SURFACE TENSION SELF-ASSEMBLY FOR
THREE DIMENSIONAL
MICRO-OPTO-ELECTRO-MECHANICAL
SYSTEMS

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Abstract

A surface tension self-assembly process is modelled and demonstrated for three dimensional Micro-opto-electro-mechanical systems (MOEMS). For MOEMS, 3-dimensional (3-D) rotated components which process free space beams travelling above the surface of a chip have important applications for displays and telecommunication components. The surface tension self-assembly process is an active method used to power the rotation and provides a compact and automated batch process.

In this thesis, the history of the MOEMS techniques and applications are briefly reviewed. The existing theoretical models for the surface tension powered self-assembly are summarized, and then a more complicated three dimensional model is suggested and compared with other models.

Surface tension self assembled corner cube reflectors (CCRs), which is a functional optical device used for optical communication links, are then demonstrated and their thermal and mechanical stability is assessed. The fabrication process consists of deep reactive ion etching (DRIE) of bonded silicon-on-insulator (BSOI) material, sacrificial etching, and out-of-plane rotation powered by remelting of thick photoresist pads. The CCR shows very accurate mechanical alignment less than 0.18° and the optical characterisations show that a signal-to-noise ratio of more than 30 dB and data transmission rate around 200bit/s can be achieved at a low drive voltage of 30V, which is a good performance required for an optical communication link. The thermal stability of mir-
rors used in the CCR shows very small angle variation under the thermal stress of $< 0.006^\circ/\circ C$. The stability further improved to near zero when a mechanical limiter mechanism is adapted. The lifetime of the mirror under the mechanical vibration is measured to more than $1.4 \times 10^7$ cycles of the internal resonance, enough for practical applications.

A non-linear model for an angular vertical combdrive used for the CCR actuation is also suggested. The model shows a good agreement with the experiment data and can be used to predict and determine an initial misalignment of the CCR mirrors.

Finally, other applications are suggested as future works, and as an example, some preliminary results for a scanner with a linked drive have demonstrated.
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Chapter 1

Introduction

1.1 Three-dimensional MEMS fabrication techniques

Micro-electro-mechanical systems (MEMS) techniques have been developed to manufacture small low-cost sensors and actuators since the 1960’s. As one of the challenging tasks of the MEMS technology, three-dimensional (3D) MEMS fabrication processes have been widely developed and several innovative 3D devices have been demonstrated. These 3D MEMS techniques can be classified into three major processes, which are bulk micromachining, surface micromachining and the LIGA process.

Bulk micromachining has been based on the various etching techniques, including isotropic and anisotropic wet and dry etching [1, 2], to selectively remove significant amounts of silicon from a substrate. Using bulk silicon etching combined with other techniques including electroplating and wafer bonding, a wide variety of quasi 3D devices - such as pumps [3], valves [4], flow sensors [5], ink jet print heads [6, 7], accelerometers [8, 9] and pressure sensors [10, 11] - have been demonstrated and commercialized. For example, Figure 1.1 shows a scanning-electron microscope (SEM) of an accelerometer. Figure 1.1(a) shows a single-crystal silicon leaf spring of the accelerometer etched
through a top wafer and bonded to an underlying silicon substrate with pre-etched pits, allowing the released spring to move. Figure 1.1(b) shows another integrated view of an accelerometer. The accelerometer structure is released by a deep reactive ion etching (DRIE) and integrated with on-chip CMOS signal-processing circuitry. The inset shows the individual silicon electrode fingers, which are 4\(\mu m\) wide and 60\(\mu m\) tall. However, most of these bulk micromachining devices have a limited range of structural dimensions and thickness. The structure is based on projections of parallel sets of two-dimensional patterns. The maximum feature height is typically limited by the thickness of a substrate - for four inch silicon wafer, it is about 500\(\mu m\). Moreover, bulk micromachining is difficult to obtain high resolution, an essential requirement in recent applications, because of the deep silicon etching used.

To overcome this thickness constraint, there is alternative technology, the so-called LIGA (Lithographie, Galvanoformung, Abformung) process. It is a thick photoresist-based fabrication procedure which requires X-ray photons for exposure. Since the LIGA process uses very short X-ray wavelengths for exposure, diffraction effects are negligible
and the limit on depth is the available thickness of resist. The thickness limitation is further improved by the fabrication of thick sheets of resist, usually PMMA, and the attachment of the exposed PMMA sheets to a substrate with plating bases. This process has realised a 1.6 cm resist height [12]. On the other hand, replanarization after electroplating in the presence of the PMMA enables the fabrication of more complicated structures. Figure 1.2 shows gears fabricated by the LIGA process. Figure 1.2(a) shows a 2 – mm deep microwave cavity resonator [13]. LIGA process provides highly vertical and smooth sidewalls as shown in Figure 1.2(a). However, the LIGA process has two drawbacks. Usually, LIGA requires very expensive equipment (i.e. a synchrotron), and produces only fixed parts. To improve these restrictions, which are critical for real 3D MEMS devices, another technique is required. Figure 1.2(b) demonstrates an alternative technology where the LIGA process is complemented by the surface micromachining. A sacrificial layer underneath the gear was removed after gear patterning so that the gear could be rotated by the rotation of an assembled shaft.
Surface micromachining involves the fabrication of micromechanical structures from thin films. Since surface micromachined structures are formed using an IC planar technology, they are typically laminar. However, sacrificial layer etching technology and the fabrication of hinges for out-of-plane assembly of devices allows 3D microstructures to be formed [14].

The basic surface micromachining process sequence can be divided into three modules. First, a sacrificial layer is deposited on the bare silicon wafer and patterned [Figure 1.3(a)]. Glass or polymer is generally used as a sacrificial layer. A passivation layer, such as a silicon nitride, can be deposited under the sacrificial layer, depending on the device application. Second, a structural layer is deposited and patterned [Figure 1.3(b)]. Silicon or metal is normally used as a structural layer. In this stage, stress control of the structural layer is important. If a strain gradient is present, it causes bending of the microstructure upon release. Although doping and thermal annealing are used to achieve a stress-released layer, it is difficult to obtain stress-free polysilicon. Recently, the problem has been solved using bonded-silicon-on-insulator (BSOI) wafers, which use single crystal silicon as a structural layer. Finally, the sacrificial layer is removed to release the mechanical parts [Figure 1.3(c)]. For the glass layer, etching in aqueous hydrofluoric acid (HF) is preferred. After dissolving the sacrificial layer, the wafer is rinsed in deionized water, and dried in a way that avoids its collapse and adhesion to the substrate - a phenomenon known as “stiction”. There are a variety of methods to prevent stiction. The released structures can be dried with sublimation technique (freeze-drying), or using a methanol-water mixture rinse (evaporation drying), or using a supercritical \( \text{CO}_2 \) technique (supercritical drying). Hydrophobic self-assembling monolayer (SAM) films can also be coated on the released structures prior to removal from the aqueous stage [15]. In case of polymer layer, the sacrificial layer can be removed using oxygen plasma. Since the plasma ashing does not require rinsing, the
stiction problem can be avoided by using polymer sacrificial layer.

By repeating these steps, one can build extremely complex structures. Examples of these are the Sandia National Laboratories five-level Sandia Ultra-Planar Multi-Level MEMS Technology (SUMMiT) [16] and the Microelectronics Center of North Carolina (MCNC) three-level Multi-User MEMS Process (MUMPS) [17]. Figure 1.4 shows examples of devices fabricated using these technologies. Figure 1.4(a) is a cross-sectional SEM view of the Sandia five-level polysilicon architecture. Figure 1.4(b) is an overhead SEM view of the salient structural features typical in MUMPS process [14]. However, both of the devices are still largely 2D structures.

There are two approaches to realise 3D surface micromachined devices. The central idea of the first approach is to use deep-etched patterns in the silicon wafer as molds for
deposited films either of polysilicon or of metal. This micromolding technique, named HEXSIL, has been used to fabricate thermally actuated microtweezers which, combined with mechanical leverage, allowed large displacements [18, 19]. Figure 1.5 shows a photograph of fabricated HEXSIL tweezers, made by MEMS Precision Instruments. Figure 1.5(a) is an overview of overhanging micro-tweezers with a compliant linkage system. Figure 1.5(b) is close-up of 80 $\mu$m tall HEXSIL tweezers.

The second approach uses controlled out-of-plane rotation, which can be implemented by various folding mechanisms. Devices using these mechanisms are largely classified into four groups [21] - bimorph structures, mechanically folded structures, structures based on external forces, and unimorph structures. Bimorph structures typically exploit the thermal expansion difference between two different materials [22]. Figure 1.6(a) shows a schematic diagram for a thermally driven bimorph cantilever actuator [22]. The thermal actuation can be controlled using an integrated heater. Figure 1.6(b) shows a tunable inductor using bimorph microactuators [23]. Because they can be used in compact configuration and addressed individually, bimorph structures are suitable for applications where arrays of actuators are necessary. The main drawback of a bimorph is that the bending radii for out-of-plane structures are normally large as
can be seen from the Figure 1.6(b). The accuracy of the erected structures is therefore not very good.

Mechanical folding methods use an external manipulator to fold a flexible or elastic joint or hinge. This technique has been widely utilized to create a number of complex and useful microstructures [24, 25]. Since this technique requires the use of an external manipulator, it is not batch compatible. It also requires a long time and large space for assembly of the device [26]. Figure 1.7(a) shows a complete self-actuated micromirror. A large area is occupied by the comb resonators that are used to fold the mirror. Figure 1.7(b) is a SEM view of a Fresnel lens assembled using a manual manipulator.

It is also possible to rotate a structure in an automated way by applying an external force to the structure. Electrostatic [27], magnetic [28, 29], or pneumatic forces [30] can be used to rotate movable parts. Figure 1.8 shows examples actuated by external force. Figure 1.8(a) is mirrors actuated by externally applied magnetic force [29], and Figure
CHAPTER 1. INTRODUCTION

Figure 1.7: MEMS devices constructed by mechanical folding [44].

Figure 1.8: Devices using external force actuation.

1.8(b) is an example of pneumatic balloon actuator [30]. This technique can provide an automated batch fabrication and a compact chip design. However, since the force is applied externally, this technique is difficult to integrate in asynchronously driven arrays and to control each individual actuator in a large array of folded structures.

The unimorph structures use volume changes in just one material for static out-of-plane rotation of microstructures. Most of the previous techniques need either an external manipulator or integrated push-pull actuators to raise the structure, whereas
the unimorph technique uses a parallel batch self-assembly technique [21, 31, 32]. Two unimorph processes, polymer shrinkage [21] and surface tension powered self-assembly [33, 34], have been suggested. In the polymer shrinkage process, a series of V-grooves which are filled with polyimide constitute a flexible joint. Rotation is powered by thermal expansion of the polyimide. However, this process requires a large number of grooves to obtain sufficient deflection. It is therefore unsuitable for precise assembly of structures such as optical components [Figure 1.9(a)] [35]. In the surface tension powered self-assembly process, the surface tension of a pad of meltable material powers the rotation. Because surface tension forces scale advantageously in the microstructure size domain, the technique is appropriate for compact geometries. However, it only allows one-time operation, and so is suited for structural assembly rather than actuation [Figure 1.9(b)] [36]. Dynamic actuation of the device can then be obtained incorporating other parts, such as combdrives.
1.2 Three-dimensional micro-opto-mechanical systems (MOEMS)

Apart from the classification of technical aspect, MEMS can be classified according to application areas, examples of which are RF MEMS, BioMEMS, and optical MEMS (MOEMS). Recently, the continuing growth in optical system applications such as optical-fiber telecommunication infrastructures and display technologies has increased attention paid to MOEMS. The emphasis of the research has mainly focused on the miniaturization and batch-process production of optical components with precise alignment and low cost. The applications of MOEMS can be classified into two groups. One is the optical structure required for precise alignment and the other is a more advanced system using small moving optical parts including display devices, optical switches, tunable lasers and filters, optical pickup disk heads and “smart dust motes”.

1.2.1 MOEMS for precisely aligned optical packaging

For optical systems including fiber-laser module and tunable laser diodes, precise alignment in the packaging of optoelectronic devices and subsystems is critical for efficiency. For example, high performance fiber-laser modules require submicrometer alignment tolerance [37]. In order to achieve precise alignment, conventional optoelectronic components are packaged using expensive and time-consuming methods. Using optical MEMS technology - so called silicon-optical-bench (SOB), extremely accurate, low-loss, optical connections between components can be achieved.

SOB technology has been widely demonstrated using anisotropic etching of single crystal silicon. In its early stage in the 1970’s, simple V-shaped grooves, which can be formed by wet etching of (100) oriented Si substrate, were used as mechanical mounts for optical fibers [38]. Two fibers can be aligned in a groove and permanently attached to
Figure 1.10: Si opto-hybrid: passive alignment from laser to optical fiber [41].

silicon chips to accuracies of 1 µm. In addition, V-grooves can be used to construct opto-
hybrids, where a fiber is accurately aligned to other components such as photodiodes, 
lasers and detectors to form more advanced systems [39, 40]. Figure 1.10 shows an 
example of an opto-hybrid, which shows passive optical alignment from a laser to a 
single mode fiber [41]. For more complex and robust components, the LIGA process 
has been adapted for the fabrication of optical MEMS devices such as multi-axis flexures 
[42].

However, as the optoelectronic systems require faster and more complicated modules 
such as a high-speed fiber-pigtailed laser as required in a high-bitrate optical network, 
the precision of the earlier SOB technology, typically ~ 1 µm, is insufficient. An alter-
native method of constructing optical breadboards has been suggested using microcom-
ponents that are fabricated by surface micromachining and out-of-plane rotation. In 
this method, various optical components are first constructed monolithically on a silicon 
substrate. Micromirrors, which can be rotated using actuators, then actively align the 
components [43, 44]. Using this active SOB technology, complicated optical system can 
be monolithically integrated on a single chip with a positioning accuracy up to 11nm
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Figure 1.11 shows a SEM micrograph of a micro-XYZ stage on a free space microoptical bench [47]. The incoming beam from a semiconductor laser is reflected first by the lower mirror and then by the upper mirror, which are integrated with a linear translation stage. In this system, X-adjustment is achieved by translation of the lower mirror, Y-adjustment by the translation of the micro-Fresnel lens, and Z-adjustment by the translation of the upper mirror. Since the resolution of translator, a scratch drive actuator, is 11 nm for each electric pulse actuation, this active SOB system can be precisely controlled into the submicrometer domain. These structures are, however, manually assembled using micromanipulators, or using surface-micromachined engines to push hinged devices out of the wafer plane [48].

Figure 1.11: The SEM micrograph of a micro-XYZ stage on a free space microoptical bench [47].

Some alternative techniques have been developed to realize fully automatic assembly for 3D MOEMS. Surface tension self-assembly is one of the automatic assembly techniques which can achieve extremely accurate precision [34, 32]. Combined with an electrostatic actuator, this process can also construct the active mirrors required for SOB systems [49]. Figure 1.12 shows a micro-scanner fabricated by surface tension self
assembly, integrated with combdrive. The combdrive is formed at the lower end of the mirror, and by applying an electrostatic voltage, an angular movement of $\approx 2.75^\circ$ can be obtained.

### 1.2.2 MOEMS for functional optical devices

MOEMS technology has also been applied to develop functional optical devices. Especially, three-dimensional MOEMS, which creates small optical sub-systems that process quasi-free space beams travelling above the surface of a chip, are used to improve device efficiency.

#### 1.2.2.1 Display system

One successful optical device which has stimulated worldwide attention to MOEMS research is the Digital Micromirror Device (DMD) developed by Texas Instrument [50]. The DMD is a sort of micromechanical spatial light modulator, consisting of large array of torsion mirrors with a $\pm 10^\circ$ rotation angle. A projection display with a high contrast ratio ($> 100:1$) and great optical efficiency can be realized using the DMD [Figure 1.13]. Because of the small size of the mirror ($16 \times 16 \, \mu m$), the switching time of the DMD is very fast on the order of $10 \, \mu s$. Whereas the DMD is a reflection type spatial light
modulator, the grating light valve (GLV) is a diffractive spatial light modulator invented at Stanford University [51]. The GLV consists of parallel rows of reflective ribbons acting as a micromechanical phase grating. A beam from laser source is diffracted by the grating, which is controlled by electrostatic deflection of alternative periods. Because of the high resonant frequency of the actuators, the switching time is only 20 ns. A projection display system using the GLV technology has been demonstrated by Cypress Semiconductor Corp., and suggested as a new competitor for next generation display systems [52].

Apart from display systems based on two-dimensional panels, a scanning beam architecture has been demonstrated using 3D micromirrors [53, 54]. An optical raster scanning display system consists of pairs of orthogonally scanning surface micromachined mirrors. Figure 1.14(a) shows system layout for capturing raster scanned images and (b) shows the top view SEM image of a single-chip raster-scanner. The first mirror determines the line-scan rate of the display and the second mirror, scanning orthogonally to the first mirror, determines the image refresh rate. This system requires a high resonant frequency from the fast mirror and very tight system timing control to achieve VGA resolution. Although the optical raster-scanning system still has some problems
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(a)

(b)

Figure 1.14: Display system using a single-chip raster-scanner [53].

to be solved, it shows quite good performance in other applications such as bar-code readers [55].

1.2.2.2 Optical switches

Optomechanical switches are other functional components commonly used in optical communication systems for signal routing, configuration switching and restoration/protection in the optical layer. First, switches using electrostatic deflection of optical fibers or planar optical waveguides can be considered. Whereas a few demonstrations have been reported for switches based on moving optical fibers [56, 57], a larger number of switches based on moving waveguides have been developed [58, 59, 60]. Figure 1.15 shows a 1 x 2 silica-on-silicon moving waveguide switch developed by CEA-LETI [41, 60]. Figure 1.15(a) shows the schematic view of the elementary 1 x 2 switch which relies on the mechanical deflection of a cantilever beam carrying an input waveguide. An electrostatic force applied to the adjacent electrodes deflects the cantilever so that the moving waveguide can select an output waveguide. Because the switching function is obtained without direct action on the light propagation, wavelength and
polarization sensitivity can both be reduced with a well designed waveguide. Figure 1.15(b) shows SEM view of the 1 x 2 switch. In this improved switch structure, the metal has been removed from the moving waveguide to separate the optical and electromechanical function. An additional lateral suspension and optical stoppers improve alignment both out-of-plane and in-plane directions. The design of the device can minimize optical loss and optical crosstalk. This optical switch shows the switching time below 1 ms and low insertion loss of < 0.5 db.

Secondly, micromirror based switches have been demonstrated. Especially, as the transmission capacity of the optical fiber communication systems has dramatically increased, the demands for functional systems have stimulated new application areas using the MOEMS technology including an optical cross-connect (OXC), a wavelength selective add/drop and a variable optical attenuator (VOA). For example, an OXC system first demonstrated by AT&T Labs was based on the insertion of small mirrors into a set of node points which switch the input light signals from a first linear array of fibers to a second array [61, 62]. Figure 1.16(a) shows the general geometry of a
Figure 1.16: 8 x 8 surface micromachined optical cross connect switch [62].

cross connect switch, and (b) shows a fabricated 8 x 8 cross connect based on 90° up mirrors [62]. Since this approach requires \( N^2 \) mirror elements for a cross-connect with \( N \) ports, these 2D devices are limited in port count to 32 x 32 and below for practical applications.

The 2D OXC, however, can be used for applications that require very small port counts. For example, 1+1 switch protection is required in an OXC. Because two fabrics carry the same traffic within an OXC to prevent a single point of failure in the network, the outputs from the OXC have to be selected by an optical switch [63]. Figure 1.17
shows a 2 x 2 bypass switch where a fiber alignment groove and vertical mirrors are formed using DRIE. Here, a blade mirror is moved in and out of the node point by the combdrive. Therefore the optical signals can propagate straight across the gap or are reflected to a bypass fiber. This bypass switch shows excellent optical isolation and switching time below 1ms.

To achieve a very large port count, alternative 3D OXC fabrics have been developed by a number of companies including Lucent Technologies, Nortel, Calient and Tellium [64]. Figure 1.18(a) shows a schematic view of a 3D OXD fabric. Two arrays of adjustable, dual-axis mirrors provide beam switching between two arrays of collimated fibers. To ensure the switching function between an input fiber and an output fiber, precise control of the mirror angle is required. The reliability of self-assembled structures and a precise mirror control algorithm are therefore key issue for the 3D OXC devices. Figure 1.18(b) shows a SEM view of a 3D OXC system fabricated by Analog Devices [41, 65]. Using this architecture, 1000 x 1000 ports and above can be realized.

Apart from these OXC devices, mirror insertion devices are also used as variable optical attenuators (VOAs) [66, 67], ADD-DROP multiplexers [68, 69], dispersion com-
Figure 1.18: 3D cross-connect switch (a) schematic view, (b) 2 x 2 dual axis tilt mirror array [41, 65].

...and tunable external cavity lasers [71, 72, 73]. These are highly desirable network functioning devices which can be used for channel equalization or add/drop of the wavelengths in dense wavelength division multiplexed (DWDM) systems involving amplification by erbium-doped fiber amplifiers (EDFAs). For example, the mechanically actuated anti-reflection switch (MARS) has been used for a modulator. A MARS device consists of a multi-layer dielectric mirror and a second, fixed mirror arranged as a Fabry-Perot cavity [Figure 1.19(a),(b)]. The reflectivity of the device can be varied by applying a simple drive voltage which deflects the movable mirror to vary the cavity length from on resonance to off-resonance. The device can also be used for gain equalisation in an erbium-doped fiber amplifier. Figure 1.19(c) shows the equalization and amplification result of a MARS filter.
Figure 1.19: MEMS equalization filter (a) schematic view, (b) fabricated chip and (c) spectra of equalization and amplification [41].

1.2.2.3 Specialised applications

MOEMS devices with other more specialised functions have been demonstrated in other application areas to enhance the efficiency of corresponding macro-optical devices. For example, to achieve a denser data storage capability, several optical disk pick-up head systems using MOEMS technology have been suggested [74, 75, 76, 77]. Figure 1.20
shows a free-space optical disk pickup head developed by Lin et al. at University of California in Los Angeles [76]. The light emitted from the self-aligned semiconductor edge-emitting laser is collimated by a micro Fresnel lens and part of the beam passing beam splitter is focused by a second Fresnel lens. The focused light is reflected upward by a 45° mirror to strike the optical disk. The reflected beam from the disk passes back through the focus lens and the beam splitter and is focused onto a photodetector built on the Si substrate. Using a 980-nm laser source, this system demonstrated an FWHM focused spot size of $1.8 \, \mu m$. Compared to a macro optical pick-up head, the monolithic optical disk pick-up head can reduce the device weight, lower the fabrication cost and enhance the device’s performance due to its higher data access rates.

Another interesting device using MOEMS technology has been developed in the sensor networks area by the University of California, Berkeley, which is the so called “smart dust mote” [78, 79, 80]. Smart dust is a millimeter-scale autonomous system that forms the basis of massive distributed wireless sensor networks. Figure 1.21 shows the conceptual diagram of smart dust and a fabricated smart dust mote. Each smart dust mote consists of sensors, sensor interface circuits, an energy source and digital control and processing. Because of the low energy consumption, small size, low cost, increase
in communication performance, the smart dust network has a number of applications including inventory control, smart office spaces, defence networks, nodes that track the movement of birds and environmental monitoring networks. One of the critical elements of smart dust is a communications system based on a micromachined corner cube retroreflector (CCR). Several CCR structures have been suggested to achieve precise alignment of the CCR [82, 83, 84].

1.2.3 Surface tension self-assembly for 3D MOEMS

A variety of methods have been used to fabricate previously described 3D MOEMS, as can be seen in the section 1.1. Although these allow successful results, most of the methods require large chip area and a complex structure to power the rotation. Therefore, attention has been focused on more compact mechanisms. Surface tension self-assembly was then suggested as an alternative.

The first demonstration of surface tension self-assembly used metal and silicon parts, powered by a semi-cylindrical fillet of molten solder [31]. A similar process using solder pads has also been demonstrated by another group, and in their research, much of the original analysis has been verified using finite element methods [85, 86, 87] [Figure
1.22(a)]. Although the process demonstrated the successful use of solder, it suffered from significant deficiencies. First, the electroplated solder and other necessary metal interlayers are corroded by the etching used to release the movable parts, leading to a high failure rate. Secondly, the rapid variation in solder viscosity at the melt temperature leads to very fast rotation, making it extremely difficult to control the assembly process [88]. A more reliable process was then developed, using a pad of borophosphosilicate glass [88], which combined with controllable viscosity could perform multiple axis assembly operations [Figure 1.22(b)]. However, because the melting point of silica itself was extremely high, this was not compatible with a standard MEMS process and the precision of the assembly was poor.

It is important for the development of surface tension self-assembly process to eliminate nonstandard processing steps, to lower the process temperature, to increase the precision and reliability of the rotation, and to apply it to the fabrication of realistic MEMS components. To achieve this, recently, a considerably improved surface tension self-assembly process based on mechanical parts formed from bonded silicon-on-insulator (BSOI) wafers and meltable pads of thick photoresist has been demonstrated
Figure 1.23: Microlens array assembled by surface tension using a photoresist hinge [36] [32, 34, 49]. These are all industry-standard materials. It was also possible to rotate parts into position by melting a thick photoresist at low temperature. The process required just two lithography steps, and used mechanical rotation limiters to achieve a high alignment accuracy. The construction of fixed 45° mirrors, electrical torsion mirror scanners, and micro lenses was achieved using this process [Figure 1.23].

In this thesis, we describe the theoretical background of surface tension self-assembly and details of the fabrication process. More complicated 3D MOEMS components using this process, including both passive and active corner cube reflectors (CCRs), are demonstrated. The non-linear actuation mechanism of the active mirror using an angular vertical combdrive is analysed. The thermal-mechanical properties of the hinge material also evaluated to understand the detailed assembly mechanism of the process. The thermal and dynamic stability of the devices are also assessed and discussed. Finally, other surface tension self assembled devices are suggested as future works.
Chapter 2

Theoretical Background of Surface Tension Self-assembly

Surface tension self-assembly uses the surface tension force of a meltable pad material to power the out-of-plane rotation of suspended movable parts [89, 90]. The force due to the weight of an object scales with volume, whereas the force due to the surface tension of a liquid scales with length so that surface tension forces appear relatively more significant as the size of a structure is reduced. For a sufficiently small size domain, surface tension is often the dominant contributor to the free energy. It can overwhelm gravitational forces, and therefore be used to power the rotation of a mechanical part out of the wafer plane. In this chapter, we review the main characteristics of surface tension force, and verify the advantageous size scaling of surface tension.

2.1 2D analysis of surface tension self-assembly

In this section, we consider how surface tension may power out-of-plane rotation starting with a brief 2-dimensional geometry. This 2-dimensional analysis of surface tension was originally devised at Imperial College [33].
2.1.1 The 2D geometrical model

The two dimensional geometry for the model is shown in Figure 2.1(a), where a movable flap is attached by a flexible hinge to a fixed base-plate. A meltable pad, which wets the hinge but not the surround, is deposited on the hinge. If the pad is melted, the free surface will deform as shown in Figure 2.1(b). The free surface perimeter, $s$, is reduced by rotation of the hinge, as shown in Figure 2.1(c). This decrease lowers the surface energy. The decrease in free energy may exceed the work needed to rotate the flap. The geometry will stabilise when a balance among torques is achieved. The pad may then be re-solidified as shown in Figure 2.1(d).

2.1.2 Analysis of the torque in 2D geometrical model

The torque and the surface energy can be calculated with respect to the rotation angle from the geometry in Figure 2.1. In this analysis, all forces other than surface tension are neglected. A cylindrical free liquid boundary is assumed, with a constant radius of curvature $r$. There are two different methods to find the net torque on the flap, surface energy minimisation and torque balance. Both give the same final result.

First, we consider the torque balance model. The surface energy per unit length, $\sigma_s$, is found in terms of the angles $\phi$ subtended by the liquid and $\theta$ of the flap shown in Figure 2.1(c) as:

$$\sigma_s = \gamma w \left\{ \frac{\phi \cos(\theta/2)}{\sin(\phi/2)} \right\}$$

(2.1)

where $\gamma$ is the surface free energy of the liquid-vapour interface per unit area, and $2w$ is the length of the hinge.

The total torque can be found as the difference between a torque due to the surface tension force and an opposing torque due to the Laplace pressure, which can be
(a) before melting of the pad

(b) just after melting of the pad material

(c) during rotation

(d) after full rotation and solidification of the pad material

Figure 2.1: Stages in the rotation of a hinge by surface tension self-assembly [90].
represented as \( T_\gamma = \gamma w \cos(\alpha) \) and \( T_p = Pw^2/2 \) per unit length, respectively. In these equations, \( \alpha \) is the angle between the liquid surface and the plane normal to the moving part, and \( P \) is the Laplace pressure. The Laplace pressure for a spherically curved surface is calculated as \( 2\gamma/r \) [89]. Using the above relations and geometric considerations, the net torque is obtained as [89]:

\[
T = \gamma w \left\{ \sin\left(\frac{\theta + \phi}{2}\right) - \sin\left(\frac{\phi}{2}\right)/\{2 \cos(\theta/2)\} \right\}
\]  

(2.2)

The initial pad volume is \( 2wh \) per unit length. The corresponding value after rotation of the flap through an angle \( \theta \) is \( \{w^2 \sin \theta + \phi r^2 - r^2 \sin \phi\}/2 \). If we assume that the liquid is incompressible, the following result is obtained by equating initial and final volumes [33].

\[
\delta = \cos^2(\theta/2)\{\phi - \sin(\phi)\} - \sin^2(\phi/2)\{4\eta - \sin(\theta)\} = 0
\]

(2.3)

where \( \eta = h/w \) is the normalized pad height.

Equation 2.3 can be solved numerically, to find \( \phi \) for a given value of \( \theta \). The result is substituted into equations 2.1 and 2.2 to yield the surface energy and torque.

Figure 2.2 shows their variation with the flap angle, for a particular normalized pad height of \( \eta = 0.6427 \) [90]. The surface energy is varying function of angle with a minimum at \( \theta = 90^\circ \). The torque is also a slowly varying function, which is initially finite and falls to zero exactly at the surface energy minimum. These results suggest that the flap will rotate from a horizontal to an upright position, at which point the driving torque will disappear. The torque variation shows that there is a stable angle where the torque is zero. It has also been verified that the position of this stable point can be controlled over a wide range by varying \( \eta \) [33].
2.1.3 Control of the equilibrium angle

By considering surface energy rather than torque balance, it is possible to determine the overall equilibrium condition. Equilibrium is reached when the surface energy is minimized. Since the pad volume is fixed, this minimum must be found subject to the constraint given in equation 2.3. This consideration yields an important relation between $\phi$ and $\psi$ at the equilibrium point [89],

$$\phi_e = 2\psi_e$$  \hspace{1cm} (2.4)

From equation 2.4, the equilibrium shape of the cylindrical droplet is found, which is shown in Figure 2.3 for three different liquid cross-sections [89]. In each case, the free boundary is an arc of a circle centered at point O and passing through the ends of the lands (point A and B) and the hinge (point C).
CHAPTER 2. THEORETICAL BACKGROUND

Figure 2.3: Pad cross-section at equilibrium for (a) $\psi_e < 90^\circ$, (b) $\psi_e = 90^\circ$, (c) $\psi_e > 90^\circ$ [89].

Figure 2.4: Final angle $\theta_f$ versus normalized pad height $\eta$, and normalized starting torque $T_0/\gamma w$ versus normalized pad height $\eta$ [33].

The value of $\eta$ needed for a given $\theta_e$ can be found by substituting equation 2.4 into equation 2.3 to obtain:

$$\eta(\theta_e) = \left\{ \sin^3(\theta_e) + \{1 + \cos(\theta_e)\}\{\pi - \theta_e + \sin(\theta_e)\cos(\theta_e)\} \right\}/\{4\sin^2(\theta_e)\} \quad (2.5)$$

Figure 2.4 shows the variation of $\eta$ with $\theta_e$ [33]. The normalized pad height increases slowly as the final angle becomes smaller and smaller. For the special case of $\theta_e = \pi/2$, 

equation 2.5 reduces to $\eta = (2 + \pi)/8 = 0.6427$. Figure 2.4 also shows the starting torque (i.e. the torque for $\theta = 0$), which can be calculated from equation 2.2 as $T_0 = \{\gamma w/2\} \sin(\phi/2)$. The maximum will be $T_{0(max)} = \gamma w/2$ per unit length. At this point the liquid cross-section is semicircular, so that $\eta = \pi/4$ or 0.7854. $T_0$ is still close to its maximum value when $\eta = 0.6427$ i.e. for $90^\circ$ rotations.

### 2.1.4 Comparison with the gravitational counter-torque

So far, only surface tension force has been considered in the calculation. We now consider the effect of the torque, $T_f$, due to the weight of the flap, which provides a counter-torque to the surface tension induced torque. This torque is given by:

$$T_f = \{\rho b^2 dg/2\} \cos(\theta)$$

where $\rho$ is the density of the flap material. $T_f$ has a maximum value, $T_{f0}$, when $\theta = 0$. To rotate the flap, $T_f$ must be overcome by the starting torque $T_0$. Using the approximation $T_0 \simeq T_{0(max)}$ and assuming that the hinge width is comparable to the flap thickness (both might be $\simeq 10\mu m$), and that gravitational terms due to the shape of the drop can be neglected, we obtain a simple expression for the maximum breadth of the flap that can be lifted as [33]:

$$b_{max} = \sqrt{\gamma/\rho g}$$

For silicon, $\rho = 2.33 \times 10^3 \text{kg/m}^3$. A typical value of surface tension coefficient is $\gamma = 0.45 \text{N/m}$ (for solder). This gives $b_{max} = 4.19 \times 10^{-3} \text{m}$. Note that $b_{max}$ is the breadth at which $T_0$ and $T_{f0}$ are comparable. For a breadth ten times smaller (i.e. $\approx 400 \sim 500 \mu m$), the surface tension torque will be approximately 100 times larger than the gravitational torque, which is then effectively negligible. The mechanism
is therefore useful for microstructures. Recently, this theoretical estimate has been validated by experiments [92].

### 2.1.5 Hinge-less structures

Although surface tension force (which scales as dimension$^1$) can easily overcome gravitational force (which scales as dimension$^3$) in microstructures, it has less success against elastic force (which scales as dimension$^2$). Therefore it is preferable to develop hinge-less structures where the elastic link is omitted [89].

In case of the hinge-less structure, a second free liquid surface exists in the gap $g$ between the fixed and moving parts [Figure 2.5]. The liquid is assumed to wet the two lands but be pinned at the edges, so that it causes an additional force to act on the flap. A net force $F$ of the additional force, which acts in the $\zeta$-direction, tends to hold the gap closed [89]. Thus there is no need for the elastic link.

![Figure 2.5: Geometry of a hingeless structure [89].](image)

The gap closure force $F$ is obtained in [89] as:

$$F = \gamma \{1 + \cos(\phi/2) - \sin(\phi/2) \tan(\psi/2)\}$$ \hspace{1cm} (2.8)

The force is large at the start of rotation, but falls to zero at equilibrium. The explanation is provided by Figure 2.6. Figure 2.6(a) shows the liquid cross-section
for $\theta_e = 90^\circ$. Figure 2.6(b) shows the flap rotating further. Because the two shapes have the same free-boundary lengths and liquid cross-sectional areas, they have the exactly same surface energy. In the absence of a hinge constraint, they are equally valid minimum surface energy configurations. Thus, there is no barrier to further rotation once equilibrium has been reached, which requires the addition of mechanical limiters.

### 2.2 3D analysis of surface tension self-assembly

So far, the 2D analytical model has been described for the behaviour of a meltable hinge. However, to fabricate more complicated structures, an accurate 3D model is required to predict the torque of a given pad design. A 3D self-assembly model has initially been developed by the Boulder group [85, 86] using a finite element simulation package, Surface Evolver, developed by Brakke of the University of Minnesota. The behaviour of solder self-assembly has been simulated with Surface Evolver.

The Boulder model is based on the calculation of the surface energy of the 3D solder profile at every rotation angle. The equilibrium angle is then the angle of minimum surface energy. Since the surface tension coefficient, $\gamma$, is assumed to be constant for the entire solder surface, the minimum surface energy corresponds to the minimum
surface area. The 3D model uses two sub-steps to find the equilibrium angle. The first step is to calculate a solder shape with a given angle and device constraints (volume, wetted area, etc.). The second step is to identify the equilibrium status from the trend line of the range of surface energies at each angle. The length and width of the solder pad used by the Boulder group are equal to minimise reflow of a solder ball. Figure 2.7 shows the variation of the surface area as a function of flap angle formed in this way. The equilibrium angle obtained in the 3D simulation is 87.4° [87]. In order to compare this result with the 2D model, the initial height and normalized height were obtained from the dimension in [87] as $h = V/2wl = 194.4 \mu m$ and $\eta = w/h = 0.648$, respectively. Substituting the value into the equation 2.5, we can obtain an equilibrium angle of 89.7°, similar to the 3D simulated result [90].

The Boulder model has also been validated by experiment [86]. Figure 2.8 shows a comparison of surface energy based model predictions and experimental data for the variation of $\psi_e$ with liquid volume. The difference between model and experiment is within 2°, with the error mainly caused by a slight variation in the volume of deposited solder [86]. A key point to note in Figure 2.7 is that the model calculates only surface area rather than surface energy, since the surface tension coefficient (defined as sur-
face energy divided by surface area) is assumed to be a constant for the entire solder joint. The Boulder model, therefore, can be used for any meltable materials, such as photoresist, without considering specific material aspects.

Combining this Boulder model with the 2D analytical model described in previous section, the 3D behavior of the photoresist pads used in our devices can be evaluated [91]. The pad shape used at Imperial College is different from the solder shape of the Boulder group, in that the length of the pad is much larger than its width. The 3D simulation result may therefore be closer to the 2D analysis. Figure 2.9 shows a simulated shape of a long meltable hinge. In our 3D model, Equations 2.3 and 2.4 are used to determine the initial geometry of the pad. Equation 2.1 is then used to calculate the surface energy. Many datafiles used for Surface Evolver can be generated automatically in our program, which enables more detailed data analysis. Since the energy change between two angles is equivalent to work, we can also calculate the
average torque acting on the moving parts. Using this model, we have analysed the
surface energy of photoresist hinge structures with various aspect ratios, and have
extended the analysis to evaluate the torque at every hinge angle.

Figure 2.10(a) shows the 3D simulated surface energies per unit length plotted versus
hinge angle for a pad of 230 $\mu$m length and 20 $\mu$m width. The initial pad shape is
determined by 2D analysis, to achieve an equilibrium angle of 90.0°. The trend is
similar to that of the Boulder group previously shown in Figure 2.7. The equilibrium
angle is determined from the minimum surface energy of the trend line, which is 89.0° in
this case. Figure 2.10(b) shows the equilibrium angles obtained for different pad length
to width ratios. As the ratio increases, the equilibrium angle increases and stabilises at
90.0° above a ratio of 100, as expected in 2D analysis.

Figure 2.11(a) shows the simulated torque per unit length plotted with the hinge
angles for various pad lengths assuming a 20 $\mu$m pad width. The torque is maximised at
the starting position and decreases to zero at the equilibrium angle. Although the cross
sectional shapes are the same for all simulated pads, the resulting torque per unit length
is different depending on the length. Figure 2.11(b) shows the starting torque obtained
for different pad length to width ratios. The starting torque decreases as the length
Figure 2.10: 3D surface energy simulation: (a) surface energies per unit length versus hinge angle for a pad with 40 $\mu m$ width and 230 $\mu m$ length; (b) equilibrium hinge angles versus pad length to width ratio.
Figure 2.11: 3D torque simulation: (a) torque per unit length versus hinge angle for a pad with 40 $\mu$m width and various lengths; (b) starting torque versus pad length to width ratio.
decreases, and the decrease becomes abrupt below a ratio of 15. The result indicates that if large counter torques are present during the surface tension self assembly process, the pad length to width ratio should be considered in the device design.

2.3 Conclusion

The surface tension force used for a three dimensional self-assembly of MOEMS components has been estimated using a two dimensional geometrical model. Using the model, we can predict equilibrium angle of a meltable pad. The model also shows that surface tension force is dominant in the $\mu m$ size domain. Therefore other forces including gravitational force can be neglected for the assembly of microstructures.

For more accurate prediction of the equilibrium angle, a three dimensional simulation using the Surface Evolver has been suggested. The surface tension force and equilibrium angle of a long cylindrical pad have been evaluated using the three dimensional model. Both the hinge angle and the surface tension torque increase as the pad width to length ratio increases. The equilibrium angle formed in the 3D case tends to the angle obtained by 2D model when the width to length ratio is greater than 100. Since the starting torque decreases abruptly in the small length region, the torque balance should be considered for a device design. We show that a 3D surface tension model can be used to calculate more accurate starting torque and equilibrium angles for the self assembly process.
Chapter 3

Fabrication Process

In this chapter, we describe a fabrication process for 3D MOEMS, which has been developed at Imperial College, based on mechanical parts formed in bonded silicon-on-insulator (BSOI) wafers with photoresist as the meltable material. Because of the use of single crystal silicon, and the low melting temperature of the photoresist, the parts remain flat after assembly. It is therefore an appropriate process for MOEMS.

3.1 Photoresist-powered self-assembly

Figure 3.1 shows the process scheme, which originally was suggested by Syms in [32]. The process is based on BSOI material obtained commercially. The substrate material of the BSOI is 100 mm dia, (100) oriented silicon. A 2 $\mu$m thick thermal oxide was grown on top of the substrate and a 5 $\mu$m thick single crystal silicon layer was bonded on top of the thermal oxide [Figure 3.1(a)]. The bonded silicon layer provides the mechanical parts, while the buried oxide is used as the sacrificial material. Because it is possible to choose the thickness of the bonded silicon ranging from a few $\mu$m to a hundreds of $\mu$m, the BSOI wafers allow the construction of thick, high quality suspended mechanical parts in single crystal material [93, 94].
Figure 3.1: Process scheme for photoresist-powered surface tension self assembly [32].
The process is then as follows. The wafer is first patterned with a mask to define the mechanical parts, using a Quintel Q4000 - IR aligner with a h-line UV light source. The bonded silicon layer is then etched by deep reactive ion etching (DRIE) in a Surface Technology Systems Single-Chamber Multiplex ICP Etcher to form mechanical parts. Etching sequence used for the DRIE is the BCO/STS Advanced Silicon Etch (ASETMS) [Figure 3.1(b)]. Since the target etching depth of the top silicon layer is 5 µm and the selectivity between silicon and photoresist is generally more than 20, a 0.5 µm thick layer of Shipley S1813 photoresist is selected as a mask layer. To obtain accurate target thickness, the relation between spin speed and the photoresist thickness was evaluated for a constant spinning time of 40 sec. Figure 3.2 shows the trend. From the graph, a 0.5 µm thick layer of Shipley S1813 photoresist can be obtained by spin-coating on the wafer at 3500 rpm for 40 sec. The exposure time was then also stepped from 2
to 10 sec. The pattern defined clearly from 6 sec onwards, so 8 sec was chosen as the exposure time to ensure complete patterning. Finally, the develop time was evaluated for Shipley MF319 developer, and 60 sec was chosen as a best condition. The sizes of the devices are of order 1 mm, with clearances between parts of 4 $\mu m$.

Next, we have optimized the DRIE condition. DRIE allows two characteristic improvements to previous version of the process. First, it allows improved feature size control to 0.2 $\mu m$, which increases the accuracy of the assembled structure. Secondly, DRIE has a very high selectivity between silicon and oxide, so that silicon etching can be stopped on the oxide layer. This increases uniformity over the wafer, and reduces the failure caused by direct adhesion of the resist to the substrate. The $ASE^{TM}$ DRIE process is used for the vertical etching. This process employs alternating cycles of etching and sidewall passivation in the silicon etch process, in order to retain a high etch rate and keep the profile of the etched structure vertical [95, 96, 97]. SF$_6$ and C$_4$F$_8$ are used as etching and passivation gases respectively.

The SF$_6$ gas supplies fluorine radicals to remove silicon. Although the etching process is isotropic, the alternating passivation step prevents lateral erosion by depositing a polymer layer on the sidewall. The C$_4$F$_8$ plasma, used in the passivation step, deposits a (C$_x$F$_y$)$_n$ polymeric layer on all substrate features. The directional ion energy supplied during the etching step first removes the passivation from the base of the features, hence exposing silicon for further etching. Details of the etch condition are shown in Table 3.1. Since the etch depth is relatively small ($\sim 5\mu m$), a low frequency of 380 kHz is applied to the platen rather than usual high frequency of 13.56 MHz to prevent a notch profile occurring at the bottom side. One etching cycle consists of etching for 7 sec and passivation for 5 sec. The total number of etch cycles is 28, which corresponds to a total time of 5 min 36 sec. The preliminary etch test shows that the etch rate is 1.23 $\mu m/min$ for silicon and 0.036 $\mu m/min$ for S1813 photoresist, which implies a
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<table>
<thead>
<tr>
<th>pressure $mT$</th>
<th>Coil RF power, $W$</th>
<th>Platen RF power, $W$</th>
<th>Platen frequency, $kHz$</th>
<th>condition for 1 cycle, gas unit: $sccm$</th>
<th>total cycle</th>
</tr>
</thead>
<tbody>
<tr>
<td>15</td>
<td>600</td>
<td>14</td>
<td>380</td>
<td>$130 SF_6 + 13 O_2$: 7 sec</td>
<td>100 $C_2F_6$: 5 sec</td>
</tr>
</tbody>
</table>

Table 3.1: DRIE condition for silicon etching.

<table>
<thead>
<tr>
<th></th>
<th>Depth, $\mu m$</th>
<th>Remaining $Tpr$, $\mu m$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Hole</td>
<td>5.28</td>
<td>0.26</td>
</tr>
<tr>
<td>Periodic pattern</td>
<td>5.81</td>
<td>0.34</td>
</tr>
<tr>
<td>Large open area</td>
<td>6.15</td>
<td>0.36</td>
</tr>
</tbody>
</table>

Table 3.2: DRIE etch depth for various patterns.

selectivity of 34. The etch depth of silicon is then 7 $\mu m$ including 20% over etching.

After etching, the photoresist mask is stripped. Figure 3.3 shows SEM views of various features in the design tested on a bare silicon wafer. Figure 3.3(a) is one of the smallest features. Figure 3.3(b) shows a periodic pattern and Figure 3.3(c) shows a large open area. Although cyclic processing leads to sidewall scallops, the process can define vertical sidewalls and small features with high accuracy, as shown in Figure 3.3(a). Somewhat surprisingly, a notched profile, i.e. a small lateral indentation at the base of the etched trench, is not seen in Figure 3.3 as expected. The DRIE process also provides the same vertical profile regardless of the pattern type, as shown by the periodic and isolated features in Figure 3.3(b) and (c). Table 3.2 shows the etch depth and remaining photoresist thickness, $Tpr$, for each pattern. The table shows a slight etch rate variation of the DRIE process depending on the pattern type. However, for BSOI wafers, a uniform etch depth can be obtained since the oxide layer beneath the silicon can be used as a stopping layer. The result also shows sufficient remaining photoresist.

Before forming the meltable pads, it is necessary to clean the wafer surface to improve the adhesion of the pads. Generally, the surface is cleaned using firstly oxygen plasma and secondly a wet etchant, Standard Clean One (SC1). Oxygen plasma cleaning removes any hard polymer which still remains on the substrate after resist stripping.
Figure 3.3: Vertical SEM view of various patterns: (a) a perforated hole, (b) a periodic pattern and (c) a large open area.
Moreover plasma cleaning can activate the silicon surface to increase the adhesion force between the silicon and the photoresist. Figure 3.4 shows the effect of oxygen plasma cleaning. Residual polymer deposited on the etched side wall was clearly removed after oxygen plasma cleaning as shown in Figure 3.4(b). The substrate was then cleaned in SC1 solution (also referred to as RCA cleaning), which is a 6:1:1 mixture of water, 30 weight percent ammonium hydroxide, and 30 weight percent hydrogen peroxide at 70°C. The SC1 solution removes organic contaminants through oxidation and dissolution, and removes particle from the silicon surface through silicon and silicon dioxide etching (undercutting particles) [98]. Using these two step post cleaning process, an ultra clean wafer surface can be obtained.

The etched wafer is then spin-coated with Hoechst AZ4562 photoresist to form the meltable pads. The wafer is then pre-baked at 90°C for 30 min, exposed to the pad mask using the Quintel aligner, and developed in Hoechst AZ400K developer (1:4 in DI water). Since the equilibrium angle is determined by the dimension of the pad, accurate control of the resist thickness is important. To obtain accurate target thickness, the relation between spin speed and the AZ4562 photoresist thickness was evaluated for constant spinning time of 30 sec. Figure 3.5(a) shows the trend, and a spin speed of
Figure 3.5: Thickness of AZ4562 photoresist: (a) versus spinning time, (b) versus exposure time for various develop time.
1350 rpm is chosen to obtain the 11 \( \mu m \) thickness of AZ4562 photoresist required for an equilibrium angle of 90°. Since the thickness of the AZ4562 resist is relatively high, the exposure and a develop times must also be optimised. Figure 3.5(b) shows the AZ4562 thickness versus exposure time for various develop times. From the graph, an exposure time of more than 100 sec and a development time of more than 4 min are sufficient to fully develop 11 \( \mu m \) thickness of AZ4562 photoresist. To allow a suitable process window, an exposure time of 150 sec and a development time of 6 min were chosen. This process resulted a pad thickness of 10.8 \( \mu m \), with dimension of 250 \( \mu m \times 40 \mu m \) [Figure 3.1 (c),(d)].

Some functioning features were also introduced in the design to prevent the detachment of the movable parts during sacrificial layer etching. A series of holes are thus formed at the silicon lands on either side of the hinge. The dimension of the hole is 2 \( \mu m \times 4 \mu m \) [Figure 3.6(a)] [32]. The resist pads are then pre-melted at 120°C for 30 min before sacrificial layer etching [Figure 3.1(e)]. In [32], Syms showed that even after self-assembly, the perforation was completely filled by resist as shown in Figures 3.6 (b) and (c). The solid resist pad therefore effectively pins the fixed and moving parts together during the long sacrificial layer etching.

To free the mechanical parts, the buried oxide is removed by wet etching in 7:1 buffered hydrofluoric acid (HF) [Figure 3.1(f)]. To reduce the etching time, 4 \( \mu m \) square holes are formed on a 20 \( \mu m \) pitch in the movable parts. The etching time is adjusted from 3 to 4 hours.

It is important to control the uniformity of etch rate for yield improvement. For example, Figure 3.7 shows the difference in wet etch rate obtained for different sizes of perforation. The lateral etch rate for parts next to a stripe opening was 29.8 \( \mu m \) for 200 min etching, while beneath small holes, it was only 2.4 \( \mu m \). This result can be explained as follows. Bubbles caused by the gaseous by-product of HF etching can be
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(a) layout  
(b) overall structure  
(c) close up of hinge hole

Figure 3.6: Layout and SEM view of a perforated hinge structure [32].

(a)  
(b)

Figure 3.7: The effect of feature shape on undercut rate in buffered HF solution.
Figure 3.8: Pad shape after surface tension self-assembly.

trapped in the small holes. These bubbles prevent the entrance of etching ions from the solution ($HF^-_2$ ion for hydrofluoric acid), which reduces the etch rate of oxide around the small holes. If the uniformity varies significantly with the layout of the mechanical layer, movable parts in low etch rate areas may still be attached to the substrate by remaining oxide and, at the same time, the anchors of some fixed parts in high etch rate areas may detached from the substrate after HF etching. There are two ways to remove these bubbles. One is to use a surfactant to reduce their surface tension of the bubbles. Unfortunately, the surfactant also reduces the adhesion between the photoresist hinge and substrate. Moreover some surfactants, such as methanol, can dissolve photoresist itself. A magnetic stirrer was therefore used instead to remove bubbles during the etching. Agitation of the solution by the stirrer removed the bubbles and also increased the uniformity of the HF etching.

The remaining rinse water is then removed by freeze-drying [15], to avoid surface tension collapse [99] [Figure 3.1 (g)]. In this step, the substrate is placed in a water-filled petri dish and then in an Edwards Modulyo freeze drier, which was evacuated by a vacuum pump. The water freezes as the pressure reduced, but before freezing, and as the pressure is reduced still further, the frozen water is removed by sublimation.
There are two problems in freeze drying. Boiling during the pumping stage causes many movable parts to be lifted off, and spontaneous cracking during ice formation also detaches the movable parts [32]. The boiling occurs because of impurity gases dissolved in the solution. The solution is therefore degassed before freeze drying to remove these gases. Cracking during ice formation can be reduced by replacing the water with a 10% solution of distilled methanol in water. The mixed solution then forms a soft ice and freezes without cracking. After the solution has been completely removed from the substrate, the chamber is vented to dry N\textsubscript{2}, which prevents the formation and subsequent evaporation of condensate on the released parts, when warm air contacts the cold surface during the vent step.

Finally, to rotate the structure to its three-dimensional configuration, the resist hinge pads are remelted in a convection oven at 155°C for 15 min [Figure 3.1 (h)]. Figure 3.8 shows a resist pad after remelting. The pad shape is similar to the 3D simulation result previously shown in Figure 2.9. To improve reflectivity and provide electrical connection across the insulating resist pads, the devices are then sputter-coated with 500 Å of Au.

### 3.2 Mechanical limiter for accurate rotation

Although the equilibrium angle obtained by the surface tension of a meltable pad can be determined by considering the surface energy of the pad as shown in equation 2.5, a hingeless structure has the problem of instability discussed in the section 2.1.5. A mechanical limiter was therefore used to increase alignment accuracy.
Figure 3.9: Layout of above-substrate limiter [34].

Figure 3.9 shows the layout of the limiter adopted in [34]. The layout uses an above-substrate limiter structure to maximize a lever length required to increase accuracy. The above-substrate limiter has been developed and improved for surface micromachined devices. The limiter basically uses two self-assembling levers rotating simultaneously in opposite directions until their ends engage each other. In case of the layout shown in Figure 3.9, the limiter provides an accurate angle of $45^\circ$.

For the design of the above-substrate limiter, Syms described a considering point where the two flaps are not identical, which result in the difference of rotation rates of the flaps [34]. If the difference of angular velocities $w_1$ and $w_2$ of the left hand catch (LH catch) and right hand catch (RH catch) is large, the limiter will jam in the middle of the rotation. A useful range between the angular velocities, so called a "capture range", can be defined to ensure the engagement of the limiter. In case of the $45^\circ$ limiter as shown in the Figure 3.9, simple geometry shows that the capture range of $0.414 < w_1/w_2 < 2.414$ is required to ensure the engagement. Using this limiter mechanism, Syms has assembled a variety of 3D micro-optomechanical structures such
Figure 3.10: Self-assembled 3D MOEMS structure: (a) overview of a corner cube reflector, (b) a close-up view of a 45° limiter.

as micromirrors, microscanners and micro lenses [32, 34, 102].

In this thesis, we have fabricated more complicated 3D structures including scanners with a linked drive and corner cube reflectors based on the same limiter mechanism. Figure 3.10 shows an example of a self-assembled corner cube reflector, which will be discussed in Chapter 4. As shown in Figure 3.10, the limiter mechanism can be completely engaged at 45° with two asymmetrical flaps, suggesting that the assembly process is unaffected by the mass of the parts. Moreover a 90° limiter was successively integrated with a 45° limiter to form a concave corner cube reflectors.

3.3 Conclusion

The 3D surface tension self-assembly fabrication process has been described. The process uses simple two mask layers, one to define the mechanical parts and the other to form the meltable pads. Patterning resolution and etching uniformity of the devices have been improved using a DRIE process and a magnetic stirrer during wet etching. The adhesion of the pads to the silicon substrate has also been improved using oxygen
plasma cleaning and SC1 cleaning after DRIE. The use of a limiter mechanism ensures the assembly accuracy of 3D MEMS devices.
Chapter 4

Self-assembled Corner Cube Reflectors

4.1 Introduction

A corner cube reflector (CCR) consists of three perpendicular mirrors, which form a concave corner. Because of the mutual orthogonality of the mirrors, light incident on the device is reflected directly back to its source. Figure 4.1 shows the directional change of an incident ray after hitting each mirror [83]. CCRs have a wide range of applications, including use as targets to measure positions and orientations accurately [110, 111], as laser cavity mirrors [112], as street signs and road markings, and as reflective devices to study the scintillation that arises when light propagates in a turbulent atmosphere [110]. While many applications only require fixed CCRs, devices that can be modulated are of considerable interest in free space optical communication.

There are two common methods of modulating the light intensity reflected by a CCR. The first method involves placing a shutter directly in front of the CCR. Mechanical shutters capable of blocking a large aperture may be both heavy and slow. Trans-
mission modulating optical switches based on GaAs multiple quantum wells (MQW) have therefore been proposed as an alternative for high-speed operation [113, 114]. The transmission of a MQW shutter can be modulated by a moderate voltage (∼ 15 V) up to 5 Mbit/s. However, it is still necessary to fabricate a separate CCR.

The second method is to modulate the retroreflection of the CCR itself. By intermittently misaligning one or more mirrors, the retroreflection may be destroyed, allowing a modulated optical signal to be transmitted back to the source. CCRs have therefore been proposed for the transmission of coded information in free-space communication links, for example between vehicles [83, 115]. Because CCR transmitters have extremely lower power consumption, they are appropriate for sensing systems that use a central controller to collect data from a number of randomly placed, self-powered sensor nodes [Figure 4.2(a)]. Recently, Pister et al. have demonstrated autonomous sensors with CCR transmitters or 'motes' in the 'Smart Dust' project [78, 80].

In principle, micro-electro-mechanical systems (MEMS) technology allows low-cost, compact CCRs to be integrated with sensors and CMOS circuits. Unfortunately, the fabrication tolerances are extremely tight, since imperfections and misalignment of the mirrors can rapidly result in low optical performance [116]. Early CCR devices [82] were fabricated as polysilicon optical MEMS, for example using the Multi-User MEMS Process (MUMPS), with mirrors folded out-of-plane on micromachined staple hinges [100].
Figure 4.2: Smart dust mote [80, 79]
(a) sensing system with retroreflective optical communication link, (b) a smart dust sensor node
However, stress-induced curvature in the polysilicon and slop in the hinges seriously degraded optical performance, even with improved hinge designs [101].

In addition, assembly of early CCRs either involved manual operation of a microprobe [82], or a dedicated on-chip micro-actuator [117, 118]. Both approaches are unsatisfactory; the former is extremely time-consuming, even when parts are linked together to speed assembly [24], while the latter can require a complex mechanism with a large chip area. Several alternative methods of powering out-of-plane rotation have been proposed, including magnetic actuation [84] and stress [119], but these have yet to demonstrate the required accuracy from complex optical structures.

Zhou et al. therefore suggested a new fabrication process to improve both the alignment and the flatness of the mirrors using bonded silicon-on-insulator (BSOI) wafers [79]. Vertically oriented mirrors are plug-connected into micromachined features on the bonded BSOI substrate as shown in Figure 4.2(b). The vertical and horizontal mirrors are all formed as thick single-crystal silicon parts, and the CCR is operated as a modulator by tilting the horizontal mirror using a microactuator built into the main BSOI wafer. Although this method has resulted in devices of extremely high quality, the assembly process is still manual.

In this chapter, we describe the first mass parallel, automated assembly method suitable for CCRs. The process involves the simple two-mask self-assembly process described in the previous chapter. The design of a self-assembling CCR structure is considered in Section 2. Fabrication is described in Section 3, and the performance of completed devices in Section 4. Precise assembly and modulation of active CCRs are both demonstrated. Conclusions are presented in Section 5. This work is in collaboration with Pister’s Smart Dust Group at Berkeley University.


4.2 Self-assembling CCR design

In this section, we consider the overall design of a self-assembling CCR. The target arrangement would ideally be as shown in Figure 4.2(b), because this topology allows both the simultaneous fabrication and the simultaneous operation of four separate corner reflectors, giving all-round coverage. Unfortunately, such an assembly would require an initially multilayer mechanical structure, which is difficult to achieve using parts formed in standard BSOI wafers. We therefore concentrate on single corners, assuming that the penalty involved in fabricating multiple corners by a self-assembly process is chip area and yield rather than assembly time.

Although it would be possible to form a single corner with the arrangement denoted Type A in Figure 4.3, even this reduced structure requires two 90° out-of-plane rotations. Previously, 90° self-assembly (although possible) was found to be less reliable and less accurate than 45° rotation [32]. Furthermore, the part count is relatively high. If a mechanical limiter is to be used, an above-substrate stop for the 90° rotating part must first be created by a previous assembly step. Formation of the stop typically involves two counter-rotating parts, so that three moving parts are needed. A Type A CCR therefore requires a total of 2 x 3 = 6 self-assembled parts.

![Figure 4.3: Alternative orientations for corner cube reflectors.](image-url)
Because a reduction in the number of movable parts is key to reliability and precision in self-assembly, we have adopted the alternative arrangement denoted Type B, which is merely a re-orientated version of the Type A layout. Here two of the mirrors are rotated by 45°, and provide a stop for a third mirror, which is rotated through 90°. In this case, a total of $2 \times 2 + 1 = 5$ moving parts are required. Each 45° structure is created by the counter-rotation of two parts, which carry a latch of the type shown in Figure 3.9 to set the final angle [34]. The latches are simply interlocking features, which mechanically contact after a 45° rotation. The overall assembly is then a mixture of simultaneous and sequential operations, as shown in Figure 4.4. The two 45° surfaces (Mirrors 1 and 2) and their corresponding frames first counter-rotate and latch together, and the 90° surface (Mirror 3) then rotates further to engage against them.

In our experiments, we have used this arrangement to construct passive CCRs and also active CCRs that can act as retro-reflection modulators. Figure 4.5 shows how the Type B assembly is adapted to create an active CCR. Each of the 45° mirror planes is
modified to mount a torsion mirror scanner, which can be rotated away from its aligned position to destroy the retro-reflection. The rotation is powered by a skewed electrostatic drive, which attracts the lower edge of the mirror down towards the substrate.

4.3 CCR fabrication, layout and assembly

In this Section, we describe the overall fabrication process and the layout and assembly of the two types of self-assembled CCR subsequently investigated.

4.3.1 Fabrication

Fabrication is similar to the process described in Chapter 3. Mechanical parts were formed in SOI with a bonded Si thickness of 5 $\mu$m and an oxide interlayer thickness of 2 $\mu$m. Later step profile measurement showed that the bonded thickness was actually 3.5 $\mu$m. Assembly was powered by pads of Shipley AZ4562 photoresist, with a thickness of 12 $\mu$m and width of 32 $\mu$m.

Melting of the pads was carried out in a convection oven. Control of the resist melting temperature was extremely important. At low temperatures, the mechanical parts did not rotate sufficiently to form right-angled corners. At high temperatures, the hinge driver pads deformed considerably, causing uncontrolled assembly. In these experiments, temperatures between 155 and 170°C were found to be suitable and complete assembly could be achieved in 15 minutes at 160°C. After assembly, the devices were sputter coated with 100 Å Cr and 500 Å Au using a Nordiko RF sputter coater to allow electrical contact and improve reflectivity. Isolation was provided by the prolonged sacrificial etch, which undercut parts far enough to prevent metal tracking between them.
4.3.2 Layout and assembly

Figure 4.6(a) shows the mask layout of a passive CCR. Here, the mechanical parts are shown in dark grey and the hinge driver pads in light grey. The 45° mirrors and their limiter mechanisms are at the bottom of the figure, while the 90° mirror is at the top. A narrow land is provided between the two 45° mirrors to separate their respective hinge driver pads. A narrower land is provided between the 90° mirror and the 45° mirrors. This distance is bridged by short projections attached on the outer side of the limiter mechanisms in the assembled structure.

Figure 4.6: (a) Mask layouts for passive corner cube reflectors; (b) complete device.
Because of the minimal effect of gravity, the three main part types (the 45° mirror, the 45° frame, and the 90° mirror) may all have very different sizes and shapes; the most important aspect is that the hinge driver pads are all identical. The 45° mirrors are of 1 mm x 1 mm area. The hinge driver pads are approximately 250 μm in length, and the initial hinge gap is 2 μm. The movable parts are perforated with 4 μm square holes on a 20 μm square grid to allow undercut, and the clearance between all parts is 4 μm.

Figure 4.6(b) shows a passive CCR after assembly; the mechanical parts have clearly rotated out of plane to form a symmetric structure corresponding to the Type B arrangement of Figure 4.3. Figure 4.7(a) shows a close-up view of the engaged limiter mechanism of one of the 45° mirrors. Clearly, the two counter-rotating parts have self-assembled to an extremely high accuracy, since little clearance between the two halves of the limiter may be seen. Figure 4.7(b) shows one of the projecting tabs used to limit the motion of the 90° mirror; again, extremely precise assembly has been achieved.

Figure 4.7: Assembled passive CCR: (a) Close-up view of a 45-degree limiter, and (b) Close-up view of a 90-degree limiter.

Figure 4.8(a) shows a plan view of an active CCR. Here, the 45° mirrors have been converted into frames that carry smaller mirrors on torsion bar supports, following a previously demonstrated design [34]. The mirrors measure 496 μm x 456 μm, and the torsion bars are 5 μm wide and 324 μm long. Each mirror carries the moving half of
Figure 4.8: (a) Mask layouts for active corner cube reflectors; (b) complete device.
an electrostatic comb drive, while the land between the mirrors now carries the fixed half. The two halves of the comb initially interlock. The electrostatic comb drive is an efficient actuator, widely used in MEMS device because of the advantageous size scaling of electrostatic forces. Such devices are typically based on variable capacitors. The comb electrode geometry has the advantageous of providing virtually constant force over its travel range [107]. There are 29 fingers on the moving half and 28 on the fixed half. The finger length and width are 60 $\mu$m and 6 $\mu$m, respectively, and the finger separation is 2 $\mu$m. Un-perforated parts, which will not be released by the sacrificial etch, block out some open areas to improve planarization during resist coating.

After assembly, the two electrodes form a skewed electrostatic comb drive. The fixed electrode is electrically isolated from the surrounding land, so that the assembled mirrors may be driven using a voltage applied between the fixed electrode and the land. Electrical contact between the movable mirror, its supporting frame, and the land is provided by the sputtered metallisation, which forms a conducting coating over the hinge driver pads. Figure 4.8(b) shows a completed active CCR, which has again assembled as expected.

4.4 Optical performance

In this Section, we described the assembly accuracy of CCRs, and the performance of active devices, considering first individual scanners and then CCR operation.

4.4.1 Assembly accuracy

The alignment precision of assembled active and passive CCRs was measured by retroreflection, using a HeNe laser mounted on a calibrated rotation stage. Table 4.1 shows the measured angles for mirrors 1, 2 and 3.
### Table 4.1: Angle measurement data for CCRs

<table>
<thead>
<tr>
<th>Sample</th>
<th>Mirror-1</th>
<th>Mirror-2</th>
<th>Mirror-3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Passive CCR</td>
<td></td>
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</tr>
<tr>
<td>BSOI4-8-#1</td>
<td>44.88</td>
<td>43.38</td>
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</tr>
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<td>Target value</td>
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<td>45.00</td>
<td>90.00</td>
</tr>
</tbody>
</table>

The data in bold type were obtained from mirrors with limiter mechanisms that appeared completely engaged, while the data in normal type were obtained from mirrors with small gaps between the mirror and limiters. The angles of the best structures are all within 0.18° (3 mrad) of their target values. Mirrors of this type have previously been shown to have low stress-induced curvature (below ± 350 nm over the whole mirror area) [32]. The misalignment significantly smaller than the CCR fabricated by Chu et al., which showed misalignment ranging from 15 mrad to 35 mrad [83]. Although Zhou et al. have demonstrated more accurate device with misalignment of 1 mrad [79], the fabrication method still based on manual assembly. Self-assembled structures have proved stable over long periods, and similar devices have survived un-packaged for several years without apparent degradation.

#### 4.4.2 Mirror scanners

The performance of individual scanners was determined by removing one of the two scanners in a CCR assembly, and measuring the scan line achieved when a HeNe laser
was incident on the other scanner at $45^\circ$ as shown in Figure 4.9(a). The substrate and the moving electrode were held at a common potential, and a sinusoidal drive voltage was applied between the fixed and moving electrodes so that an actuation torque was generated, as shown in Figure 4.9(b).

Figure 4.9: (a) Experimental geometry for measurement of isolated scanners; (b) electrode geometry.

Figure 4.10(a) shows the variation of the peak-to-peak scan angle with drive frequency at different drive voltages. The resonant frequency decreases slightly from around 1100 Hz as the voltage increases, as shown in Figure 4.10(b). Similar effects have previously been observed with skewed [34] and staggered [49] comb drives. A maximum scan angle of $\approx 20^\circ$ was obtained at the largest voltage used (90 Vp-p), and the electrical resonant frequency was 1066 Hz at this voltage. Because the torque is nominally proportional to voltage squared, the mechanical resonant frequency is twice the electrical resonant frequency, or 2132 Hz. Figure 4.10(b) also shows the variation of the resonant scan angle with voltage squared. In fact, the scan angle increases supralinearly with $V^2$, so that larger than expected deflections are obtained at high voltages.

This behaviour may be explained using simple theory as follows. For a damped, driven second-order system comprising a moment of inertia $I$ mounted on a torsion element of stiffness $k$, the equation of motion is [121]:

\[
\ddot{\theta} + 2\xi \omega_n \dot{\theta} + \omega_n^2 \theta = K V^2
\]
Figure 4.10: Measurement of optical scan angles
(a) variation of scan angle with drive frequency, at different drive voltages; (b) variation of electrical resonant frequency and scan angle with voltage squared.
\[ \frac{d^2 \theta}{dt^2} + 2 \zeta \omega_r \frac{d \theta}{dt} + \omega_r^2 \theta = \frac{T(\theta)}{I}. \] (4.1)

Here, \( \theta \) is the turn angle, \( T(\theta) \) is the driving torque, \( \omega_r = \sqrt{k/I} \) is the angular resonant frequency, and \( \zeta \) is the damping coefficient. For an electrostatically-driven system, the torque is given by:

\[ T(\theta) = \frac{1}{2} \frac{\partial C}{\partial \theta} V^2. \] (4.2)

Here, \( V \) is the voltage, and \( C \) is the electrode capacitance. For a skewed electrostatic comb drive, \( C \) depends on the angle \( \theta \) in a complicated manner. The capacitance may be found numerically or quasi-analytically; for an example, see [122]. In general, we may write near \( \theta = 0 \):

\[ \frac{\partial C}{\partial \theta} = c_0 + c_1 \theta + c_2 \theta^2 + \cdots. \] (4.3)

Here, the constants \( c_0, c_1, c_2 \) etc. may be found by differentiating the angular variation of the capacitance and fitting a power series to the result. For a skewed or staggered comb drive, \( \frac{\partial C}{\partial \theta} \) may initially increase with angle, until the moving comb passes through the fixed comb, after which it may then decrease. In the initial phase, only a small, positive value of \( c_1 \) need be considered.

With a sinusoidal excitation, we may write \( V = V_0 \cos(\omega t) \), where \( \omega \) is the angular frequency, so that \( V^2 = V_0^2 \{1 + \cos(2\omega t)\}/2 \). Thus, \( V^2 \) contains both a DC component and an AC component at twice the driving frequency. Combining Equations 4.1, 4.2 and 4.3 and retaining (for example) terms only up to \( c_1 \), we obtain:

\[ \frac{d^2 \theta}{dt^2} + 2 \zeta \omega_r \frac{d \theta}{dt} + \{\omega_r^2 - \frac{c_1 V_0^2}{4I}\} \theta = \left(\frac{c_0 V_0^2}{4I}\right)\{1 + \cos(2\omega t)\} + \left(\frac{c_1 \theta V_0^2}{4I}\right) \cos(2\omega t). \] (4.4)
The solution of equation 4.4 can be obtained numerically, and the method is described in the next chapter. In this chapter, we confine ourselves to qualitative remarks. The result differs from the standard linear dynamics of a driven torsional oscillator in two important respects. Firstly, the last term in Equation 4.4 contains both \( \theta \) and \( t \). This feature implies that the response will be non-linear when \( c_1 \) is non-zero, so that higher harmonics will be introduced into the response when the deflection is large. Secondly, the coefficient of \( \theta \) has altered, and the effective angular resonant frequency \( \omega_{re} \) has become:

\[
\omega^2_{re} = \omega^2_r - \frac{c_1V_0^2}{4I}.
\]  

(4.5)

Using a binomial approximation, we obtain \( \omega_{re} = \omega_r - \frac{c_1V_0^2}{8I\omega_r} \). Thus, we might expect a voltage-dependent reduction in the resonant frequency that varies linearly with \( V_0^2 \), as was observed experimentally in Figure 4.10(b). Although its origin is entirely electrostatic, this behaviour has been interpreted as being equivalent to a voltage-dependent reduction in the mechanical stiffness [122], which will in turn give rise to larger deflections at higher voltages than might be expected from the simple voltage squared response implied by Equation 4.2. This prediction is also in agreement with our experiments.

The limiting resonant frequency may be found as follows. For a thin rectangular plate, the moment of inertia is given by:

\[
I = \frac{MW_1^2}{12}.
\]  

(4.6)

Here \( M = W_1W_2t\rho \) is the mass of the plate, \( W_1 \) and \( W_2 \) are its extents perpendicular to and parallel to the torsion axis, respectively, \( t \) is its thickness, and \( \rho \) is the density of the plate material. The torsional stiffness is
\begin{equation}
    k_\phi = 2 \times KG/L 
\end{equation}

where $L$ is the length of each torsion bar, where the shear modulus $G$ is

\begin{equation}
    G = E/[2(1 + \nu)]
\end{equation}

where $E$ and $\nu$ are the Young’s modulus and Poisson’s ratio of the bar material, respectively, and the torsion constant is [123]:

\begin{equation}
    K = wt^3\left\{\frac{1}{3} - 0.21 \cdot \left(\frac{t}{w}\right) \cdot (1 - \frac{t^4}{12w^4})\right\}. \quad (4.9)
\end{equation}

Here $w$ and $t$ are the width and depth of the torsion bar, respectively. Assuming that $W_1 = 496 \, \mu m$, $W_2 = 456 \, \mu m$, $L = 324 \, \mu m$, $t = 3.5 \, \mu m$ and $w = 324 \, \mu m$, and that $E = 1.3 \times 10^{11} \, N/m^2$, $\nu = 0.42$, and $\rho = 2330 \, kg/m^3$ (to model a silicon structure), a resonant frequency of $f_r = \omega_r/2\pi = 2780 \, Hz$ is obtained. Including the additional moment of inertia of the comb electrodes in a similar calculation, $f_r$ falls to 2390 Hz, in reasonable agreement with the experimental data.

The performance of the mirror can be evaluated by calculating the quality factor (Q-factor), which is a measure of energy loss or dissipation per cycle in relation to the energy stored in the fields inside the mirror. The Q factor is defined by

\begin{equation}
    Q = \frac{2\pi \text{maximum energy storage during a cycle}}{\text{average energy dissipated per cycle}}
    = \frac{2\pi W_0}{P T} = \frac{\omega_0 W_0}{P}
\end{equation}

where $W_0$ is the stored energy, $P$ is the power dissipation, $\omega_0$ is the angular resonant frequency, and $T$ is the period of an oscillation [124].
For high Q values, Q can be defined to a very good approximation using the bandwidth $\Delta \omega$:

$$Q = \frac{\omega_0}{\Delta \omega} = \frac{f_0}{\Delta f}$$  \hfill (4.11)

where $\Delta \omega$ is defined as the difference between the half-power frequencies $\omega_1$ and $\omega_2$, where

$$| \theta_0(\omega) | = \frac{1}{\sqrt{2}} | \theta_0(\omega_0) |$$  \hfill (4.12)

for $\omega = \omega_1$ and $\omega = \omega_2$.

The Q-factor varies with the peak scan angle. Using equation 4.12 and measured data from Figure 4.10, the Q value of the mirror is estimated as $\approx 20$ at small deflections.

### 4.4.3 Performance of active CCRs

Signal modulation and the data transmission performance of an active CCR was then measured, using the arrangement shown in Figure 4.11. A pellicle beam splitter was used to redirect a HeNe laser beam retro-reflected from an active CCR onto a silicon photodiode. The optical signal generated when the CCR was driven from a signal generator was then detected by the photodiode and passed to an oscilloscope. The path length from the CCR to the photodetector was 50 cm.

Figure 4.12 shows diffraction patterns obtained from a stationary CCR. Figure 4.12(a) is the measured diffraction pattern of an experimental active CCR, and Figure 4.12(b) is a theoretical far-field diffraction pattern for a perfect CCR [79]. Typically, the beam reflected from a CCR contains a diffraction pattern arising from the 6-sided polygonal aperture of the device [79]. In this case, the pattern is less distinct, because
the polygon is no longer a regular hexagon, but it still contains some structure.

Figure 4.11: Measurement set-up for modulation by an active CCR [79]

Figure 4.12: Diffraction patterns obtained from (a) a real active CCR (b) an ideal CCR

Figure 4.13 shows a typical modulation characteristic for an active CCR, obtained using a digital storage scope when the drive was a continuous sinusoidal voltage of 52 Vp-p, at the resonant frequency. The output is modulated, with sharp peaks whenever the mirrors are aligned. Since the reflected light shows several fringes [as previously shown in Figure 4.12(a)], as a result, the peaks in the detected signal also have sidelobes.
CHAPTER 4. SELF-ASSEMBLED CORNER CUBE REFLECTORS

Figure 4.13: Time variation of the retroreflected signal obtained with a sinusoidal voltage.

However, we have found that the fringe pattern becomes much less distinct as the path length increases, and only single, sharp resonant peaks are generated. The high degree of symmetry of the response again suggests good initial alignment.

Figure 4.14 shows the detected signal obtained when the active CCR is modulated using a 26 V square wave at a frequency of 20 Hz. At both the upward and downward steps in voltage, the second order dynamics of the torsion mirrors gives rise to decaying oscillations at the resonant frequency, so that about 7.5 ms is required to stabilise fully the mirror position and hence the detected signal. There is some evidence of ‘beating’ between the oscillations of the two individual mirrors. However, their decay is exponential to a reasonable approximation.

This behaviour may again be compared with simple theory, as follows. For small signals and light damping, the solution to Equation 4.1 for a step excitation is:
Here, $\theta_f$ is the final angle. The damping factor $\zeta$ may be estimated from the time $t$ needed for the oscillations to decay to $1/e$ of their initial value, namely $\tau = 1/2\pi f_r \zeta$. In this case, $\tau = 2.65 \, ms$, so that $\zeta = 0.026$.

This value may be related to the Q-factor as follows. At higher deflections, the apparent Q-factor decreases compared to small deflections, but its definition in the section 4.4.2 is questionable because of the non-linear effects previously described. The Q-factor may be estimated from the damping factor as $Q = 1/2\zeta$. In this case we obtain $Q = 19.25$, in good agreement with the results obtained from the frequency response data.

A mechanical modulator with a resonance in the kHz range should in principle have a bit rate that is measured in kbit/s. Unfortunately the long settling time obtained

$$\theta(t) = \theta_f \{1 - \exp(-\zeta \omega_r t) \cos(\omega_r t)\}$$ (4.13)

Figure 4.14: Time variation of the retroreflected signal obtained with a square wave voltage.
when the damping is low considerably reduces the effective bit rate. To transmit distinguishable signal values, transient oscillations should be allowed to decay sufficiently between bits. After a time $t = 2\tau$, the oscillations will have decayed to 13% of their initial amplitude, while the corresponding value for $t = 3\tau$ is 5%. Taking $\tau \approx 2.5\, \text{ms}$ then yields a bit rate of between 133 and 200 bit/s.

The signal-to-noise ratio (SNR) of the active CCR can be defined as

$$SNR = 20 \cdot \log \frac{A_{signal}}{A_{noise}}$$

(4.14)

where $A_{signal}$ is the difference in signal between turn-on and turn-off, and $A_{noise}$ is the amplitude of the steady-state noise. Zhou et al. have suggested that a bit-error probability of $10^{-6}$ requires a SNR of 19.5 dB [79]. Using equation 4.14 and data from the figure 4.14, the SNR of the active CCR at 26 $V_{p-p}$ operating voltage is estimated as 25.6 dB, which is already adequate. In fact, the SNR value can be improved by increasing the mirror deflection angle. Figure 4.15 shows the relation between the SNR values and the electrostatic forces, which are proportional to the applied voltage squared. Here, the data were obtained by comparing the signal with digitally extracted noise in the steady-state region of measured traces similar to Figure 4.14. As the voltage increases, more of the reflected light deviates from the detector in the off state, and the SNR increases. The SNR reaches 30 dB at a drive voltage of $\approx 30\, \text{V}$ and saturates at 36 dB, at $\approx 50\, \text{V}$. Figure 4.16 shows the detected signal obtained at this voltage. There are different transient oscillations at turn-on and turn-off, because the mirror deviation is now sufficient to cause the retroreflected beam to miss the detector entirely at turn on.

Although the optical performance of the CCR is sufficient for an optical communication system, there is a drawback resulting from the current mirror design. Since the mirror surfaces are perforated to improve sacrificial layer etching, power loss can
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Figure 4.15: Signal to noise ratio against the electrostatic forces

Figure 4.16: Detected signal obtained from an active CCR with a 20 Hz driven at $48V_{p-p}$
be expected during reflection. The current design has 4 \( \mu m \) square holes on a 20 \( \mu m \) square grid. Simply based on considerations of area, a loss of 4% (16 \( \mu m^2 / 400 \mu m^2 \)) per mirror is expected. If the signal beam is reflected from a complete CCR structure, a total power loss of 11.5% is expected. However, additional diffraction losses may also arise. These losses may be avoided by adopting a modified fabrication process that does not require etch holes, for example involving substrate removal.

4.5 Conclusions

Passive and active optical corner cube reflectors have been fabricated by a surface tension powered self-assembly method based on out-of-plane rotation of bonded silicon-on-insulator parts powered by meltable pads of photoresist. An extremely simple two-mask process has been used, and low process temperatures were involved. The use of bonded silicon ensures high mirror flatness and surface quality. A mechanical limiter arrangement ensures excellent alignment accuracy, which was less than 0.18° for both device types. This result represents the first demonstration of a precision high enough for effective CCR operation by a purely automatic, mass-parallel assembly method, opening the way to low cost fabrication of such complex structures.

Modulation and data transmission have been demonstrated using an active CCR with electrostatically driven torsion mirror scanners. Mirror deflections sufficient for full modulation of the retroreflected signal have been achieved at low drive voltages. A signal-to-noise ratio of 30 dB has been achieved at a drive voltage of 30 V, saturating at 36 dB at 50 V. The Q value of the torsion mirrors is 20, leading to a relatively lengthy ring-down and limiting the maximum data rate to around 200 bit/s. These characteristics are similar to previously demonstrated CCR types, implying that the use of self assembly does not involve a performance penalty.
Chapter 5

Modelling of CCR Response

5.1 Introduction

Combdrives have been widely used for the electrostatic actuation of MEMS devices. Depending on the electrode geometry, combdrives can be used for linear or angular displacements. The former type is called an in-plane combdrive whereas the latter type is an asymmetric or angular vertical combdrive. Compared to a parallel-plate actuator [79], combdrives have the advantages of low operating voltage, high resonant frequency when coupled with an elastic suspension and high stability.

The surface tension self-assembled MOEMS devices described in the previous chapters also used combdrives for actuation. Angular vertical drives were used for the corner cube reflectors. Although the actuation mechanism of in-plane combdrives have been analysed by many authors [109, 107], little has been reported on the mechanism of this type of actuator [122]. In this chapter, we numerically solve the non-linear behavior of the angular vertical combdrive, and compare the model predictions with the experimental result described in the previous chapter.
5.2 General solution method

In any electrostatic actuator, the driving force or torque is important. For an in-plane combdrive, the driving force $F$ can be expressed as follows [107]:

$$F = \frac{nt\varepsilon_0 V^2}{g}$$ (5.1)

where, $n$ is the number of the electrodes, $t$ is the thickness of the electrodes, $\varepsilon_0$ is the dielectric constant of the air gap ($8.85 \times 10^{-12} \text{ Fm}^{-1}$), $g$ is the gap between the electrode fingers and $V$ is the applied voltage. For an angular vertical combdrive, it is difficult to obtain an analytic expression for the torque created in the comb, because the torque itself depends on the relative angular position of the electrodes. However, numerical methods can be used to evaluate the torque.

The basic motion equations for a torsional oscillator and the solution form are expressed in the previous chapter as shown in the equations 4.1, 4.2, 4.3, 4.4 and 4.5. To solve the differential Equation 4.4 numerically, we used the Runge-Kutta method, which has been widely accepted as an efficient method of solving first-order differential equations. The algorithm first computes four auxiliary quantities $A_n, B_n, C_n, D_n$ to solve a first-order differential equation $y' = f(x, y)$ [125]. These quantities are given by:

$$A_n = hf(x_n, y_n), \quad B_n = hf(x_n + \frac{1}{2}h, y_n + \frac{1}{2}A_n)$$

$$C_n = hf(x_n + \frac{1}{2}h, y_n + \frac{1}{2}B_n), \quad D_n = hf(x_{n+1}, y_n + C_n)$$ (5.2)

where, $h$ is the step length. The solution is then obtained as a recurrence formula:

$$y_{n+1} = y_n + \frac{1}{6}(A_n + 2B_n + 2C_n + D_n).$$ (5.3)
The truncation error per step in the Runge-Kutta method is of the order $h^5$, and the method is, therefore, a fourth-order method.

A function of commercial software MATLAB 6.0, ODE45, solves differential equations using the Runge-Kutta method. The ODE45 is based on the explicit Runge-Kutta (4,5) formula [126] and repeats the calculation of $f(x, y)$ with high accuracy. We used the function to solve Equation 4.4. Since Equation 4.4 is second order, it is first expressed in terms of two coupled first order equations, of the form:

$$\theta_1' = \theta_2$$

(5.4)

$$\theta_2' = \left(\frac{c_0V_0^2}{4I}\right)\{1 + \cos(2\omega t)\} + \left[\left(\frac{c_1V_0^2}{4I}\right)\{1 + \cos(2\omega t)\} - \omega_r^2\right]\theta_1 - 2\zeta\omega_r\theta_2.$$  

(5.5)

The ODE45 function is then used to solve Equations 5.4 and 5.5 simultaneously.

As a first step in solving the equation, we have to determine the damping factor $\zeta$. The damping factor can be determined from the resonance curve of the mirror as [121]:

$$\zeta = \frac{1}{2}\left(\frac{\omega_2 - \omega_1}{\omega_n}\right)$$

(5.6)

where, $\omega_n$ is the resonant frequency of the mirror, $\omega_1$ and $\omega_2$ are the frequencies where the amplitude of the mirror oscillation falls to $1/\sqrt{2}$ of its peak value. We can obtain the damping factor from Figure 4.10(a). Since the resonance curve shows non-linear behavior at large amplitude, the damping factor is determined from the data set with the smallest amplitude, as 0.0209.

The second step is to define the limiting resonant frequency $\omega_r$, which is the resonant frequency for small amplitude oscillations, and to choose suitable coefficient ranges. The
analytic approximation of the effective resonant frequency, Equation 4.5, can be used for this purpose. Using the data set used for Figure 4.10(b), the limiting resonant frequency can then be extrapolated to 2236 Hz, and the coefficient $c_1$ estimated approximately to be $4.82 \times 10^{-13}$. To choose a suitable range of $c_0$, equation 4.4 can be further approximated by assuming, $\theta = \theta_0 + \theta_1 \exp(iw't) + \theta_2 \exp(2iw't) + \cdots$. Substituting this expression into equation 4.4 and expanding it in relation to $\theta_0$, we can obtain an approximation as follows:

$$\theta_0 w_r^2 = \frac{V_0^2}{4I} c_0 + \frac{V_0^2}{4I} c_1 \theta_0$$  \hspace{1cm} (5.7)$$

$$\theta_0 = \frac{V_0^2}{4I} c_0 \times \frac{1}{(w_r^2 - c_1 V_0^2 / 4I)}$$  \hspace{1cm} (5.8)$$

Substituting the coefficient $c_1$ of $4.82 \times 10^{-13}$, and using the measured data set, the coefficient $c_0$ can be approximated as $1.65 \times 10^{-11}$. Equations 5.4 and 5.5 can then be solved by varying the coefficients $c_0$ and $c_1$ around these analytically estimated values. The boundary conditions are taken as $\theta_1 = 0$ and $\theta_2 = \frac{\partial \theta}{\partial t} = 0$ at $t = 0$.

A least square fitting method is used to determine the coefficients $c_0$ and $c_1$. Correctness of the simulation is estimated using a coefficient of determination ($R^2$), which is an indicator from 0 to 1 that reveals how closely the estimated values for the trendline correspond to the actual data. The coefficient can be expressed as [127]:

$$R^2 = 1 - \frac{\sum_{i=0}^{n} (Y_i - \hat{Y}_i)^2}{\left(\sum_{i=0}^{n} Y_i^2\right) - \frac{\left(\sum_{i=0}^{n} Y_i\right)^2}{n}}$$  \hspace{1cm} (5.9)$$

where, $\hat{Y}_i$ is the predicted value and $n$ is the number of data points. The simulation
result is most reliable when the R-squared value is at or near 1.

Equation 4.1 can be solved for higher order coefficients, by substituting more of the terms in Equation 4.3 into Equation 4.4. Using this method, the torque acting on the mirror can be determined.

5.3 Numerical modelling of frequency response

Figure 5.1 shows the predicted time response of a mirror, obtained using the coefficient values $c_0 = 0.21 \times 10^{-13}$, $c_1 = 4.79 \times 10^{-13}$ and $c_2 = 43.31 \times 10^{-13}$, which are numerically determined as explained in the previous section, for different applied voltages. The frequency of the sinusoidal applied voltage is assumed to be $1067$ Hz. Because the driving torque is proportional to voltage squared, the period of the resulting oscillation is $1/(2f) = 0.46$ ms. Figure 5.1(a) shows the response at low applied voltage. The magnitude of the oscillation initially increases, then slightly decreases, then finally stabilizes, as expected from a linear device. At a high voltage, the oscillation amplitude increases for a longer time and stabilizes as shown in Figure 5.1(b). At a very high applied voltage, the oscillation amplitude increases with time as shown in Figure 5.1(c), and eventually becomes infinite. This result shows the non-linear response, because the electrical torque term in Equation 4.4 is expressed in combination of both angle and time.

The non-linear response can also be seen from the detailed curve shape of the oscillation. Figure 5.2 shows the steady-state oscillation curve obtained with various applied voltages. At a low voltage, initial shape of the oscillation, which is symmetric with a period of $0.46$ ms, is preserved after long time as shown in Figure 5.2(a). This period is same as that of the driving torque. Figure 5.2(b) shows the shape at a high voltage. The period becomes longer, $0.47$ ms in this case, and the symmetric shape starts to
Figure 5.1: Numerically simulated relation between time and angle using coefficients $c_0$, $c_1$ and $c_2$ with applied sinusoidal voltage of (a) $50 V_{p-p}$, (b) $80 V_{p-p}$ and (c) $100 V_{p-p}$. 
Figure 5.2: Detailed oscillation curve with applied voltage of: (a) $50 V_{p-p}$, (b) $80 V_{p-p}$ after 100 ms and (c) $100 V_{p-p}$ after 10 ms.
disappear. Finally at a very high voltage, the curve after 10 ms shows a non-symmetric and unstable shape with a period of 0.49 ms as shown in Figure 5.2(c). The non-linear behavior of the angular vertical combdrive at the high applied voltage therefore alters both the oscillation period and the harmonic content.

The frequency response of the mirror can be evaluated by calculating the stabilized amplitude from the time response obtained at different frequencies. By comparing these numerical prediction with real data, the coefficients describing the variation of the capacitance in Equation 4.3 can be determined.

Figure 5.3 shows the least square fitted result and Table 5.1 shows the coefficients using the data sets corresponding to the voltages of 52 and 92 V. Figure 5.3(a) shows the model prediction obtained from a first-order approximation for the torque. Although the graph correctly shows the shift of the resonant frequency with voltage, the absolute angles do not coincide very well. Moreover, in the simulated trend, we cannot see the distortion near the resonant frequency shown in the experimental result for the highest voltage. Figure 5.3(b) shows the result obtained using a second-order approximation. The result is now much closer to the experiment and the distortion in the response at high voltage can also be seen. Approximation to higher order in $\theta$ is not necessary, as can be seen from the R-squared value in Table 5.1. The mirror actuation torque can therefore be expressed as:

$$ T(\theta) = \frac{1}{2} \frac{\partial C}{\partial \theta} V^2 = (0.21 + 4.79\theta + 43.31\theta^2) \times 10^{-13} \times V^2. $$

The solution fits well for the non-linear behavior of high voltage up to 97 %, whereas for low voltage, the predicted result fits up to 83 %. This difference may result from measurement error at low voltage. Since the displacement change at low voltage is
Figure 5.3: Numerically simulated relation between frequency and angle obtained using only the coefficients $c_0$ and $c_1$ in the Equation 4.3.
approximation for capacitance

<table>
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<th></th>
<th>(c_0)</th>
<th>(c_1)</th>
<th>(c_2)</th>
<th>(c_3)</th>
<th>(R^2(52V))</th>
<th>(R^2(91V))</th>
</tr>
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<td>1st order</td>
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<td>(5.35 \times 10^{-13})</td>
<td></td>
<td></td>
<td>0.7002</td>
<td>0.5912</td>
</tr>
<tr>
<td>2nd order</td>
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<td>(4.79 \times 10^{-13})</td>
<td>(43.31 \times 10^{-13})</td>
<td>0.8256</td>
<td>0.9693</td>
<td></td>
</tr>
<tr>
<td>3rd order</td>
<td>(0.21 \times 10^{-13})</td>
<td>(4.79 \times 10^{-13})</td>
<td>(43.31 \times 10^{-13})</td>
<td>(0.02 \times 10^{-13})</td>
<td>0.8256</td>
<td>0.9693</td>
</tr>
</tbody>
</table>

Table 5.1: Coefficients of the variation of the capacitance obtained from numerical simulation.

small, the measurement cannot be performed as accurately as in the high voltage case.

Figure 5.4: Comparison of predicted peak deflection and electrical resonance frequency with the measured data.

Figure 5.4 shows the predicted peak deflection for other two applied voltages of 65 V and 78 V obtained using equation 5.10. The simulated results fit well with the experimental data at higher voltages.

5.4 Transient modelling of CCR response

Using a model that has been validated for the steady-state response, the transient response (e.g. Figure 4.16) may be predicted, and the power transmitted to the receiver
may be simulated.

The calculation starts with ray tracing through the CCR transmission system. Figure 5.5(a) shows ray tracing through an ideal CCR [116]. When an incident ray along the incident direction \( -\hat{n}_i \) strikes the CCR, the ray is reflected from each of the three mirrors in turn and exits the CCR along the direction \( \hat{n}_i \). The effective area can be defined as the area within which ray can strike all three mirrors and be reflected back to the light source. Figure 5.5(b) shows the effective area for the case where \( 2n_{ix} \geq n_{iz} \geq n_{iy} \geq n_{ix} \). Since the incident rays generally strike the CCR along the body diagonal direction, we can use the effective area in Figure 5.5(b) for the simulation.

To calculate the power transmitted to the receiver, Zhu et al. characterized the CCR by the differential scattering cross section (DSCS), which is defined as the reflected power per unit solid angle of observation per unit illumination irradiance, \( d\sigma(\hat{n}_i, \hat{n}_o)/d\Omega_o \). Here, \( \hat{n}_o \) is the reflected beam direction and \( \Omega_o \) is the solid angle of observation [116]. The received power \( P_r \) can then be expressed as [116]:

\[
P_r = I_i r_m^3 \int_{\Omega_o \in \Omega_r} \frac{d\sigma(\hat{n}_i, \hat{n}_o)}{d\Omega_o} d\Omega_o.
\]

(5.11)

where, \( I_i \) is the light intensity incident on the CCR, \( r_m \) is the reflectivity of the mirror and \( \Omega_r \) is the solid angle subtended by the receiver. When the direction of illumination and observation are collinear \( (\hat{n}_o = \hat{n}_i) \), which is reasonable because the receiver is usually placed along the axis of illumination in a CCR link system, and the distance between the receiver and the CCR is larger than the diameter of the receiver, the collinear differential scattering cross section (CDSCS) can be expressed as [79]:

\[
P_r \simeq I_i r_m^3 \frac{d\sigma(\hat{n}_i, \hat{n}_i)}{d\Omega_o} d\Omega_o.
\]

(5.12)

The power detected at the receiver then can be modelled by calculating the differ-
Figure 5.5: Optical model of the CCR: (a) ray tracing and (b) effective area of CCR surface.
ential scattering cross section (DSCS). The DSCS can be expressed using an optical relation as:

\[
\frac{d\sigma(\hat{n}_i, \hat{n}_o)}{d\Omega_o} = \frac{I_o R^2}{I_i} = \frac{|E_o(\hat{n}_i, \hat{n}_o)|^2 \cdot R^2}{2I_i}.
\] (5.13)

where, \( R \) is the distance between the receiver and the CCR.

During the CCR modulation, when one of the mirrors is misaligned by an angle \( \delta \), the electric field amplitude at the receiver position, \( E_o(\hat{n}_i, \hat{n}_o) \), is also changed. Because the radius of curvature of the mirror is sufficient large due to the use of BSOI material, we can use a relatively simple relation for the electric field amplitude modified from the relation in [116] as:

\[
E_o(\hat{n}_i, \hat{n}_o) = \sqrt{2I_i} e^{-i(k d + \pi N)} \cdot \int \int_{S_{uv}} e^{-i\varphi_{uv}(u,v)} dudv.
\] (5.14)

where, \( k = 2\pi/\lambda \), \( \lambda \) is the wavelength of the light (which is 6328 Å for a HeNe laser), \( N \) is the number of reflections of the beams, the \( u-v \) plane is the plane perpendicular to the incident beam direction \( \hat{n}_i \), \( S_{uv} \) is the total effective area and \( \varphi_{uv}(u,v) \) is the phase delay of a beam at the \((u,v)\) plane. Assuming only one mirror is modulated (for example, the \( xy \)-mirror), the phase delay of the beam can be expressed as:

\[
\varphi_{uv}(u,v) = \varphi_{xy}(x,y) = 2k \cdot \cos[\theta(\hat{n}_{xy}, \hat{n}_i)] \cdot y \cdot \delta_y.
\] (5.15)

where, \( \delta_y \) is the \( y \)-component of the deviation after the mirror has rotated through the angle \( \delta \), and \( \hat{n}_{xy} \) is the normal vector of the rotated \( xy \)- mirror plane. The CDSCS can be simulated using Equation 5.13, 5.14 and 5.15. Figure 5.6 shows the CDSCS versus the misalignment angle \( \delta \) for different mirror sizes. The incident direction is set along the body diagonal, \( \hat{n}_i = (1/\sqrt{3}, 1/\sqrt{3}, 1/\sqrt{3}) \), which is reasonable for communication link where the distance from the light source to the CCR is larger than the
Figure 5.6: CDSCS versus misalignment angle $\delta$ for different CCR mirror sizes.

The size of the CCR. Figure 5.6 shows that the CDSCS angle increases as the mirror size increases and that small misalignment of 3 mrad is enough to switch the CCR from the turn-on to the turn-off state. This result is also consistent with the literature [116].

The dynamic response of the CCR can then be modelled by substituting the angle of the mirror obtained in previous section into the misalignment term of this CCR model. In reality, the applied voltage used to actuate the CCR is a square wave with frequency of 20 Hz at different voltages. Figure 5.7 shows the predicted dynamic response of a CCR at a low voltage of 26 $V_p-p$. Figure 5.7(a) shows the DSCS obtained in the ideal case, when all the three mirrors are aligned perfectly. This result can be compared with the experimental result shown in Figure 4.14. Ideally, the DSCS has maximum value when the mirror angle is zero. The signal at the beginning of the turn-off state, therefore, shows a beating which corresponds to the mirrors oscillating in the opposite direction. The experimental result, however, shows higher value at the beginning of the turn-off state, and no beating. This means the CCR initially must have an angular mis-
Figure 5.7: Dynamic response of a CCR actuated by a square wave with frequency of 20 Hz and voltage of 26 $V_{p-p}$: (a) for an ideal case and (b) for a CCR with a misalignment of 1 mrad.
alignment. Figure 5.7(b) shows the DSCS assuming an initial misalignment of 1 mrad. In this case, the predicted DSCS shape is consistent with the experimental data.

The dynamic response of the CCR at a high voltage can also be predicted. Figure 5.8 shows the step response of a CCR at a voltage of $48 V_{p-p}$, without and with a misalignment, respectively. In this case, when the voltage is turned on, the deviation of the CCR is sufficient to cause the retroreflected beam to miss the detector entirely. This result a 'clipping' of the DSCS as can be seen in Figure 5.8(a). Figure 5.8(b) shows the DSCS assuming an initial misalignment of 1 mrad. Because the oscillation angle of the mirror at the beginning of the turn-off state exceeds the initial misalignment, some beating oscillations can be seen in Figure 5.8(b).This result also coincides with the experimental data as shown in Figure 4.16.

Although the model suggested in this section is well matched with the experimental data, the predicted and experimental results do not show exactly same shapes. The reason for these differences lies in the actual geometry and actuation type of the experimental CCR device. The CCR used in the measurement has a larger geometrical area than the three perpendicular mirrors. For example, there is a land region between mirror-1 and mirror-2, as shown in Figure 4.8. This region effects the ray trace reflected from the mirrors. The other factor is the size of mirror-3. In the previous model, we assumed that each mirror has a same size, with a square overall shape. In fact, mirror-3 has a different size and shape compared to mirror-1 and 2. Actuation of the mirror is also different in the real device. We assumed for simplicity that only one mirror was actuated in the simulation. In fact, two mirrors are actuated in the experimental CCR. To model the dynamic response of the CCR more accurately, a more realistic device geometry should be considered.
Figure 5.8: Dynamic response of a CCR actuated by a square wave with frequency of 20 Hz and voltage of $48V_{p-p}$: (a) for an ideal case and (b) for a CCR with a misalignment of 1 mrad.
5.5 Conclusion

A model for the dynamic response of a corner cube reflector has been proposed, based on the solution of the non-linear equations of motion for a torsional oscillation driven by an angular vertical combdrive. From the solution, it has been shown that the torque generated by the angular vertical combdrive depends on the second order of the turn angle, which results in a deformation in the shape of the resonance curve at high voltage. The numerical solution also shows a shift in the resonant frequency with drive voltage. The model can predict the resonant frequency at an arbitrary input voltage.

The dynamic response of a CCR actuated by a square-wave input voltage has also been modelled. The theoretical predictions agree well with the experimental results. From the model, a small initial misalignment of the CCR mirrors can also be determined.
Chapter 6

Stability of surface tension
self-assembled 3D MOEMS

6.1 Introduction

So far, we have presented surface tension self-assembled devices and its dynamic model. Although surface tension powered self-assembly is a compact batch fabrication method, there are possible concerns about the structural rigidity of a hinge made from polymer materials. For example, relatively short life-times have been reported for a conjugated polymer actuator [137]. On the other hand, Ebefors et al. have demonstrated a robust hinge structure made by polyimide [138]. Since polymer materials usually have high thermal expansion coefficients, the thermal stability of self-assembled MOEMS components requires evaluation. Similarly, dynamic testing is also required to evaluate mechanical stability. Although some dynamic testing of MOEMS devices [139, 140] and of RF-MEMS devices [141] has been reported, full evaluation of surface tension self-assembled devices has not been carried out.

In this chapter, we have investigated both the thermal and the dynamic stability of
self-assembled MOEMS components formed by photoresist powered self-assembly. The devices used for testing are corner cubes which are described in chapter 4. Depending on the test purposes, one of the mirrors with/without limiter is evaluated. In section 6.2, we determine the thermal-mechanical constants of the hinge material. In section 6.3, experimental measurements of the thermal stability of self-assembled MOEMS components are presented. Both metallized and non-metallized components, and components constrained using mechanical limiter mechanisms are evaluated. The results are compared with the predictions of finite element analysis. In section 6.4, dynamic testing of MOEMS components is described. Conclusions are presented in section 6.5.

6.2 Thermal-mechanical constants of photoresist

To evaluate the thermal stability of self-assembled devices, it is essential to know the thermal-mechanical constants of the hinge material (e.g., its Young’s Modulus, Poisson’s ratio and linear thermal expansion coefficient). In our experiment, Shipley AZ4562 photoresist is used as the hinge material. Unfortunately suitable data are not reported in the literature. To determine these constants, a resonant frequency measurement technique suggested by Petersen [142] and further developed by Osterberg was used [143]. This technique required the fabrication of suspended parts using photoresist as a structural material.

Arrays of suspended cantilever beams and fixed-fixed beams formed in AZ4562 photoresist with 40 $\mu$m width, 11 $\mu$m thickness and different lengths were prepared on a silicon substrate with an oxide sacrificial layer. First, the AZ4562 photoresist was spin coated at 1350 rpm for 30 sec to form an 11 $\mu$m thick layer on a 100 mm dia substrate using a Karl Süss Gyrset spinner equipped with a rotating wafer cover to control the local air flow. To define beams, the photoresist was patterned using a structural feature
mask by UV lithography, using a Quintel Q4000-IR aligner. The wafers were then hard-baked in an oven for 30 min to enhance adhesion of the photoresist to the substrate. Three hard-bake temperatures of $120^\circ C$, $140^\circ C$ and $160^\circ C$ were used, to represent the actual re-melting temperature used during surface tension self assembly. To free the beams, the oxide sacrificial layer beneath the photoresist was removed by wet etching for 3 hours in 7:1 buffered hydrofluoric acid. The remaining washing water was then removed by freeze-drying. A Cr layer of 100 Å thickness was then sputter-coated on the beams to allow an AC voltage to be applied between the beams and the substrate.

Figure 6.1 shows the two kinds of beam structure used in subsequent experiments. Figure 6.1(a) shows cantilever beams which have an intrinsic upward bending caused by a stress gradient within the resist following from preferential surface evaporation of solvent [142] and an intrinsic tensile stress in the Cr-layer. Figure 6.1(b) shows fixed-fixed beams, where much less bending can be seen.

### 6.2.1 Measurement of Young’s modulus

The Young’s modulus of the photoresist can be calculated from the resonant frequency of cantilever beams. Since the thickness of the Cr layer is negligible compared to the
Beam configuration  |  1st mode \((\beta_i L)^2\) | 2nd mode \((\beta_i L)^2\) | 3rd mode \((\beta_i L)^2\)  
--- | --- | --- | ---  
Cantilever  | 3.52 | 22.0 | 61.7  
Fixed-fixed  | 22.4 | 61.7 | 121.0  

Table 6.1: Eigenvalues for a simple beam with different beam configurations.

<table>
<thead>
<tr>
<th>Hard bake temperature, °C</th>
<th>120</th>
<th>140</th>
<th>160</th>
</tr>
</thead>
<tbody>
<tr>
<td>Young’s modulus ((\times 10^9)), N/m²</td>
<td>6.10</td>
<td>7.36</td>
<td>6.88</td>
</tr>
</tbody>
</table>

Table 6.2: Young’s modulus of AZ4562 photoresist with different hard bake conditions.

thickness of the photoresist, the un-damped free vibrations of a beam can be estimated using Euler bending theory [121]. The governing equation for undamped vibration is:

\[
EI\partial^4y/\partial x^4 + \rho A\partial^2y/\partial t^2 = 0
\]  

(6.1)

where, \(A\) is the cross-sectional area of the cantilever, \(I\) is its second moment of area, \(E\) is the Young’s modulus, and \(\rho\) is the density of the beam material, \(y\) is the transverse deflection and \(t\) is the time.

Using this equation, with the boundary conditions \(y = \partial y/\partial x = 0\) at \(x = 0\) and \(\partial^2 y/\partial x^2 = \partial^3 y/\partial x^3 = 0\) at \(x = L\), the resonant frequency of a cantilever may be found as:

\[
\omega_i = \frac{(\beta_i L)^2}{L^2} \cdot \sqrt{EI/\rho A}
\]  

(6.2)

Similar solutions may be found for different beam configurations. The values of \((\beta_i L)^2\) thus obtained are shown in Table 6.1.

Figure 6.2 shows the measured electrical resonant frequencies plotted against reciprocal length squared. These data represent averages taken from five sets of cantilevers, and the resonant frequencies were very consistent from set to set. The graph shows a linear relation, in agreement with Equation 6.2. Since hard baking can change both the thickness and density of the photoresist by driving off residual solvent and carrying
out additional crosslinking, we measured both these values after hard baking. Knowing from measurement that \( I = 4.37 \times 10^{-21} \, m^{4} \), \( A = 4.38 \times 10^{-10} \, m^{2} \) and \( \rho = 1268 \, kg/m^{3} \), the Young’s modulus of the resist can be found from the slope of the graph. Samples prepared with three different hard baking temperatures were measured to investigate the effect of process temperature. Table 6.2 shows the calculated Young’s modulus. The value shows little variation over the temperature range from 120 to 160 °C, which corresponds to the melt temperature used in surface tension self-assembly. The melting temperature therefore shows a relatively stable process window.

### 6.2.2 Measurement of thermal expansion coefficient

The thermal expansion coefficient (TCE) of the photoresist can be estimated from the vibration of fixed-fixed beams. Since the beam is constrained at both ends, the system is now governed by the equation for a beam subject to an axial stress \( \sigma \). In case of a
tensile axial stress, the beam bending equation is [144]

\[ EI \partial^4 y/\partial x^4 - \sigma A \partial^2 y/\partial x^2 = -\rho A \partial^2 y/\partial t^2 \]  \hspace{1cm} (6.3)

From Equation 6.3 and using the boundary conditions \( y = \partial y/\partial x = 0 \) at \( x = 0 \) and \( x = L \), we can obtain the resonant frequency as:

\[ \omega_i = \sqrt{\beta_i^4(EI/\rho A) + \beta_i^2(\sigma/\rho)} \]  \hspace{1cm} (6.4)

When the temperature of the fixed-fixed beam is raised, the beam experiences further thermal stress if differential thermal expansion occurs relative to the substrate. By measuring the change of resonant frequency during a heating cycle, the thermal stress can be calculated. Together with any intrinsic stress, the thermal expansion coefficient is then deduced from the thermal stress equation as:

\[ \sigma = E_{PR} \times (\alpha_{PR} - \alpha_{Si}) \times \Delta T \]  \hspace{1cm} (6.5)

where, \( E_{PR} \) and \( \alpha_{PR} \) are the Young’s modulus and TCE of the photoresist, \( \alpha_{Si} \) is the TCE of the substrate silicon and \( \Delta T \) is the temperature increase.

The first resonant frequency of an unstressed fixed-fixed beam is \( 22.4/3.52 = 6.36 \) times larger than that of a cantilever of the same length, as can be seen from Table 6.1. Using the first resonant frequency of a cantilever as described in section 6.2.1, and measuring the first resonant frequency of a fixed-fixed beam, the built-in stress \( (\sigma_1) \) of the beam can be determined using Equation 6.4. The temperature was then raised and the frequency shift was measured. From the results, the thermal stress \( (\sigma_2) \) and the TCE of the resist can be obtained using Equations 6.4 and 6.5.

Measured mechanical resonant frequencies of cantilever beams and fixed-fixed beams with a length of \( 3150 \mu m \) at room temperature, calculated intrinsic stresses and thermal
expansion coefficients (TCEs) are shown in Table 6.3. The frequency shift ratio is the variation in resonant frequency per $1^\circ C$ temperature change observed during the temperature excursion. The TCE values of the photoresist are smaller and more stable, compared to the values of other common polymers, as shown in Table 6.4. However, since the process-induced stress increased rapidly as original melting temperature rose, the melting temperature should be carefully selected.

<table>
<thead>
<tr>
<th>Hard bake temperature, $^\circ C$</th>
<th>resonant frequency (cantilever), Hz</th>
<th>Resonant frequency (fixed-fixed beam), Hz</th>
<th>Processing stress ($\times 10^5$), N/m$^2$</th>
<th>Frequency shift ratio, Hz/$^\circ C$</th>
<th>Thermal expansion coefficient ($\times 10^{-6}$), $^\circ C^{-1}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>120</td>
<td>396</td>
<td>5614</td>
<td>5.88</td>
<td>14.71</td>
<td>3.11</td>
</tr>
<tr>
<td>140</td>
<td>442</td>
<td>5810</td>
<td>5.63</td>
<td>73.85</td>
<td>5.46</td>
</tr>
<tr>
<td>160</td>
<td>428</td>
<td>7330</td>
<td>10.3</td>
<td>23.82</td>
<td>3.67</td>
</tr>
</tbody>
</table>

Table 6.3: Thermal expansion coefficients of AZ4562 photoresist with different hard bake conditions.

<table>
<thead>
<tr>
<th>Material</th>
<th>Young’s Modulus ($\times 10^9$), N·m$^{-2}$</th>
<th>Thermal expansion coefficient ($\times 10^{-6}$), $^\circ C^{-1}$</th>
<th>Poisson’s ratio</th>
</tr>
</thead>
<tbody>
<tr>
<td>Dielectric</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>SiO$_2$</td>
<td>73.1</td>
<td>0.45</td>
<td>0.17</td>
</tr>
<tr>
<td>Semiconductor</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Si</td>
<td>130</td>
<td>2.6</td>
<td>0.28</td>
</tr>
<tr>
<td>Polymer</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>PMMA</td>
<td>2.4 - 3.4</td>
<td>50 - 90</td>
<td>0.35-0.4</td>
</tr>
<tr>
<td>Epoxy</td>
<td>$\sim 3.2$</td>
<td>45 - 65</td>
<td>0.35</td>
</tr>
<tr>
<td>Polyimide</td>
<td>$\sim 3.1$</td>
<td>40 - 50</td>
<td>0.35</td>
</tr>
<tr>
<td>PVC</td>
<td>2.4 - 4.1</td>
<td>70 - 80</td>
<td>0.41</td>
</tr>
<tr>
<td>Polycarbonate</td>
<td>2.4</td>
<td>66</td>
<td>0.39</td>
</tr>
<tr>
<td>Nylon-6</td>
<td>2.4 - 3.1</td>
<td>280</td>
<td>0.41</td>
</tr>
<tr>
<td>Polystyrene</td>
<td>2.7 - 4.2</td>
<td>34 - 210</td>
<td>0.34</td>
</tr>
<tr>
<td>AZ4562*</td>
<td>7.36</td>
<td>5.46</td>
<td>0.42</td>
</tr>
<tr>
<td>Metal</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Al</td>
<td>70.3</td>
<td>23.1</td>
<td>0.33</td>
</tr>
<tr>
<td>Au</td>
<td>78</td>
<td>5.7</td>
<td>0.42</td>
</tr>
<tr>
<td>Zn</td>
<td>108.4</td>
<td>30.2</td>
<td>0.25</td>
</tr>
</tbody>
</table>

Table 6.4: Mechanical constants of materials used in MEMS [145].
6.2.3 Measurement of Poisson’s ratio

Finally, a thermal expansion measurement was performed on a resist-coated silicon wafer to obtain Poisson’s ratio. Figure 6.3 shows a schematic diagram of the experiment. A 100-mm dia single crystal silicon wafer coated with AZ4562 photoresist was placed on a hot plate. Differential thermal expansion between the silicon and the photoresist caused the wafer to bow into a spherical convex surface. Laser reflection was used to determine the curvature. The incidence angle of the laser was set to 30°, and the angle variation of laser beam reflected from the wafer during the heating was measured at two points, A and B.

![Schematic diagram of experiment used for thermal constants measurement.](image)

Figure 6.3: Schematic diagram of experiment used for thermal constants measurement.

If we assume the initial substrate to be flat, the change in the radius of curvature (R) after thermal load can be determined from the equation, \( r_{AB} = R \times \theta \), where \( r_{AB} \) is approximately the distance between points A and B for large R, and \( \theta \) is the angle...
between points A, O and B. From the geometry, $\theta$ can be determined as $(\theta_A + \theta_B)/2$, where $\theta_A$, $\theta_B$ are the angle variation obtained during the thermal excursion at the points A and B, respectively. To evaluate the mechanical constants, Stoney’s formula [146] for the curvature of a strained wafer was then used:

$$\sigma_{PR} = \frac{E_{Si}}{6R} \times \frac{(d_{Si})^2}{(1 - \nu_{Si}) \cdot d_{PR}} \quad (6.6)$$

Where $\sigma_{PR}$ is the stress of the photoresist, $E_{Si}$ and $\nu_{Si}$ are the biaxial Young’s modulus and Poisson’s ratio of the substrate, and $d_{Si}$ and $d_{PR}$ are thicknesses of the substrate and photoresist, respectively.

For the thermal expansion relation, Equation 6.5 can be used. In this case, the Young’s modulus of the photoresist must be changed to the biaxial value, $E_{PR}/(1 - \nu_{PR})$. Using Equations 6.5 and 6.6, the thermal-mechanical constants relation can then be obtained as [146]

$$\frac{E_{PR} \times (\alpha_{PR} - \alpha_{Si})}{1 - \nu_{PR}} = \frac{\theta_A + \theta_B}{2} \times \frac{1}{d_{PR} \cdot r_{AB}} \times \frac{E_{Si} \cdot (d_{Si})^2}{6 \cdot (1 - \nu_{Si}) \Delta T} \quad (6.7)$$

Knowing $\alpha_{Si}$ and $\nu_{Si}$, the value of $\nu_{PR}$ can be calculated from Equation 6.7 and the results obtained in the previous section.

For the bowing test, an AZ4562-coated silicon wafer with a thickness of 546 $\mu$m and a resist thickness of 10 $\mu$m was used. The resist was hard baked at 140 $^\circ$C. A Young’s modulus of $1.3 \times 10^{11} \text{ N/m}^2$ and a Poisson’s ratio of 0.28 for [100] oriented silicon were used in the calculation. The Poisson’s ratio of the AZ4562 photoresist was then found as 0.42. This value is reasonable by comparison with data for other polymer materials [see Table 6.4].
6.3 Thermal stability of self-assembled MOEMS

Thermal stability measurements were performed on three types of self-assembled components, each of which is a sub-component of a corner cube, as shown in Figure 6.4. These included 45° mirrors [Figure 6.4(a)], 90° mirrors [Figure 6.4(b)], and 45° mirrors with a mechanical limiter [Figure 6.4(c)]. The components were placed on a heater, and the temperature was varied from 25°C to 145°C. A laser beam reflection method was used to measure the resulting angular variation of the mirrors. Figure 6.5 shows a schematic of the thermal stability measurement. A beam from a He-Ne laser mounted on a rotation platform with a Vernier angle gauge was reflected on the mirror surface. The laser was rotated from perpendicular to the substrate plane to perpendicular to the mirror surface. The mirror angle was then measured with a resolution of 0.02°. As the temperature rose, the angle was measured at 10°C intervals. Similar measurements were performed on uncoated and Au-coated samples.

Figure 6.6 shows the variation of mirror angle with temperature for a 49°-rotated mirror, for different temperature excursions. Figure 6.6(a) shows a small excursion and 6.6(b) a larger one. In Figure 6.6(a) the angle gradually decreases as the temperature rises. The angle variation with temperature shows a roughly linear relation with a slope of $-0.00542/°C$, and reversible behaviour up to a temperature of $\sim 110°C$. However, when the temperature rises beyond 120°C, the angle alters rapidly and shows irreversible characteristics as shown in Figure 6.6(b). This result suggests that the melting point of the AZ4562 photoresist lies between 110 and 120°C. The hinge therefore remelts and further rotates to reduce the surface energy. This behaviour was confirmed for other 45°-rotated mirrors.

For Au-coated mirrors, the angle also decreases with the temperature, as shown in Figure 6.7 for a 52°-rotated mirror. When the temperature rises above 120°C, melting of the photoresist can again be seen, as shown in Figure 6.7(b). However, in
Figure 6.4: Components used for thermal constants measurement: (a) 45° mirror, (b) 90° mirror and (c) 45° mirror with mechanical limiter.
Figure 6.5: Schematic diagram for thermal stability measurement of self-assembled components.

Contrast to the un-metallized mirror, the angular variation of the metallized mirror alters, depending on the position of the optical beam with respect to the mirror. This can be explained by the residual stresses and the difference in the thermal expansion coefficients (TCEs) between the gold layer and the silicon layer, as well as the photoresist hinge expansion. Since the metallized mirror is a film-substrate structure, there is initially internal residual stress, and the mirror is curved downward. However, because the TCE of gold ($5.7 \times 10^{-6} \degree C^{-1}$) is larger than the TCE of silicon ($2.6 \times 10^{-6} \degree C^{-1}$), the Au-film is under compressive stress as the temperature is raised and the mirror curvature increases. The angle variation therefore slightly varies from $-0.00417$ to $-0.00604/\degree C$ depending on the measuring point. However, the amount of variation is similar to that of the non-metallized mirror. The thermal behaviour can be compared with other results obtained using polymer actuators. For example, Ebefors et al. have reported a degradation of polyimide v-groove actuators above the melting point [138].

Finally, to compensate for melting of the photoresist pads, the temperature stability of $45^\circ$ mirrors with mechanical limiters [Figure 6.4(c)] was measured, as shown in Figure
Figure 6.6: Variation of angle with temperature for a un-metallized mirror, with temperature excursion (a) below and (b) above the melting point.
Figure 6.7: Variation of angle with temperature for a metallized mirror, with temperature excursion (a) below and (b) above the melting point.
6.8. For three mirrors measured, the devices show almost zero temperature sensitivity and the angle does not change even when the temperature is increased above $150^\circ C$. After three repeated temperature cycles up to $150^\circ C$, the mirror angle was 44.88°, very slightly different from the initial angle of 44.85°.

The thermal stability of the self-assembled 45° mirror [Figure 6.4(a)] was then estimated numerically using the data obtained in previous sections. Finite element simulation software (ANSYS) was used for the simulation and the thermal-mechanical constants of silicon and AZ4562 photoresist are summarized in Table 6.4. The mirror dimensions are $976 \mu m \times 3.5 \mu m$ with a length of $956 \mu m$. The hinge dimensions are $240 \mu m \times 20 \mu m$, following a slight shrinkage during the melting process. There are also $2430 \mu m \times 6 \mu m$ rectangular holes over the hinge structure to enhance sacrificial layer etching. Since the mirror has a symmetric geometry, only half of the structure was simulated, using symmetric boundary conditions.

Figure 6.9(a) shows the simulated rotation of the mirror, one end of which is fixed...
Figure 6.9: Thermal stability simulation results for (a) a mirror attached only by one edge and (b) a mirror equipped with a mechanical limiter.
to the substrate by the hinges, for a temperature rise of $100^\circ C$. The mirror angle alters at a rate of $-0.0014/\degree C$. This result is of the same order but slightly smaller than the experimental result. This difference may result from the inaccurate three dimensional hinge shape used in the simulation and the different TCE values of the resists used in the cantilever experiment and the self-assembled mirror structure. For design optimisation, simulation shows that if the rectangular holes over the hinge structure are removed, the thermal stability is further improved to $-0.0007/\degree C$. A simulation was also performed for the case of a mirror whose free end is engaged by a mechanical limiter. In this case, the mirror bends rather than rotates [Figure 6.9(b)].

Therefore, we conclude that the thermal stability of surface tension self assembled devices is greatly improved by using mechanical limiters, and has sufficient tolerance for most applications. Moreover, we know from the simulation results that the thermal stability of the devices can be further enhanced by optimizing the design (for example, reducing the size of etch access holes near hinges) without changing the materials.

6.4 Vibration stability of self-assembled MOEMS

The mechanical stability of MOEMS components is another concern for real-world applications. The response to external vibration was therefore evaluated for the components previously shown in Figure 6.4. Mirrors with and without a mechanical limiter were assessed. Figure 6.10 shows the arrangement for measurement of in-plane dynamics. To provide external excitation, the component was mounted on a small stage attached to a circular piezoelectric diaphragm. In-plane displacements of the mirror were measured using an optical microscope equipped with $\times 20$ and $\times 50$ objectives, a video camera and an on-screen cursor measurement system. The maximum resolution of this system is $\approx 1\, \mu m$. The piezoelectric transducer showed an approximately linear response with
a voltage, and $1.4 \mu \text{m/V}$ displacement was obtained at the frequency of 1 kHz. Its frequency response was flat at low frequencies, with a primary resonance at 390 Hz. Above this frequency, the response reduced quickly. However, measurements could still be made at high frequency and measurements were made from 50 Hz to 10 kHz. The maximum input power of the transducer is 150 mW.

Figure 6.11 shows the measured frequency responses of surface tension self-assembled mirrors. Two un-engaged mirrors (one rotated 90° and the other rotated 45°), and one 45° mirror engaged with a mechanical limiter as shown in Figure 6.4 were measured. Un-engaged mirrors had resonant frequencies of 2830 and 2863 Hz for mirror angles of 90° and 45° respectively. In case of the mechanically limited mirror, no resonance peak appeared within the frequency range.

The acceleration externally applied to the mirror can be calculated as follows. Since the transducer was driven by a sinusoidal voltage, its displacement can be written as:
The acceleration of the transducer is then:

\[ \ddot{y} = -A\omega^2 \sin \omega t \]  

(6.9)

The maximum acceleration applied to the mirrors is then \(| \ddot{y} | = A\omega^2\). Since the response of transducer is 1.4 \( \mu m/V \) at 1 kHz, the displacement of the transducer at the applied voltage of 20 Vp-p is 28 \( \mu m \), which corresponds to 2 \( \times \) A. The maximum acceleration is therefore 552.70 \( m/s^2 \) or 56.4 g. Under this acceleration, none of the mirrors showed any failure. Life cycles of the devices were then measured. Three 90\(^\circ\)-rotated un-engaged mirrors were mounted on the transducer, which was driven at 20 V and 2840 Hz, the resonant frequency of the device. The mirror angle was measured every 30 min. The angle of all three mirrors was stable within 0.2\(^\circ\) until 1.4 \( \times \) 10\(^7\) cycles,
and the measurement was stopped. This result can be compared with the performance of other actuators based on polymer materials. Smela et al. reported a life cycle of 10,000 for a device formed from conjugated polymers [137] and Ebefors et al. reported more than \(1.6 \times 10^8\) cycles for polyimide v-groove joints [138]. The life cycle of the AZ4562 pads appears sufficient compared to other polymer actuators. The AZ4562 photoresist pads therefore endured both high external acceleration and the internal resonant frequency conditions, demonstrating the mechanical robustness of the hinges.

The resonant vibration modes of the mirrors can also be estimated using ANSYS. To do so, we assumed a density of \(2330\, kg/m^3\), Young’s modulus of \(1.3 \times 10^{11}\, N/m^2\) and Poisson’s ratio of 0.28 for silicon. For the photoresist hinges, the values obtained in section 6.2 were used, as shown in Table 6.4. Figure 6.12 shows the first mechanical resonant modes obtained from the simulation. For a mirror simply hinged by the photoresist pads, the predicted first mechanical resonant frequency is 3054 Hz, as shown in Figure 6.12(a). For a mirror with a limiter, both ends of the mirror are fixed. In this case, the first mechanical resonant frequency rises considerably to 24430 Hz, as shown in Figure 6.12(b), which is far above the capability of the transducer. The difference between the measured frequencies and the simulated values is mainly due to the variation of the devices dimension during process. For example, if the thickness of the mirror changes from 3.5 \(\mu m\) to 3.0 \(\mu m\), the resonance frequency changes from 3054 to 2629 Hz.
Figure 6.12: Shape of first mechanical resonant mode for (a) a mirror attached only by one edge and (b) a mirror equipped with a mechanical limiter.
6.5 Conclusion

The thermal-mechanical constants of the hinge material (AZ4562 photoresist) used for surface tension self-assembled 3D MOEMS components have been measured. Young's moduli and thermal expansion coefficients showed relatively constant values under various hard bake conditions. The re-melting process window therefore ranges from $120^\circ C$ to $160^\circ C$, which is sufficient for surface tension self assembly.

The thermal and mechanical stability of self-assembled MOEMS components has been evaluated and compared to simulation results. The components are surprisingly stable. The angle variation of all the components tested under the thermal stresses was less than $0.006^\circ /^\circ C$ up to $110^\circ C$. However, melting of AZ4562 photoresist occurs between $110 - 120^\circ C$, which changes the mirror angle significantly for mirrors without mechanical limiters. A limiter enhances the thermal stability to near zero, and the angle appears stable up to $150^\circ C$. The finite element simulation result also shows that the stability of the devices can be further improved by optimizing the device design without changing materials.

The dynamic mechanical stability of the components has also been evaluated using vibration testing. No failure was observed in any of the devices tested, for external accelerations greater than 50 g. The lifetime of devices has been measured to more than $1.4 \times 10^7$ cycles of the internal resonance, which is enough for practical applications. Surface tension self-assembled mirrors based on AZ4562 photoresist hinges therefore show relatively stable thermal response and considerable resistance to external mechanical vibrations.
Chapter 7

Conclusion and Future Work

7.1 Summary

In this thesis, fabrication of three dimensional MOEMS components using surface tension powered self-assembly has been described. The process uses just two mask layers, one to define the mechanical parts and the other to form the meltable pads. The use of bonded silicon on insulator wafer provides high flatness and surface quality for optical applications. Patterning resolution have been improved to 0.2 $\mu m$ by using a DRIE process. Wet etching uniformity has also been improved by adopting a magnetic stirrer during etching. A polymer material, e.g. AZ4562 photoresist, has been used as a hinge material to provide the surface tension torque for out-of-plane rotation, which enables a low process temperature compatible with the IC-industry. The use of a limiter mechanism ensures the assembly accuracy of 3D MEMS devices.

Two types of corner cube reflectors (passive and active) have been fabricated and characterised. The surface tension self assembled CCRs showed a very accurate mechanical alignment less than 0.18°. The active CCR device was actuated by electrostatically driven torsion mirrors, mirror deflection of the device was sufficient to modulate the
signal in the receiver stage at a low drive voltage. A signal-to-noise ratio of more than 30 dB was achieved at a drive voltage of 30 V, which is suitable for optical data transmission. The maximum data transmission rate was estimated around 200 bit/s. The surface tension self-assembled CCR therefore achieved a sufficiently good performance for use in an optical communication link.

A reasonable model for the dynamic response of a corner cube reflector with a non-linear actuator has been proposed and confirmed by good agreement with experimental data. The torque generated by the angular vertical combdrive used to actuate the mirror depends on the second order of the turn angle, which has been shown to result in a shift in the resonant frequency with drive voltage. The model prediction was precise enough to allow the determination of a small initial misalignment of the CCR mirrors.

Measurement of the thermal-mechanical constants of the hinge material, such as Young’s moduli, Poisson’s ratios and thermal expansion coefficients, have shown that the re-melting process window ranges from 120°C to 160°C, sufficient for surface tension self assembly. The thermal and mechanical stability of self-assembled MOEMS components has been evaluated. The components are very stable. Melting of AZ4562 photoresist occurs between 110 – 120°C, and below the melting temperature, the angle variation of all the components tested under the thermal stress was less than 0.006°/°C. The mechanical limiter mechanism has been shown to enhance the thermal stability to near zero, and the assembly appears stable up to 150°C (i.e., above the melting temperature). Finite element simulation has shown that thermal stability can be further improved by optimizing the device design, without changing the materials used.

In case of the dynamic mechanical stability, which was assessed by applying external vibration, no failure was observed for external accelerations greater than 50 g. The lifetime of devices has been measured over more than $1.4 \times 10^7$ cycles of the internal resonance, enough for practical applications. Surface tension self-assembled compo-
nents based on photoresist hinges therefore show very stable thermal and mechanical responses.

7.2 Future work

Although a variety of devices with controllable responses have been fabricated and have shown excellent long-term stability, there are still many challenges to be considered. First, a complete communication system based on surface tension self-assembled CCRs could be fabricated. This system would require other components, such as a laser-transmitter, a photodetector, a battery and a signal amplifying circuit. The voltage required to actuate the CCR should also be reduced, for example by optimizing the combdrive design. Secondly, a model of CCR dynamic response should be further improved to predict more accurately the device performance.

In addition, other optical devices could be fabricated by surface tension self-assembly. One possible application is an efficient optical scanning micro-mirror. Scanning mirrors have a wide range of applications, such as projection imaging systems [50], bar-code reading [55], optical data storage, printers, and displays [53]. A compact optical system based on micromachined scanners and silicon optical bench technologies can be built as shown in Figure 7.1. Here an integrated scanning-display module forming a planar-image display is realised by raster scanning the light beam in two orthogonal directions using two mirrors suspended on a torsion bars [55].

To actuate the scanner, electrostatic combdrives have been used most frequently, because they can produce large deflections at low voltages. As we have seen, the fabrication process is also relatively simple. Especially, in-plane combdrives are easy to fabricate, and when combined with an elastic linkage, can operate at a low voltage with sufficiently large deflection [55]. However, if an elastic linkage is used between
an in-plane combdrive and the scanning mirror itself, an additional mechanical hinge is required. Pin-and-staple microhinges, widely used in most surface micromachined MOEMS devices, causes wobbling and damping of the mirror actuation because of the sloppiness of the structure, resulting in unrepeatability of the scanning [55].

In the following sections, an early prototype of a new type of scanner with an in-plane combdrive, which is linked to the bottom of a mirror through an elastic linkage, is demonstrated. The scanner uses photoresist pads, which rigidly connect the elastic linkage to the mirror, preventing wobbling and damping. The process and design can provide a compact structure with a reduced operating voltage.

### 7.3 Scanner design

Figure 7.2 compares the new scanner actuated by an in-plane combdrive with the older version actuated by an angular vertical combdrive. In each case, the scanners have three similar parts: a 45° reflecting surface, a supporting frame, and a combdrive. The mirror is a 0.5 mm × 0.6 mm rectangle, connected to the frame by two 5 μm × 350 μm torsion bars. As usual, the mirror and frame counter-rotate and then latch together during assembly. The combdrive actuates the mirror by an electrostatic force. Each
scanner, however, has different operating characteristics depending on the combdrive design.

The old design uses a combdrive consisting of a set of skewed electrodes attached to the mirror, which are aligned above a fixed set of electrodes attached to the substrate. Because the torsion mirror is confined in a frame, the number of moving electrodes is limited [Figure 7.2(a)], so that the torque cannot be increased, for example, to reduce the drive voltage.

The new design uses an elastic linkage, which consists of two torsional bars, and a suspension structure to connect the moving electrodes to the mirror. The new design has two advantages. First, the elastic linkage flexibly connects the mirror to the horizontal combdrive placed outside the mirror frame, which enables an increase of the number of electrodes, resulting in an increase of the electrostatic force [Figure 7.2(b)]. Moreover, because surface tension self-assembled linkage does not use pin-and-staple joints, the back-lash problem shown in other devices [55] can be solved. Each elastic linkage bar has a dimension of 70 µm length with 16 µm width. Secondly, the suspension structure, which is based on a folded flexure, ensures that the straight motion is transferred from the combdrive to the torsion mirror [108]. One of the main problems of the in-plane combdrive is lateral instability [107], which comes from the undesirable displacement of the movable electrodes in the orthogonal direction [x-direction in the Figure 7.2(b)]. Therefore, a structure that is very compliant in the desired direction of motion while being very stiff in the orthogonal direction is required. A folded flexure (Figure 7.3) satisfies this requirement [108]. Since this suspension design reduces the development of axial forces, the folded flexure spring shows a much larger linear deflection range, and is adopted in the new scanner design as shown in Figure 7.2(b). The suspension structure consists of four folded beams, where each beam has a length of 864 µm and a width of 5 µm.
Figure 7.2: Comparison of two scanners with (a) angular vertical and (b) linked, in-plane drives.
CHAPTER 7. CONCLUSION AND FUTURE WORK

Figure 7.3: A folded-flexure spring design.

Figure 7.4: Surface tension self-assembled scanners

(a) with an asymmetric drive [25]  
(b) with an in-plane drive
Figures 7.4(a) and (b) show assembled scanners actuated by an asymmetric comb-drive and by an in-plane combdrive, respectively. The combdrive of the new scanner consists of 55 interdigitated fingers on the moving comb and 54 fingers on the fixed comb, whereas the old design consists of 19 moving comb fingers and 18 fixed comb fingers. A much larger electrostatic force can therefore be generated. The dimension of each electrode fingers is $90 \mu m \times 3 \mu m$, and $5 \mu m$ depth. The gap between adjacent electrode fingers is $5 \mu m$.

### 7.4 Scanner fabrication

Fabrication is based on the process previously described in chapter 3. After out-of-plane rotation, the device is sputter coated with Cr/Au to allow electrical contact and to improve reflectivity.

There were difficulties in the fabrication of the new scanner. Figure 7.5 demonstrates some failed devices at different stages of processing. Figures 7.5(a) and (b) show stiction and out-of-plane rotation of the moving electrodes of the in-plane combdrive, and Figure 7.5(c) shows vertically mis-aligned electrodes, respectively.

First of all, it was important to maintain the relative orientation of the moving and fixed fingers of the combdrive. Because the moving electrodes were suspended above the substrate and connected to the mirror only by two photoresist hinges, they easily rotated out-of-plane. Alternatively, if the moving fingers moved downwards during the sacrificial layer etching, they adhered to the substrate [Figure 7.5(a)]. Finally, if the small suspension anchors attached to the substrate were lifted off by over-etching, the moving fingers were rotated entirely out-of-plane [Figure 7.5(b)].

The vertical misalignment of electrodes [Figure 7.5(c)] is also a critical source of failure during operation as described in the following section. The re-melting temperature
Figure 7.5: Various failure figures of scanner with a linked combdrive
(a) stiction of a moving electrodes (b) out-of-rotation of a moving electrodes and (c) mis-aligned moving electrodes
and time were the main parameters used to control the alignment of the moving fingers. After assembly was completed by the mechanical limiter during the re-melting process, the remaining surface tension torque of the photoresist hinge still caused the shuttle electrodes to move. To reduce the excess torque induced by the surface tension of the melting photoresist, the re-melting temperature and time were optimized. Lowering the re-melting temperature from 155°C to 145°C and the time from 15 min to 8 min improved the alignment between the electrodes without structural change.

Secondly, any silicon fragments around the spring linkage that remained after sacrificial layer etching prevented the movement of the electrodes. Because the etch could not be stirred very aggressively during the etching, the fragments sometimes remained on the substrate. The fragments not only hinder the mechanical movement of the moving electrodes, but can also cause an electrical short. More consideration of the design is required to solve the problem.

7.5 Characteristics of self-assembled scanners

Despite these difficulties, preliminary optical scanning characteristics were measured for self-assembled scanners with linked drives. Figure 7.6 shows a completed device. The mirror frame stands above the substrate at 45°, after engagement of the limiter [Figure 7.6(b)]. The mirror is actuated by the combdrive through an elastic linkage and photoresist pads. Since the pads directly connect the spring and the mirror, there is no wobbling or damping effect, which can ensure accurate scanning repeatability [Figure 7.6(a)]. The folded-flexure structure, apart from two anchors, is correctly suspended about 2 μm above the substrate as can be seen from Figure 7.6(c). The scanner can therefore rotate about an axis parallel to the substrate.

Optical characteristics were measured by applying a sinusoidal drive voltage between
Figure 7.6: Self-assembled scanner with a spring linkage
(a) overall scanner structure, (b) engagement of the limiter and (c) suspension of the two anchors
the fixed electrodes and the surrounding land. Movement of the scanner was observed using a drive voltage of $20 \ V_{p-p}$ at a resonant frequency of 2100 Hz. However, one further critical failure mode was observed during the operation, namely stiction of the moving electrodes to the substrate. If the moving electrodes were misaligned vertically with the fixed electrodes by even a small amount, the drive voltage generated a downward component of the electrostatic force, pulling the moving electrodes down to the substrate. Although careful control of the re-melting temperature and time can reduce the amount of misalignment, since the gap between the substrate and electrodes is only $2 \ \mu m$, it is difficult to avoid this type of stiction. It was therefore impossible in the time available to gather systematic data between the frequency and angle variation. To solve the problem, either a thicker sacrificial layer or removal of the substrate is required. Alternatively, Tang et al. suggested making a strip of conductor at the substrate biased at the same potential [109]. Such considerations are required for a next device design. However, the results do show the possibility to reduce the operating voltage compared to the earlier design of scanner [34].

7.6 Conclusion

Torsion mirror scanners with linked drives have been fabricated using the surface tension self-assembly method in conjunction with a mechanical limiter. The assembled device showed a precise mechanical alignment. Direct connection between the elastic linkage and the mirror prevented unnecessary wobbling and damping, ensuring accurate scanning repeatability. Although statistical data has not yet been obtained, the scanning characteristic measurement showed a reduction of the operating voltage to $20 \ V_{p-p}$ compared with the older design, which required $68 \ V_{p-p}$. Scanners with linked drives can therefore be expected to yield a large scanning angle. Further work is required to
improve the device design to prevent the stiction of moving electrodes.

Surface tension self-assembly has therefore been verified as a good method for the fabrication of three dimensional MOEMS devices. Various applications to other fields such as RF MEMS and Bio-MEMS also remain as future work. A 3-D structure in RF devices can enhance their performance greatly by achieving high Q-factors. For example, Gerald et al. have demonstrated 3-D surface tension self assembled RF-components such as inductors with high Q-factors [141]. Other RF-components including resistors and capacitors can be fabricated and integrated on the systems using surface tension self assembly. On the other hand, in the Bio-MEMS field, few devices have been demonstrated using an out-of-plane rotation mechanism. A 3-D structure may, however, provide various advantages in the future. For example, a 3-D vertical cantilever structure can be used as a force sensor for investigation of cell-substrate interaction. Although Petronis et al. fabricated a vertical cantilever sensor with high aspect ratio using DRIE, the cantilever showed fragile tapered profiles [147]. A 3-D surface tension assembly can then provide more robust method to realise a high aspect ratio vertical cantilever.
Bibliography


