58 mTorr. These simple examples indicate that the mole ratio is not the determining
factor, but instead, the processing pressure and the processing temperature are the most important parameters that determine the final cavity pressure. Low final cavity pressures using a LPCVD silicon nitride sealing layer, are obtained at a high deposition temperature and a low deposition pressure.

9.3 Fabrication Sequence

A scheme of the fabrication process is shown in fig. 9.4. The substrates are (100) n-type silicon wafers. The fabrication sequence begins with a dry etching step to define the alignment marks into the silicon substrate (Mask #1). Next, boron is implanted (dose=5x10^{15}\text{cm}^{-2}\text{ and energy}=100\text{keV}) through a photoresist mask (Mask #2) to define the bottom electrode (see fig. 9.4a). This is followed by a blanket phosphorous implant (dose=1x10^{15}\text{cm}^{-2}\text{ and energy}=150\text{keV}) of the back-side of the wafer to obtain a good back side contact. After plasma ashing the photoresist, the wafer is cleaned and is given an HF-dip (1 min. in HF:H_2O=1:100) to remove the native oxide. A 200 nm thick, stoichiometric LPCVD silicon nitride layer is grown for the insulating (anchoring) layers (see also figs. 9.3a,b). The first sacrificial layer consists of a 1.18\mu m thick PECVD oxide layer, grown from a 2% SiH_4 (in N_2) and N_2O mixture. This layer is densified for 30 minutes at 800°C in a nitrogen ambient to improve the thermal stability. Patterning of the oxide layer (Mask #3) is done by wet chemical etching using buffered HF (BHF). After cleaning, the wafers are given an HF-dip to remove the native oxide and to ensure proper attachment of the next LPCVD polysilicon layer. This polysilicon layer is 1.50\mu m thick and is grown by pyrolizing silane (50 sccm) at a temperature of 590°C and a pressure of 250 mTorr, giving a deposition rate of approximately 6.3 nm/min. (see fig. 9.4b). The following step is the implantation of boron (dose=1x10^{16}\text{cm}^{-2}\text{ and energy}=150\text{keV}) through a photoresist mask (Mask #4) to define the electrode of the resonant beam. After plasma ashing the photoresist and subsequent cleaning, the length and width of the beams are defined by reactive ion etching (RIE) in a SF_6 plasma (Mask #5). At the same time, the polysilicon is removed at the places for the substrate metallization contacts (see fig. 9.4c). This is followed by the deposition, densification and patterning (Mask #6) of the second PECVD sacrificial oxide layer (see fig. 9.4d). The thickness of the oxide layer is approximately 1.55\mu m. Wet oxidation at 1000°C for 8 minutes forms a 80 nm thick oxide layer in which the anchors (and etch channels) are defined using BHF (Mask #7). For electric insulation, a
50 nm LPCVD silicon nitride layer is grown which serves as the insulation layer between the beam- and cap electrodes (see fig. 9.4e and figs. 9.3a,b). The next step involves cleaning and removal of the native oxide, followed by LPCVD of a polysilicon layer for the cap with a thickness of approximately 2.64 µm. A blanket boron implantation (dose=1x10^{16} cm^{-2} and energy=100 keV) forms the conductor for the cap electrode. Patterning of the doped polysilicon layer defines the cap structure (Mask #8, see fig. 9.4f). This is done using RIE in a SF₆/N₂ plasma, which gives an etch selectivity of 40:1 for polysilicon over silicon oxide.
Fig. 9.4a-i Processing sequence for sealed polysilicon resonators. The different stages of the process are explained in the text. 9.4a-h: cross-sectional view B-B’ and 9.4i: cross-sectional view A-A’ of fig. 9.2.
The etching is stopped as soon as the underlying silicon nitride and silicon oxide double layer of the etch channels is exposed. From this point on, thick photoresist layers (3\(\mu\)m) have to be used to ensure sufficient step coverage. Annealing in a nitrogen ambient for 1 hour at a temperature of 1000\(\degree\)C is necessary for the electric activation of the implanted boron and moreover, to obtain a low tensile strain in the polysilicon layers. Following a thorough cleaning step, the sacrificial layer etch and subsequent freeze drying procedure is performed. Sacrificial layer etching is done in an undiluted (49%) HF-solution and takes about two hours. Quenching in DI-water is done for 30 minutes and next IPA is added. The wafers are rinsed for 1 hour in an IPA solution, followed by a final rinse cycle in cyclohexane for 1 hour. Next, the freeze drying is carried out as described in the previous section (see fig. 9.4g). The mechanical test structures can now be examined to check whether the mechanical stress is in the region of interest and whether sacrificial layer etching and freeze drying have been successful. LPCVD of approximately 200 nm stoichiometric silicon nitride is used to close off the etch channels. The surface passivation of the pn-junction of the bottom electrodes in the substrate is important. Dry thermal oxidation is the best way to accomplish this, but experiments revealed buckled beams even after growing oxide layers as thin as 10 nm at a temperature of 800\(\degree\)C. It was found that the leakage currents of the pn-junctions that are passivated with silicon nitride are on the order of 10\(\mu\)A/cm\(^2\), which is considered to be satisfactory. The contact windows for the substrate-, beam- and cap electrodes are defined using RIE in a CHF\(_3\)/O\(_2\) plasma (Mask #9). Metallization consists of a 1\(\mu\)m thick aluminum layer. Prior to the metallization, the thin films that have accumulated on the backside of the wafer are stripped by means of RIE in a SF\(_6\) and CHF\(_3\) plasmas. Metal patterning (Mask #10) is done by wet chemical etching. The final step is a 5 minutes anneal in a N\(_2\)/H\(_2\)O ambient at 450\(\degree\)C to obtain good ohmic contacts. Final cross sections of the sealed resonator are shown in figs. 9.4h and 9.4i.

**9.4 RESULTS**

The SEM photograph of fig. 9.5a shows a top view of a sealed resonator. To inspect the inside of the cavity for residues, and check whether or not the beams are free standing, caps have been manually removed. This was done by peeling off a carefully attached tape. The cavity inside turned out to be clean and displayed free standing beams having smooth surfaces (see figs. 9.5b,c).
Fig. 9.5 (a) SEM photograph of a sealed polysilicon resonator.

Fig. 9.5 (b) Resonator with cap removed showing cavity interior.
Fig. 9.5 (c) Close up of cavity interior.

Fig. 9.5 (d) Magnified view of part of the cross section.
Cross-sectional views of the resonator are shown in fig. 9.5d. These SEM photographs clearly indicate (part of) the free standing beam and the shape of the cap (compare fig. 9.4h).

### 9.4.1 Electrical and Electromechanical Behaviour

Electrical measurements indicated sheet resistances of $24 \text{\,}\Omega/$square, $30 \text{\,}\Omega/$square and $22 \text{\,}\Omega/$square for the bottom electrode, the resonator electrode and the cap electrode, respectively. The contact resistance turned out to be about $1 \text{\,}\Omega$ for a $20 \times 20 \mu\text{m}^2$ contact for all three electrodes. Standard I-V measurements used to check the integrity of the nitride insulating anchors revealed high ohmic ($\text{G}\Omega$’s) anchors. The residual strain has been extracted from the polysilicon ring and buckled beam diagnostics and is within the range from $-8 \times 10^{-7}$ (compressive) to $+6 \times 10^{-5}$ (tensile).

First tests aimed at finding the resonance frequency have indicated that care must be taken with respect to probing the samples. Static charges induce electric forces that cause “pull-in” of the beams, resulting in permanent attachment of the latter to the cap or to the substrate. This phenomenon was observed while testing uncapped resonators.

![Admittance Bode plot of an electrostatically driven vacuum encapsulated polysilicon resonator in a one-port configuration](image)

**Fig. 9.6** Admittance Bode plot of an electrostatically driven vacuum encapsulated polysilicon resonator in a one-port configuration [9.13]. Resonator dimensions are $210 \times 100 \times 1.5 \mu\text{m}$. The observed resonant frequency is $323,682 \text{\,Hz}$. 
Figure 9.6 shows a typical example of a measured Bode plot showing the conductance and the susceptance as a function of the driving frequency of a 210 µm long polysilicon beam resonator.

### 9.4.2 Quality factor and sealing pressure

Vacuum sealing provides pressures in the molecular regime, where the gas molecules are not interacting with each other. Energy losses due to viscous damping are negligible in this pressure regime. The damping mechanisms that remain, include losses due to individual collisions of the gas molecules with the resonating surfaces (momentum damping), losses into the mount and intrinsic (material) damping. If momentum damping is considered to be the dominant loss mechanism and if the resonator was operating in free space, the mechanical quality factor as a function of the ambient pressure \( p \) can be expressed as [9.28]:

\[
Q_n = \frac{3}{8\sqrt{6}} \frac{\alpha_n^2}{p} \frac{h}{l} \sqrt{\frac{\pi E \rho R T}{M}}
\]

where \( \alpha_n \) is a constant depending on the edge conditions and on the mode of vibration \( n \) (\( \alpha_1 = 4.73 \) for a clamped-clamped beam), \( h \) and \( l \) are the thickness and the length of the resonator, respectively, \( E \) and \( \rho \) are the Young’s modulus and the specific mass of the resonator material, respectively, \( R (=8.31 \text{JK}^{-1}\text{mol}^{-1}) \) is the universal gas constant, \( T \) is the absolute temperature and \( M \) is the molecular mass of the gas. The mechanical quality factor of the one-port resonators can be extracted from the measured Bode-plot and turned out to be a function of the resonator length. It is noted that the quality factor varied across the wafer, probably as a result of variations of the sealing pressure.

In order to measure the quality factor as a function of pressure, a leak must be introduced to the cavity. Fracturing the cap is not considered to be the best solution, since this would remove the upper gap and thus would give misleading results if effects such as squeeze film damping are significant. Therefore, an especially designed “dual cavity structure” has been used to measure the quality factor as a function of the pressure inside the cavity. The structure consists of two cavities that are interconnected through a narrow channel. One cavity houses the resonator. By fracturing the other cavity and placing the structure in a vacuum chamber, the pressure of the cavity containing the resonator could be regulated by the external vacuum system. This way, the resonator is still sandwiched between two air gaps, and thus
forms a realistic representation of the actual situation. The results are shown in fig. 9.7. The graph indicates that the quality factor is inversely proportional to the pressure (as predicted by eq. (9.4)) in the range from 0.01 to 1.0 mbar. This suggests that in this pressure regime, momentum damping is the dominating loss mechanism. However, measured $Q$-values are more than one order of magnitude smaller than predicted and some form of “enhanced momentum damping” exists. This discrepancy has also been observed by Zook et al. [9.30]. The primary cause for the discrepancy is the fact that the assumption of free space surrounding the resonator which is used to derive eq. (9.4) is not true. Squeeze film effects have to be taken into account [9.5, 9.8]. This is further strengthened by the distinct values for $Q$ that are measured of a $310 \mu m$ long beam with (i.e., dual structure) and without a cap, or in other words, with two gaps and a single gap, respectively. The $Q$-factor for the structures with two gaps was approximately a factor 2 to 3 smaller than the $Q$-factor of the structures with a single gap. Details of the theory of squeeze film effects are beyond the scope of this chapter and reference is made to the literature, e.g., see reference [9.8] and references therein. The stationary value of the $Q$-factor at very low pressures is determined by intrinsic damping effects and by losses into the mount. Measurements have indicated stationary $Q$-factors close to 18,000 (see fig. 9.8).

![Fig. 9.7](image)

*Fig. 9.7* Measured quality factor as a function of ambient pressure for beams with and without cap and for different beam lengths. Beam thickness and width are $1.5 \mu m$ and $100 \mu m$, respectively, lower and upper gap spacing are approximately $1.2 \mu m$ and 1-1.5 $\mu m$, respectively.
The reactive sealing process with LPCVD silicon nitride was done at three different process pressures of respectively 200, 50 and 20 mTorr. Typical values of measured quality factors are given in table 9.1.

<table>
<thead>
<tr>
<th>Sealing Pressure [mTorr]</th>
<th>Quality factor at beam length of</th>
<th>510 µm</th>
<th>410 µm</th>
<th>310 µm</th>
<th>210 µm</th>
</tr>
</thead>
<tbody>
<tr>
<td>200</td>
<td></td>
<td>60</td>
<td>80</td>
<td>150</td>
<td>300</td>
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<td>50</td>
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<td>20</td>
<td></td>
<td>300</td>
<td>400</td>
<td>700</td>
<td>2000</td>
</tr>
</tbody>
</table>

*Table 9.1* Measured average quality factors of sealed resonators with varying beam length after reactive sealing with silicon nitride at different process pressures. Standard deviation of measured quality factors is large, averaging about 50% of the given values.

The dual cavity structure described above is also used to determine the final cavity pressure of sealed resonators. By matching the Bode plots of sealed resonators and dual cavity resonators, a first indication of the cavity pressure is obtained. This revealed sealing pressures in the range from 0.1-0.2, 0.03-0.05 and 0.02-0.04 mbar which is in fair agreement with predicted values of respectively 0.1, 0.03 and 0.01 mbar obtained from eq. (9.3) for the process pressures as given in table 1. More experimental data together with a theoretical model of the resonator behaviour are given in [9.13].

### 9.5 CONCLUSIONS

Resonators housed in evacuated cavities have been successfully fabricated with surface micromachining using sacrificial layer spacers. Low strain fine-grained LPCVD polysilicon has been used for the structural parts and undoped PECVD silicon oxide as the sacrificial layer material. Electrostatic excitation and detection was used as the driving and sensing mechanism. Electrical insulation of the different electrodes is accomplished by means of LPCVD silicon nitride layers. A novel freeze drying method using cyclohexane has been developed to prevent sticking. The advantages of this procedure as compared to other known freeze-drying procedures are its enhanced speed and the fact that no vacuum equipment is required. Reactive sealing with LPCVD silicon nitride yields cavity pressures close to the processing pressure. First measurements have indicated the typical resonance characteristics that are expected for resonators operated in a one-port configuration. Quality factors are determined by enhanced momentum damping due to squeeze film
effects, and turn out to be inversely proportional to the ambient pressure for moderate sealing pressures (0.01-1.0 mbar). Measured sealing pressures are estimated to be in the range of 0.01-0.2 mbar depending on process pressure, resulting in quality factors up to 3000 (highest measured values) for the 210 µm long beams. A more dedicated sealing procedure at lower deposition pressures and/or higher temperatures is required to obtain cavity pressures in the regime, where the quality factor is independent of pressure and is solely determined by intrinsic losses and losses into the mount. Subsequent annealing at elevated temperatures may further reduce the sealing pressure due to outdiffusion of gaseous species. Measurements have indicated that quality factors close to 18,000 are feasible at pressures below 0.01 mbar.

REFERENCES


Chapter Ten

CONCLUSIONS

10.1 GENERAL CONCLUSIONS

Chapter one:

There exists an increasing demand for new transducers based on IC fabrication and other thin film technologies. In this thesis the fabrication of different types of electrostatic microactuators based on polysilicon surface micromachining techniques is described. Electrostatic actuators are used for the transformation of electric input energy into mechanical output energy. They benefit from available microfabrication techniques and materials as developed in IC technology and exhibit good performance characteristics such as high energy density in micron sized gaps, high efficiency and fast response time.

Chapter two:

Surface micromachining can be used to fabricate deformable and mechanism type of microstructures with more than one degree of freedom. Polysilicon surface micromachining is based upon the selective removal of sacrificial layers from a multilayer sandwich of patterned thin films consisting of sacrificial silicon oxide layers and structural polysilicon thin films. The ability to control residual stress in polysilicon thin films, characterise polysilicon mechanical properties and model the silicon dioxide sacrificial layer etch process as well as the development of methods to prevent stiction, together with a variety of new fabrication processes and the possibility to integrate electronics make polysilicon surface micromachining a well developed and versatile technology. A notorious problem in surface micromachining is stiction of polysilicon structures after rinsing and drying. A novel freeze drying method using cyclohexane has been developed to prevent stiction problems and was used as a fabrication technique to realise the structures and devices presented in this thesis.
Chapter three:

The mechanisms that cause stiction of polysilicon structures, which are fabricated by surface micromachining techniques, have been investigated in order to obtain a more fundamental understanding of stiction.

It was found that during the drying process, attractive capillary forces are responsible for bringing micromechanical structures into contact with the substrate. A model for the surface tension forces during drying has been developed and applied to doubly clamped beams. For these beams a pull-in length exists beyond which the beams are forced into contact with the underlying substrate. Measurements of the pull-in length are in qualitative agreement with the theory. The measured values of the pull-in length are somewhat lower than predicted by theory. This is suggested to result from dynamical effects during the drying process.

After complete drying, structures remain stuck to the substrate. The measured adhesion energy of the resultant bond has been determined using the detachment length of the beams after complete drying. The resulting bonding strength's were independent of the used rinsing liquids, water and isopropanol. The measured adhesion energies ranged from 0.05 to 0.10 J/m² for hydrophobic silicon surfaces and from 0.24 to 0.26 J/m² for hydrophilic silicon surfaces. These energies indicate that van der Waals forces are responsible for stiction of hydrophobic surfaces and hydrogen bridging is the dominant bond mechanism in case of the hydrophilic surfaces. These results are in agreement with wafer bonding experiments.

Chapter four:

The fabrication of electrostatic actuators demands small gaps and compliant or high aspect ratio polysilicon microstructures. This requires anisotropic etching techniques of silicon. Reactive ion etching using SF₆, O₂, CHF₃ gas mixtures for the anisotropic etching of silicon has been investigated.

The etching behaviour was found to be affected by loading, the mask material and the cathode material. Reproducable and uniform results have been obtained by using a silicon cathode and a silicon dioxide mask. Surface response methodology was used to characterise etch rate, mask selectivity, bias voltage and anisotropy as a function of the RF power, the process pressure, the SF₆ flow, the O₂ flow and the CHF₃ flow in order to optimise anisotropic etching conditions.

The effect of several variables on the measured responses has been discussed.
The addition of CHF$_3$ can be used to produce smooth etch surfaces in the anisotropic regime and is useful for a fine tuning of the anisotropy. AES measurements indicate that anisotropic etching results from sidewall passivation by silicon oxide species. The anisotropic etch mechanism in SF$_6$, O$_2$, CHF$_3$ plasmas is based on ion-enhanced inhibitor etching. SF$_6$ provides the reactive neutral etching species, in the form of F atoms. O$_2$ supplies the inhibitor film forming species that passivate the surface with a SiO$_x$F$_y$ layer. SF$_6$ and CHF$_3$ generate ion species, SF$_x^+$ and CF$_x^+$ respectively, that suppress the formation the inhibitor film at horizontal surfaces.

The fabrication of structures with aspect ratios of about 10 has been demonstrated. The process is applied to deep trench etching and fabrication of high aspect ratio structures used in micromachining.

Chapter five:
An electrostatic actuator design has been presented where a deformable mechanical structure is bent around a fixed curved electrode by means of electrostatic forces. Such a design is attractive because relatively large deflections and force generation can be obtained. For the shape of the curved electrode simple polynomials have been used.

A theory based upon energy methods is presented to describe the static behaviour of the actuator. Furthermore 3D coupled electromechanical simulations using CoSolve-EM have been performed. The results from both models are in qualitative agreement with each other. When the beam deflection is not constrained by the curved electrode geometry, instable behaviour occurs at a certain pull-in voltage and a hysteresis exists to release the structure after pull-in. For polynomial designs, with an order above two, it was found that the beam deflection becomes constrained by the curved electrode geometry before pull-in occurs. Our models predict completely stable behaviour in this situation.

Curved electrode actuators have been fabricated from polysilicon by surface micromachining techniques using a one-mask process. Electric insulation has been realised by stand off bumper structures between the movable beam and the fixed electrode or by a silicon nitride sidewall layer.

Measurements of non-constrained beam deflections show that the qualitative behaviour of the energy model is in agreement with theory but that the pull-in voltages are higher than theoretically predicted. This effect is a result of the presence of a groundplane as shown in 3D coupled electromechanical simulations. Constrained designs employing bumper
structures show a stepwise behaviour as a result of a number of stable positions at the bumper positions. The static behaviour of these designs has been modelled by CoSolve-EM and was found to be in fair agreement with experimental data. Experimental data of samples with a continuous sidewall insulator did not show stable behaviour up to maximal deflection in contrast to our theoretical results. It is suggested that this is caused by imperfections at the sidewall surfaces, as a result of the fabrication process, which prevent the movable beam from smoothly zipping along the curved electrode and act like small bumpers leading to local instabilities.

Curved electrode structures are therefore mainly suited for bi-stable actuator applications like for example microswitches, microgrippers, microvalves and micro pumps.

Chapter six:

The design, fabrication and experimental results of lateral comb-drive actuators for large displacements at low driving voltages is presented. A comparison of several suspension designs is given, and the lateral large deflection behaviour of clamped-clamped beams and a folded flexure design has been modelled. An expression for the axial spring constant of folded flexure designs including bending effects from lateral displacements, which reduce the axial stiffness, has also been derived. The maximum deflection that can be obtained by comb-drive actuators is bounded by electromechanical side-instability. Expressions for the side-instability voltage and the resulting displacement at side-instability are given. The electromechanical behaviour around the resonance frequency was described by an equivalent electric circuit.

Devices have been fabricated by polysilicon surface micromachining techniques using a one mask fabrication process. Static and dynamic properties have been determined experimentally and are compared with theory. Static properties are determined by displacement-to-voltage, capacitance-to-voltage and pull-in voltage measurements. The lateral Young's modulus of the polysilicon layer has been obtained from static displacement-to-voltage measurements and turned out to be approximately 170 GPa. The modelled lateral spring constant of the folded beam design was somewhat larger than experimentally determined values. This is likely to be a result of compliant trusses in our fabricated design in contrast to rigid trusses that have been assumed in our theoretical model, or by a non-rectangular cross section of the beams as a result of underetching. The theoretical axial spring constant of a folded beam which includes a reduction in spring constant with increasing
lateral deflection was found to be in good agreement with experimental results in contrast to theoretical spring constants simply determined from Hooke's law. The electromechanical behaviour can be adequately described by the presented equivalent circuit which was used to extract the resonance frequency and quality factor from admittance measurements.

Typical actuator characteristics are deflections of about 30 µm at driving voltages around 20 V, a resonance frequency around 1.6 kHz and a quality factor of approximately 3. An approach towards position control and feedback was briefly discussed.

Chapter seven:

The design and fabrication of an electrostatically driven lower-stator axial-gap wobble motor has been presented. In contrast to side drive motors, the stator poles are located underneath the rotor instead of surrounding the rotor sides. This results in a higher torque generation and easily accessible rotor structures that are suited for mechanical power transmission to other structures that can be fabricated on-chip.

Based on small tilt angles, a rigid disk rotor and simplified electrostatic fields, a theoretical model describing the static and dynamic behaviour of the motor has been given. For the realised micromotors, the torque generation can be in the range of nNm at high electrostatic fields. The torque coverage for different excitation schemes in case of both open-loop excitation and closed-loop excitation, which requires position feedback, is discussed. Because of large normal forces, generated by the axial electrostatic field, the motors can be easily operated in no-slip conditions. The kinetic behaviour is complex and related to a cycloid. A fixed point at the rotor even moves opposite to the rotational direction of the rotor during small time intervals of a wobble cycle. This may have consequences for certain applications. Based on side-driven micromotor results an equation of motion for the rotor has been derived that includes rotational rotor inertia, viscous drag and coulombic frictional terms. The rotational inertia of the rotor is mainly determined by its inertia about the rocking axes.

Fabrication is based on a four mask process using polysilicon surface micromachining techniques. Silicon nitride has been used for electrical insulation between the rotor and stator poles and silicon oxides were used as sacrificial layers that have been removed in an HF solution. The rotor and stator poles have been constructed from doped polysilicon. To increase the stiffness of the rotor it is made from polysilicon and a thick sputtered
amorphous silicon film. A new ball bearing design, which is not self-aligned, has been used for the rotor in order to avoid photoresist step coverage problems.

Motors have been successfully operated at driving voltages of a few Volts. Motor performance has been characterised by measuring the gear ratio and start and stop voltage measurements using single pole, open-loop excitations with square-wave voltage signals. Although some variation in gear data has been observed for different motors, the gear ratio seems to be independent of the driving frequency up to a maximum measured frequency of 10 kHz. The gear ratio was found to be strongly dependent on the driving voltage. It is suggested that this is caused by the mechanical deformation of the rotor that results from the axial electrostatic forces. The gear ratio was also found to be dependent on operation time showing an initial increase and decrease of the gear ratio after which it remained constant until motor failure. This behaviour is suggested to result from wear at the ball bearing and the surfaces at the contact point circle. Motor lifetimes varied between a few ten thousand wobble cycles to some millions of wobble cycles for two different process runs. This resembles operation times ranging from a few minutes to several hours at rotor speeds between a few and several hundred rpm. A theoretical prediction of the transient response of the motor has been given using measured and estimated calculated parameters for the equation of motion.

Chapter eight:

A surface micromachining process is presented which has been used to fabricate electrostatic microactuators that are interconnected with each other and linked to other movable microstructures by integrated gear linkages. The gear linkages consist of rotational and linear gear structures and the electrostatic microactuators include curved electrode actuators, comb drive actuators and axial gap wobble motors.

The micromechanical structures are constructed from polysilicon. Silicon dioxide has been used as a sacrificial layer and silicon nitride was used for electrical insulation. A cyclohexane freeze drying technique is used to prevent problems with stiction. First experimental results show that electrically powered actuators successfully drive various micromechanisms. The work is a first step towards mechanical power transmission in micromechanical systems. Mechanical power transmission of microactuators may strongly increase the number of useful applications and may lead to new possibilities for microelectromechanical systems.
Chapter nine:

Basic design issues and a fabrication process based on surface micromachining techniques for electrostatically driven vacuum encapsulated polysilicon resonators are presented.

Resonators housed in evacuated cavities have been successfully fabricated with surface micromachining using sacrificial layer spacers. Low strain fine-grained LPCVD polysilicon has been used for the structural parts and undoped PECVD silicon oxide as the sacrificial layer material. Electrostatic excitation and detection was used as the driving and sensing mechanism. Electrical insulation of the different electrodes is accomplished by means of LPCVD silicon nitride layers. A novel freeze drying method using cyclohexane has been developed to prevent sticking. The advantages of this procedure as compared to other known freeze-drying procedures are its enhanced speed and the fact that no vacuum equipment is required. Reactive sealing with LPCVD silicon nitride yields cavity pressures close to the processing pressure.

First measurements have indicated the typical resonance characteristics that are expected for resonators operated in a one-port configuration. Quality factors are determined by enhanced momentum damping due to squeeze film effects, and turn out to be inversely proportional to the ambient pressure for moderate sealing pressures (0.01-1.0 mbar). Measured sealing pressures are estimated to be in the range of 0.01-0.2 mbar depending on process pressure, resulting in quality factors up to 2000 for 210 μm long beams. A more dedicated sealing procedure at lower deposition pressures and/or higher temperatures is required to obtain cavity pressures in the regime, where the quality factor is independent of pressure and is solely determined by intrinsic losses and losses into the mount. Subsequent annealing at elevated temperatures may further reduce the sealing pressure due to outdiffusion of gaseous species. Measurements have indicated that quality factors close to 18,000 are feasible at pressures below 0.01 mbar.

10.2 CONCLUDING REMARKS

A fabrication technology based on polysilicon surface micromachining techniques and first actuator designs based on electrostatic actuation have been presented in this thesis. Emphasis has been given to the design of actuators which can generate relatively large forces and displacement. High output actuators are generally attractive in many (possible) applications.
The fabrication of microactuators based on polysilicon surface micromachining techniques is an emerging technology in search of applications. Further development of microactuators depends strongly on the development of useful applications. Polysilicon surfacemicromachining is most suited to fabricate small microsystems where all functions are integrated on-chip. Many applications require millimeter or larger workspaces and output forces of mN. Therefore an upscaling of submillimeter devices, which generally generate forces in the range of 1 to 10 µN, is necessary. This can be easily realised by increasing planar dimensions. However, in order to fabricate robust actuators and increase output forces, this also calls for an increase in the third dimension, i.e. structure thickness. Possible solutions to this problem are the deposition of very thick doped silicon layers or wafer bonding techniques with anisotropic etching techniques, or plating methods based on high aspect ratio UV or X-ray exposure. With these fabrication techniques and provided that micron sized air gaps still can be obtained, many of the advantages that are offered by electrostatic actuation, as described in Chapter 1, still hold.

Important aspects for microactuators are feedback and control, mechanical power transmission and microtribology. At this point these issues are in an infantile stage and are important subjects for further research.
APPENDIX A:

RESPONSE SURFACE METHODOLOGY

Response surface methodology (RSM) is a statistical technique by which experimental strategy and data analysis are combined efficiently to generate a parametric model that represents the process response. It can be applied to any process in which the response can be measured in a continuous fashion and where the settings can be manipulated independently. Once the response has been modelled, graphical representations of the response surfaces formed in the parameter space can be generated for use in process optimization. RSM is only applicable for responses that can be represented as continuous functions.

Response surface designs are fractional factorial designs requiring three or more levels of each process variable. The number of trials in a full factorial design is $k^f$ where $k$ is the number of levels and $f$ is the number of factors. A fractional factorial design is a selected subset of such trials. The number of trials or experimental runs in the design must exceed the number of coefficients in the model. In general a design should be chosen that will support a full quadratic model which includes linear, two factor interactions and quadratic terms for curvature. The general form of the full quadratic model is:

$$Y = b_0 + \sum_{i=1}^{f} b_iX_i + \sum_{i=1}^{f} b_{ii}X_i^2 + \sum_{i=1}^{f-1} \sum_{j=2}^{f} b_{ij}X_iX_j$$  \hspace{1cm} (A1)

where the $X_i$ represent the independent input variables (i.e., process parameters), the $b_i$ are the coefficients for the linear terms, the $b_{ii}$ are the coefficients for the quadratic terms, and the $b_{ij}$ are the coefficients for the cross terms. In order to estimate the regression coefficients in this model, each variable must take at least three levels. This suggests the use of factorials designs of the $3^k$ series. One disadvantage of these series is that with more than three variables the experiments become large. Furthermore, the coefficients $b_{ii}$ of the squared terms are estimated with relatively low precision from a $3^k$-factorial. Special designs for fitting second order response surfaces have been developed which are called central composite rotatable designs.

If there are more than 6 process variables under consideration, screening designs should be used as a first step to determine the most significant variables. The unknown coefficients in the model can be found by the method of least squares fitting from the experimental data. In matrix notation the $n$ distinct sets of conditions can be written as:
\[ \eta = X \beta \]  
(A2)

where \( \eta \) is the \( nx1 \) vector of expected values for the response, \( X \) is the \( nxp \) matrix of independent variables, and \( \beta \) is the \( px1 \) vector of parameters. Since \( x_1, x_2, \ldots, x_p \) are the columns of \( X \), the normal equations can be written as:

\[ X'(y - \hat{y}) = 0 \]  
(A3)

where the prime(’) means transpose. Upon substitution of \( \hat{y} = X b \):

\[ X'(y - X b) = 0 \]  
(A4)

or

\[ X' X b = X' y \]  
(A5)

Since it is supposed that the columns of \( X \) are linearly independent, \( X' X \) has an inverse, and:

\[ b = [X' X]^{-1} X' y \]  
(A6)

The full form of eq. (A6) is shown in table A1. The effect of experimental error on the validity of a parameter model can be addressed using statistical analysis. First the experimental error can be estimated by repeating experimental data and calculating the standard deviation of the replicate differences:

\[ s^2 = \frac{\sum_i (y_i - \bar{y})^2_{obs}}{(n-p)} \approx \frac{S_g}{(n-p)} \]  
(A7)

where \( (y_i - \bar{y})^2_{obs} \) corresponds to the variation of an observation \( y_i \) from the mean \( \bar{y} \) of the observations of the experiment, \( n \) is the number of observations and \( p \) is the number of variables in the model.

Also the lack-of-model-fit can be estimated by performing trials that are necessary to determine the number of coefficients and calculating the deviation between the model and the experimental data. An indication of the agreement between model and data is provided by the \( R^2 \) index:

\[ R^2 = \frac{\sum_i (y_i - \bar{y})^2_{pred}}{\sum_i (y_i - \bar{y})^2_{obs}} \]  
(A8)
in which \((y_i - \bar{y})^2_{pred}\) represents the variation of a predicted response \(y_i\) from the mean \(\bar{y}\) of the predicted responses and \((y_i - \bar{y})^2_{obs}\) corresponds to the variation of an observation \(y_i\) from the mean \(\bar{y}\) of the observations of the experiment.

\[
\begin{array}{c|ccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccccc\[b_0 = \frac{7}{44} \sum X_0 y - \frac{3}{88} \sum_{i=1}^{5} X_{ii} y \]

\[b_i = \frac{1}{24} \sum X_i y \]

\[b_{ii} = \frac{11}{352} \sum X_{ii} y + \frac{1}{352} \sum_{i=1}^{5} X_{ii} y - \frac{3}{88} \sum X_0 y \]

\[b_{ij} = \frac{1}{16} \sum X_{ij} y \]

**Table A1** Full form of equation (A6) and calculation of the regression coefficients for a central composite rotatable second order design for five parameters.

**REFERENCES**


APPENDIX B:

LARGE DEFLECTION BEHAVIOUR OF A BEAM FIXED AT BOTH ENDS

A bar with uniform $EI$, fixed at both ends and subject to a vertical load $P$ at the centre is onefold statically indeterminate (see Fig. 1).

The emphasis is not on the deflections being large due to the flexibility of the bar, but rather on the presence of longitudinal forces $N$ that develop. For reasons of symmetry only the right-hand half will be analysed. The bending moment at a point $(x,y)$ is:

\[ M = EI \frac{d^2y}{dx^2} = Ny + M_0 - \frac{P}{2}(L-x) \]  \hspace{1cm} (B1)

where $M_0$ is a bending moment at the centre to satisfy boundary conditions. The solution of this equation is:

\[ y = \frac{P}{2Nt} \left( \sinh tx + \tanh \frac{tl}{2} \left( 1 + \cosh tx \right) \right) + \frac{P}{2N} (L-x) \]  \hspace{1cm} (B2)

where

\[ t = \sqrt{N / EI} \]  \hspace{1cm} (B3)

at $x = 0$ (the centre) the deflection becomes:

\[ \delta = \frac{PL^3}{8EI} \left( \frac{u - \tanh u}{u^3} \right) \]  \hspace{1cm} (B4)

where

\[ u = \frac{tL}{2} \]  \hspace{1cm} (B5)

If $N=0$, then $t=0$ and $u=0$, and the expression becomes:
\[ \delta = \frac{PL^3}{24EI} \quad (\text{B6}) \]

which is the deflection according to the conventional theory. Equation (B4) contains the unknown \( u \) which is a function of the equally unknown \( t \). Therefore to find \( \delta \), a further relationship is necessary. This can be found from the longitudinal expansion of the bar during deflection. The change in length is:

\[ \Delta l = \frac{1}{2} \int_0^L \left( \frac{dy}{dx} \right)^2 \, dx \quad (\text{B7}) \]

Since the slope is small the axial force can be considered constant along the bar. Hence:

\[ N = AE \frac{\Delta l}{L} = \frac{AE}{2L} \int_0^L \left( \frac{dy}{dx} \right)^2 \, dx \quad (\text{B8}) \]

Taking the first derivative of eq. (B2) and squaring it, substitution in (B8) leads to:

\[ N^3 = \frac{AE P^2}{8} \left[ \frac{3}{2} - \frac{1}{2} \text{Tanh}^2 u - \frac{3}{2} \frac{\text{Tanh} u}{u} \right] \quad (\text{B9}) \]

and since \( N = EI/t^2 \):

\[ P = \frac{16\, EI \sqrt{2I/A}}{L^3} u^3 \left( \frac{3}{2} - \frac{1}{2} \text{Tanh}^2 u - \frac{3}{2} \frac{\text{Tanh} u}{u} \right)^{\frac{1}{2}} \quad (\text{B10}) \]

Combining this expression for \( P \) with equation (B4):

\[ \delta = 2 \sqrt{\frac{2I}{A}} \left( u - \text{Tanh} u \right)^{\frac{1}{2}} \left( \frac{3}{2} - \frac{1}{2} \text{Tanh}^2 u - \frac{3}{2} \frac{\text{Tanh} u}{u} \right)^{\frac{1}{2}} \quad (\text{B11}) \]

The center deflection for a given \( P \) can be found by obtaining \( u \) from the implicit expression (B10) and solve equation (B11).

REFERENCES

APPENDIX C: LARGE DEFLECTION BEHAVIOUR OF A CLAMPED-GUIDED BEAM

In fig. C1 a beam with one end fixed and the other guided is shown.

![Sketch of clamped-guided beam including parameters.](image)

**Fig. C1** Sketch of clamped-guided beam including parameters.

When one of the ends of the beam is not constrained in the $x$-direction axial forces will not develop. In this case large deflection theory has to be applied and the differential equation that has to be solved is:

$$M = EI \frac{d\theta}{ds} = P \left( L - x - \Delta \right) \tag{C1}$$

Differentiating with respect to $s$ gives:

$$\frac{d^2 \theta}{ds^2} = -\frac{P}{EI} \frac{dx}{ds} = -\frac{P}{EI} \cos \theta \tag{C2}$$

Integrating this equation results in:

$$\frac{1}{2} \left( \frac{d\theta}{ds} \right)^2 = -\frac{P}{EI} \sin \theta + C_i \tag{C3}$$

The integration constant $C_i$ can be found from the boundary condition, which is, that the slope at the middle of the beam is maximal $\theta_0$ from which follows:

$$C_i = \frac{P}{EI} \sin \theta_0 \tag{C4}$$

yielding:

$$\frac{d\theta}{ds} = \sqrt{\frac{2P}{EI} \left( \sin \theta_0 - \sin \theta \right)} \tag{C5}$$

The bar is assumed to be inextensible, in other words, it will not change its length during bending. Therefore:
\[ \int_0^{\theta_0} ds = \frac{1}{2} L \]  
(C6)

Combining equation (C5) and (C6) the following expression results:

\[ \frac{1}{2} L \sqrt{\frac{2P}{EI}} = \int_0^{\theta_0} \frac{d\theta}{\sqrt{\sin \theta_0 - \sin \theta}} \]  
(C7)

In order to bring the right side to the standard form of elliptic integrals a change in variables is introduced:

\[ \sin \phi = \sqrt{\frac{1 + \sin \theta}{1 + \sin \theta_0}} \]
\[ p = \sqrt{\left(\frac{1 + \sin \theta_0}{2}\right)^2} \]  
(C8)

After some manipulation this gives:

\[ \frac{1}{2} L \sqrt{\frac{P}{EI}} = \int_{\phi_1}^{\pi/2} \frac{d\phi}{\sqrt{1 - p^2 \sin^2 \phi}} \]
\[ \phi_1 = \arcsin \left(\frac{1}{2} p^2\right) \]  
(C9)

This equation has one unknown, the modulus \( p \). It can be found by trial and error and numerically solving the integral. Since \( dy = 2 \, ds \sin \theta \), the vertical deflection is given by:

\[ \delta = 2 \sqrt{\frac{EI}{2P}} \int_0^{\theta_0} \frac{\sin \theta \, d\phi}{\sqrt{\sin \theta_0 - \sin \theta}} \]  
(C10)

By using the same substitution as before:

\[ \delta = 2 \sqrt{\frac{EI}{P}} \int_{\phi_1}^{\pi/2} \frac{\left(2 \, p^2 \sin \phi - 1\right) \, d\phi}{\sqrt{1 - p^2 \sin^2 \phi}} \]  
(C11)

The expression for the deflection of a cantilever beam loaded at the tip can be obtained in the same way and is given by:

\[ \delta = \sqrt{\frac{EI}{P}} \int_{\phi_1}^{\pi/2} \frac{\left(2 \, p^2 \sin \phi - 1\right) \, d\phi}{\sqrt{1 - p^2 \sin^2 \phi}} \]  
(C12)
where \( p \) can be found from:

\[
\sqrt{\frac{pL^2}{EI}} = \int_{\phi_1}^{\pi/2} \frac{d\phi}{\sqrt{1 - p^2 \sin^2 \phi}}
\]

(C13)

REFERENCES

APPENDIX D: DETAILED PROCESSING SEQUENCES

PROCESS SEQUENCE: CURVED ELECTRODE ACTUATORS
COMB DRIVE ACTUATORS

Wafers: p-type, 100-oriented, 3 ", 5-10 Ωcm.

O 1 St. cleaning:
- 5 min. fuming nitric acid (100%) I
- 5 min. fuming nitric acid (100%) II
- DI quick dump rinse
- 15 min. boiling nitric acid (70%)
- DI quick dump rinse
- spin drying

O 2 Wet Thermal Oxidation, 2 µm SiO₂, 10 hrs at 1150 °C

O 3 LPCVD polySi:
- p - 250 mTorr
- T - 590 °C
- SiH₄ - 50 sccm
- t - 16 hrs (∼ 6 µm)

O 4 Thick PR front side protection

O 5 strip backside:

O 6 Standard cleaning

O 7 HF (1:100) dip

O 8 Diffusion, solid source boron diffusion, 1100 °C, N₂, 3 hours

O 9 Strip Boron Oxide, BHF, 60 min.

O 10 Standard cleaning

O 11 PECVD SiO₂:
- T - 300 °C
- SiH₄ - 200 sccm
- N₂O - 710 sccm
- p - 650 mTorr
- P - 60 Watt LF
- t - 21 min. d=0.6 µm

O 12 Standard S1805 Lithography:
PR spinning
- spinning HMDS, 4000 rpm, 30 sec.
- spinning Shipley S1805, 4000 rpm, 30 sec.

Pre bake, 20 min., 90 °C, hot plate
Exposure, vacuum contact, 2.5 sec., integrated, 15 mW/cm²,
Development, 60 sec., Shipley 351 developer (1:1)
Post bake, 20 min., 135 °C, hot plate
O 13  Clean RIE chamber,
   \( \text{O}_2 \) - 20 sccm
   \( p \) - 100 mTorr
   \( P \) - 200 Watt
   \( t \) - 10 min.
   styros electrode

O 14  RIE SiO\(_2\)
   \( \text{CHF}_3 \) - 10 sccm
   \( p \) - 20 mTorr
   \( P \) - 50 Watt
   styros electrode
   \( T \) - 25 °C
   \( t \) - \( \approx \) 21 min.

O 15  Plasma ash PR, 150 °C, 200 Watt, 50 sccm \( \text{O}_2 \), 15 min.

O 16  RIE polySi:
   \( \text{SF}_6 \) - 30 sccm
   \( \text{O}_2 \) - 11 sccm
   \( \text{CHF}_3 \) - 7 sccm
   \( p \) - 100 mTorr
   \( P \) - 100 Watt
   \( t \) - \( \approx \) visual etch stop, \( \approx \)9 min, 30 sec.
   Use etch step 5 min. maximal
   Silicon electrode
   critical step, do not overetch, stop -> inspection
   small gaps exhibit RIE lag!
   additional etch as short as possible if necessary

O 17  Standard cleaning also add dummy wafers

O 18  Sacrificial layer etching, BHF, 1 \( \mu \)m/ 13 min
   place dummy wafer opposite to front side process wafer

O 19  dilution rinsing, 15 min., use clean quartz/teflon ware in next steps

O 20  add IPA (carefull)

O 21  IPA rinse, 15 min.

O 22  Cyclohexane rinse, 15 min.

O 23  Freeze drying, -10 °C, \( \text{N}_2 \) flow

O 24  evaporation Al backside, 1 \( \mu \)m
PROCESS SEQUENCE: ELECTROSTATIC WOBBLE MOTORS

Wafers: p-type, 100-oriented, 3 ", 5-10 \( \Omega \) cm.

O 1  St. cleaning: 
- 5 min. fuming nitric acid (100\%) I
- 5 min. fuming nitric acid (100\%) II
- DI quick dump rinse
- 15 min. boiling nitric acid (70\%)
- DI quick dump rinse
- spin drying

O 2  LPCVD Si\(x\)Ny:
\[
\begin{align*}
 & p & - 200 \text{ mTorr} \\
 & T & - 850 \degree \text{C} \\
 & DCS & - 70 \text{sccm} \\
 & NH_3 & - 18 \text{sccm} \\
 & t & - 120 \text{ min.} \quad d \approx 1 \mu \text{m}
\end{align*}
\]

O 3  LPCVD polySi:
\[
\begin{align*}
 & p & - 250 \text{ mTorr} \\
 & T & - 590 \degree \text{C} \\
 & SiH_4 & - 50 \text{sccm} \\
 & t & - 90 \text{ min.} \quad d \approx 0.5 \mu \text{m}
\end{align*}
\]

O 4  Standard cleaning

O 5  HF (1:100) dip

O 6  Diffusion, solid source boron diffusion, 1100 \degree \text{C}, N\(_2\), 60 min.

O 7  Strip Boron Oxide, BHF, 60 min.
\(R_{sq} = 67.5 \\Omega\)

O 8  Standard S1813 Lithography **MASK STATOR:**
PR spinning 
- spinning HMDS, 4000 rpm, 30 sec.
- spinning Shipley S1813, 4000 rpm, 30 sec.
Pre bake, 20 min., 90 \degree \text{C}, hot plate
Exposure, 6 sec., integrated, 15 mW/cm\(^2\).
Development, 60 sec., Shipley MF 312 developer (1:1)
Post bake, 20 min., 135 \degree \text{C}, hot plate

O 9  RIE polySi:
\[
\begin{align*}
 & SF_6 & - 30 \text{sccm} \\
 & O_2 & - 10 \text{sccm} \\
 & CHF_3 & - 7 \text{sccm} \\
 & p & - 100 \text{ mTorr} \\
 & P & - 100 \text{Watt} \\
 & t & - \approx 2 \text{min.} \\
 & \text{Silicon electrode}
\end{align*}
\]

O 10 Standard cleaning

O 11 LPCVD Si\(x\)Ny:
\[
\begin{align*}
 & p & - 200 \text{ mTorr} \\
 & T & - 850 \degree \text{C} \\
 & DCS & - 70 \text{sccm} \\
 & NH_3 & - 18 \text{sccm}
\end{align*}
\]
Appendix D

O 12  Standard S1813 Lithography **MASK CONTACT**

O 13  RIE Si\textsubscript{x}N\textsubscript{y}

- CHF\textsubscript{3}  - 25 sccm
- O\textsubscript{2}  - 5 sccm
- p  - 10 mTorr
- P  - 75 Watt
- t  - \approx 60 min.  d= 0.5 \mu m

O 14  Standard cleaning

O 15  PECVD SiO\textsubscript{2}:

- T  - 300 °C
- SiH\textsubscript{4}  - 200 sccm
- N\textsubscript{2}O  - 710 sccm
- p  - 650 mTorr
- P  - 60 Watt LF
- t  - 70 min.  d=2 \mu m

O 16  Standard S1828 Lithography **MASK BEARING:**

- PR spinning  - spinning HMDS, 4000 rpm, 30 sec.
- - spinning Shipley S1828, 4000 rpm, 30 sec.

- Pre bake, 30 min., 90 °C, hot plate
- Exposure, 12 sec., integrated, 15 mW/cm\textsuperscript{2}
- Development, 60 sec., Shipley MF 312 developer (1:1)
- Post bake, 30 min., 120 °C, hot plate

O 17  RIE SiO\textsubscript{2}

- CHF\textsubscript{3}  - 10 sccm
- p  - 20 mTorr
- P  - 50 Watt
- styros electrode
- T  - 10 °C
- t  - \approx 58 min.

O 18  RIE Si\textsubscript{x}N\textsubscript{y}

- CHF\textsubscript{3}  - 25 sccm
- O\textsubscript{2}  - 5 sccm
- p  - 10 mTorr
- P  - 75 Watt
- t  - \approx 21 min.

O 19  O\textsubscript{2} plasma ashing:

- P  - 150 Watt
- T  - 150 °C
- O\textsubscript{2}  - 50 sccm
- p  - 2.00 mbar
- t  - 20 min.

O 20  RIE Si:

- SF\textsubscript{6}  - 12 sccm
- N\textsubscript{2}  - 100 sccm
- p  - 100 mTorr
- P  - 50 Watt
- aluminium electrode
Detailed processing sequences

O 21
Standard cleaning

O 22
LPCVD TEOS:
- \( p = 400 \ \text{mTorr} \)
- \( T = 700 \ ^\circ\text{C} \)
- \( t = 130 \ \text{min.} \)
- \( d = 1 \ \text{µm} \)

O 23
LPCVD polySi:
- \( p = 250 \ \text{mTorr} \)
- \( T = 590 \ ^\circ\text{C} \)
- \( \text{SiH}_4 = 50 \ \text{sccm} \)
- \( t = 360 \ \text{min.} \)
- \( d = 2.0 \ \text{µm} \)

O 24
Standard cleaning

O 25
HF (1:100) dip

O 26
Diffusion, solid source boron diffusion, 1100 °C, \( \text{N}_2 \), 60 min.

O 27
Strip Boron Oxide, BHF, 60 min.
- \( \text{R}_{sq} = 7.7 \ \Omega \)

O 28
Standard cleaning

O 29
HF (1:100) dip

O 30
Sputtering Si:
- \( p = 3.0 \ \times \ 10^{-2} \ \text{mbar} \)
- \( P = 500 \ \text{Watt} \)
- \( \text{Ar} = \text{stand 121} \)
- \( t = 6 \ \text{hours} \)
- \( d = 6.0 \ \text{µm} \)

O 31
Standard cleaning

O 32
Annealing, 450 °C, \( \text{N}_2 \), 1 hour

O 33
PECVD SiO\(_2\):
- \( T = 300 \ ^\circ\text{C} \)
- \( \text{SiH}_4 = 200 \ \text{sccm} \)
- \( \text{N}_2\text{O} = 710 \ \text{sccm} \)
- \( p = 650 \ \text{mTorr} \)
- \( P = 60 \ \text{Watt LF} \)
- \( t = 40 \ \text{min.} \)
- \( d = 1.2 \ \text{µm} \)

O 34
Standard S1813 lithography MASK ROTOR

O 35
RIE SiO\(_2\)
- \( \text{CHF}_3 = 10 \ \text{sccm} \)
- \( p = 20 \ \text{mTorr} \)
- \( P = 50 \ \text{Watt} \)
- \( t = \approx 25 \ \text{min.} \)

O 36
\( \text{O}_2 \) plasma ashing

O 37
RIE Si:
- silicon electrode
- \( \text{SF}_6 = 30 \ \text{sccm} \)
- \( \text{O}_2 = 10 \ \text{sccm} \)
- \( \text{CHF}_3 = 7 \ \text{sccm} \)
- \( p = 100 \ \text{mTorr} \)
- \( P = 100 \ \text{Watt} \)
- \( t = \approx 10 \ \text{min.} \)
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| O 41 | RIE backside  
  - strip poly: SF$_6$-30 sccm/p-50 mTorr/P-100 Watt/t-2'.40"  
  - strip TEOS/Si$_x$N$_y$: CHF$_3$,O$_2$-25:5,p-10 mTorr/P-75 Watt/t-16 min.  
  - strip poly: SF$_6$-30 sccm/p-50 mTorr/P-100 Watt/t-40'  
  - strip Si$_x$N$_y$: CHF$_3$,O$_2$-25:5,p-10 mTorr/P-75 Watt/t-13' |
| O 42 | Standard cleaning |
| O 43 | Sacrificial layer etching, undiluted HF (50%), 15 min. |
| O 44 | QDR rinsing + spin drying (slow) |
| O 45 | evaporation Al backside, 1 μm |
PROCESS SEQUENCE: ELECTROSTATIC ACTUATORS WITH INTEGRATED GEAR LINKAGES

Wafers: p-type, 100-oriented, 3", 5-10 Ωcm.

O1 St. cleaning:  
- 5 min. fuming nitric acid (100%) I  
- 5 min. fuming nitric acid (100%) II  
- DI quick dump rinse  
- 15 min. boiling nitric acid (70%)  
- DI quick dump rinse  
- spin drying

O2 HF dip
O3 LPCVD SiₓNy:  
\[ p = 200 \text{ mTorr} \]  
\[ T = 850^\circ\text{C} \]  
\[ \text{DCS} = 70 \text{ sccm} \]  
\[ \text{NH}_3 = 18 \text{ sccm} \]  
\[ t = 120 \text{ min.} \quad d \approx 1 \mu\text{m} \]

O4 HF dip
O5 LPCVD polySi:  
\[ p = 250 \text{ mTorr} \]  
\[ T = 590^\circ\text{C} \]  
\[ \text{SiH}_4 = 50 \text{ sccm} \]  
\[ t = 90 \text{ min.} \quad d \approx 0.5 \mu\text{m} \]

O6 Standard cleaning
O7 HF (1:100) dip
O8 Diffusion, solid source boron diffusion, 1100 °C, N₂, 60 min.
O9 Strip Boron Oxide, BHF, 60 min.
\[ \text{Rsq} = 67.5 \Omega \]
O10 Standard S1813 Lithography MASK STATOR:  
PR spinning  
- spinning HMDS, 4000 rpm, 30 sec.  
- spinning Shipley S1813, 4000 rpm, 30 sec.
Pre bake, 20 min., 90 °C, hot plate  
Exposure, 6 sec., integrated, 15 mW/cm²  
Development, 60 sec., Shipley MF 312 developer (1:1)
Post bake, 20 min., 135 °C, hot plate

O11 RIE polySi:  
\[ \text{SF}_6 = 30 \text{ sccm} \]  
\[ \text{O}_2 = 10 \text{ sccm} \]  
\[ \text{CHF}_3 = 7 \text{ sccm} \]  
\[ p = 100 \text{ mTorr} \]  
\[ P = 100 \text{ Watt} \]  
\[ t = \approx 2 \text{ min.} \]
Silicon electrode

O12 Standard cleaning
O13 HF dip
O14 LPCVD SiₓNy:
p - 200 mTorr
T - 850 °C
DCS - 70 sccm
NH₃ - 18 sccm
t - 60 min. \( d \approx 0.5 \mu m \)

O 15 Standard S1813 Lithography **MASK CONTACT:**
O 16 RIE SiₓNy
- CHF₃ - 25 sccm
- O₂ - 5 sccm
- p - 10 mTorr
- P - 75 Watt
- t - \( \approx 7 \) min.

O 17 Standard cleaning
O 18 PECVD SiO₂:
- T - 300 °C
- SiH₄ - 200 sccm
- N₂O - 710 sccm
- p - 650 mTorr
- P - 60 Watt LF
- t - 70 min. \( d = 2 \mu m \)

O 19 Standard S1813 Lithography **MASK REDUCTION:**
O 20 Wet etch SiO₂ BHF, 3 min., 45 sec.
O 21 Standard cleaning
O 22 Standard S1828 Lithography **MASK BEARING:**
- PR spinning - spinning HMDS, 4000 rpm, 30 sec.
- spinning Shipley S1828, 4000 rpm, 30 sec.

Pre bake, 30 min., 90 °C, hot plate
Exposure, 12 sec., integrated, 15 mW/cm²
Development, 60 sec., Shipley MF 312 developer (1:1)
Post bake, 30 min., 120 °C, hot plate

O 23 RIE SiO₂
- CHF₃ - 10 sccm
- p - 20 mTorr
- P - 50 Watt
- styros electrode
- T - 10 °C
- t - \( \approx 58 \) min.

O 24 RIE SiₓNy
- CHF₃ - 25 sccm
- O₂ - 5 sccm
- p - 10 mTorr
- P - 75 Watt
- t - \( \approx 21 \) min. (1 µm -> 14 min.)

O 25 O₂ plasma ashing:
- P - 150 Watt
- T - 150 °C
- O₂ - 50 sccm
- p - 2.00 mbar
t - 20 min.

O 26 RIE Si:
SF\textsubscript{6} - 12 sccm
N\textsubscript{2} - 100 sccm
p - 100 mTorr
P - 50 Watt
aluminium electrode
T - 10 °C
t - ≈ 10 min.

O 27 Standard cleaning
O 28 LPCVD TEOS:
p - 400 mTorr
T - 700 °C
- 50 sccm
t - 130 min. d = 1 µm

O 29 Standard S1828 Lithography MASK ANCHORS:
O 30 Wet etch SiO\textsubscript{2} BHF, 30 min.
O 31 Standard cleaning
O 32 HF (1:100) dip
O 33 LPCVD polySi:
p - 250 mTorr
T - 590 °C
SiH\textsubscript{4} - 50 sccm
t - 16 hours d = 6 µm

O 34 Standard cleaning
O 35 HF (1:100) dip
O 36 Diffusion, solid source boron diffusion, 1100 °C, N\textsubscript{2}, 60 min.
O 37 Strip Boron Oxide, BHF, 60 min.
Rsq = 2.2 Ω
O 38 PECVD SiO\textsubscript{2}:
T - 300 °C
SiH\textsubscript{4} - 200 sccm
N\textsubscript{2}O - 710 sccm
p - 650 mTorr
P - 60 Watt LF
t - 40 min. d=1.2 µm

O 39 Annealing, 3 hours, 1100 °C, N\textsubscript{2}, 180 min.
O 40 Standard S1813 lithography MASK ROTOR
O 41 RIE SiO\textsubscript{2}
CHF\textsubscript{3} - 10 sccm
p - 20 mTorr
P - 50 Watt
t - ≈ 25 min.

O 42 O\textsubscript{2} plasma ashing
O 43 Thick PR spinning and pre bake
O 44 RIE backside
strip poly/TEOS/Si\textsubscript{x}N\textsubscript{y}/poly: SF\textsubscript{6}-30 sccm/p-50 mTorr/P-100 Watt/t-15'
strip Si\textsubscript{x}N\textsubscript{y}: CHF\textsubscript{3},O\textsubscript{2}-25:5,p-10 mTorr/P-75 Watt/t-12'
O 45 O₂ plasma ashing

O 46 RIE Si:

- silicon electrode
- SF₆  - 30 sccm
- O₂  - 10 sccm
- CHF₃  - 7 sccm
- p  - 100 mTorr
- P  - 100 Watt
- t  - ≈ 19.5 min.

O 47 Lithography step coverage 6 μm **MASK OPEN RAILS**

1 PR spinning
- spinning HMDS, 4000 rpm, 30 sec.
- spinning Shipley S1828, 4000 rpm, 30 sec.

Pre bake, 20 min., 90 °C, hot plate

2 PR spinning
- spinning HMDS, 4000 rpm, 30 sec.
- spinning Shipley S1828, 4000 rpm, 30 sec.

Pre bake, 20 min., 90 °C, hot plate

Exposure, 90 sec., integrated, 15 mW/cm²
Development, 120 sec., Shipley 351 developer (1:1)

Post bake, 10 min., 120 °C, hot plate (large reflow)

O 48 RIE Si:

- SF₆  - 12 sccm
- N₂  - 100 sccm
- p  - 100 mTorr
- P  - 50 Watt

- aluminium electrode
- T  - 10 °C
- t  - ≈ 10 min.

O 49 Standard cleaning

O 50 Sacrificial layer etching, undiluted HF (50%), 50 min.

O 51 Freeze drying procedure

O 52 evaporation Al backside, 1 μm
PROCESS SEQUENCE: SEALED CAPACITIVE RESONATORS

Wafers: n-type, 100-oriented, 3", 5-10 Ωcm.

O 1  St. cleaning: - 5 min. fuming nitric acid (100%) I
- 5 min. fuming nitric acid (100%) II
- DI quick dump rinse
- 15 min. boiling nitric acid (70%)
- DI quick dump rinse
- spin drying

O 2  Standard 31 Lithography MASK ALN:
PR spinning - spinning HMDS, 4000 rpm, 30 sec.
- spinning Shipley 1400-31, 4000 rpm, 30 sec.
Pre bake, 20 min., 90 °C, hot plate
Exposure, 8 sec., integrated, 15 mW/cm²
Development, 60 sec., Shipley MF 312 developer (1:1)
Post bake, 20 min., 135 °C, hot plate

O 3  RIE Si: SF₆ - 12 sccm
P - 40 Watt
p - 20 mTorr
t - 2 min.

O 4  Standard cleaning

O 5  Standard 31 Lithography MASK BOT

O 6  Ion Implantation ICE Lab.
front side: B, 5*10¹⁵ cm⁻², 100 keV
back side: P, 1.5*10¹⁵ cm⁻², 150 keV

O 7  O₂ plasma ashing:
P - 150 Watt
T - 150 °C
O₂ - 50 sccm
p - 2.00 mbar
t - 30 min.

O 8  Standard cleaning

O 9  LPCVD Si₃N₄:
p - 200 mTorr
T - 800 °C
DCS - 22 sccm
NH₃ - 66 sccm
t - 36 min. d≈ 2000 Å

O 10  PECVD SiO₂:
T - 300 °C
SiH₄ - 200 sccm
N₂O - 710 sccm
p - 650 mTorr
P - 60 Watt LF
t - 35 min. d≈1 µm

O 11  Annealing 30 min. at 800 °C in N₂
O 12 Standard 31 Lithography MASK GAP
O 13 Wet etching in BHF, ≈ 13 min.
O 14 Standard cleaning
O 15 HF-dip (1:50), 1 min.
O 16 LPCVD polySi (beam):
   \begin{align*}
   \text{p} & \quad - \quad 250 \text{ mTorr} \\
   \text{T} & \quad - \quad 590 \text{ °C} \\
   \text{SiH}_4 & \quad - \quad 50 \text{ sccm} \\
   \text{t} & \quad - \quad 240 \text{ min.} \quad \text{d}=1.5 \mu \text{m}
   \end{align*}

O 17 Standard 31 lithography MASK TOP
O 18 Ion implantation ICE Lab.
   front side: B, $1 \times 10^{16}$ cm$^{-2}$, 100 keV
O 19 O$_2$ plasma ashing, 30 min.
O 20 Standard 31 lithography MASK POLYCUT
O 21 RIE polySi:
   \begin{align*}
   \text{SF}_6 & \quad - \quad 12 \text{ sccm} \\
   \text{N}_2 & \quad - \quad 100 \text{ sccm} \\
   \text{p} & \quad - \quad 100 \text{ mTorr} \\
   \text{P} & \quad - \quad 20 \text{ Watt} \\
   \text{t} & \quad - \quad \approx 7 \text{ min.}
   \end{align*}

O 22 Standard cleaning
O 23 PECVD SiO$_2$:
   \begin{align*}
   \text{T} & \quad - \quad 300 \text{ °C} \\
   \text{SiH}_4 & \quad - \quad 200 \text{ sccm} \\
   \text{N}_2\text{O} & \quad - \quad 710 \text{ sccm} \\
   \text{p} & \quad - \quad 650 \text{ mTorr} \\
   \text{P} & \quad - \quad 60 \text{ Watt LF} \\
   \text{t} & \quad - \quad 52 \text{ min.} \quad \text{d}=1.5 \mu \text{m}
   \end{align*}

O 24 Annealing 30 min. at 800 °C in N$_2$
O 25 Standard lithography MASK PSG
O 26 Wet etching in BHF, ≈ 20 min.
O 27 Standard cleaning
O 28 Wet thermal oxidation (channel oxide):
   \begin{align*}
   1000 \text{ °C}, 12 \text{ min. without ramp, N}_2-1 \text{ slm} \quad \text{d} \approx 800 \text{Å}
   \end{align*}
O 29 Standard 31 lithography MASK ANCHOR
O 30 Wet etching in BHF, ≈ 2.5 min.
O 31 Standard cleaning
O 32 HF-dip (1:50), 1 min.
O 33 Optional: LPCVD Si$_3$N$_4$:
   \begin{align*}
   \text{p} & \quad - \quad 200 \text{ mTorr} \\
   \text{T} & \quad - \quad 800 \text{ °C} \\
   \text{DCS} & \quad - \quad 22 \text{ sccm} \\
   \text{NH}_3 & \quad - \quad 66\text{sccm} \\
   \text{t} & \quad - \quad 9 \text{ min.} \quad \text{d}=500 \text{ Å}
   \end{align*}

O 34 Standard cleaning
O 35 HF-dip (1:50), 1 min.
O 36 LPCVD polySi (Cap):
   \begin{align*}
   \text{p} & \quad - \quad 250 \text{ mTorr}
   \end{align*}
Detailed processing sequences

T - 590 °C
SiH₄ - 50 sccm
t - 420 min. \( \approx 2.7 \mu m \)

O 37 Optional: Blanket ion implantation ICE Lab.
  front side: B, \( 1 \times 10^{16} \text{ cm}^{-2} \), 100 keV

O 38 St. cleaning

O 39 Mechanical anneal, 60 min., 900/1000 °C in N₂

O 40 Standard 37 Lithography MASK CAP:
  PR spinning
    - spinning HMDS, 3500 rpm, 30 sec.
    - spinning Shipley 1400-37, 3500 rpm, 30 sec.
  Pre bake, 30 min., 90 °C, hot plate
  Exposure, 8 sec., integrated, 15 mW/cm²
  Development, 60 sec., Shipley MF 312 developer (1:1)
  Post bake, 30 min., 135 °C, hot plate

O 41 RIE polySi:
  SF₆ - 5 sccm
  N₂ - 50 sccm
  p - 20 mTorr
  P - 40 Watt
  t - \( \approx 15 \text{ min.} \)

  SF₆ - 12 sccm
  N₂ - 100 sccm
  p - 100 mTorr
  P - 20 Watt
  t - \( \approx 18 \text{ min.} \)

O 42 O₂ plasma ashing

O 43 Standard cleaning

O 44 Sacrificial layer etch, HF (50%), 120 min.

O 45 Dilution rinse in DI water, 60 min.

O 46 IPA rinse, 60 min.

O 47 Cyclohexane rinse, 60 min.

O 48 Freeze drying, 15 min. in N₂

O 49 (a) Optional: Dry thermal oxidation, 800 °C, O₂- 1 slm, 5-10 min.
  (b) LPCVD Si₃N₄:
    p - 200 mTorr
    T - 800 °C
    DCS - 66 sccm
    NH₃ - 22 sccm
    t - 36 min. \( \approx 2000 \text{ Å} \)

O 50 Standard 37 lithography MASK CONTACT

O 51 RIE Si₃N₄
  CHF₃ - 25 sccm
  O₂ - 5 sccm
  p - 10 mTorr
  P - 75 Watt
  t - 2.5 min.

O 52 Standard cleaning
O 53  HF-dip (1:50), 1 min.
O 54  Evaporation of Al, front side, d= 1 µm
O 55  Standard 37 lithography, no mask
O 56  RIE backside
       strip Si₃N₄, 2000 Å
       strip poly, 2.7 µm
       strip Si₃N₄, 500 Å
       strip poly, 1.5 µm
       strip Si₃N₄, 2000 Å
O 57  Standard 37 lithography MASK METAL
O 58  PAN etch, 40 °C, 5 min.
O 59  Resist strip in fuming nitric acid, 10 min.
O 60  Evaporation of Al, back side, d= 1 µm
O 61  Annealing, 10 min. in N₂/H₂O at 450 °C
APPENDIX E: ELECTROSTATICS

Electrostatics in free space

An electrostatic field due to stationary electric charges in free space will be considered. The fundamental postulates of electrostatics in free space are:

\[ \nabla \cdot \vec{E} = \frac{\rho}{\varepsilon_0} \tag{E1} \]

\[ \nabla \times \vec{E} = 0 \tag{E2} \]

where \( \vec{E} \) is the electric field intensity which is defined as the force per unit charge that a very small stationary test charge experiences when it is placed in a region where an electric field exists, \( \rho \) is the volume charge density of free charges and \( \varepsilon_0 \) is the permittivity of free space (\( \varepsilon_0 = 8.85 \times 10^{-12} \)). The first equation implies that a static electric field is not solenoidal unless \( \rho = 0 \). The second equation asserts that static electric fields are irrotational.

In practical applications one is usually interested in the total field of an aggregate or a distribution of charges. This can be more conveniently obtained by the integral forms of eq. (E1) and (E2):

\[ \oint_S \vec{E} \cdot ds = \frac{Q}{\varepsilon_0} \tag{E3} \]

where \( Q \) is the total charge contained in the volume \( V \) bounded by the surface \( S \). This equation is a form of Gauss's law which asserts that the total outward flux of the E-field over any closed surface in free space is equal to the total charge enclosed in the surface divided by \( \varepsilon_0 \). By integration of the curl relation over an open surface and invoking Stokes’s theorem one has:

\[ \oint_C \vec{E} \cdot dl = 0 \tag{E4} \]

where the line integral is performed over a closed contour \( C \) bounding an arbitrary surface.

Electric Potential

Because scalar quantities are easier to handle than vector quantities the curl-free electrostatic vector field is expressed as the gradient of a scalar field which is called the electric potential:
\[ \vec{E} = -\nabla V \]  \hspace{1cm} (E5)

The electric potential has physical significance as it is related to the work in carrying a charge from one point to another. The work must be done against the field explaining the negative sign. The potential difference between two points \( P_2 \) and \( P_1 \) is defined by:

\[ V_2 - V_1 = \int_{P_1}^{P_2} \vec{E} \, dl \]  \hspace{1cm} (E6)

**Conductors in static electric fields**

In this section the electric field and charge distribution inside the bulk and on the surface of a conductor will be examined. If an electric field is set up in a conductor the exerting electric force will redistribute introduced charges in such a way that both the charge and the field inside vanish. When there is no charge in the interior of a conductor, \( \vec{E} \) will be zero. Since \( \vec{E} = 0 \) inside a conductor it has the same electric potential everywhere. The surface of a conductor is an equipotential surface. Under static conditions the \( \vec{E} \)-field on a conductor surface is everywhere normal to the surface. The tangential component of the \( \vec{E} \)-field on a conductor surface is zero. The normal component of the \( \vec{E} \)-field at a conductor/free-space boundary is equal to the surface charge density divided by the permittivity of free space.

**Dielectric in static electric fields**

Ideal dielectrics do not contain free charges but contain bound charges. A dielectric material can be polarised in the presence of an external electric field. Because a polarised dielectric gives rise to an equivalent volume charge density \( \rho_p \) eq. (E1) must be modified to include the effect of \( \rho_p \):

\[ \nabla \cdot \vec{E} = \frac{\rho + \rho_p}{\varepsilon_0} \]  \hspace{1cm} (E7)

Defining a polarisation vector \( \vec{P} \) as:

\[ -\nabla \cdot \vec{P} = \rho_p \]  \hspace{1cm} (E8)
equation (E7) can be written as:

\[ -\nabla \cdot \left( \varepsilon_0 \vec{E} + \vec{P} \right) = \rho \]  \hspace{1cm} (E9)

Now a new fundamental field quantity, the electric flux density \( \vec{D} \), is defined such that:

\[ \vec{D} = \varepsilon_0 \vec{E} + \vec{P} \]  \hspace{1cm} (E10)

The use of vector \( \vec{D} \) enables one to write a divergence relation between the electric field and the distribution of free charges in any medium without dealing with the polarization vector \( \vec{P} \) or the polarization charge density \( \rho_p \). Combining eqs. (E9) and (E10) gives:

\[ -\nabla \cdot \vec{D} = \rho \]  \hspace{1cm} (E11)

or in integral form:

\[ \oint_s \vec{D} \cdot ds = Q \]  \hspace{1cm} (E12)

Equation (E12) is another form of Gauss's law stating that the total outward electric flux over any closed surface is equal to the total free charge enclosed in the surface. When the dielectric properties of the medium are linear and isotropic the polarization is directly proportional to the electric field intensity and:

\[ \vec{D} = \varepsilon_0 \varepsilon_r \vec{E} \]  \hspace{1cm} (E13)

where \( \varepsilon_r \) is a dimensionless constant known as the relative permittivity or dielectric constant of the medium.

If the electric field is very strong bound electrons can be accelerated under the influence of the electric field and avalanche effects of ionization due to collisions may occur. This phenomenon is called dielectric breakdown. The maximum electric field intensity that a dielectric material can withstand without dielectric breakdown is called the dielectric strength of the material.
Capacitance

The potential of an isolated conductor is directly proportional to the total charge on it. One can write:

\[ Q = CV \]  \hspace{1cm} (E14)

where the constant \( C \) is called the capacitance of the isolated conducting body. The capacitance is the electric charge that must be added to the body per unit increase in its electrical potential. A capacitor consists of two conductors separated by free space or a dielectric medium. Of considerable importance are parallel plate capacitors which consists of two parallel conducting plates of area \( A \) separated by a uniform distance \( d \). A cross section is shown in fig. E1. The plates have a uniformly distributed charges \( +Q \) and \( -Q \) with surface densities \( +\rho_s \) and \( -\rho_s \).

![Fig. E1 Cross section of a parallel-plate capacitor.](image)

The electrical flux density can be found from eq. (E12). The total outward electric flux over a rectangular box enclosing one of the capacitor plates as shown in fig. E1 only has a contribution from the lower face which is equal to \( \vec{D} \cdot A \) when fringing fields at the edges are neglected. This total outward electric flux is equal to the total free charge \( Q \) enclosed. Using expression (E13) the expression for the electrostatic field becomes:

\[ E = \frac{Q}{\varepsilon \varepsilon_0 A} \]  \hspace{1cm} (15)

The potential difference can be found from eq. (E6):

\[ V_{21} = V_2 - V_1 = \int_{\varepsilon}^{d} \frac{Q}{\varepsilon \varepsilon_0 A} dl = \frac{Q d}{\varepsilon \varepsilon_0 A} \]  \hspace{1cm} (E16)

and finally the capacitance for the parallel plate capacitor can be found from eq. (E14) yielding:
\[ C = \frac{Q}{V_{21}} = \frac{\varepsilon_r \varepsilon_0 A}{d} \]  \hspace{1cm} (E17)

Capacitors are often combined in various ways in electric circuits. The two basic versions are series and parallel connections. The equivalent capacitance \( C_{ser} \) of several capacitances in series is given by:

\[ \frac{1}{C_{par}} = \frac{1}{C_1} + \frac{1}{C_2} + \frac{1}{C_3} + \cdots + \frac{1}{C_n} \]  \hspace{1cm} (E18)

and the equivalent capacitance \( C_{par} \) of several parallel connected capacitances is given by:

\[ C_{par} = C_1 + C_2 + C_3 + \cdots + C_n \]  \hspace{1cm} (E19)

**Electrostatic Energy and Forces**

As already stated before the electric potential is related to the work in carrying a charge from one point to another. The electrical energy in the form of several quantities can be found from:

\[ W_e = \frac{1}{2} Q V = \frac{1}{2} \int \partial \cdot E \, dv = \frac{1}{2} C V^2 \]  \hspace{1cm} (E20)

The electrostatic co-energy is defined by:

\[ W'_e = W_e - Q V = -\frac{1}{2} Q V = -\frac{1}{2} \int \partial \cdot E \, dv = -\frac{1}{2} C V^2 \]  \hspace{1cm} (E21)

When an isolated system of charged bodies is considered, the mechanical work done by the system is:

\[ dW = F_Q \, dl \]  \hspace{1cm} (E22)

where \( F_Q \) is the total electrostatic force acting on the body under the condition of constant charges. Since the system is isolated with no external supply of energy the mechanical work must be done at the expense of the stored electrostatic energy that is:

\[ dW = -dW_e = -F_Q \, dl \]  \hspace{1cm} (E23)

Since \( dl \) is arbitrary this leads to:
\[ F_Q = -\nabla W_e \] 

(E24)

In cartesian coordinates the component forces are:

\[
\begin{align*}
(F_Q)_x &= -\frac{\partial W_e}{\partial x} \\
(F_Q)_y &= -\frac{\partial W_e}{\partial y} \\
(F_Q)_z &= -\frac{\partial W_e}{\partial z}
\end{align*}
\] 

(E25 a,b,c)

or in case the system can rotate about an axis the torque \( T_Q \) can be found from its derivative to the angle of rotation \( \phi \):

\[
T_Q = -\frac{\partial W_e}{\partial \phi}
\]

(E26)

When the conducting bodies are held at a fixed potential through connections to external sources a displacement by a conducting body would result in a change in total electrostatic energy and requires the sources to transfer charges to the conductors in order to keep them at their fixed potentials. The total energy supplied by the sources to the system is:

\[
dW_{source} = V \, dQ
\]

(E27)

The mechanical work done by the system is:

\[
dW = F \, V \, dl
\]

(E28)

where \( F \) is the electric force on the conducting body under the condition of constant potential. The charge transfer also changes the electrostatic energy of the system by an amount:

\[
dW_e = \frac{1}{2} V \, dQ
\]

(E29)

Conservation of energy demands that:

\[
dW + dW_e = dW_{source}
\]

(E30)

The electrostatic force under the condition of constant potential now becomes:
\[ f_v = \nabla W_e \]  
(E31)

which only differs by a sign change from the electrostatic force at constant charge.

**Static or Dynamic electrostatic fields?**

A final question that has to be answered is when electrostatic fields can be considered as quasi-static fields or have to be considered as dynamic fields. For the time-harmonic varying electrostatic field, with angular frequency \( \omega \), of a parallel plate condensator with circular plates that have a radius \( R \) that is large compared to the plate distance \( d \) the following expression can be derived:

\[
E = E_0 \left[ 1 - \frac{\omega^2 \varepsilon_0 \varepsilon_r \mu_0 r^2}{4} \right]
\]  
(E32)

This electric field is can be approximated by a time dependent quasi-static electrostatic field when:

\[
\frac{\omega R}{2c} << 1 \]  
(E33)

where \( c \) is the speed of light in vacuum (3.10^8 m/s). For example for a radius of 1mm a quasi static approach can be used up to frequencies of about 100 Ghz.
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BIOGRAPHY

Rob Legtenberg was born on June 21, 1964, in Hengelo, The Netherlands. He received a B.Sc. degree in Applied Physics from the Hogere Technische School, Enschede, The Netherlands, in 1985. His graduate project at the University of Twente, Enschede, The Netherlands, dealt with the fabrication of passive and active planar optical waveguides by spin- and dip coating.

After fulfilling his military service, he joined the Micromechanical Research Group at the University of Twente as a process engineer in 1986. His work included the development of micromachining techniques and fabrication and characterisation of micromechanical devices. The main subjects were resonating microbridge mass flow sensors and vacuum encapsulated electrostatically driven polysilicon resonators. Cooperative work on the realisation of resonating force sensors, micropumps, flow sensors and liquid dosing systems was also performed.

In 1992 he started the research on electrostatic actuators fabricated by surface micromachining techniques, as described in this thesis, in the same group at the MESA Research Institute at the University of Twente. During the summer of 1994 he visited the MEMCAD group at the Massachusetts Institute of Technology (MIT), supervised by Prof. S. D. Senturia, to work on coupled electromechanical simulations of curved electrode actuators using the CoSolve-EM system.

Besides his work he enjoys music and piano playing, reading, billiards, fitness, as well as jogging and outdoor activities. Growing bonsai trees, home improvement and spending time with friends are also amongst his favourite activities.


In 1992 trad hij als promovendus in dienst van het MESA Research Institute van de Universiteit Twente en startte het onderzoek naar electrostatische actuatoren die door middel van "surface micromachining" technieken worden gefabriceerd, zoals beschreven in dit proefschrift. Van juni 1994 tot en met augustus 1994 werkte hij aan het Massachusetts Institute of Technology (MIT), onder leiding van Prof. S. D. Senturia, aan gekoppelde electromechanische simulaties van kromme electrode actuatoren met behulp van het CoSolve-EM systeem.

Naast zijn werk houd hij van muziek, piano spelen, lezen en biljarten. Hij slooft zich regelmatig uit in het fitness centrum, doet wat aan joggen en houd van kamperen. Verder zijn het kweken van bonsai, klussen in huis en uitgaan met vrienden favoriete bezigheden van hem.