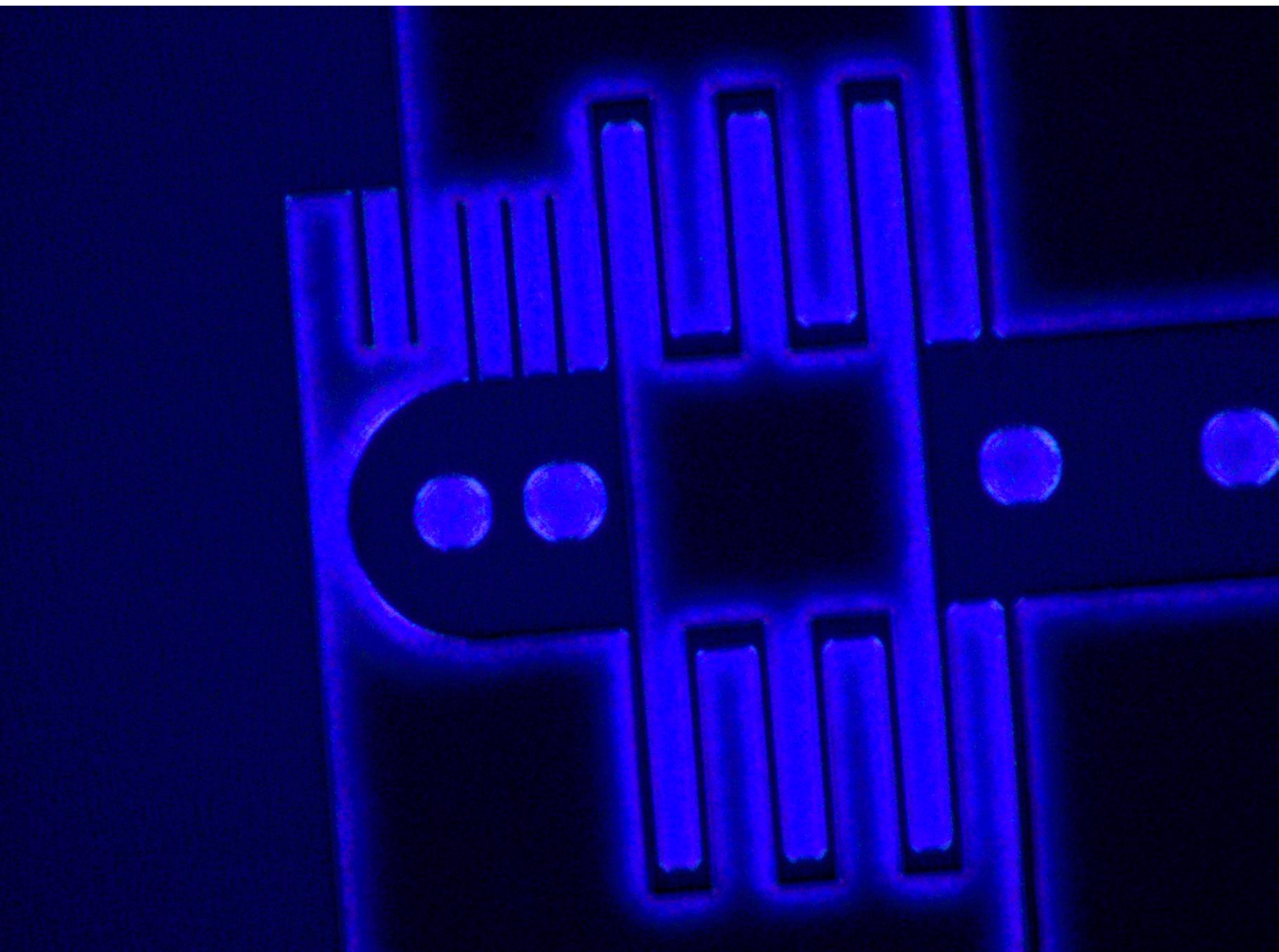




An Evaluation of Silicon Carbide Based Bimorph Actuators for Optical Coherence Tomography Applications

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After completing this thesis, I can say that it was one of the hardest projects I have undertaken so far in my life. At certain points progress was excruciatingly slow. After getting a fresh batch of incomprehensible results, a critical machine in the cleanroom would stop working and your laptop would decide to take a vacation, all at the same time. When this happens you get plenty of time to wonder why problems like to bunch up together and hit you all at once and whether the saying that, if you really want something, the universe conspires for you to achieve it, should really be the opposite. At other moments though, after the quest for answers leads to some success, the feeling that your thesis is finally gaining some momentum is exhilarating. Many people helped along this journey and it should be said that, although one person's name goes in the authors, a thesis is truly a team effort. This page is dedicated to all of you.

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Abstract

Optical coherence tomography (OCT) probes rely on MEMS micro-scanners to provide fast and reproducible scans covering as much area as possible. A popular choice of actuators employed in this role are electrothermal bimorph actuators. They comprise of two layers of different materials which are rigidly joined together. Differential expansion under heating causes the bimorph to deflect. Polycrystalline silicon carbide has been proposed to replace polysilicon as a multi-purpose material in MEMS but it has not been yet explored as a bimorph material. Under this premise, an in-plane bimorph actuator based on doped polycrystalline silicon carbide and silicon was proposed to fulfill the requirements of an OCT probe. This actuator suffered from yield and reliability issues, which were, however, linked primarily to the design and not the material itself. Therefore, in order to evaluate silicon carbide as a bimorph “hot” layer, an optimization and characterization procedure was carried out to determine its relevant mechanical, thermal and electrical properties. Specifically, the residual stress, Young modulus, coefficient of thermal expansion, thermal coefficient of resistance and electrical resistivity were determined. The layer residual stress and resistivity could be reduced by adjusting the SiH_2Cl_2 , C_2H_2 and NH_3 gas ratio, achieving $0.03 \text{ } \Omega \text{ cm}$ and 300 MPa respectively. The resistivity of the layer is adequate for it to act as a heater. The electrostatic pull-in instability technique was used to extract the Young modulus for different layers and it was found to decrease with increasing Si/C composition, with a value of 250 GPa for our selected optimum layer. The coefficient of thermal expansion was determined through fabricated v-beam actuators to be 4.3 ppm/K which indicates that SiC must be heated more to produce strain comparable to other bimorph “hot” materials. As a final step in poly-SiC evaluation, we constructed rudimentary bimorph poly-SiC/ SiO_2 cantilevers which were subsequently used to test the static and dynamic performance of this material combination. Our conclusion is that poly-SiC is a good bimorph “hot” material that operates optimally at higher temperatures, can be used in more diverse bimorph designs and is more suited to applications that require high force. Throughout the material characterization process, we used numerical and analytical models extensively. This helped us identify good modelling practices and led to the refinement of the models, allowing us to obtain better matching between theoretical predictions and experimental data. Based on these findings of the poly-SiC properties and better models, we propose a novel in-plane, spring form actuator for use in an OCT micro-scanner. It employs a poly-SiC/ SiO_2 material combination in a mechanical amplifying geometry and in simulation, it achieves 80.9 mN of force and $250 \text{ } \mu\text{m}$ of displacement at 43 mW power consumption and a 0.3 mm^2 footprint. All these parameters are an improvement over the initial design.

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1. Introduction

Micro-scanners are a family of mechanical opto-electromechanical systems (MOEMS) that find extensive application in many optical systems, such as spectroscopy, different kinds of microscopy (confocal, fluorescence, coherence), projection displays etc. Their primary purpose is to scan a light beam over a sample surface. When these devices are miniaturized, they rely exclusively on MEMS actuators for motion, which are the micro world equivalent of motors.

One application that relies on micro-scanners is Optical Coherence Tomography (OCT). Below we will discuss the operating principles of OCT to get a better understanding of how the micro-scanner and by extension the MEMS actuators that comprise it are affecting the overall system performance.

1.1. Application and Motivation

Optical Coherence Tomography (OCT) is a medical imaging technology that uses infra-red light to scan translucent/opaque surfaces such as skin, producing full volume maps up to 2 mm in depth and a resolution equivalent to a low-power microscope. It is used for early cancer detection and unlike a biopsy, it is non-invasive and can be performed in-vivo. OCT is also a much faster diagnostic method and provides a way to cheaply monitor the progression of lesions and carcinomas. However, high cost is an obstacle in its widespread adoption from private medical practitioners.

OCT is based on the combination of a broadband light source with a Michelson interferometer. Infrared light with a wavelength of around 1300 nm is selected in order to avoid absorption from the sample tissue. The OCT main principle of operation is the selection of an optical signal through interference. At first, a beam of light is emitted by a low coherence, broad bandwidth source and is subsequently split into two parts by a beam-splitter. One part is directed towards a reference mirror and the other towards the sample under examination. The sample beam is scanned laterally on the sample and is either reflected, refracted or absorbed. As reflection occurs at the interface of two materials with very different refractive indexes, the reflected or back-scattered light carries information about the tissue structure underneath the surface. The back-scattered light is gathered by the optics of the system and directed on the surface of a photodiode, where it interferes with the reference beam. Interference between the two beams will occur only if their path-length difference is less than the coherence length of the light source, L_c , given by the equation: $L_c = \frac{2\ln(2)}{\pi n} \frac{\lambda^2}{\Delta\lambda}$, where λ is the central wavelength and $\Delta\lambda$ the spectral width of the Gaussian spectrum light source and n the refractive index of the medium. In this way, the photo detector detects only backscattered light of interest, in a process called coherence gating.

The depth (transverse) maximum resolution is determined by L_c . The lateral resolution is determined by the optical power of the optics. In general, the axial and lateral resolutions of all OCT systems are decoupled from one another. The maximum scan depth is limited by the beam attenuation due to light scattering and absorption in the tissue, the optics transmission losses and the photodiode dynamic range. OCT is categorized into time domain OCT (TD-OCT) and frequency domain OCT (FD-OCT).

In TD-OCT, the reference mirror translates into the axial direction and acts as a variable optical delay line. In this way back-scattered optical signals from different depths can be selected. A full depth scan (A-scan) is obtained by translating the reference mirror over its full displacement range. By repeating A-scans over the entire region of interest, a 3D volume image of the sample can be constructed where each voxel carries information on the scattering/absorption characteristics of the tissue underneath.

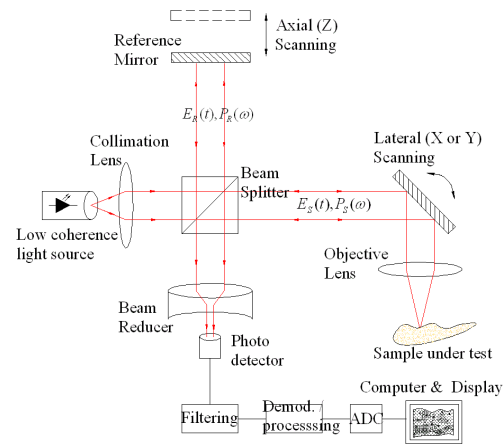


Figure 1: Example of a time domain OCT system.

The axial scanning in the reference arm is one of the biggest speed bottlenecks in TD-OCT. Frequency domain OCT (FD-OCT) tackles this problem by transforming the axial scan from the time domain to the frequency domain. Through the Wiener-Khinchine theorem, which states that the spectral power amplitude of the back-scattered beam equals the Fourier transform of the axial distribution of the sample scattering potential, FD-OCT can compute the full axial scan in parallel by multiplexing multiple broadband light sources and analyzing their back-scatters separately, by demultiplexing them in a spectrometer-like fashion. This way it avoids the movable reference mirror by employing a sweeping wavelength source or a grating and linear detector array, acting as a serial or a parallel spectrometer respectively. This distinction further categorizes FD-OCT into time encoded FD-OCT (TEFD-OCT) and spatially encoded FD-OCT (SEFD-OCT) which both use the same Fourier transform principle but differ in the light source and detector arrangements. FD-OCT systems have been routinely reporting A-scan acquisition rates in excess of 100 kHz and higher theoretical SNR than TD-OCT ([1]). However they are also associated with non-uniform axial resolution and worse lateral resolution [2]. Nevertheless, A-scan is no longer the speed limiting factor in those systems. Instead, the scanning mechanism starts playing a significant role.

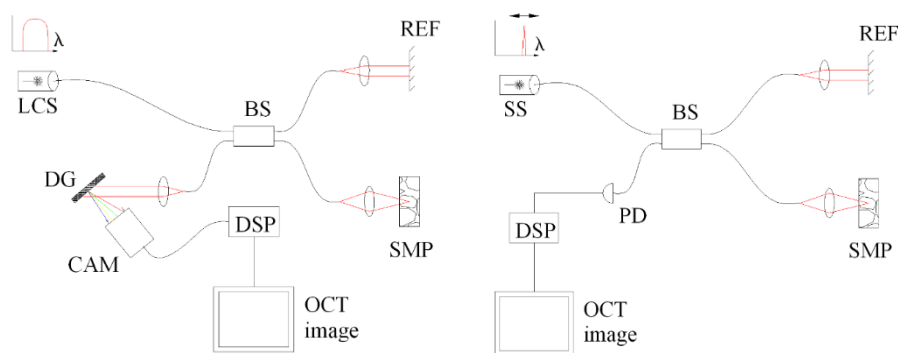


Figure 2: Simplified diagrams of spectral discrimination by SEFD-OCT (left) and TEFD-OCT (right). Components include: low coherence source (LCS), swept source (SS), beam-splitter (BS), reference mirror (REF), sample (SMP), diffraction grating (DG), linear detector array (CAM) acting as a spectrometer, photodiode (PD) and digital signal processor (DSP).

Scanning mechanism

In both TD-OCT and FD-OCT, the system depends on a scanning mechanism which is responsible for steering the sample beam to obtain the lateral scans in a fast, reproducible and reliable way. OCT probes can be categorized as forward imaging, side imaging and circumferential imaging, depending on the direction of beam scanning in relation to the probe body. Forward imaging probes are typically used as seek tools by doctors to detect problematic areas. Depending on the type of scanning, different kinds of micro-scanners are called for.

Tilt type micro-scanners typically employ out-of-plane actuators to periodically tilt a suspended aluminum micro-mirror around its axis of rotation, effectively scanning the light beam over a straight line. A more advanced option is to use scanning lenses instead of a mirror, as reported in [3]. This has the advantages of a) a flat imaging plane, which leads to less distortions and constant lateral resolution (Figure 3), and b) avoiding the dynamic mirror deformation which can lead to lateral resolution degradation. When using scanning lenses however, we have to use lateral translation instead of tilting. For this, piston type micro-scanners are employed, in which a platform carrying the lens load is pushed-pulled by in-plane actuators lying on the sides.

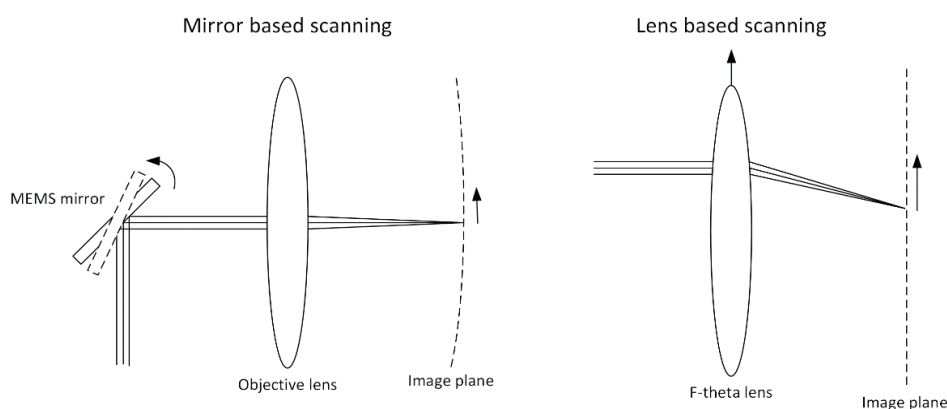


Figure 3: Mirror vs lens based scanning.

Scanners are dynamic in nature and hence can benefit greatly from any dynamic effects. Such an effect is mechanical resonance, which maximizes the conversion of mechanical energy to work and thus improves efficiency. Every structure exhibits multiple resonance frequencies, each associated with a different vibration mode. The simplest resonance mode, which is the easiest to stimulate, occurs at the structure first resonance frequency, more commonly known as the natural frequency f_0 . Actuation at that frequency will lead to a maximization of the achieved displacement and thus is highly advised. Actuating a scanning structure at higher resonance modes can also be advantageous as it can lead to a more efficient 2D scanning scheme. For example resonance 2D scanning based on Lissajous patterns (Figure 4), as demonstrated in [3], can help sample a surface more efficiently.

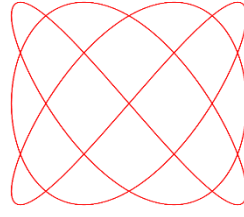


Figure 4: Lissajous curve. These patterns are generated by parametric equations of the form: $x = A\sin(at + \delta)$, $y = B\sin(bt)$.

In any case, the MEMS scanner and by extension the actuators employed, determines the scanning speed (frames per second and thus device responsivity) and field of view (frame size) of the OCT system. Furthermore, the actuators force output determines the maximum load mass that can be effectively actuated. In this context, the load is a lens or a mirror and thus the mass can give an indirect indication of optics quality and its immediate result, lateral resolution. Therefore, the actuators are one of the most important subcomponents of the OCT system as a whole, as two of the system main specifications depend on them. In Table 1 the mapping between actuator parameters and overall system specifications is presented. In the next subsection we introduce the specifics of the OCT probe that was targeted for the bimorph actuators integration.

Table 1: Actuator parameter to OCT system specifications mapping.

Actuator Parameter	OCT specification
Displacement	Field of View
Maximum frequency	Frames per second
Footprint	Overall size
Power consumption	Battery life
Force output	Optics quality (lateral resolution)

BiopsyPen© probe

BiopsyPen© is a medical OCT diagnostic probe for dermal diagnosis and monitoring of non-melanoma skin cancers. It is based on a swept source TEFD-OCT system and it aims at reducing the overall system cost by integrating as many subsystems as possible (light source, photonics chip, MEMS scanner). The requirements for the MEMS scanning actuators as part of the BiopsyPen© probe are summarized in Table 2. Emphasis is placed on displacement and force.

Table 2

BiopsyPen© MEMS Scanner Specifications

Displacement in x direction (longitudinal)	±250 μm
Displacement in y direction (lateral)	±250 μm
Displacement in z direction (transverse)	±250 μm
Power Supply [V]	12 V
Footprint, y direction (lateral)	2 cm
Footprint, z direction (transverse)	2 cm
Force	1 mN
Resonant Frequency	100 Hz

1.2. Overview of MEMS Actuators

As a first step we briefly review the available MEMS actuator technologies in order to satisfy the requirements on Table 2. MEMS actuators are categorized according to the physical mechanism they use to produce mechanical work and their specifications vary accordingly.

Electrostatic actuators are based on the principle of electrostatic attraction between two plates at different electric potentials. The electrostatic force is non-linear and is given by $F_{el} = \frac{1}{2} \frac{\epsilon \epsilon_0 A V^2}{d^2}$. As the gap between the plates, d , increases, F_{el} becomes quadratically smaller. Correspondingly, as the gap decreases, the plates can snap together due to the effect of pull-in instability, which is a common failure mechanism for these devices ([4]). For this reason, the only way to use electrostatic actuation when large displacement is required is by using impractically high actuation voltages, always limited by the electrical breakdown, or in conjunction with mechanical amplification techniques which extend the actuators travel range (e.g comb drive, inchworm etc), usually in expense of force output, as reviewed in [4]. Frequency wise, the electrostatic actuators show favorable characteristics with up to 10^6 Hz operation ([5]). Moreover, as they are purely capacitive in nature, they have low power consumption requirements. Finally, their fabrication is in most cases straight forward and they can be easily integrated with CMOS electronics.

Magnetic actuators use micro-magnets and coils or current carrying lines to induce motion through the Lorentz force, given in its simplest form for straight wires by: $F_{mag} = I l \times B$, where I is the current, l is the wire length and B the applied magnetic field vector. Alternatively, other mechanisms may also be used, such as magnetostriction which was showcased to be able to produce useful deflections without strong external magnetic fields if the magnetostrictive materials are of sufficient thickness ([6]). In general, due to the disadvantageous scaling of magnetic forces into the micro-domain (s^3 or s^4 , with s being the critical dimension), they are more suitable when deflections larger than $10 \mu\text{m}$ are required ([7]). Similarly, in [8], the magnetic force is reported to be proportional to the square root of the magnet mass and power consumption and thus is not well suited for very small dimensions. Same with the electrostatic actuators, magnetic also suffer from pull-in instability issues. However, the biggest burden with magnetic actuators is their fabrication. Most magnetic materials are not CMOS compatible, they require lower processing temperatures to avoid demagnetization, and the direction of magnetization is also a property that must be controlled. Magnetic actuators usually require high power consumption as force is directly related to current. Concerning operating frequency, magnetic actuators can achieve moderate operating frequencies of up to 10^4 Hz as reported in [10].

Piezoelectric actuators incorporate piezoelectric materials, such as lead zirconate titanate (PZT), to produce strain and force upon application of an electric field across them through metal electrodes. They are capable of high force which depends on their stiffness but need mechanical amplification techniques to acquire higher displacements ([9]). They have very good switching speeds (up to 10^6 Hz as reported in [10]) and their power consumption is limited, as they are capacitive in nature, with low voltage operation possible for thin films, due to high dielectric constants ([11]). Fabrication of piezoelectric films is a difficult process, as, apart from non-standard materials, the material needs optimization in order to ensure stability of its characteristics (for a detailed discussion on piezo materials the reader is invited to read [11]).

Electrothermal actuators are amongst the simplest actuating elements in the MEMS world. They rely on the principle of thermal expansion to attain force and displacement. Different architectures can take advantage of this mechanism: Heating up two stacked layers of different materials in the form of bimorphs, applying heat to expand displacement amplifying beam configurations (v-beam) or adjusting conductors width to engineer thermal gradients which in turn lead to differential expansion (u-beam) are all demonstrated in the literature ([18],[13],[14]). Alternatively, shape-memory-alloys (SMA) like NiTiNOI also use heat to recover their original shape after deformation, producing very high forces in doing so. In all cases, electrothermal actuators can produce high forces and displacements. As with piezoelectric actuators, their force output is directly related to their effective stiffness k_{eff} : $F_{th} = k_{eff}d$, with d being the displacement from its rest position. Therefore, stiff materials like silicon, which are common in MEMS technology, exhibit high force output. Their weak point is frequency response, because of the usually low thermal cutoff frequency. Even so, frequencies up to 10^4 Hz are reachable. Concerning power, as they rely on heat for their operation through the mechanism of Joule heating, this characteristic is usually not ideal. Finally, fabrication is often possible with few masks and CMOS integration can be easily achieved.

Table 3: Summarized comparison of different MEMS actuation technologies. Data extracted from [9] and [10].

Actuation principle	Typical Displacement (m)	Typical Force (N)	Max Frequency (Hz)	Power	Fabrication
Electrostatic	10^{-6}	10^{-4}	10^6	Low	Simple
Magnetic	10^{-3}	10^{-4}	10^4	Very high	Low temperature processing and incompatible materials
Piezoelectric	10^{-6}	10^{-3}	10^6	Low	PZT optimization, incompatible materials
Electrothermal	10^{-4}	10^{-3}	10^4	High	Simple

The actuator categories described above are compared in Table 3. Based on our applications requirements we have opted for an actuator based on the electrothermal principle, as they usually combine good displacement and force performance. From that family of actuators, bimorphs are particularly interesting, as they can be scaled to very small dimensions, are applicable to many different actuator architectures and CMOS integration is usually easy. We further review the available literature on electrothermal bimorph actuators and the proposed architectures.

Bimorph Thermal Actuators

In [15] Gilgunn et al. designed and analyzed analytically, numerically and experimentally a lateral in-plane electrothermal actuator incorporating 3 metal layers inside SiO_2 , fabricated using a standard CMOS process. Geometric ratios and material parameters were optimized to attain maximum displacement for a given geometry and ΔT , yielding $2.1 \mu\text{m}$ at $\Delta T = 112^\circ\text{C}$ and $W \times L \times t = 1.3 \times 100 \times 4.2 \mu\text{m}$. A new figure-of-merit, the efficiency-volume ratio, was used for evaluation and comparison with other actuators. Increasing thermal isolation from the anchor after a certain point provided diminishing returns regarding tip displacement while notably reducing the thermal cutoff frequency, as the dominant thermal path shifted from conduction to convection.

In [16] Pal et al. proposed an electrothermal actuator structure composed of three bimorphs (Al/SiO₂) and two rigid beams that transforms out-of-plane motion to in-plane, demonstrating a maximum in-plane displacement of 135 μm at 3.8 V_{DC} for the optimized design. Out-of-plane displacement was also reported which was attributed to temperature variation along the bimorph lengths. Several strategies to minimize this out-of-plane displacement were proposed but not implemented.

Chu Duc et al. ([17]) investigated the influence of the constraint effect in polymer-silicon in-plane bimorph actuators. An interdigitated silicon structure with a metal heater on top was employed to enhance heat dissipation into the high CTE but thermally insulating polymer. The constraint effect is reported to enhance the apparent CTE of the interdigitated silicon-polymer block by three times compared to the no-constraint case, when the trenches aspect ratio is high enough ($w \gg h$). The maximum voltage however is restricted to 2.5 V due to the low destruction temperature of the SU8 polymer. The device maximum displacement was measured at 14 μm with 2.5 V_{DC} applied voltage and 5 μm thick polymer layer.

In [18] Samuelson et al. reported on the construction of an imaging probe for endoscopic optical coherence tomography based on a MEMS mirror with hidden folded dual s-shaped bimorph electrothermal actuators (FDSB) composed of Al, SiO₂ as active materials and an embedded Pt heater. This type of actuator sacrifices maximum out-of-plane displacement to achieve 0° tip tilt motion. An innovation of this design is the placement of the actuators underneath the mirror which faces towards the backside of the wafer in order to effectively increase the fill factor to 32% and simplify device mounting onto a flexible PCB. Furthermore, the device is enclosed in a substrate cavity in order to protect the mirror during the assembly process, something, however, that mandates the maximization of the initial vertical displacement of the mirror in order to eliminate any blocking of the optical path. The geometric parameters of the FDSBs were optimized to obtain high initial vertical displacement along with acceptable structure rigidity and range of motion. The MEMS mirror was fabricated on a SOI wafer and for the critical release step, the devices were mounted individually on a carrier wafer. A combination of anisotropic and isotropic front side dry etching yielded the final released devices. Measurements indicate a maximum mirror vertical displacement of 319 μm and a $\pm 23^\circ$ scan angle at 4.8 V_{DC} and 288 mW power consumption per axis. The authors assembled and tested a 2.8 mm diameter prototype probe which achieved a system sensitivity of 74 dB at 2.5 frames/s, while successfully mapping and detecting cancerous hypodermic regions in vivo on a mouse test subject.

Samuelson et al. recently reported in [19] on the design and implementation of a MEMS mirror with low tilt piston displacement using two folded dual s-shaped bimorph elements (FDSB) connected in a ladder configuration. This architecture is the direct scaling of the FDSB actuator to achieve higher displacement. Geometric parameters were analytically optimized for high displacement at 0° tip tilt and an acceptable structure rigidity. The active materials were, as before, Al and SiO₂ with an embedded Pt heater. A maximum vertical displacement of 90 μm with 1.2 V_{DC} activation voltage and a fill factor of 28% was achieved. Device release was identified as the most critical step in the fabrication process, with the most consistent results reported when it was performed with a single device per batch. The authors advised a compromise to minimize undesired undercut and speed up the fabrication, where release is carried out in groups of 10 devices. The device exhibits small non-linearity at lower operation voltages and a maximum tilt angle of 0.25° which the authors note that is still too high for use in applications such as Fourier Transform Spectrometers (FTS) and will require drive signal tuning to compensate. Finally, the system requires calibration between uses as to maintain mirror alignment.

In [20], Kwan et al. described two improvements to the conventional v-beam in-plane actuator. The first improvement is the optimization of the v-beam lengths or tilt angles, while retaining uniform cross-sections, to accommodate the thermal expansion of the central beam, or “shuttle” as it is referred in the paper, which act against the desired in-plane motion. The second improvement is the optimization of the v-beam cross-sections to widen their high-temperature expansion zones through manipulation of the current density along their length. In order to compare the improved v-beam actuator with the conventional design, the product of the maximum displacement and the output force was used as a figure-of-merit. The authors modelled their proposed concepts analytically, verified them through simulations and tested them experimentally with fabricated devices. The maximum displacement was measured in unloaded actuators and the force was estimated by measuring the displacement of the actuators under load of a standard spring. The length/tilt angle optimization produces a 10% improvement in the FOM over the conventional design whereas the width optimization accounts for a 65% improvement, which comes primarily from an enhanced actuation force.

1.3. First generation of bimorph actuator

A first generation bimorph actuator was already designed and fabricated to satisfy the requirements of the project. A vertically aligned silicon carbide/silicon bimorph was used to bridge two fixed anchor points. The silicon carbide layer is electrically connected through aluminum metal contacts and acts as a heater through Joule heating. The initial stress of the silicon carbide layer would buckle the bimorph and result in an initial offset. Upon supplying power, the bimorph would bend on the opposite direction, effectively cancelling out the offset to 0. A COMSOL simulation and a final fabricated test actuator are presented Figure 5. These bimorph actuators would subsequently be employed for supporting and actuating a platform load, as shown in Figure 7.

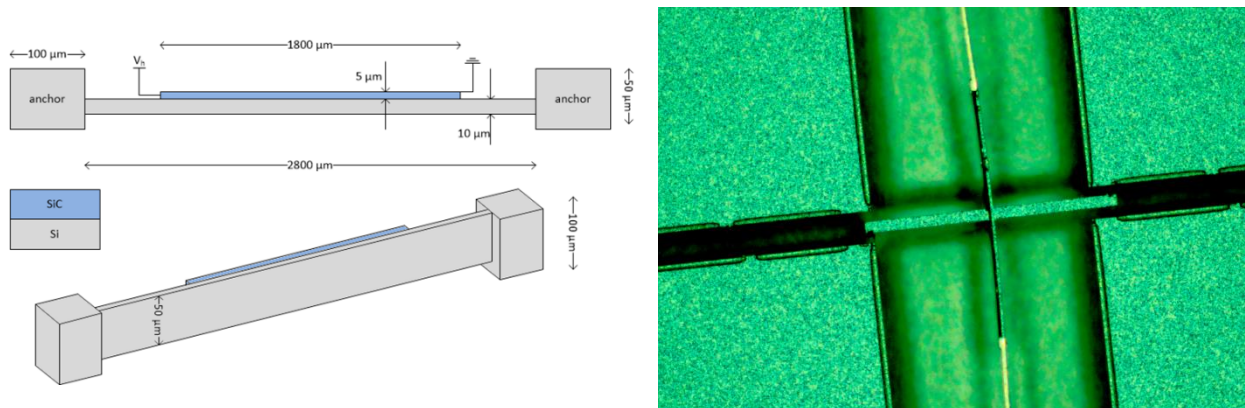


Figure 5: First iteration of a SiC/Si bimorph actuator in a fixed-fixed bridge configuration. The bimorph is visible at the right image, where the silicon carbide layer (black) is placed next to the Si layer and is connected with aluminum metal lines. The perpendicular Si section acts as a load and a measuring structure.

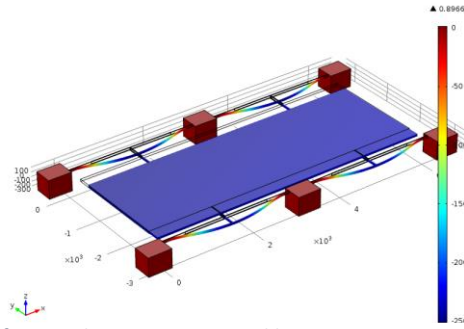


Figure 7: Simulated platform architecture, actuated by 1st generation of bimorph actuator devices.

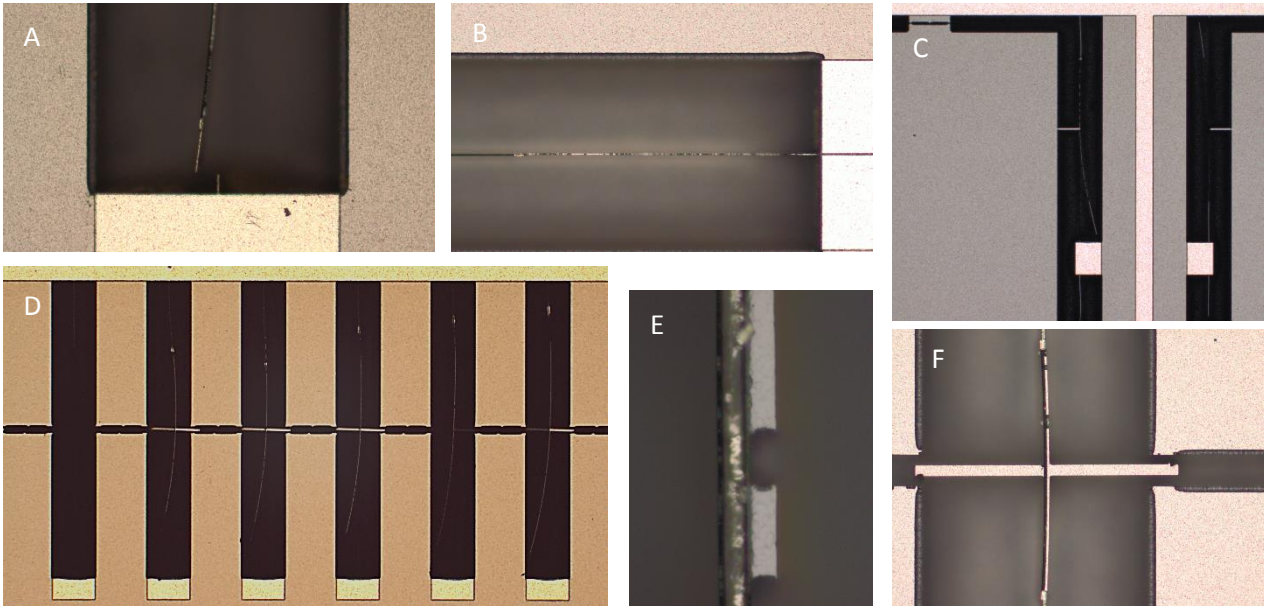


Figure 6: Failure modes of the 1st generation actuator. A, C, D – Crack near anchor due to high stress, B – metallization burn due to high current, E,F – Si delamination.

After fabrication, we noticed that the yield of the devices was too low, with approximately only 24% of the fixed-fixed actuators surviving and most of the bimorph actuators being cracked near the anchor point (Figure 6). This pointed at high residual tensile stress present in the silicon carbide layer.

Operating of the unbroken devices revealed further problems. When the devices were tested for reliability, by being subjected to multi-cycles operation, they showed unexpected early electrical open-circuit failure (Figure 8). From this, the second conclusion was drawn: the thin Al metal lines were insufficient at supplying enough power to the SiC layer without severe degradation. Lastly, as can be clearly seen in Figure 6-E, the Si layer exhibited cracking and discontinuities, which, though not a catastrophic failure itself, would undoubtedly lead to reduced performance and deviation from the simulated behavior. In turn, this could be avoided by choosing a different material as a “cold” layer.

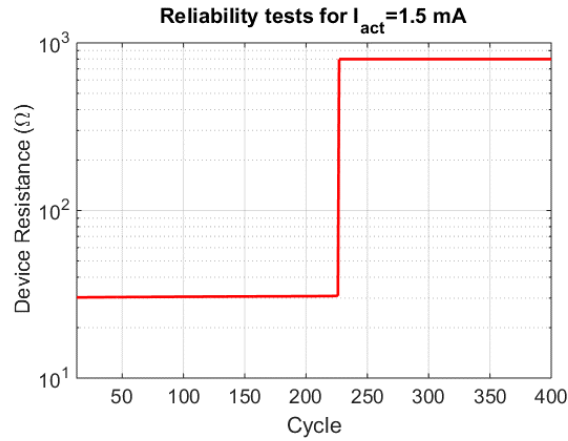


Figure 8: Reliability test of first generation actuator. The device showed very minor gradual resistance increase until failure at cycle no. 227. Though open circuit was the cause of failure, resistance doesn't increase further than 1 kΩ due to the compliance limit of the source.

1.4. Problem statement and research approach

Even though the first generation devices failed to achieve their designed specifications, the option of silicon carbide as a material for eletrothermal bimorphs is still worth exploring. The failure of the first iteration of the bimorph can be traced primarily to design flaws and not to the material itself. However more work is required to ensure that simulations before the first device are closer to physical reality and thus we can rely on them to predict the outcome of concepts before engaging in time-consuming prototyping. Thus the main topics of this thesis are formulated as follows:

- a. To evaluate silicon carbide for use in electrothermal bimorph actuators.
- b. To develop adequate models that explain the failure of the first device and assess the quality of future electro-thermal bimorph actuators, before their fabrication.

To approach these tasks, we deemed it necessary to study silicon carbide in more detail, through a dedicated material characterization process. For that we employed often-used eletromechnical structures, such as cantilevers and v-beam actuators, which, apart from helping us characterize the material, also help us verify the models, by identifying the crucial points that we need to pay attention to, to ensure accurate electrothermal simulations. In this way, we also bridge the gap between simulations and experimental results, effectively addressing the second topic as well.

1.5. Thesis outline

This thesis is organized as follows: In chapter 2, a review of bimorph actuators from the literature is given, the theoretical bimorph principles are presented and a lumped element model is attempted.

In chapter 3 the theoretical background behind the material selection is presented and the choice of silicon carbide is motivated.

In chapter 4, silicon carbide is characterized and evaluated as a potential “hot” layer material in electrothermal bimorphs. Device models are developed in parallel, which, apart from complementing the characterization process, also help identifying the best practices that can be applied in model construction to limit the deviation between simulation and experimental results.

In chapter 5 we propose a bimorph actuator design that attempts to rectify the problems identified in the first generation device and encompasses the strengths of the material to maximize its effectiveness. Also a fabrication flow is proposed.

Finally, this thesis is concluded in chapter 6, where we summarize its contributions and we propose promising future work directions.

2. Bimorph actuation principles

2.1. Bimorph thermal actuation principles

When a material is heated, its equilibrium interatomic distances in most cases become larger. This leads to an expansion strain according to $\varepsilon = \frac{\Delta L}{L} = \alpha(T - T_0)$, where α is the coefficient of thermal expansion of the material and $T - T_0$ the temperature increase. This expansion however, is usually too small to be useful.

Bimorph actuators, as the name suggests, are composed of two layers of different materials joined together. When heating is applied, each material expands at different rates according to $\Delta L_1 = \alpha_1 L(T - T_0)$, $\Delta L_2 = \alpha_2 L(T - T_0)$. Given that the length L and temperature T of the two layers are the same, the different coefficients of thermal expansion α , lead the two materials to expand at different rates. This produces a misfit strain which results in bending at the tip, in the direction of the material with the lowest α . Therefore they amplify the small expansion strains into large displacements in the direction perpendicular to the materials. The bended bimorph assumes approximately the shape of a circle arc of length L and radius r (Figure 9).

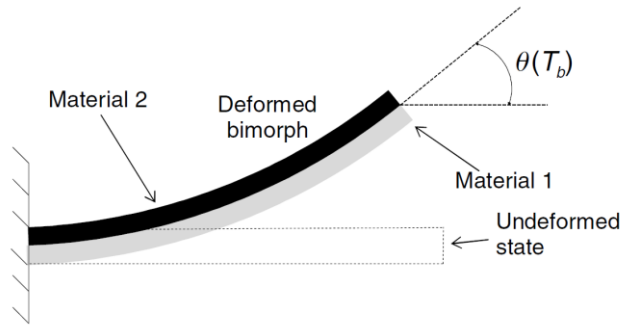


Figure 9: Simple bimorph operation. T_b is the beam temperature.

The vertical displacement at the tip can be calculated analytically from:

$$d = r - r \cos \theta$$

where r is the radius of curvature of the bimorph and ϑ is the deflection angle at the tip, also equal to the arc angle. In turn, r is given by:

$$r = \frac{(w_1 E_1 t_1^2)^2 + (w_2 E_2 t_2^2)^2 + 2w_1 w_2 E_1 E_2 t_1 t_2 (2t_1^2 + 3t_1 t_2 + 2t_2^2)}{6w_1 w_2 E_1 E_2 t_1 t_2 (t_1 + t_2) (\alpha_1 - \alpha_2) \Delta T}$$

with w the layer width, t the layer thickness and E the layer Young modulus. The angle at the tip ϑ is calculated from:

$$\theta = \frac{L}{r}$$

Assuming small bending ($\vartheta \rightarrow 0$, $\cos \vartheta \rightarrow 1$ and $\sin \vartheta \rightarrow 0$), the vertical displacement can be approximated using the first two terms of a Taylor series expansion:

$$d = r - r\cos\theta = r - r\left(1 - \frac{1}{2}\theta^2\right) = \frac{1}{2}r\theta^2 = \frac{L^2}{2r}$$

Therefore the tip displacement is linearly proportional to the difference of the coefficients of thermal expansion of the two materials and the temperature increase and quadratically proportional to the length of the bimorph. Regarding the force output, by applying Hook's law, the thermal stress generated by the bimorph is given by: $F = kd$, with k the bimorph stiffness which is directly related to the materials Young moduli and the bimorph area moment of inertia.

It has been shown in [21] that given a fixed total bimorph thickness, the layer thickness ratio resulting in maximum deflection is given by: $\frac{t_1}{t_2} = \sqrt{\frac{E_2}{E_1}}$. The force generated at maximum deflection is zero whilst the maximum force output is encountered at zero displacement.

Concerning the heat transfer in the bimorph, the heat dissipates towards the environment through three thermal paths: conduction to the substrate through the anchor or surrounding air, convection in the surrounding air and radiation. It has been shown in [24] that in the μm domain and for temperatures below 1000°C , the thermal conduction, is by far the dominant heat transfer mechanism. However, even though air is considered a thermal insulator, small air gaps can also serve as thermal conduction paths and must be taken into account. Thermal conduction is governed by the equation: $\vec{q} = -k\nabla T$, where $\vec{q}$ is the local heat flux density, k the thermal conductivity and ∇T the temperature gradient.

2.2. Lumped element modelling

A rudimentary modelling of the bimorph actuator system is through the lumped element approach. This method, although not as accurate as a full finite element numerical model, allows us to quickly isolate the important parameters and interdependencies. We apply this method to the thermal and subsequently the mechanical domains of the system. This process is described below.

Joule heating is usually the preferred mechanism used to heat the bimorph, either through an external heater element or through the bimorph layer itself. The thermal resistance increases as we move further away from the heat sinking anchor, along the length of the bimorph beam. Furthermore, as electrical resistance is a function of temperature, heat generation is also non-uniform along the bimorph. As a result, the bimorph exhibits non uniform temperature distribution along its length and should ideally be modelled as a distributed thermal RC line, charged by distributed joule heat sources. However, for the sake of simplicity, the system thermal time constant can be roughly approximated by a single heat capacitance $C_{th,b}$ charged through a heat resistance $R_{th,b}$. A more elaborate thermal model including thermal conduction, convection, an actuator load and thermal isolations can be seen in Figure 10. Explanations of the parameters are found in

Table 4. In general, thermal resistances depend on the cross-sectional area and material thermal conductivity and thermal capacitances are a function of mass and material specific heat. Convection depends on the surface area and the coefficient of convection h , which includes information on the medium (air) and the type of convection (forced or natural).

Thermal isolations decouple the thermal response between the different parts. Given sufficient isolation, a fast response can still be achieved even with a big thermal capacitance attached as an actuator load. Also, from the schematic in Figure 10 we can infer that heating up and cooling

down times can be minimized by removing the thermal isolation to the anchor to allow for unobtrusive thermal conduction. In this case and for large $R_{th,isol.load}$, the bimorph thermal time constant is approximately $\tau_{th} = R_{th,b}C_{th,b} \sim l^2\rho C/k$, where k and C are the material thermal conductivity and capacitance respectively, l is the actuator size and ρ is the material density ([12]). The thermal cutoff frequency of the system is thus given by: $f_{T,th} = \frac{1}{2\pi\tau_{th}}$. From these equations it is obvious that fast response is achieved with an overall small actuator size and materials of high thermal conductivity and small specific heat. In Table 5 we summarize the parameters of the thermal model and their physical correspondence.

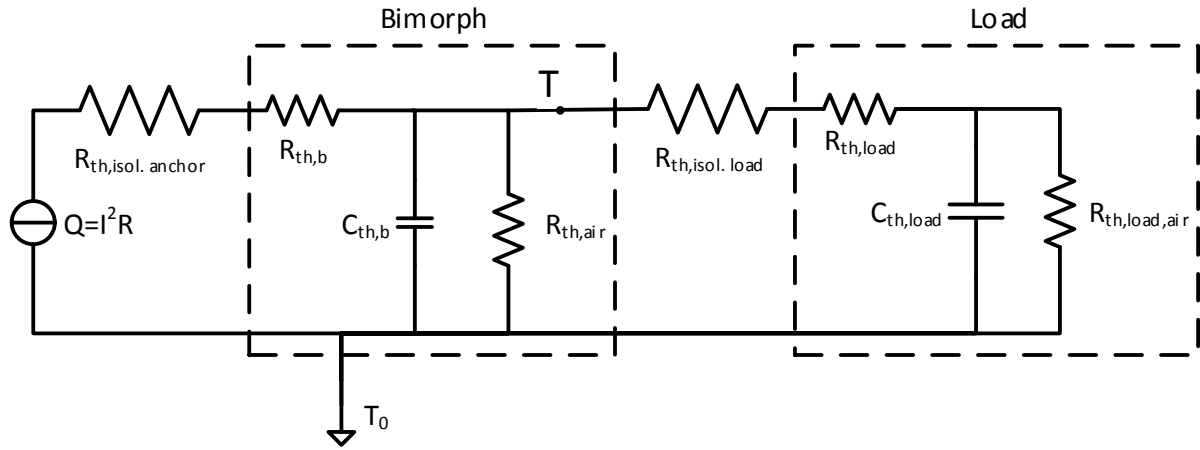


Figure 10: Thermal model.

Table 4: Explanation of symbols: k – thermal conductivity, C_p – specific heat, h_{air} – convection coefficient, m – mass.

Parameter	Description	Physical Dependency
$R_{th,b}$	Bimorph thermal conduction resistance	k_1, k_2 , cross-section (t_1, t_2, w_1, w_2)
$C_{th,b}$	Bimorph thermal capacitance	$C_{p,1}, C_{p,2}, m_b$
$R_{th,air}$	Bimorph thermal convection resistance	h_{air} , total bimorph surface area
$R_{th,load}$	Load thermal conduction resistance	k_{load} , cross-section
$C_{th,load}$	Load thermal capacitance	$C_{p,load}, m_l$
$R_{th,load,air}$	Load thermal convection resistance	h_{air} , total load surface area
$R_{th,isol.anchor}$	Anchor-bimorph junction thermal resistance	$k_{isol.anchor}$, cross-section
$R_{th,isol.load}$	Load-bimorph junction thermal resistance	$k_{isol.load}$, cross-section
T_0	Ambient temperature	

The mechanical model of the system takes as input the temperature of the bimorph, translates it to thermal force and applies this force to a second order underdamped spring mass system to produce displacement. The schematic can be seen in Figure 11. The thermal model output T is considered through the variable ΔT , which is defined as $\Delta T = T - T_0$. F_{TH} is the thermal bending momentum, given by $F = kd$ for a simple mass spring system, and is related to ΔT through the equations mentioned above. k is the actuator stiffness and it is related to the effective Young modulus of all the layers. m refers to the actuator mass and b is a parameter related to the amount of mechanical damping in the system. Frequency-wise, this model only predicts the bimorph resonance at its lowest, natural frequency and not any higher frequency modes. This is sufficient for our purposes, as actuation in higher frequencies is usually not achievable. Finally, in Table 5 there is an overview of the mechanical model parameters and their physical interpretation.

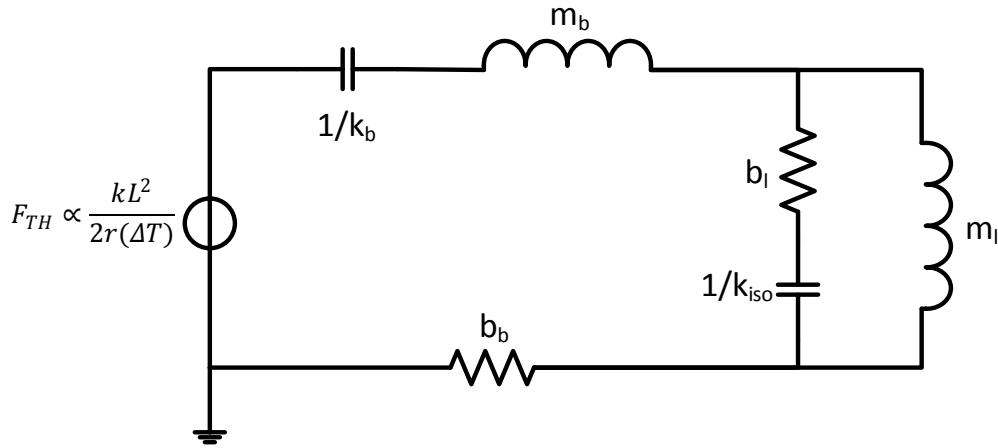


Figure 11: Mechanical model. Subscripts b and l stand for bimorph and load respectively, while iso is for isolation.

Table 5: Explanation of symbols: E – Young modulus, $\Delta\alpha$ – difference in thermal coefficient of expansion between the two materials.

Parameter	Description	Physical Dependency
k	Effective stiffness	E_1, E_2, t_1, t_2
r	Radius of curvature	$E_1, E_2, t_1, t_2, w_1, w_2, \Delta\alpha, \Delta T$
m	Mass	
b	Total damping through air, mechanical clamping points, thermoelastic losses etc	

In order for the lumped element model to give any meaningful predictions, we need to extract several material and geometry dependent parameters with sufficient accuracy. This however is difficult for certain parameters, such as total damping b , as there are no analytic expressions or even generally agreed upon models that we can immediately apply. As a result, the lumped element approach can be used only to get a qualitative estimation of which parameters are going to play a major role.

An insight gained from the lumped element model is the decoupling of the thermal cutoff frequency and the mechanical resonance frequency. In order to maximize the oscillations amplitude, it would be advantageous to have the mechanical resonance frequency of the system below or close

the thermal cutoff frequency. At first glance this may seem not possible because the mechanical resonance and thermal cutoff frequencies are initially widely apart and are both largely influenced in the same way by the system mass (e.g Figure 12). However, we can de-couple the two by attaching a thermally insulated mass to the actuator, such as a movable platform. In this way, we can shift mechanical resonance to a lower frequency without affecting much the thermal cutoff frequency, effectively bringing the two closer together.

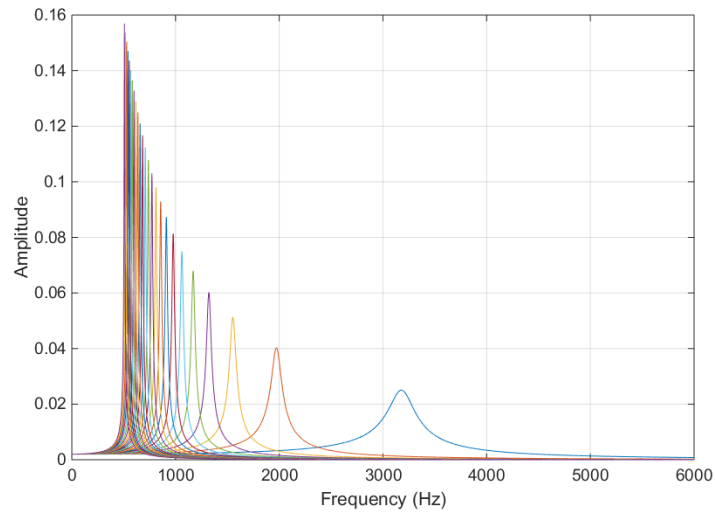


Figure 12: Oscillating amplitude for different load mass (lighter – right to heavier – left)

3. Bimorph materials

3.1. Materials overview

Apart from design choices, the selection of which materials will comprise the bimorph actuator is crucial in achieving the desired performance. Several parameters must be carefully balanced to ensure fast response and good displacement. The modulus of elasticity E determines the force required to elastically stretch or compress the material. Thus in a bimorph, it plays a role in the displacement and force output. The coefficient of thermal expansion (CTE) is a straightforward indicator of maximum displacement, as the difference in $CTEs$ between the two layers is the underlying mechanism of movement. Material thermal conductivity and specific heat determine the bimorph thermal frequency response whereas electrical conductivity is a primary factor in maximum achievable temperature. The melting point is an indication of the range of temperatures that the material can safely operate in. Lastly, the ability of the materials to retain their strength at high temperatures and after many heating/cooling cycles is also crucial with respect to reliability. In that regard, refractory materials are preferred.

A list of possible candidate materials is shown in Table 6. It is important to note, that all material properties are strongly dependent on temperature. For metals, electrical conductivity decreases with temperature, whereas their thermal conductivity, which is dominated by the electrons contribution expressed by the Wiedemann–Franz law, stays approximately the same¹. In semiconductors, electrical conductivity decreases with temperature, as carrier concentration increases. Thermal conductivity is determined by phonons and electrons, hence exhibits a more complex behavior. In insulators, thermal conductivity is dominated by phonon contribution, which translates to a peak like character for dielectric crystals and increasing behavior for amorphous substances (for a detailed discussion on thermal conductivity of different materials see [25]).

Intuition dictates that a combination of materials with very different $CTEs$, high thermal conductivity and small specific heat would be ideal for a bimorph. Furthermore, high E would lead to high output force whereas low E to high displacement. Ideally, the “hot” layer should also exhibit metal-like electrical conductivity, whereas the cold layer should be an insulator to limit heat production in the active region of the bimorph. Another point is that if the actuator also serves as a load bearing structure, then it must have high yield strength, which usually is a characteristic of crystalline materials. Finally, fabrication limitations should also be taken into consideration. Metals and their composites are deposited through physical vapor deposition techniques, such as sputtering and evaporation, which provide relatively poor step coverage in trenches. On the other hand, Si products can be grown through oxidation or in chemical vapor deposition furnaces, which typically offer much better step coverage and conformity.

The most common material combination used in bimorphs is Al/SiO₂ because of the very large difference in CTE . Other tried combinations include Al/W ([26]), polyimide/Ni ([27]), Cu/W ([28]) and

¹ Thermal conductivity is given by: $k = k_e + k_p$, where k_e is the electron contribution and k_p is the phonon contribution. Wiedemann–Franz law states that: $k_e = \frac{\pi^2 K_B^2 T}{3q^2 \rho}$, namely that k_e is proportional to temperature and inversely proportional to resistivity, which is also a function of temperature. As T increases, ρ also increases linearly and thus k_e stays approximately the same.

even graphene/epoxy ([29]). Pt is often used as an external heater element because of its ability to withstand high temperatures.

Table 6: Several candidate materials and their relevant properties in room temperature. Data taken from www.matweb.com.

Material	Young modulus, E (GPa)	CTE, α (ppm $\text{m}^{-1} \text{K}^{-1}$) (at 527 °C)	Thermal conductivity, k ($\text{W m}^{-1} \text{K}^{-1}$) (at room temperature)	Specific heat, C_p ($\text{J g}^{-1} \text{K}^{-1}$) (at room temperature)	Electrical conductivity, σ (S m^{-1}) (at room temperature)	Melting/disintegration point, ($^{\circ}\text{C}$)
Si	169	4.15	124	0.713	$10^{-6} - 10^2$	1410
SiO ₂ (fused quartz)	70	0.6	2	0.7	Insulator	1710
Si ₃ N ₄	260	3.2	15	0.7	Insulator	2660
Al	68	27.4	210	0.9	$37 \cdot 10^6$	660
Al ₂ O ₃	370	8.5	46	0.753	Insulator	2050
AlN	330	5.3	180	0.74	Insulator	2230
W	400	4.4	163.3	0.134	$20 \cdot 10^6$	3370
Au	77.2	15.2	301	0.128	$44.6 \cdot 10^6$	1060
Pt	171	10.25	69.1	0.134	$9.4 \cdot 10^6$	1770
Mo	330	6	138	0.217	$17.5 \cdot 10^6$	2620
Polyimide	5.8	144	0.2	1.09	Insulator	570
SiC (β , single crystal)	420	4-6	360	0.69	$10^{-4} - 10^2$	2730

An argument for and against can be made for many material combinations from the table above. For example, although polyimide is a material with very high CTE, it is not well suited to the “hot” layer role because it is a very good thermal insulator and thus cannot be easily and uniformly heated. Metals such as Al seem ideal for this, however it should be noted that they usually only allow for out-of-plane actuators as they cannot be deposited conformably inside holes and trenches. This somewhat limits the structures that can be achieved.

Silicon carbide has higher reported Young modulus and thermal conductivity than many other materials which hint towards high force and good thermal characteristics. Although its CTE is not the best, it lends itself to CMOS compliant fabrication techniques and combines a lot of other desirable properties such as robustness and adjustable conductivity. For these reasons, we selected silicon carbide to fill the role of the active layer. Finally, another factor weighing in on our decision to select SiC, was the possibility to explore a bimorph actuator constructed purely with Si and its compounds. This facilitates CMOS process compatibility and is related to ease of fabrication.

3.2. 3C-SiC properties

Silicon carbide is a semiconductor ceramic compound of silicon that exhibits high values of bandgap, breakdown voltage, sublimation temperature, thermal conductivity, Young modulus, yield strength, chemical inertness and radiation resistance ([30]). The electrical properties alone (Table 7) make it a good candidate for use in high temperature ($>600^{\circ}\text{C}$), high power applications, where silicon electronic devices fail at roughly 250°C . Furthermore, the higher Young modulus compared to silicon

allows for stiffer, more robust and hence more reliable structures, a desirable characteristic in MEMS. As a result it is a very good candidate for use in MEMS devices operating at high temperatures.

Table 7: Electrical properties of 3C-SiC in comparison to Si, highlighting its suitability for high power applications. [22]

	3C-SiC	Crystalline Si
Melting Temperature (°C)	2730	1414
Bandgap (eV)	2.36	1.12
Breakdown field (MV/cm)	1	0.3
Electron mobility (cm² / V s)	800	1400
Hole mobility (cm² / V s)	320	450
Saturated electron velocity (cm/s)	10 ⁷	1.3 10 ⁷

The SiC atomic structure consists of a Si atom bonded tetrahedrally to four neighboring C atoms (Figure 13). This is the base unit out of which all the SiC polytypes are constructed. The Si and C atoms form bilayers, whose stacking sequence and orientation determines the polytype (Figure 14). SiC exhibits over 200 different polytypes, with research focused primarily on the most commonly occurring three: 4H-SiC, 6H-SiC and 3C-SiC. The prefix in their names expresses the number of stacked Si-C bilayers, followed by the crystal system (C – cubic, H – hexagonal, R – rhombohedral), which form the crystal unit cell. The cubic polytype, 3C-SiC, otherwise known as β -SiC, has a zinc blende crystal structure, which is similar to diamond and is distinctly more practical, as it is the only polytype capable of being grown epitaxially on a host substrate [31]. The other two hexagonal polytypes, are only available in reduced diameter SiC wafers.

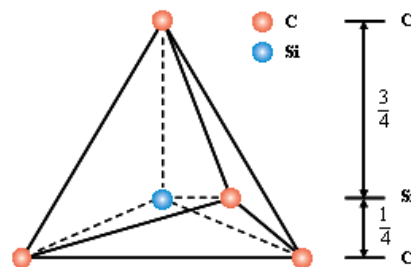


Figure 13: SiC atomic structure.

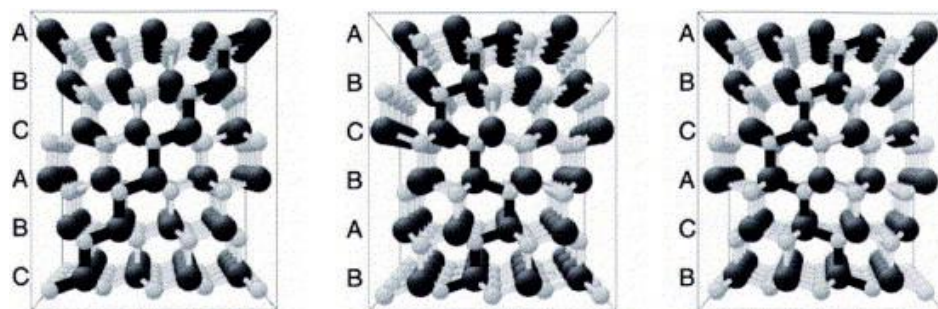


Figure 14: SiC crystal structure of 3C, 4H and 6H polytypes respectively. Black dots represent Si atoms.

Polycrystalline 3C-SiC (poly 3C-SiC) does not require epitaxial alignment and thus can be easily deposited and in-situ doped at relatively low temperatures with low pressure chemical vapor deposition on many different substrates. As with all SiC polytypes, its high chemical inertness makes wet etching difficult, as it involves either high temperatures (for molten KOH), or presents inconveniences such as lack of appropriate masking materials (for an HF/HNO₃ mixture). Hence dry etching is preferred, which is based on a hydro bromide (HBr) chemistry ([32]).

Doping of SiC can be achieved through nitrogen, phosphorus, arsenic or antimony for n-type and aluminum or boron for p-type, with in-situ insertion of dopant gases in the deposition chamber or through implantation, although the effectiveness of the latter is limited ([33]). In-situ n doping is often done with nitrogen, as ammonia is a convenient source. Nitrogen is preferentially incorporated into carbon lattice sites whereas Al into silicon sites. The ionization energy of the impurities depends on the local surrounding around the dopant atoms. In general, the ionization energies for n-type dopants such as nitrogen and phosphorus are around the same order of magnitude as silicon, approximately 50 meV. However, p-dopants present ionization energies higher than the thermal energy at room temperature. This causes the SiC p-type impurities to be only partially ionized at room temperature and hence, the electrical properties to have a strong dependence on the temperature, where a stabilization of the electrical properties above room temperature is expected.

In terms of mechanical properties, SiC is a superior material to single crystal Si. Both materials are ceramics and brittle in nature, however SiC is harder and thus more wear resistant. SiC and Si stress-strain curves for different temperatures are shown in Figure 15. From these graphs we observe that both materials exhibit a linear elastic region with their yield strength point being dependent on temperature, ultimately followed by rupture at high induced strains (curve flattening). However, Si shows a brittle-to-ductile transition (DBBT) at ~400 °C, demonstrating strain hardening above that temperature, whereas SiC exhibits a strain hardening region immediately from room temperature. This means that SiC is more resistant to fracture at room temperature. Moreover, silicon shows predominantly plastic deformation under large deflecting loads above 600 °C whereas SiC mechanical properties gracefully degrade up to 900 °C, at which temperature it is still capable of elastic deformation. Therefore, SiC mechanical properties also show favorable scaling with temperature.

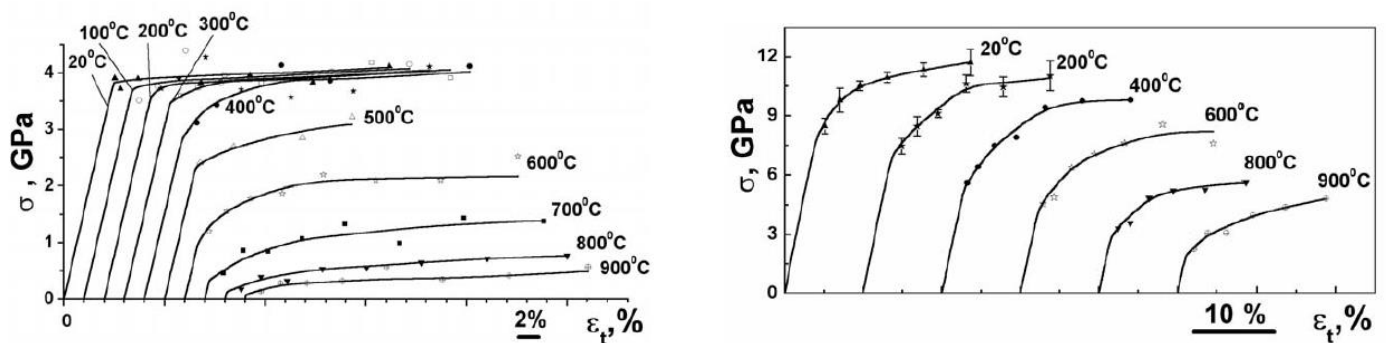


Figure 15: σ - ϵ curves of SiC (right) and Si (left), from [23].

As a final note, given that OCT is a biomedical application, use of safe materials is always sought. The strong Si-C covalent bonds may indicate high chemical and surface inertness but that is not enough to ensure biocompatibility, where the material must also exhibit low wear debris release rate and low degradation product toxicity ([34]). In recent research, 3C-SiC has shown promise as a biocompatible material. In vitro tests showed evidence of the material biocompatibility and hemocompatibility (no thrombogenesis) as shown in [35]. In vivo-tests of implanted single crystal 3C-SiC chips in mice brains for up to 35 days demonstrated reduced immuno-reactivity as compared to standard <100> silicon. Hence, as 3C-SiC is also a semiconductor, it is a promising candidate for biosensing and bioelectronics applications, in addition to being a safe material for deployment in close proximity to human tissue.

4. Polycrystalline SiC characterization

To evaluate poly-SiC as a suitable material for electro-thermal actuators it is important first, to explore the range of material parameters that can be obtained by changing the deposition parameters and second, to extract the optimized layer exact mechanical and electrical properties. Having accurate material parameters will allow us to simulate the MEMS device behavior and calculate the performance of different device concepts in a fast and convenient way. In this chapter we describe the layer optimization and characterization process.

The mechanical properties of interest in a thermal bimorph actuator are the material stiffness, expressed through Young modulus for isotropic materials and the coefficient of thermal expansion (*CTE*). The Young modulus relates to the exerted force and mechanical integrity of the actuator whereas the *CTE* is related to maximum displacement and force. Finally, residual stress after deposition should be controlled to ensure adequate device yield.

Regarding the material electrical properties, the ability to exhibit semi-metallic behavior is important because it would allow the active material of the bimorph to act as a heater. Low resistivity and thus high doping is essential to maximize current and subsequently heat production through Joule heating, as given by the equation: $Q = I^2 R$. The electrical behavior of the material in the presence of increased temperature, expressed through the Thermal Coefficient of Resistivity (*TCR*) is also of interest, as it would allow us to evaluate polycrystalline SiC as a temperature sensing material.

In the remaining sections of this chapter we discuss simple experimental procedures to extract the material properties that we selected above and we develop numerical and analytical models to reduce the uncertainties of measurement and improve the accuracy of the results.

4.1. Deposition of layers

A layer of polycrystalline 3C-SiC is formed with a 2-precursor gas process, in a chemical vapor deposition furnace under low pressure of 600 mTorr and at a temperature of 860 °C. The gases used were acetylene (C_2H_2 – ACE) and di-chloro-silane (SiH_2Cl_2 – DCS) as the carbon and silicon precursors respectively and ammonia (NH_3) as a nitrogen source for n-doping. C_2H_2 and NH_3 were diluted with a ratio of 5% in hydrogen gas before entering the furnace. Hydrogen was used due to its non-reactivity as a carrier gas. Several recipes were tested with varying gas ratios and the deposition time was set to 1 hour. The chosen recipe was selected to achieve an acceptable residual stress/resistivity trade off.

Residual stress and resistivity were measured as they usually present a tradeoff. Residual stress in poly-SiC is a strong function of deposition pressure, temperature and doping and was discussed in depth in [41]. Stress was quickly estimated through wafer bow measurement on a Tencor Flexus 2908 system. Material resistivity ρ is related to sheet resistance by $\rho = R_s t$ hence 5-point sheet resistance and SiC film thickness measurements were carried out on each wafer. The recipes are presented below in Table 8 and the results in Table 9.

Table 8: LPCVD poly 3C-SiC recipe tests. ACE and NH₃ flow rates are corrected for the 5% dilution in H₂. Molar quantities account for the number of Si/C/N atoms in the molecules.

	C precursor	Si precursor	Dopant source	Gas mixture			
	ACE (C ₂ H ₂) (sccm)	DCS (SiH ₂ Cl ₂) (sccm)	Ammonia (NH ₃) (sccm)	DCS/ACE flow	Molar Si/C	N molar % in total	Deposition rate (nm/h)
Reference (SiC700)	16	80	3.75	5	2.5	3.24	761
Test 1	17.5	50	3.75	2.9	1.4	4.22	349
Test 2	17.5	50	2.25	2.9	1.4	2.58	263
Test 3	15	100	2.25	6.7	3.3	1.70	692
Test 4 (SiC40)	15	100	3.75	6.7	3.3	2.80	797
Test 5	15	100	4.5	6.7	3.3	3.35	978
Test 6 (SiC300)	15.5	90	4.5	5.8	2.9	3.59	844

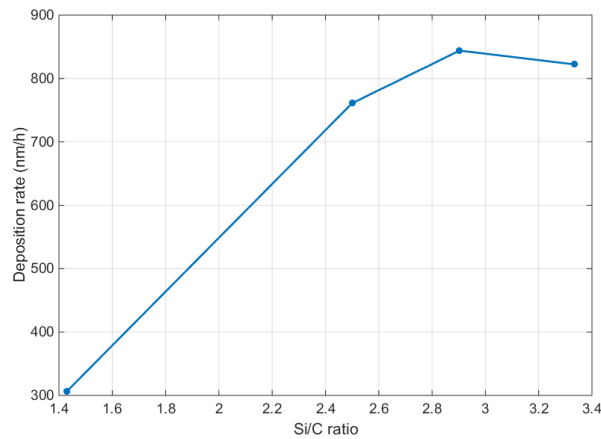


Figure 16: Deposition rate as a function of Si/C molar ratio. In case of multiple depositions at a specific Si/C ratio, the average is considered. Deposition is faster when we increase the amount of the silicon containing gas in the mixture.

Table 9: LPCVD poly 3C-SiC recipe test results.

	Stress (MPa)	Resistivity (Ω·cm)
Reference (SiC700)	670	0.0061
Test 1	1.1	59
Test 2	1.1	1.38
Test 3	-75	2.5
Test 4 (SiC40)	40	0.36
Test 5	207	0.29
Test 6 (SiC300)	300	0.03

The deposited layers were not checked for chemical composition but it is safe to assume silicon carbide was successfully formed in all cases, as the layers closely resemble those reported earlier in our group in [38]. We should mention that even though silicon and carbon atoms are found in a 1:1 ratio in silicon carbide, optimum deposition mandated higher silicon precursor gas flow (Figure 16). This may be because of lower adsorption of the DCS molecule towards the wafer surface.

The layer stoichiometry and structure depends heavily on the deposition temperature and DCS/ACE gas ratio. It was shown in [38] that in silicon rich layers (DCS/ACE>5) the orderly crystalline structure of silicon carbide is disturbed by the excess silicon atoms, resulting in smaller average grain radius. All the films demonstrated moderately high thickness variation across the wafer surface, with a maximum of $\pm 7.2\%$ occurring for layer Test 5. It should also be noted that all the films demonstrated moderately high roughness (assessed optically), with a maximum occurring for the SiC700 layer and monotonously decreasing with increasing DCS/ACE ratios above 5, as was also observed in [38]. This can be attributed to the grain structure of the layers, as poly-SiC layers have a tendency to exhibit columnar grains across the growth direction ([39]).

The layer thickness is a critical parameter that is used in many derivations of material parameters. However, due to the characteristics we mentioned above (high thickness variation, roughness, opaqueness of the film), optical thickness measurement of the SiC layers proved to be problematic. Hence the most credible method of determining it was through time consuming contact profilometry. Nevertheless, this method also suffers from small inaccuracy as the profiler probe tip radius is much larger than the fine grained roughness present in the films.

From the recipes presented above, SiC700 and SiC300 show promise for use in a bimorph actuator, where resistivity and stress should be kept low. Therefore, subsequent tests have focused on those two layers.

4.2. Residual stress

Residual film stress after deposition has been recognized as a major concern in solid films since the introduction of deposition technology, as it routinely exceeds the bulk yield strength of the materials involved. Some of the problems that it can lead to, are defect formation in the crystalline substrate, buckling, cracking, ablation and surface roughening. In MEMS, device fabrication usually includes a release step where the movable parts are detached from the substrate. The build in stress that is released during this step can be substantial and can result in device breaking as was also shown in section 1.3. Hence stress buildup should be controlled and the device design should be adjusted to accommodate it.

The residual stress of a thin film on a substrate is composed of two parts, the intrinsic and the thermal stress ($\sigma_{res} = \sigma_{intr} + \sigma_{th}$). The intrinsic stress originates from the lattice mismatch between the thin film and the substrate and the film growth mechanism. The thermal stress is caused by the different coefficients of thermal expansion of the two materials, which, in conjecture with the usually high deposition temperature of the film, causes different contractions as the wafer cools down to room temperature, and thus strain induced stress. Doping can also cause intrinsic stress as the regular lattice structure is interrupted by inserting atoms with different atomic spacing than the substrate material.

SiC700 and SiC300 were deposited on Si and SiO₂ substrates and the stress was measured through the wafer bow, after backside etching. The results are presented in Table 10. The behavior of the silicon carbide layer varies but a common trend is that PECVD SiO₂, although less stressful itself, causes higher residual stress in both layers.

Table 10: Measured residual stress on different substrates.

Substrate	SiC 700 (Reference)	SiC 300 (Test 6)
Si	272 MPa	64 MPa
Thermal SiO ₂	357 MPa	11 MPa
PECVD SiO ₂	425 MPa	163 MPa

This behavior can be attributed to higher thermal stress σ_{th} caused by a bigger difference in deposition temperatures. PECVD SiO₂ is deposited at 450 °C whereas thermal wet oxidation is performed at 1100 °C. As can be seen in Table 6, silicon dioxide has lower CTE than the substrate silicon. Consequently, when brought back to room temperature, the strain induced for the thermal silicon dioxide is higher than that for PECVD silicon dioxide, causing bigger thermal compressive stress on that film. Poly-SiC is deposited at 860 °C and has the highest CTE of all. Hence, after cooling down to room temperature, it contracts more than the underlying substrate and therefore it exhibits tensile stress. However, the higher compressive stress of the thermal SiO₂ overcompensates it, and thus this is expressed as less apparent stress for that combination. This also leads us to believe that the thermal stress component plays a dominant role in attached films.

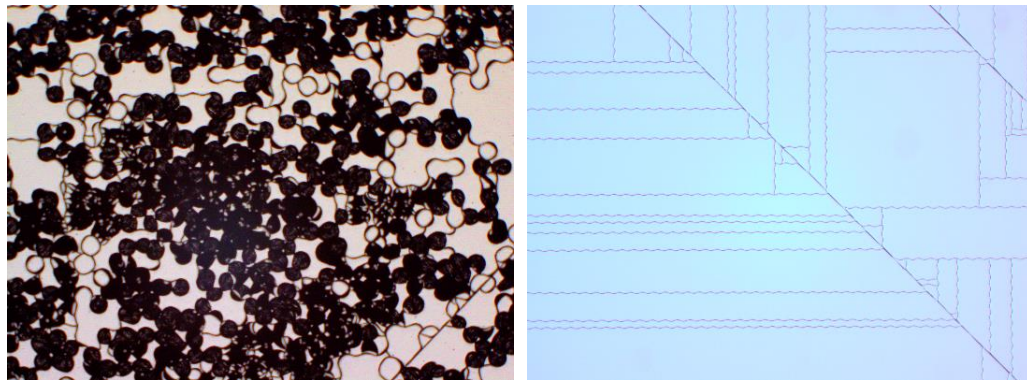


Figure 17: On the left is an optical microscope picture of an interrupted LPCDV poly-SiC deposition, showing the typical island formation process of Volmer-Weber film. The regular, wavy lines are crack marks of the wafer underneath, formed on the wafer, which was previously deposited with PECVD SiO₂, after the SiC furnace treatment (clearly seen on the right).

An important point is that as the substrate surface is crucial to the growth mechanism of the film, a stress gradient may develop along the growth direction, which depends on the substrate material and remains even after releasing the film from the substrate. The reason for this is that the surface atoms on a thin film experience a different surrounding than the bulk atoms and thus have a different equilibrium lattice constant. As the surface atoms are mechanically constrained by the bulk atoms, they experience surface stress. This is included in the intrinsic stress component. On the other hand this is not the case with the thermal stress component, as it is reasonable to assume that it is zeroed when the applied strain is relieved by detaching the film from the substrate (this released

elastic energy can cause device cracking as mentioned earlier). The high roughness and polycrystalline nature of the layers indicate Volmer-Weber cluster-based film growth, as can also be observed from the microscope image of an interrupted SiC deposition in Figure 17. Furthermore, at 860 °C deposition temperature, it is reasonable to assume that SiC also follows the compressive-tensile-compressive behavior for intrinsic stress of high mobility Volmer-Weber thin films ([36]) as observed also in other Si based films such as poly-Si and Si₃N₄ ([37]). Therefore it is expected to show a gradient compressive stress component with maximum value σ_0 at the interface between the film and the substrate.

Based on the previous discussion, we deemed worthwhile to explore the intrinsic stress component in order to determine the stress composition. A simple COMSOL model of the gradient stress was constructed by assuming in-plane compressive stress (σ_{xx} and σ_{yy} components), uniformly varying from σ_0 to 0 along the z direction and acting on cantilever structures. The cantilever x-z cross sections is shown in Figure 18. The gradient stress causes the cantilevers to curve upwards and hence we use deflection measurements on many die locations of a single wafer to estimate the value of σ_0 at the lowest plane.

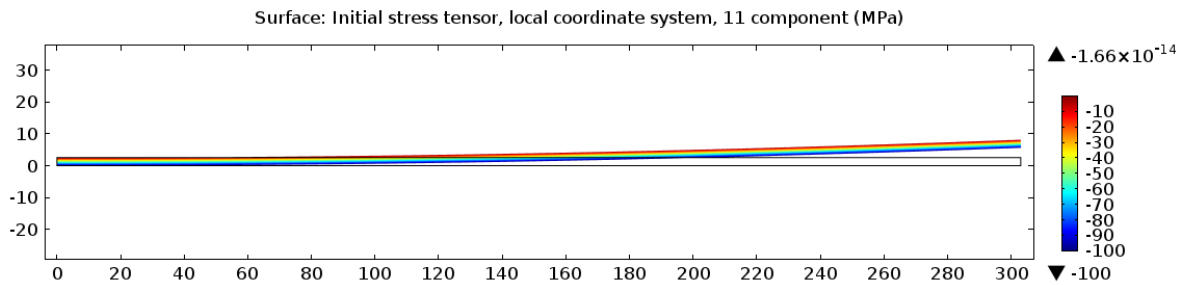


Figure 18: Cantilever COMSOL model for gradient stress analysis. The applied σ_{xx} component, varying from $\sigma_{xx} = \sigma_0 = -100$ MPa at $z = 0$ to $\sigma_{xx} = 0$ at $z \approx 2.4 \mu\text{m}$ (the cantilever thickness) is visible.

The results are shown in Figure 19, indicating $\sigma_0 \approx 27 \pm 12$ MPa and generally less than 50 MPa for a SiC300 layer grown on a layer of PECVD SiO₂. The large variation on different wafer locations indicates that intrinsic stress is very dependent on spatial features of the underlying layer. The total residual stress, as measured with wafer bow measurements on this wafer, is ≈ 250 MPa. Therefore the intrinsic stress plays a relatively minor role in total stress composition, as it accounts for only approximately 11% of the total. Therefore, we are led to believe that the large variation in residual stress values observed for different layers in Table 8, is the result of different thermal stress components. As the deposition temperature is constant for all layers, the difference must be caused by differing expansion rates, which in turn is caused by varying layer compositions. This topic warrants an investigation on its own and is deemed too complex to be treated as part of this work.

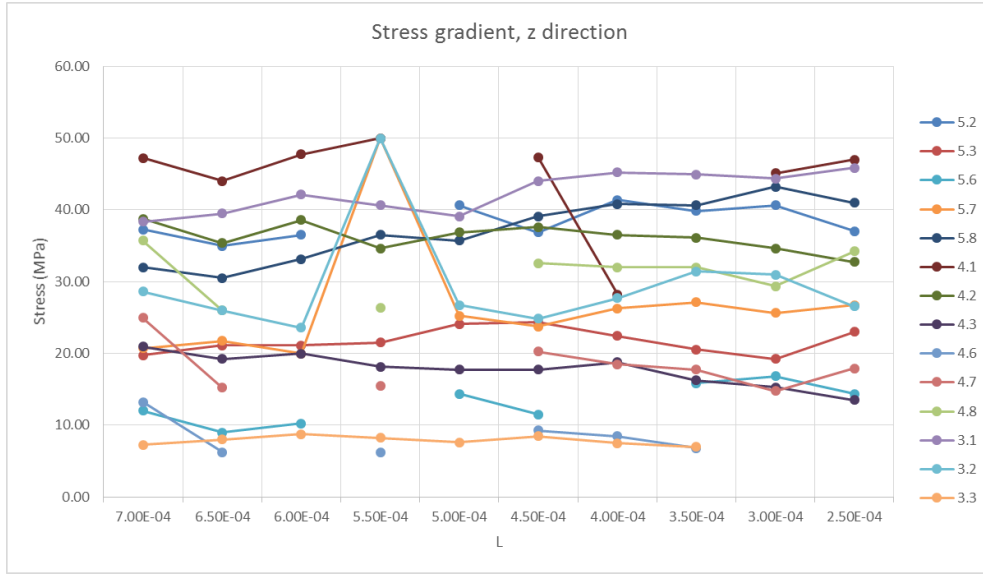


Figure 19: σ_0 for a series of cantilevers with length L , over many dies, estimated by the COMSOL model. The die coordinates are shown in the legend on the right (see Appendix B for a reference).

Finally, ending this section we can draw the following conclusion: The residual stress of deposited poly-SiC films is primarily caused by the thermal stress component, which is the result of differential expansion between the different layers. The reason behind this differential expansion is different deposition temperatures T_{dep} and/or different expansion rates CTE . Therefore, reduction of the residual stress can be achieved through matching of the $T_{dep}CTE$ product of each layer. This means that high CTE layers must be deposited in lower temperatures. Thermally grown SiO_2 Regarding bimorph actuators, the residual stress adds an offset to the initial position of the actuator and thus stress minimization is not a hard requirement. The main concern is device survivability and yield, as was also demonstrated in the first iteration of the bimorph actuator.

4.3. Resistivity

As a polycrystalline material, poly-SiC resistivity is the result of grain interactions as well as crystalline material properties. On grain boundaries, the lattice mismatch causes the formation of locally amorphous SiC, defects and subsequently traps, which act to reduce the free carriers available for conduction. Furthermore, the trapped charges create local electric fields which further prevent the free flow of current across grain boundaries. In energy terms, this is expressed by the creation of energy barriers between the grains. Therefore, grain boundaries exhibit locally higher resistance. Increasing the temperature allows for thermionic emission of carriers across those barriers and thus a reduction in boundary resistivity. Thus the existence of grains increases the material temperature dependence.

The effect of doping on resistivity in crystalline semiconductors is well known and documented. In particular for the poly-SiC crystals, it has been previously shown ([40]) and further validated in this work, that increasing doping up to a certain point causes reduction in resistivity, as carrier concentration increases with more nitrogen dopant atoms being incorporated into the lattice by carbon substitution. Beyond this point increasing doping causes conductivity to drop. It is hypothesized that this happens because nitrogen atoms are being forced to occupy interstitial positions, increasing the lattice defects, but most importantly because higher doping changes the morphology of the layer by altering the average grain size. In [38] and [40], a grain size dependence on doping percentage is indeed demonstrated, where it initially increases but ultimately decreases at higher NH_3 flow (the corresponding conductivity vs doping graph from [40] is shown in Figure 22). This affects the resistivity through the mechanisms discussed in the previous paragraph. The case of accidental nitridation of the Si wafers is not considered, partly because stoichiometric Si_3N_4 can only be produced in temperatures exceeding 1000°C ([42]) and more importantly, because the carbon precursor flow was kept much higher than the nitrogen precursor during all the tests (Table 8). The aggregate behavior of doping can be seen in Figure 20 and per each DCS/ACE ratio in Figure 21.

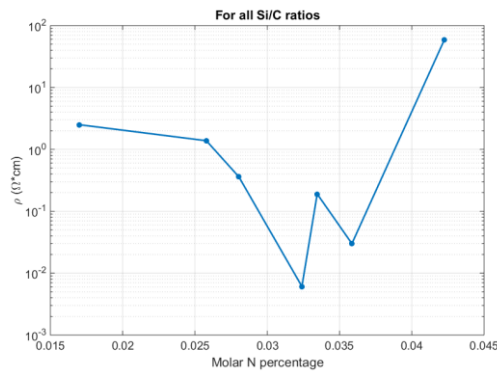


Figure 20: Resistivity vs NH_3 /Total gas flow ratio, regardless of DCS/ACE ratio. The irregular behavior in the middle of the curve shows that the DCS/ACE ratio plays a role in the resistivity.

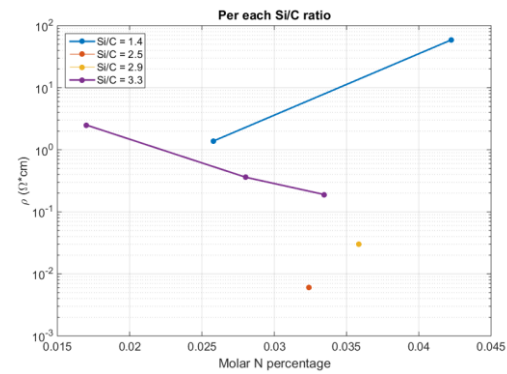


Figure 21: Resistivity vs NH_3 /total gas flow ratio, per each Si/C ratio separately.

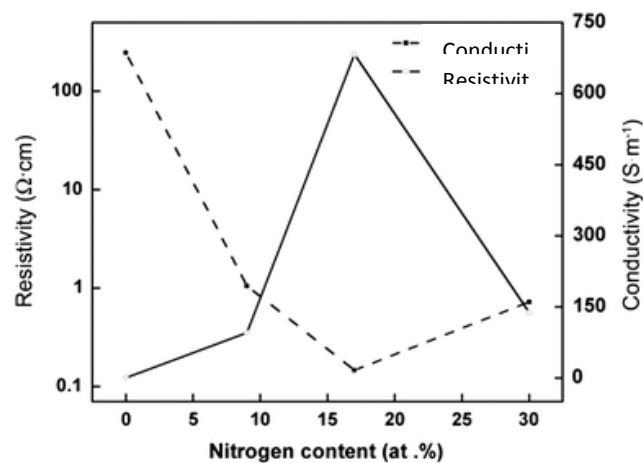


Figure 22: Conductivity vs doping as demonstrated in [40].

The layer SiC700 has the lowest resistivity and thus is the most suitable to be used as a heater. However its high residual stress could cause the reappearance of the yield problem we experienced. Therefore a compromise is reached with the SiC300 layer.

4.4. Young modulus characterization using the Electrostatic Pull-in Instability method

The Young modulus E is a mechanical property that relates a material linear elastic stress to its strain and essentially expresses the material stiffness. It is very useful for simulating the structural behavior of homogenous materials. Due to their specific crystal orientation, single crystals usually exhibit a stiffness difference depending on the direction of loading, hence they are anisotropic. On the other hand, polycrystalline materials, which are made out of many single crystal grains with random orientations, can be macroscopically approximated as homogeneous, as the grains act to aggregate the crystal elastic properties. However when the polycrystalline material consists of only a few grains, the Young modulus becomes a function of the individual crystal orientations and grain interactions. Moreover, even on many-grain systems, the grain characteristics can vary across the depth of the layer. This induces residual stress which in turn can influence the mechanical behavior of the material and cause undesired strains. In this study we are not interested in the grain composition and microscopic structure of the material but rather in the macroscopic properties of polycrystalline SiC. Hence we consider our poly-SiC as an isotropic material.

A method to electrically estimate the Young modulus of an arbitrary conductive material is the electrostatic pull in instability technique (EPI) described by Sadeghian et al. in [43]. A bias voltage is applied between the substrate and a suspended, conductive cantilever. The total force experienced by the cantilever at any point is given by the sum of the attractive electrostatic force and the restoring spring-like mechanical force: $F = \frac{1}{2} \frac{\epsilon A}{(d-x)^2} V^2 - kx$, where A is the cantilever area, d is the initial distance from the substrate, x is the displacement from the resting position, V the applied voltage and k the spring constant. As the gap closes, the electrostatic force will rapidly increase. At a certain voltage level called the pull-in voltage V_{pi} , the mechanical restoring force of the cantilever is not strong enough to counter the electrostatic force, causing the cantilever to snap to the substrate. At that point, a sudden current increase between the two electrodes can be observed. Given the cantilever geometrical properties we can extract the material Young modulus either through a numerical approach or with an analytical expression using a single measurement of V_{pi} .

This technique is advantageous relative to Atomic Force Microscopy probing as instead of a point contact force which can be liable to slippage, indentation and errors in positioning, it uses a non-contact continuous force and detects the value of interest electrically. Furthermore, in comparison to the resonance frequency technique, EPI is better because it does not induce temperature change in the cantilever from Joule heating and it is independent of the material reflectivity. Given these advantages, we opted to use the EPI technique for E estimation.

A series of cantilevers with varying lengths were fabricated on different SiC layers in order to estimate the Young modulus per layer by applying the EPI technique (Figure 23). Two experiment runs were performed, each using different fabrication batches. In the first run, the selected layer (SiC300) was evaluated. In the second run, the layers SiC700, SiC300 and SiC40 were measured to examine the Young modulus sensitivity to doping. SiC300 was re-examined to establish the repeatability of the layer properties given the same furnace parameters across batches. For the first experiment, data were collected from 18 dies, each with 10 cantilevers with lengths from 250 μm to 750 μm . The second experiment used samples from 2-4 dies per wafer and spanned the majority of the 11 cantilevers per die. Each cantilever was measured 3 times. A total of 279 cantilevers were measured in both experiment runs.

The measurement setup is shown in Figure 24. The cantilevers dimensions were extracted from SEM inspection. Moreover, as the initial gap between the substrate and the cantilever is critical in our estimation, the initial curvature of the cantilever must be taken into account for defining an appropriate gap function $G(x, \rho)$, defined as the gap distance of a cantilever with curvature ρ at local coordinate x . For that purpose the horizontal profile of each cantilever was extracted through white light interferometry (WLI) and is reported in the Figure 25.

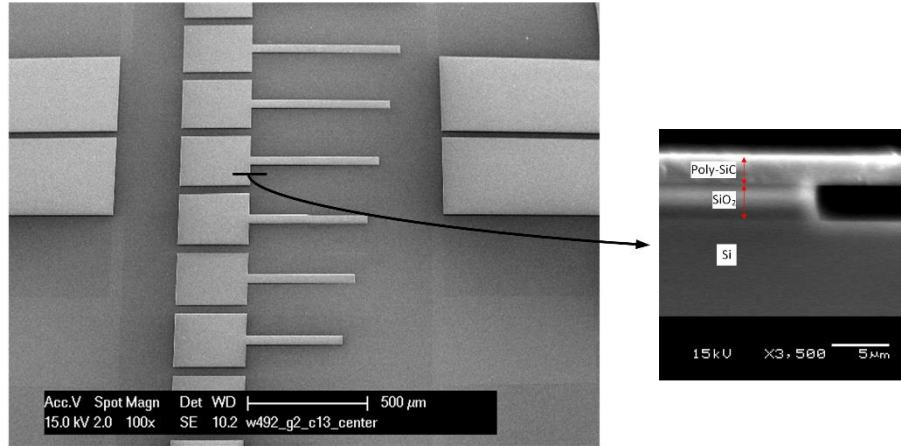


Figure 23: Series of isolated cantilevers with varying lengths from 200 μm to 700 μm . The cross section of one cantilever is shown on the right.

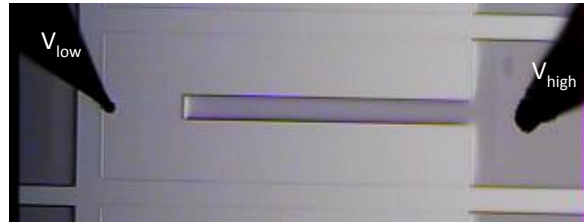


Figure 24: EPI V_{pl} measuring setup.

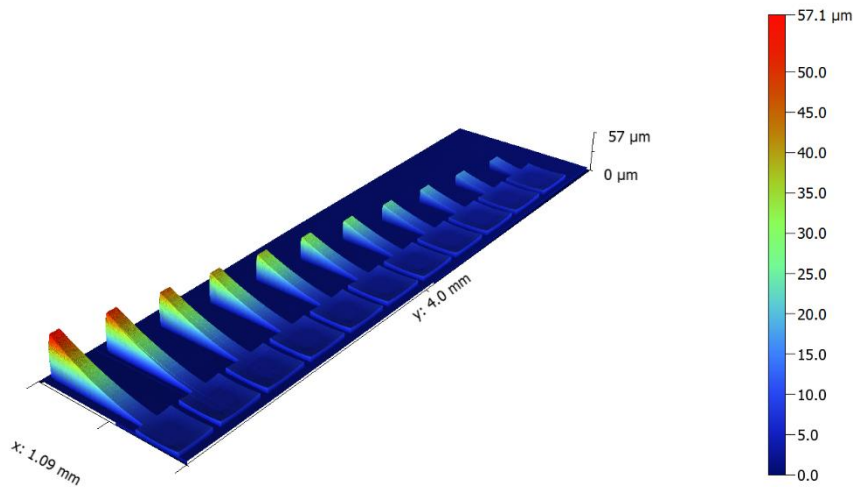


Figure 25: Cantilever WLI raw data. The axis scale is not proportional to highlight the curvature trend.

A MATLAB script was developed to automatically extract as much information as possible from the WLI data for each cantilever. First the cantilever shape was analyzed with identification of its different regions: anchor, undercut, cantilever and substrate. The curvature was calculated by least squares fitting the undercut and cantilever regions to a circle using the Pratt method ([44]), which yields more accurate fits when the data points concentrate on a small arc of the circle, as in our case.

In our case it was found that the cantilevers were tilted, exhibiting an angle offset from the anchor. Hence just the curvature is not enough to accurately model their gap to the substrate. Indeed, as can be observed in Figure 26, modelling the cantilever purely as the bottom section of a circle with radius ρ would result in the inaccurate green profile and a gap function: $G(x, \rho) = g_0 + \rho \left(1 - \cos \frac{x}{\rho}\right)$. After incorporating the angle offset φ to the gap function we get an accurate fit (grey line) and the formula becomes:

$$G(x, \rho) = g_0 + \rho \left(\cos \varphi - \cos \left(\frac{x}{\rho} + \varphi \right) \right)$$

The circle fit to the data is $(y - y_c)^2 + (x - x_c)^2 = \rho^2$, where (x_c, y_c) is the center of the circle. In the case of no cantilever tilt, $x_c = x_{AB}$, with x_{AB} being the x coordinate of the boundary between anchor and undercut, as calculated from the shape analysis algorithm. The angle offset φ can be calculated from the x shift of the center over the case without cantilever tilt ($x_{AB} - x_c$), through the equation:

$$\varphi = \tan^{-1} \frac{x_{AB} - x_c}{\sqrt{\rho^2 - (x_{AB} - x_c)^2}}$$

The error of the case with $\phi = 0^\circ$ is shown in Figure 27. The average φ and ρ values for the layers under consideration are reported in Table 11. Hence without considering the φ offset, the error would be considerable.

Table 11: Average circle fit parameters extracted for each layer.

	SiC700	SiC300	SiC40
ϕ (deg)	1.21	0.21	0.37
ρ (mm)	7.78	30.72	11.38

The SiC layer thickness is important to E estimation, as will be shown by the sensitivity analysis in Table 13. Hence, although it can be estimated from the WLI data itself, it was also accurately measured per die through contact profilometry, which proved to be a more accurate method. Finally we should note that as the cantilevers dimensions are in the order of microns, very small dimension effects on Young modulus which are important in the nanoscale, such as native oxides, surface contamination and defect density, can be ignored ([43]). An example of the results of the shape analysis is presented in Figure 26.

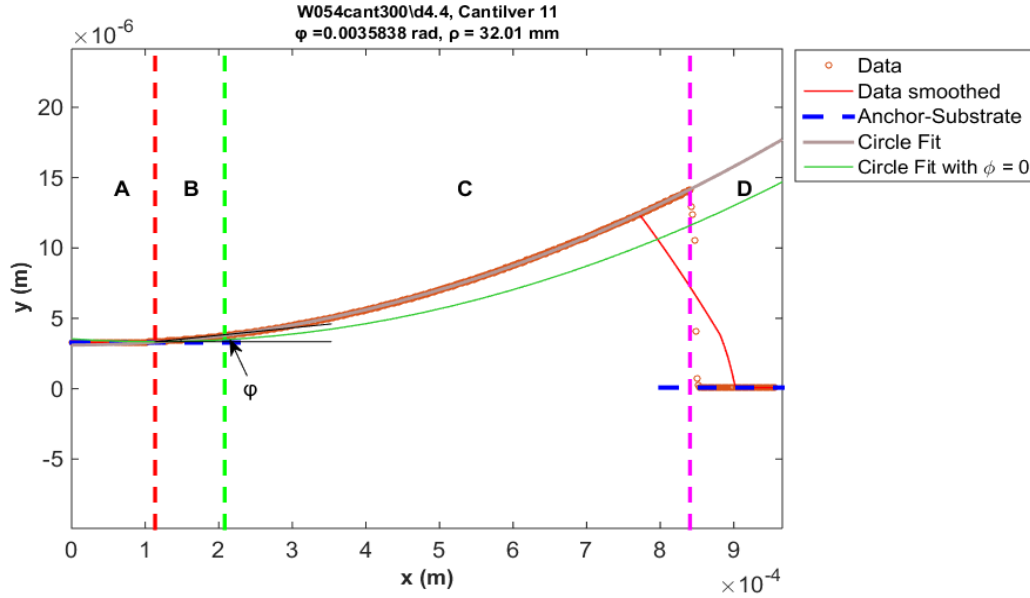


Figure 26: Shape analysis result. The regions of the cantilever are coded as: A - anchor, B - undercut, C - cantilever, D - substrate. Using only the curvature to model the cantilever would result in the green line profile. The correction for ϕ gives an accurate fit to the WLI data (gray profile).

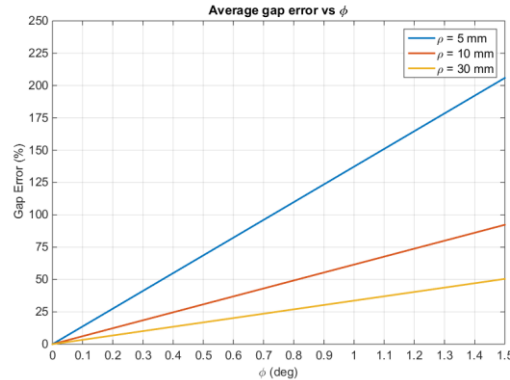


Figure 27: Average gap error for different curvature cantilevers calculated as percentage of $G(x, \rho)$ for $\phi=0$.

In order to further automate the measurement process and quickly extract the pull-in voltage from the measured I-V curves, a V_{PI} detection algorithm was developed in MATLAB. This algorithm relies on a heuristic search for the most probable V_{PI} value. The algorithm detects the 25 highest peaks in $-dR/dV$, sorts them in descending order and searches for those that satisfy the condition: $R_{before} > 10 \text{ G}\Omega$, $R_{after} \leq 7 * R_{before}$ and $R_{after} \leq 5 \text{ G}\Omega$, where R_{before} and R_{after} are the measured resistance values before and after the peak. These threshold values were selected as they were found to give minimum errors

in V_{PI} determination for all the data. Furthermore, after the automatic V_{PI} detection algorithm, a series of checks are put in place to ensure minimum amount of false positives. These checks, if not asserted, will cause the V_{PI} value to be discarded:

- If the initial resistance (after an initial settling period which, after observation of multiple I-V curves, is set to 2V) is less than 3 G Ω then the cantilever is assumed to be stuck.
- In subsequent successful repetitions over the same cantilever, V_{PI} must not deviate more than $10 \cdot V_{step}$ from the initial measurement, where V_{step} is 30 mV. This check is there to filter out any false V_{PI} assignments in repeated measurements.
- V_{PI} values must be monotonously decreasing with increasing cantilever length.

Two example I-V characteristics can be seen on the next page, in Figure 29. The pull-in voltage can be clearly identified by the sharp transition from tens of G Ω to sub M Ω resistance at 40.2 V. The V_{PI} detection algorithm accurately pinpoints this while correctly ignoring the other dR/dV spikes. We should also note that a weighted average was employed to calculate the final V_{PI} value across the 3 repetitions: $V_{PI} = V_{PI,1} + \frac{1}{2} V_{PI,2} + \frac{1}{3} V_{PI,3}$. Finally the detected average V_{PI} values were fitted with a power function: $V_{PI}(L) = aL^b + c$.

The pull-in measurement usually results in stiction of the cantilever to the substrate as is shown in Figure 28, hence each measurement is destructive and can only be repeated once. In order to recover the cantilevers that were stuck to the substrate, we applied heat, increasing the wafer temperature to 200 °C for 10 minutes. However, this method did not produce any results, thus hinting that the cause of stiction is not capillary forces from trapped moisture. Considering the low resistivity of the cantilevers, micro-welding on the point of contact is a reasonable possibility as the underlying stiction cause. Thus the recovery of measured cantilevers can be assumed impossible. In the rare cases where measurement did not destroy the cantilevers, V_{PI} was found to consistently shift to slightly higher absolute values on subsequent repetitions. This could be caused by charge accumulation by tribocharging, ion or electron injection etc ([45]).

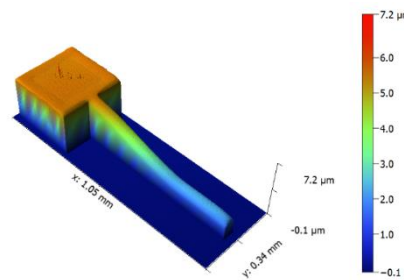


Figure 28: Cantilever stuck to the substrate after measurement.

The extracted and processed measurement data from the 2nd experiment can be viewed in Figure 30, Figure 31 and Figure 32.

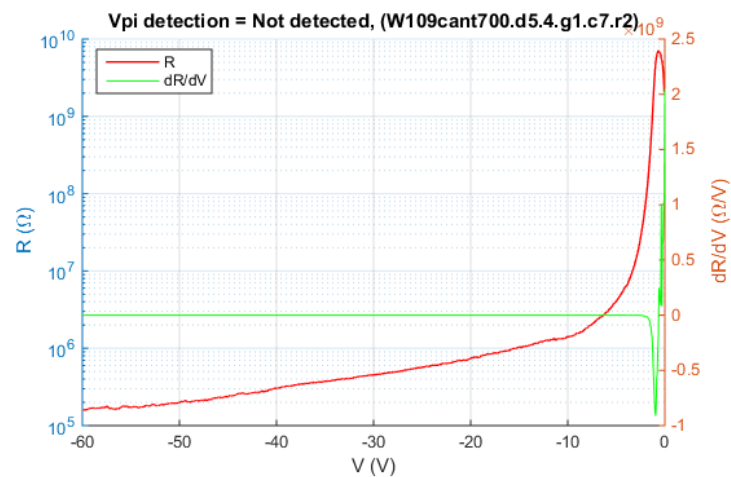
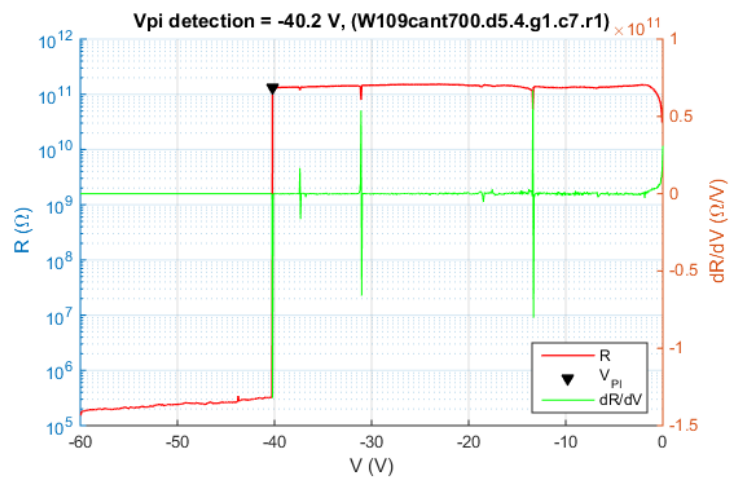
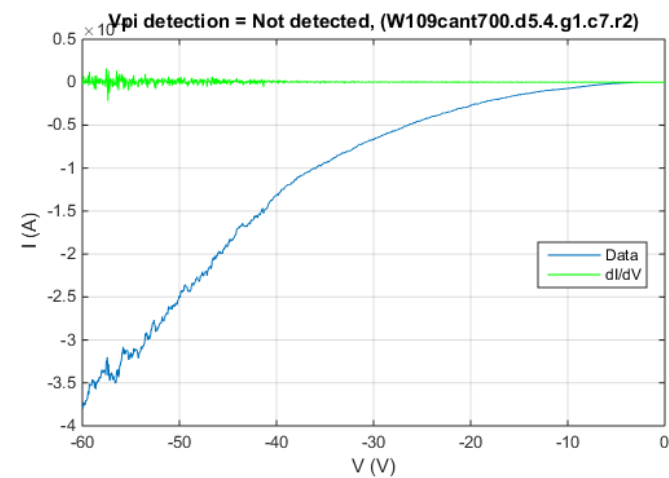
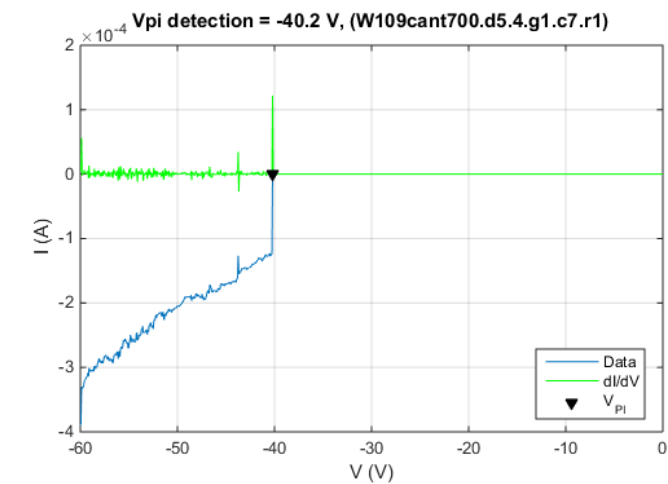


Figure 29: Example I-V and R-V characteristics of 1st (left) and 2nd (right) measurement on a cantilever on the SiC700 wafer. The high pull-in voltage is justified by the high curvature that cantilevers exhibit on this layer. The 2nd measurement showcases a typical I-V curve of a cantilever that has undergone stiction to the substrate. Notice the lack of any sharp transitions in R , in that case.

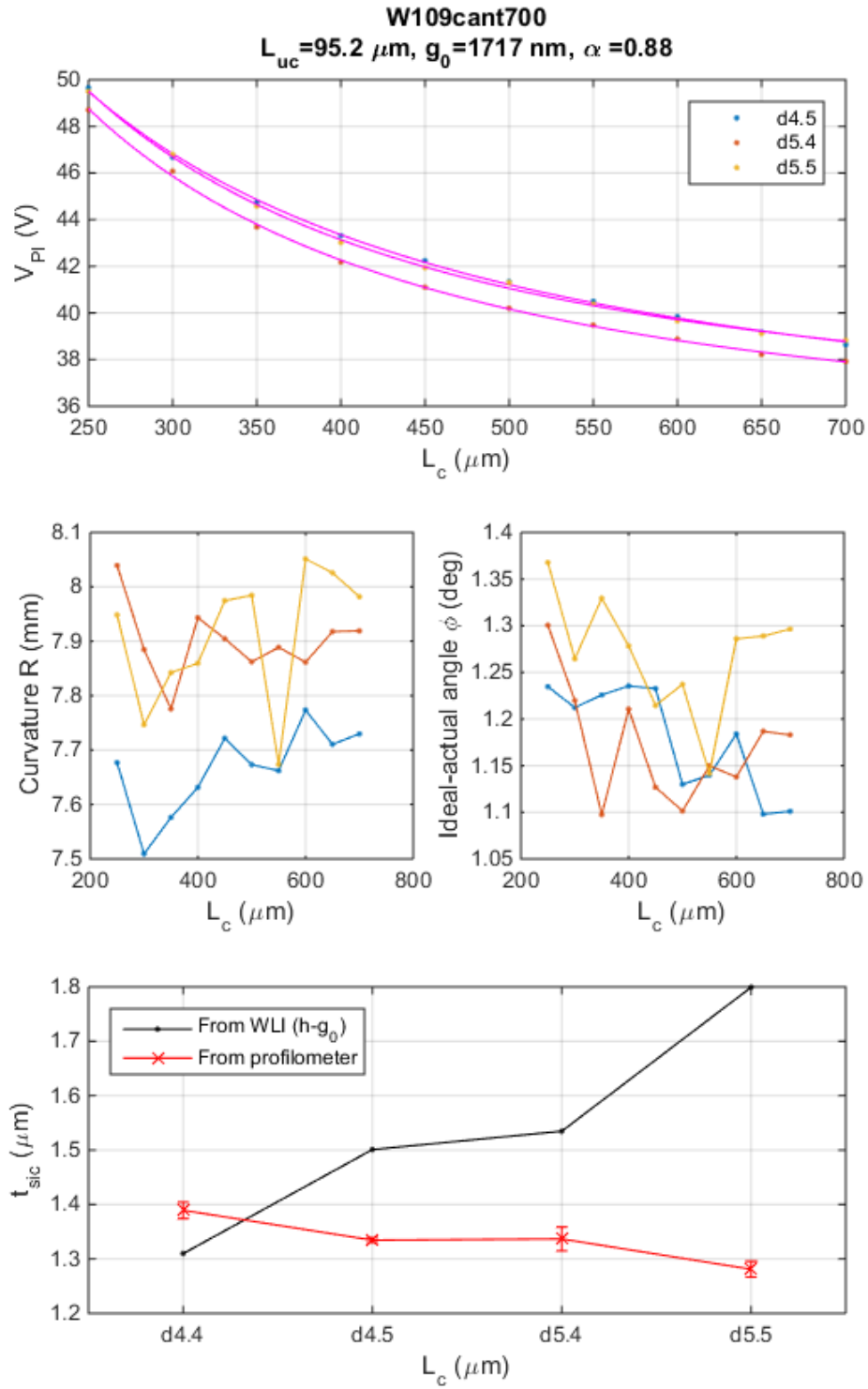


Figure 30: Data extracted for 2nd experiment run from the wafer with the SiC700 layer. L_c is the cantilever length.

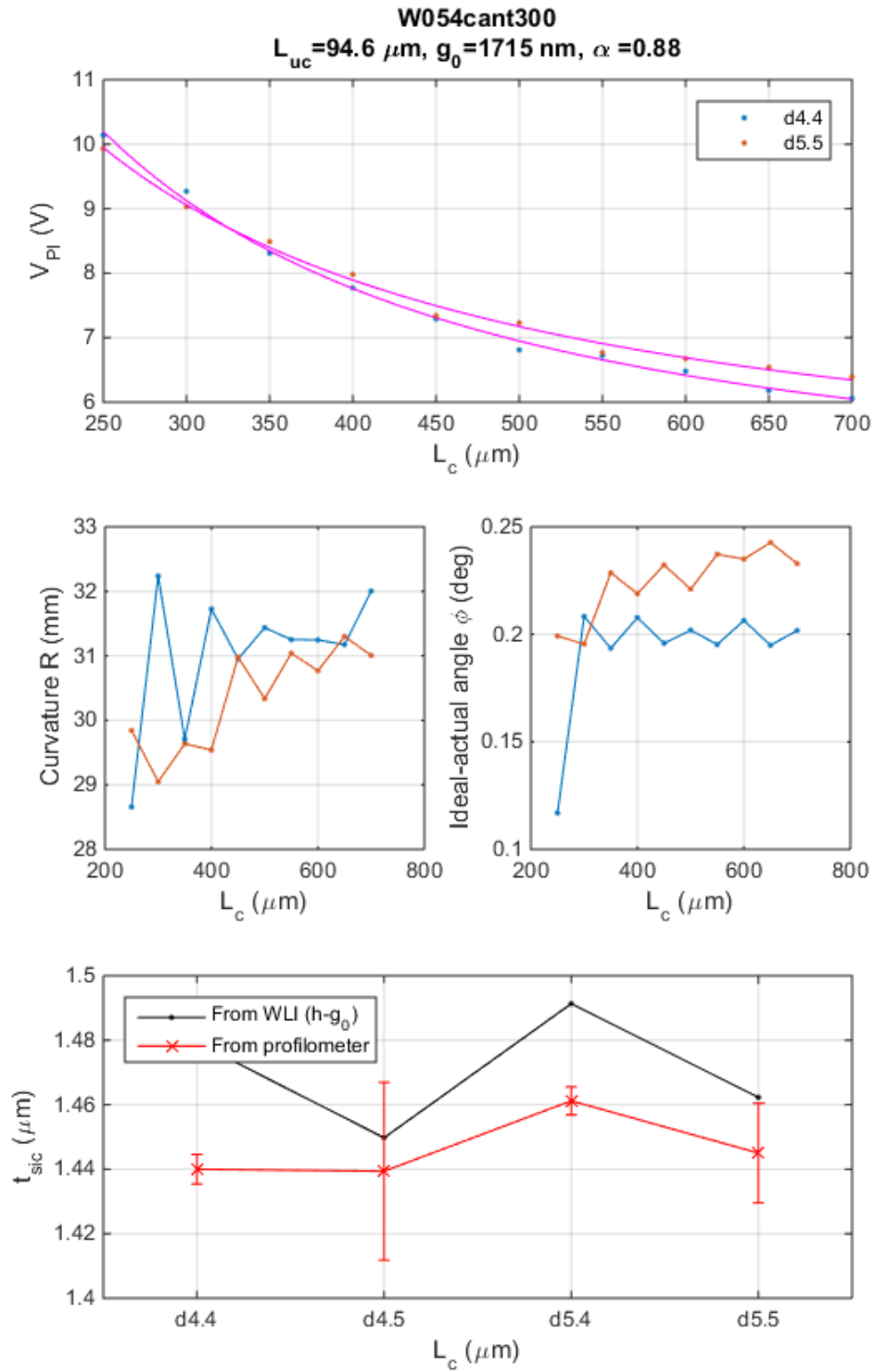


Figure 31: Data extracted for 2nd experiment run from the wafer with the SiC300 layer. L_c is the cantilever length.

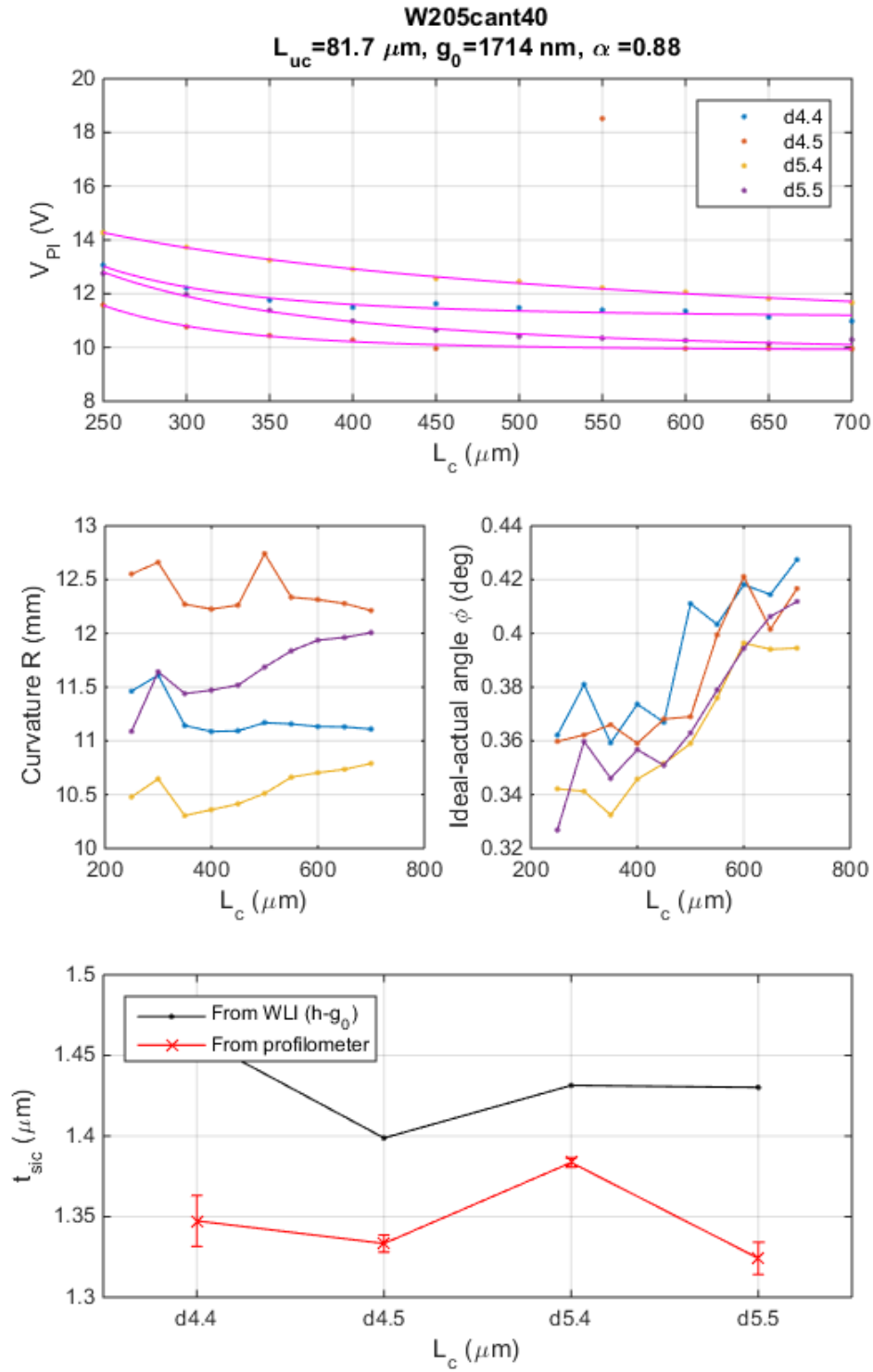


Figure 32: Data extracted for 2nd experiment run from the wafer with the SiC40 layer. L_c is the cantilever length.

Using the measurement data as extracted with the methods described above, the value of Young modulus was calculated by both analytical and numerical models. In the analytical model a 4th order approximation was used, given by the equation below, as was reported in [46]:

$$V_{PI} = \sqrt{\frac{\tilde{E}\tilde{I}}{\epsilon_r\epsilon_0 b}} \times \frac{2 \int_0^L (\varphi'')^2 dx}{2 \int_0^L \frac{\varphi^2}{G^3} dx + 6C \int_0^L \frac{\varphi^3}{G^4} dx + 12C^2 \int_0^L \frac{\varphi^4}{G^5} dx}$$

where $\tilde{E}$ is the effective Young modulus, $\tilde{I}$ is the moment of inertia, $\varphi(x)$ is the natural mode shape of the cantilever, $G(x, \rho)$ is the gap function, C is a constant integral term that depends on the geometry, $\epsilon_r\epsilon_0$ is the electrical permittivity of the air and b is the cantilever width. As the fabrication method involves etching of a sacrificial layer, the appearance of undercut is inevitable. The effect of the undercut is to reduce the cantilever apparent stiffness. To correct for it, the effective cantilever length was calculated: $L_{eff} = L + \alpha_{corr}L_{uc}$, where $\alpha_{corr} = 0.88$ is a correction factor calculated through electromechanical numerical simulations of the cantilever with and without undercut and L_{uc} is the measured undercut. According to [48], the undercut shape (protruding tip underneath the anchor base) plays a significant role only when $L_{uc} < b$, which is not our case. Therefore the undercut shape is not taken into account in our calculations.

We evaluated the analytical model sensitivity to variations in all its input variables. Considering the Young modulus E to input variable P , the sensitivity $S_E(P)$ of E with respect to P is defined as the partial derivative $S_E(P) = \frac{\partial E}{\partial P}$. We have assumed small parameter variations ∂P , given in Table 12, over which E can be approximated as a straight line. We present the sensitivity values for the positive and negative variation in Table 13. As we see, $S_E(P)$ is usually symmetric. The Young modulus calculations are most sensitive to geometrical parameters like L , curvature ρ and angle offset φ .

Table 12: $|\partial P|$ where $S_E(P)$ is considered linear and typical error values ΔP , for different parameters P .

Parameter, P	t (nm)	g_0 (nm)	V_{PI} (V)	L (μ m)	ϕ ($^\circ$)	ρ (mm)
$ \partial P $	10	10	0.01	1	0.01	0.1
Typical ΔP	50	1	0.015	40	-	-

Table 13: Sensitivity values.

	$t+$	$t-$	g_{0+}	g_{0-}	V_{PI+}	V_{PI-}	$L+$	$L-$	$\varphi+$	$\varphi-$	$\rho+$	$\rho-$
$\frac{dE}{dP}$	-0.73	-0.75	-0.13	-0.13	0.13	0.13	0.78	0.78	-3.81	-3.86	3.82	3.85
	GPa/nm		GPa/nm		GPa/mV		GPa/ μ m		GPa/.01 $^\circ$		GPa/.1 mm	

The numerical model used (Figure 33) was dimensions accurate, with the cantilever curvature, φ correction and the anchor embedded into the model. Hence calculation of an effective length L_{eff} is redundant. As the system is symmetrical, only half was modelled. The system is solved through the Newton-Raphson method and large deformations are assumed. Young modulus is decremented gradually and the system is solved for each value. When pull-in is reached, the system is non-stable and the solver does not converge. In an effort to reach convergence, the solver backtracks to a stable solution and tries a new value. This method is time consuming because multiple iterations are required in order for this process to narrow down the final E value.

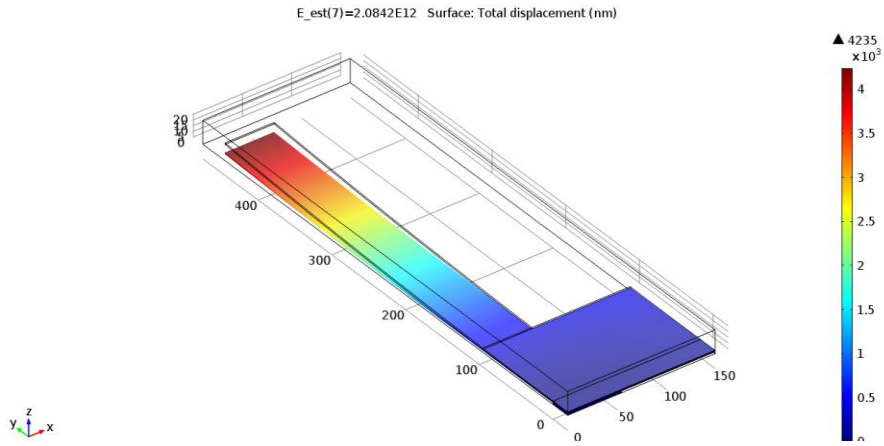


Figure 33: Stationary displacement solution of the Young modulus numerical model in COMSOL. Half the cantilever/anchor system is modelled. The anchor base is also visible.

The analytical formula results for the 1st and 2nd experiment can be seen in Figure 34 and Figure 35 respectively. The calculated Young modulus values for each measured die were filtered and averaged to remove outliers and reduce uncertainty (red curves on the figures below). The error margins were calculated from those averaged curves.

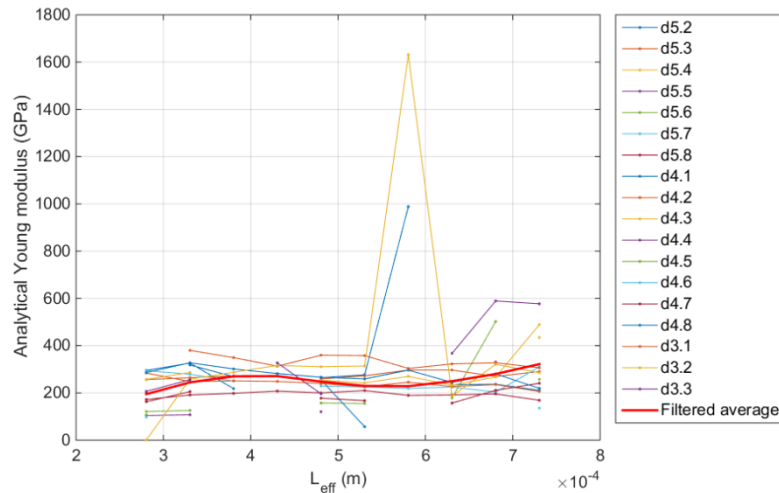


Figure 34: Effective young modulus analytical calculation for the 1st experiment n layer SiC300.

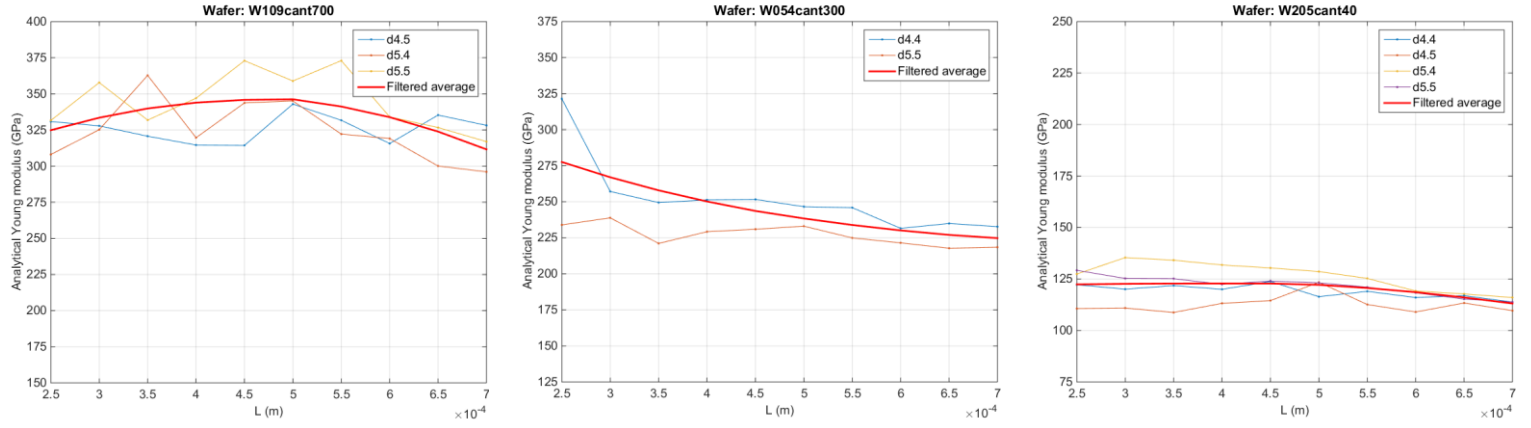


Figure 35: Results of the analytical model for each layer and cantilever effective length in the 2nd experiment.

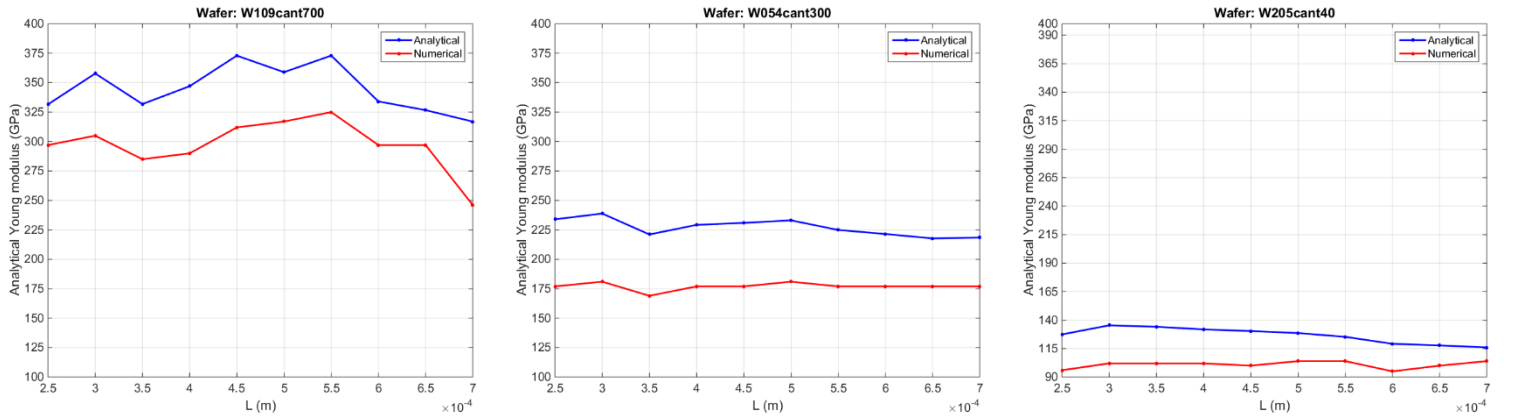


Figure 36: 2nd experiment numerical vs analytical model results for one die per layer.

The resulting value for the 1st experiment is $E_{\text{eff, SiC300}} = 254 \pm 34$ GPa which agrees to previously reported data ([49]). The analytical results for the 2nd experiment run yield an average value of 334 ± 11 GPa for SiC700, 245 ± 18 GPa for SiC300 and 120 ± 3 GPa for SiC40. As solving the numerical model is very time consuming, it was evaluated for one die per layer. The results are compared to the analytical solution in Figure 36. The value for SiC300 is consistent within the error margins across different fabrication runs and following the same measurement procedure, which indicates the method validity.

As seen in Figure 36, the two modelling approaches produce non-identical results. The reason for this deviation was searched in the points where these two models differ:

- The numerical model includes a full modelling of the anchor and undercut whereas the analytical model relies on an effective length calculation to account for the effect of the undercut, as discussed previously.
- In the analytical model, the cantilever shape is defined separately than the gap G . Specifically, φ in the V_{p1} formula is defined as the natural (first) mode shape from the Euler-

Bernoulli beam theory¹. On the contrary, in the numerical model, the gap G and shape φ both correspond to a circle segment of radius ρ and angle offset φ .

C) The analytical model is not full order and thus is an approximation.

The difference between the numerical and analytical models as seen for one die per layer, is too high to be attributed to the effective length difference. Difference B is presented in graph form in Figure 37. An initial estimation is also that the difference is too minor to produce the deviation by itself. Likewise, the difference C is relatively minor as the difference between the full order model and the fourth order model is only 1.06 % ([50]). Further investigation on this part is deemed necessary.

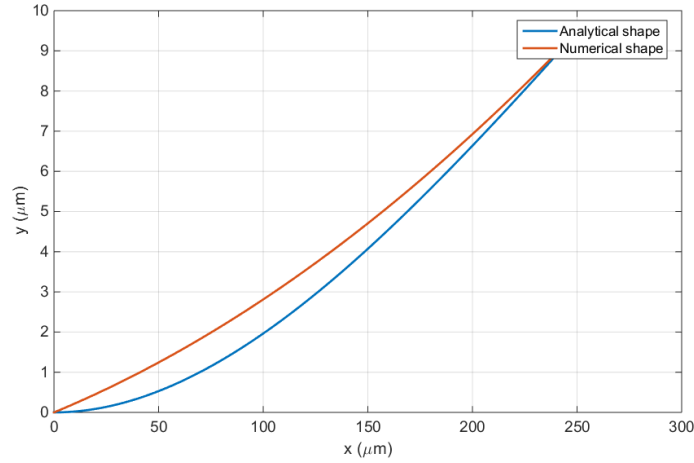


Figure 37: Cantilever shape difference between the analytical and the numerical model. Please note that the gap between the cantilever and the substrate is the same in both cases. The shape refers only to the mechanical behavior of the cantilever. The two curves are normalized to the same scale for comparison.

Regardless of the model, higher E values appear to be associated with lower DCS/ACE ratios. This trend cannot be attributed to modelling errors being exacerbated from high cantilever curvature, because the flattest cantilevers (SiC300) still exhibit higher E values than SiC40. Therefore, the lesser Young modulus for silicon rich layers SiC300 and SiC40 may be attributed to the less stoichiometric composition of these layers. Explanation of this behavior is outside the scope of this thesis. Furthermore, as Young modulus is a property that arises from the interatomic bonds and distances in the material, it is temperature dependent. However, its thermal coefficient is reported to be very small ([51]) and thus any dependence is neglected in this discussion.

As a concluding remark, the EPI method appears to be very sensitive to the model accuracy and leads to high variations in E estimation, even after careful consideration of multiple possible error sources. Therefore, just a single measurement is not enough for accurate calculation of E . However, following the streamlined procedure described in this section, a relatively fast estimation can be performed.

¹ The mode shapes for a continuous cantilever beam from the Euler-Bernoulli beam theory are given as:

$\varphi_n(x) = A_n \left\{ (\sin n\pi - \sinh n\pi) \left(\sin \frac{n\pi x}{L} - \sinh \frac{n\pi x}{L} \right) + (\cos n\pi - \cosh n\pi) \left(\cos \frac{n\pi x}{L} - \cosh \frac{n\pi x}{L} \right) \right\}$, where $n=1,2,3,\dots$ the mode number.

4.5. TCR characterization

The material was also characterized from the perspective of Thermal Coefficient of Resistivity (TCR) in order to determine how suitable it is to be used as a temperature sensing element. The resistance was measured over the temperature range of $20^{\circ}\text{C} - 200^{\circ}\text{C}$ on a Cascade Microtech probe station with a thermally controlled chuck. These measurements were performed with limited power to avoid any self-heating that could influence the result. Also, as TCR concerns the relative change in resistance, the geometry and dimensions of the sample under test need not be considered.

The TCR was initially calculated through the linearized resistance formula: $R(T) = R_0[1 + \alpha(T - T_0)]$, where R_0 is the resistance at reference temperature T_0 and α is the TCR and is shown in Figure 38. As the resistance appears to follow a 2nd polynomial behavior, we can use a linear interpolation for $\alpha = f(T)$, effectively turning the linearized resistance formula above to a piecewise approximation. The resistance of poly-SiC is reported to follow a second order polynomial curve for up to 700°C ([38]) and therefore a linear extrapolation to higher temperatures is also justifiable. Alternatively, the resistance-temperature dependence can be modeled more accurately directly with a second order formula as $R(T) = R_0[1 + \alpha(T - T_0) + \beta(T - T_0)^2]$, which yields $\alpha = -0.0022\text{ 1/K}$ and $\beta = 2.2 \cdot 10^{-6}\text{ 1/K}^2$ as seen in Figure 38 or with Mott's conductivity law, where $\sigma \propto \sigma_0 e^{-(T_0/T)^{1/4}}$, which encompasses the tunneling effect across grain boundaries in polycrystalline materials ([47]).

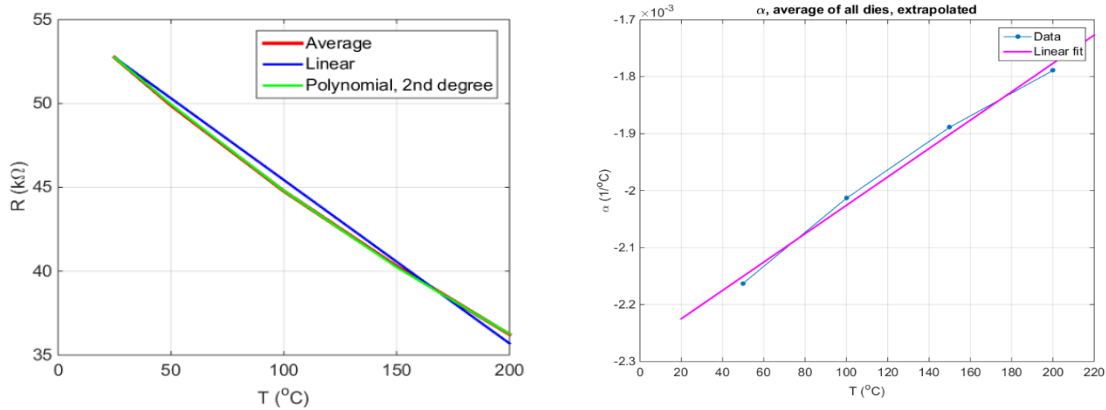


Figure 38: SiC300 layer average resistance and TCR vs temperature.

As SiC is a semiconductor, a negative TCR is expected (NTC). Furthermore, for resistance based temperature sensing, a large value of TCR is sought. Therefore, poly-SiC, with average TCR of -0.0018 1/K , is a mediocre choice. In comparison, Pt is reported to have a thin film TCR of around 0.003 1/K . Besides, even for that limited temperature range, poly-SiC shows non-linear behavior as TCR also changes with temperature. Therefore we conclude that compared to Pt, SiC is an inferior material for temperature sensing mainly because of its non-linearity. However, we can still calculate a temperature estimation which, given the straight forward conversion of temperature increase to displacement, could potentially be used for a feedback control implementation of the bimorph actuator.

4.6. CTE characterization using V-beam actuators

In order to find the coefficient of thermal expansion of the SiC layer, v-beam test actuators were fabricated and measured. The actuator displacement is a strong function of the material CTE and temperature. The finished device is shown in Figure 39 and the fabrication steps can be found in the

Appendix. Devices consisting of two parallel v-beams were selected for this procedure. In summary, a current source was connected to the actuator and its displacement is measured optically. The resistance of the device was simultaneously measured, which, combined with the TCR values extracted in the previous section, lead to an average temperature value. It is worth noting that because the main structural components of the v-beam actuator (2 v-beams, 4 springs) are made of the same material, the Young modulus doesn't influence the displacement obtained. A material with larger E would lead to stiffer springs but also to higher force output from the v-beams and hence these effects would cancel each other out. Therefore, any errors in estimating E in the previous section don't play a significant role.

The fabricated actuator can be seen in Figure 39 and was characterized with the configuration shown in Figure 40. A current source supplied current in 0.5 mA steps, from 0.5 mA up to the source compliance limit, which was 20V. The voltage across the pads was also measured and the device resistance was calculated through Ohm's law. The displacement was measured optically with the technique presented in [52]. The procedure was repeated for 4 different devices and the results were averaged. An example of the measured values is presented in Table 14.

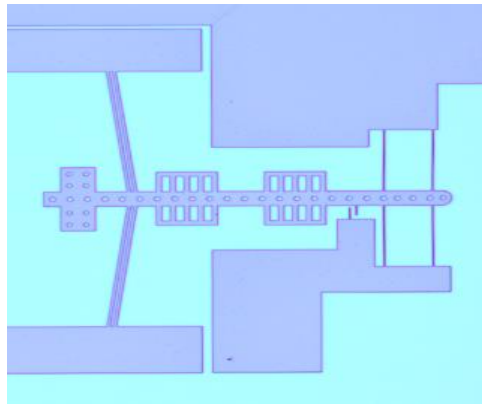


Figure 39: Optical image of one of the finished 2-section v-beam actuators used in CTE characterization.

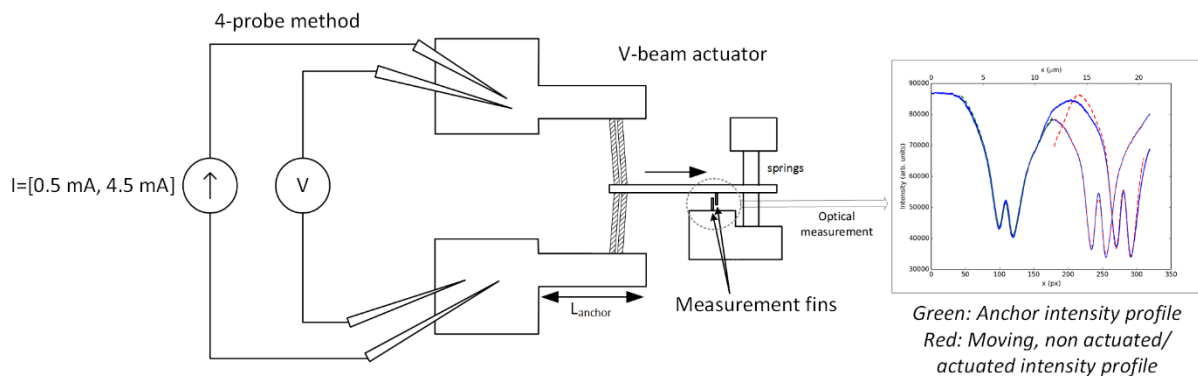


Figure 40: CTE extraction setup.

Table 14: Measured values for die 4.4.

I_{source} (mA)	V_{source} (V)	V_{pad} (V)	R_{device} (k Ω)	T_{average} (°C)	$\Delta R_{2-4 \text{ probe}}$ (Ω)	R_c (Ω)	Displacement (nm)
0.5	3.07	2.86	5.72	30	420	200	≈ 0
1	6	5.59	5.59	40.6	410	195	150
1.5	8.71	8.12	5.41	69.8	394	187	349
2	11.16	10.4	5.20	106.7	380	180	617
2.5	13.38	12.45	4.98	147	372	176	939
3	15.37	14.29	4.76	189.6	360	170	1294
3.5	17.16	15.92	4.55	234.6	354	167	1689
4	18.77	17.39	4.35	278.1	346	163	2092
4.3	19.69	18.22	4.24	302.6	342	161	2300

To measure the resistance, the 2-probe method uses the current supplying probes to measure the voltage drop across the sample. This has the inherent disadvantage of including the wire and contact resistances into the measurement ($R = R_{\text{dev}} + \Delta R_{2-4 \text{ probe}}$, where $\Delta R_{2-4 \text{ probe}} = 2R_w + 2R_c$). In order to avoid this, a four probe method can be used, whereas we use a separate pair of probes attached to a high input impedance voltmeter to measure the voltage drop across the pads. The wire resistance is very low, below 10 Ω , therefore the resistance difference $\Delta R_{2-4 \text{ probe}}$ between the 2-probe and the 4-probe methods is dominated by the contact resistances. The R_c is estimated from the data and also presented in Table 14.

The contact resistance originates from the Schottky barrier between the tungsten metal probe and the SiC semiconductor pad. In our case the current flow is also facilitated by traps at the rough polycrystalline surface and the layer damage caused by the sharp probe tips. We observe a drop in R_c with increasing current that can be attributed to increased thermionic emission of charge carriers across the Schottky barrier¹.

The first numerical modelling attempt in COMSOL was a fully coupled multi-physics model including electrical, thermal and structural physics with their corresponding cross-dependencies. Multiple iterations of this model led to its refinement, with each iteration quality being judged on the grounds of its predicted device resistance R_{dev} matching the experimental data. The final model includes thermal convection, a thermal conduction path through the air to the substrate and a linear least squares interpolation of the TCR parameter, as measured in 4.5. Even though air has very high thermal resistance, the small gap (3.2 μm) necessitated the inclusion of the air conduction path to the substrate. Furthermore, as discussed previously in section 4.2, the gradient stress causes the devices to buckle and hence plays an important role in the measured displacement. Hence it was also included, after calculation with a procedure similar to the one described in section 4.2. The model produced small, temperature-dependent R_{dev} discrepancies from the measured values. The cause of these is hypothesized to be probe placement variations for each sample. Therefore, the model is also calibrated with a geometric correction factor, whereas the anchor length L_{anchor} (Figure 40) is adjusted until $R_{\text{dev,COMSOL}} = R_{\text{dev,measured}}$ at $I = 0.5$ mA. The anchor correction values calculated in this way are

¹ According to Richardson's law for thermionic emission across a barrier, the current is given by: $I = AA_G T^2 e^{\left(\frac{-\phi}{kT}\right)}$ where A is the cross-section area, A_G the effective Richardson constant, ϕ the barrier height and k the Boltzmann constant. Thus the contact resistance can be theoretically approximated with $R_c = V / AA_G T^2 e^{\left(\frac{-\phi}{kT}\right)}$, with $V = (V_{\text{source}} - V_{\text{pad}}) / 2$ the estimated voltage across the contact. Upon plotting, the measured R_c resembles this formula with reasonable parameter assumptions in place and thus we can confirm that thermionic emission is indeed the physical cause.

within reasonable bounds and are given in Table 15. The final model used had an average resistance mismatch across all samples and currents of 370 Ω . This deviation is the sum of *TCR* and geometric inaccuracies.

Table 15: Effective anchor length to account for probe placement variations. The nominal anchor length was 315 μm .

Die	Effective L_{anchor} (μm)
4.4	375
4.5	359
5.4	464
5.5	486

However, even after many iterations to eliminate the uncertainties, the full model led to inaccurate results. The problem was isolated to the heat generation and removal, where thickness, *TCR* and probe placement uncertainties made the problem of finding the temperature along the v-beams underdetermined. Hence *CTE* extraction was problematic, as demonstrated in Figure 41, where high intra-die variation can be observed.

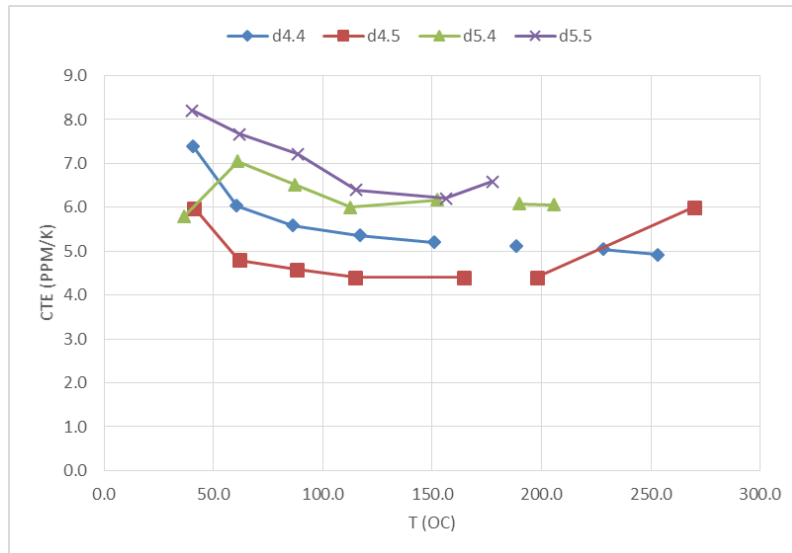


Figure 41: Initial output of COMSOL model for CTE. The electrical characteristics of the system introduce extra uncertainty which appears as intra-die variation.

To solve this issue, we omitted the heat generation through Joule heating and instead opted for an immediate definition of temperature along the v-beams as estimated from the resistance drop of the device. This reduced model, although not resembling the temperature profile on the v-beams as accurately as simulating the full Joule heating case, improved the intra-die variation. The results of this approach can be seen in Figure 44 and the reduced COMSOL model used is shown in Figure 42. Finally, the whole *CTE* extraction procedure along with intermediate results at each stage, is summarized in flowchart form in Figure 43.

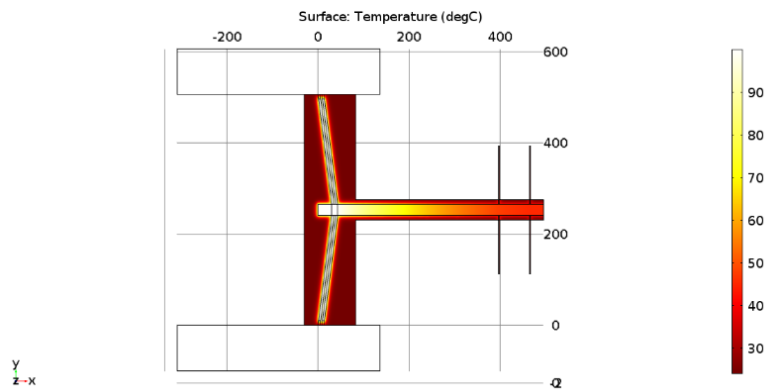
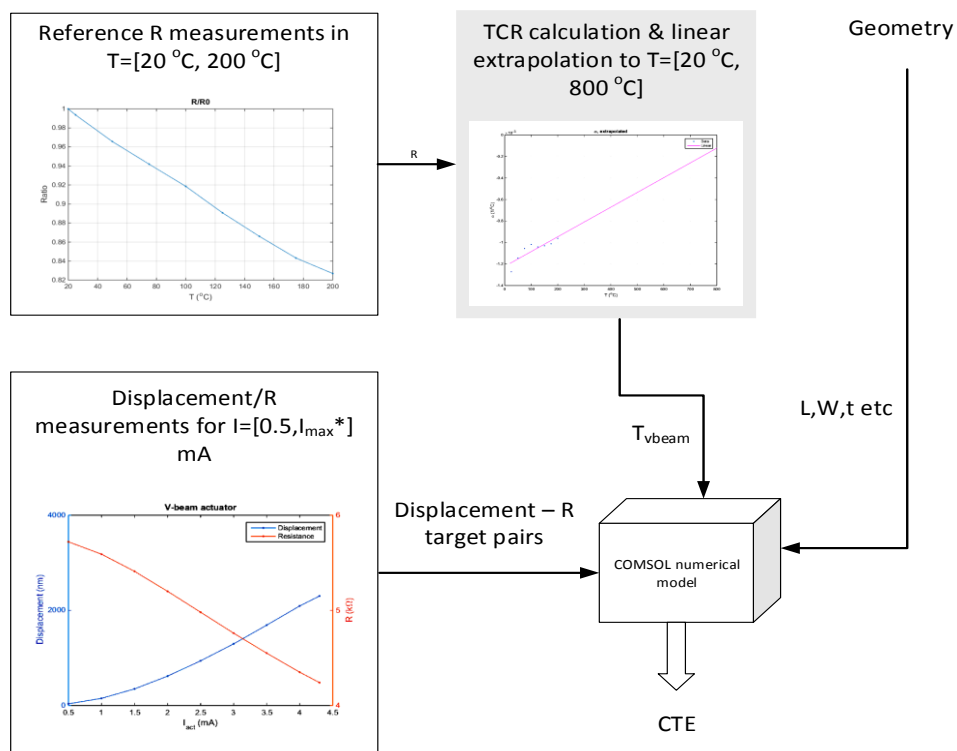


Figure 42: COMSOL reduced model for CTE extraction. Air is modelled only underneath the structure to save computation time.



*: I_{max} is determined from the current source (Keithley) compliance limit which is 20V.

Figure 43: CTE extraction flowchart.

The results of the reduced model can be seen in Figure 44 and they have reasonably low variation across dies in order for us to extract a useful CTE value. The irregular high CTE at $T = [35\text{ }^{\circ}\text{C}, 70\text{ }^{\circ}\text{C}]$ and any subsequent spikes are attributed to a factor related to the mechanical domain. Upon white light interferometry inspection, the v-beams appear to experience high gradient stresses, which buckle the actuators downwards, as seen in Figure 45. This will result in out of plane motion and subsequently, smaller measured x displacement. This is more apparent for small actuation power, where the initial out-of-plane motion caused from the buckling is responsible for a larger portion of the total structure movement. Moreover, when the actuator moves, the mobile measurement fin (Figure 40) tends to get out of focus, as the depth of field is quite limited in high optical magnifications. This in turn leads to optical SNR deterioration and lower displacement measurement accuracy. Finally, without an accurate way to set or measure the temperature of the device locally, any CTE calculation with this method will have an inherent uncertainty.

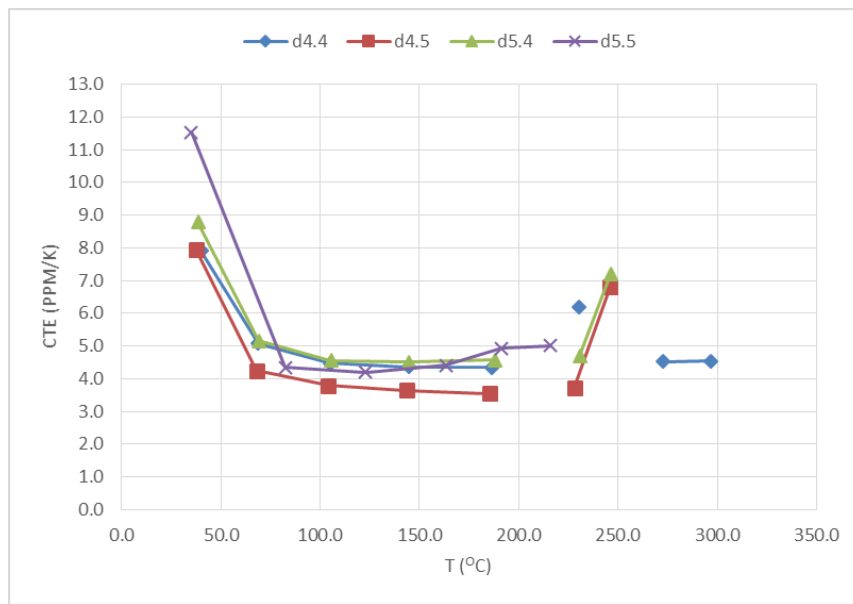


Figure 44: CTE vs T curves extracted through the procedure described above, for devices on different dies.

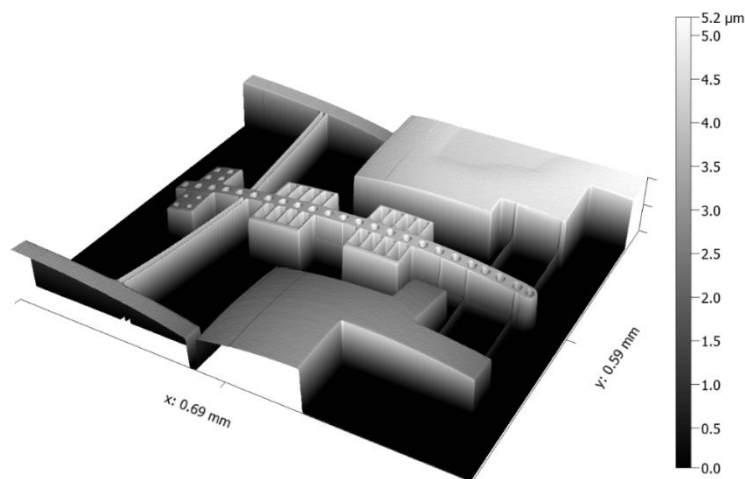


Figure 45: Sample buckling. Deformation is exaggerated for clarity. As the actuators are made from a single layer, the deformation hints to a plane stress gradient being present along the z direction, which is manifested as an apparent compressive stress.

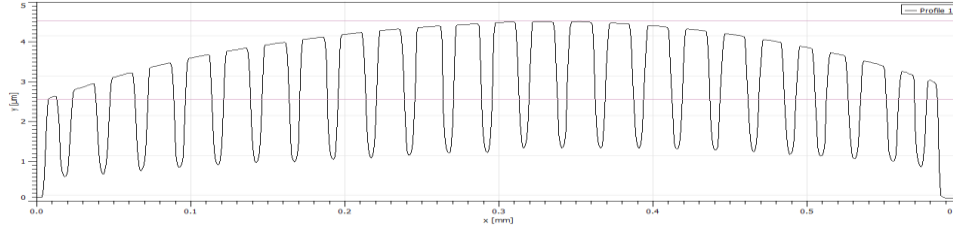


Figure 46: x-x' profile of the v-beam actuator shuttle. The height difference between maximum and minimum points is $\approx 2 \mu\text{m}$. The sacrificial layer etching holes are also visible. The structure is buckled but still suspended.

In conclusion, the method described above for *CTE* extraction gives a filtered average value of **$4.3 \pm 0.4 \text{ ppm/K}$** . It requires detailed knowledge of the device geometry and transfers the effort of determining the *CTE* from the measuring side to the modelling side. Regardless, it is a method that can be employed to produce satisfactory accurate measurements of *CTE* for multiple materials that can be deposited through chemical vapor deposition or physical vapor deposition methods.

4.7. Out of plane bimorphs

As a final step in determining the poly-SiC based bimorphs feasibility, a series of test vertical bimorph cantilevers with different lengths were fabricated. They are composed of a $\text{SiO}_2 - \text{SiC} - \text{Al/Pt}$ stack acting as “cold” layer – “hot” layer – heater, with thicknesses $2 \mu\text{m} - 2 \mu\text{m} - 200 \text{ nm}$ respectively. We chose to include Pt in our tests because it is more resistant to high temperatures than Al. Given that the *CTE* of thermally grown SiO_2 is widely reported in the literature to be $0.5 \text{ ppm/}^\circ\text{C}$ ([53]), apart from showcasing poly-SiC/ SiO_2 bimorph functionality, we can also use these bimorphs to calculate the *CTE* of poly-SiC indirectly, by measuring their deflection and temperature.

The bimorphs were actuated with two methods, depending on the metal of the heater:

- A) In the case of bimorphs with Pt heaters, the embedded heater was driven at 20 mA and 40 mA and the resistance and deflection at those two operating points were measured. The heating power applied can be easily calculated from: $Q = I_{act}^2 R(T)$.
- B) In the bimorphs with the Al heaters, the heater degraded fast (melted) and did not allow for direct actuation as with the Pt heaters. As a result, a temperature controlled chuck was used to set the temperature of the whole wafer at 25°C and 200°C , measuring the deflection at those two temperatures respectively.

A COMSOL simulation which was conducted to assess the influence of the heater layer on the structural behavior of the bimorph under heating confirmed that the metal plays only a minor role for high $t_{\text{hot layer}}/t_{\text{heater}}$ ratios, as is our case: The Pt layer adds a 6% offset to the final *CTE* estimation.

The first method allows direct deflection measurements between ‘off’ and ‘on’ states but conveys average temperature information indirectly, either through the $R(T)/R_0$ resistance ratio ($T = \left[\frac{R(T)}{R_0} - 1 \right] \frac{1}{\alpha} + T_0$) or through numerically solving a COMSOL model for the temperature profile, given heating power Q and the ambient temperature. With the second method, it can be safely assumed that the temperature of the bimorph anchor, a parameter that the COMSOL model is sensitive to ($\Delta CTE / \Delta T_{\text{anchor}} \approx 0.06$), is uniform and known. However, it is time consuming since it involves measuring the distance of the cantilever tip to the substrate every time, in order to cancel out any

vertical movement of the chuck under heating. Additionally, because of the limited heating power of the chuck, it produces smaller deflections and thus less accurate measurements. Unfortunately, the second method is the only available option in the case of SiO₂ – SiC – Al bimorphs as the Al heater cannot produce sufficient deflections without melting, something that also hints to the higher required operating temperature of the SiC-SiO₂ bimorph.

In both of these methods the deflection of the cantilevers was measured optically, using the focus control of a microscope equipped with a 20x objective lens which has a nominal 1.6 μm depth of focus (DOF) specification (Mitutoyo M Plan Apo 20X). The z-motion of the lens was done in μm steps, hence the lens DOF dominated the uncertainty. Six focus measurements were performed each time to enhance accuracy. The standard deviation of those on average was indeed ± 1.6 , corresponding to the theoretical DOF of the microscope. It is important to note that when measuring the focus position of the substrate, we chose a location away from the cantilever, as the isotropic etching of Si causes the surface underneath the cantilever to have a profile like in Figure 47. In order to maximize the sensitivity of the measurements to the CTE, the longest cantilever (700 μm long, 100 μm wide) was measured in four central dies on the wafer.

A two-dimensional numerical model was used for CTE extraction, shown in Figure 48. It includes thermal conduction, convection and the cantilevers are modeled as a circle segment with initial curvature extracted from the tip height, as measured from the focus measurements. The results are presented on Table 16.

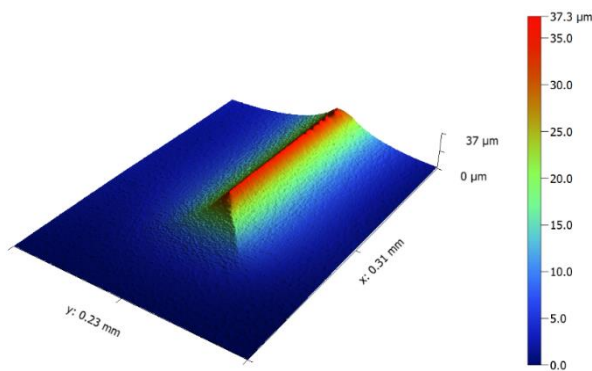


Figure 47: Substrate shape underneath the cantilever, caused by the isotropic plasma etch of Si.

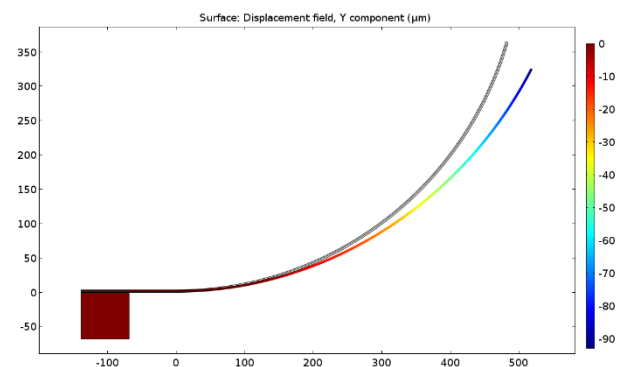


Figure 48: Numerical model of curved bimorph cantilever.

Table 16: Test out-of-plane bimorphs measurement data.

Method A, Pt	$\Delta z_{20 \text{ mA}}$	$\text{CTE}_{20 \text{ mA}}$	$\Delta z_{40 \text{ mA}}$	$\text{CTE}_{40 \text{ mA}}$
SiC 300 – SiO ₂	24.8 $\pm$ 5.8	10 $\pm$ 0.4	88.6 $\pm$ 3.8	10.2 $\pm$ 0.1
SiC 700 – SiO ₂	10.4 $\pm$ 0.9	5.56 $\pm$ 0.3	43.8 $\pm$ 3.9	6 $\pm$ 0.1
Method A, Al	$\Delta z_{20 \text{ mA}}$	$\text{CTE}_{20 \text{ mA}}$	$\Delta z_{40 \text{ mA}}$	$\text{CTE}_{40 \text{ mA}}$
SiC 300 – SiO ₂	-	-	-	-
SiC 700 – SiO ₂	-	-	-	-

Method B, Pt	$\Delta z_{T,L}$	CTE
SiC 300 – SiO ₂	$\Delta z_{100,700} = 15.6 \pm 3.1$ $\Delta z_{200,700} = 38.3 \pm 3.2$	3.0 ± 0.2
SiC 700 – SiO ₂	$\Delta z_{100,700} = 17.1 \pm 2.8$ $\Delta z_{200,700} = 40.8 \pm 4.3$	3.9 ± 0.5
Method B, Al	$\Delta z_{T,L}$	CTE
SiC 300 – SiO ₂	$\Delta z_{200,L=700} = 39.4$ $\Delta z_{200,L=600} = 24.2$ $\Delta z_{200,L=500} = 30.2$	3.75 ± 1
SiC 700 – SiO ₂	$\Delta z_{200,L=700} = 87.7$ $\Delta z_{200,L=600} = 30.2$ $\Delta z_{200,L=500} = 24.7$	4.94 ± 2

The *CTE* values extracted with method A appear to be inconsistent with the results obtained earlier. The sensitivity of the *CTE* value to the SiC layer thickness was $\approx 0.13 \text{ ppm K}^{-1} / 100 \text{ nm}$ and to the vertical displacement $\approx 0.07 \text{ ppm K}^{-1} / \mu\text{m}$. Therefore an experimental error concerning these values is crossed out as it must be unrealistically high to justify these deviations. The reason could lie elsewhere, like at the influence of the metal layer or the thermal modelling of the bimorph.

Method B appears to give more realistic estimations. First of all, the metal heater layer plays a more significant role than anticipated, as we can clearly observe an increase in effective *CTE* in the case of Al versus Pt, which has a lower reported *CTE* value than Al. Nevertheless, in both heater cases we observe an increase of *CTE* with doping. This may be because of the more silicon rich composition of SiC300, resulting in *CTE* values closer to the reported Si *CTE* value of 2.6 ppm/K . In conclusion, we note that the results of method B are more consistent with what we obtained in section 4.6 and therefore we propose it as an alternative *CTE* estimation procedure.

Thermal mechanical resonance

The fabricated bimorph cantilevers were also used to test the feasibility of reaching resonance through thermal actuation. The idea behind this is that even small periodical mechanical moments can lead the structure to resonate. These moments can be produced by thermal excitation, even at relatively high frequency, given that the heating and cooling processes follow an exponential behavior and thus fast but small ΔT is possible (Figure 49). The thermal capacity of the system along with the damping losses will determine the amplitude of these oscillations and the minimum amount of power we have to pour into the system that will lead to resonance. We describe the experiment and its results below.

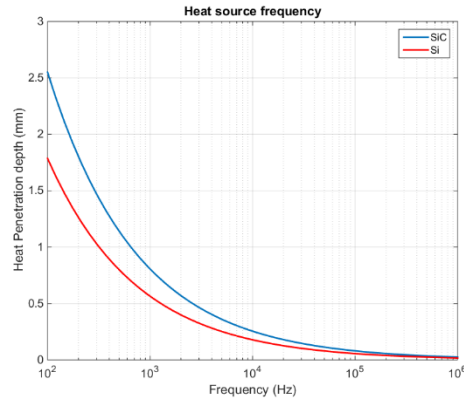


Figure 49: Given an oscillating heating source lying on the surface of a material, the depth at which small temperature oscillations are still observable before they are smudged out depends on the material specific heat, density and thermal conductivity and is roughly given by $z = \sqrt{4 \frac{k}{\rho C_p f}}$. In that regard, SiC is a better performing material than Si. From this graph we can see that heat based kHz actuation is, at least, theoretically possible for sub mm structures.

First, the oscillation modes and frequencies of the longest bimorph with Pt heater ($L = 700\mu\text{m}$) were calculated through a COMSOL 2D model (Figure 50). Next, an Agilent 33220A function generator was used to supply a sinusoidal signal to the simple cantilever bimorph actuator. We swept the frequency from low to high and monitored the motion of the actuator tip through a 20x objective magnification lens. The voltage source was set to account for the wire and contact resistance. The results are presented in Figure 51.

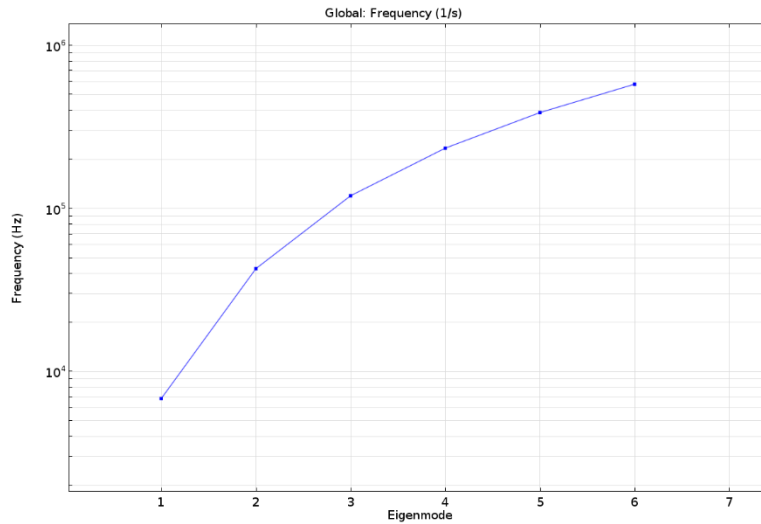


Figure 50: Resonance frequencies for each oscillation mode predicted by the COMSOL 2D model. First mode resonance is predicted at 6.83 kHz.

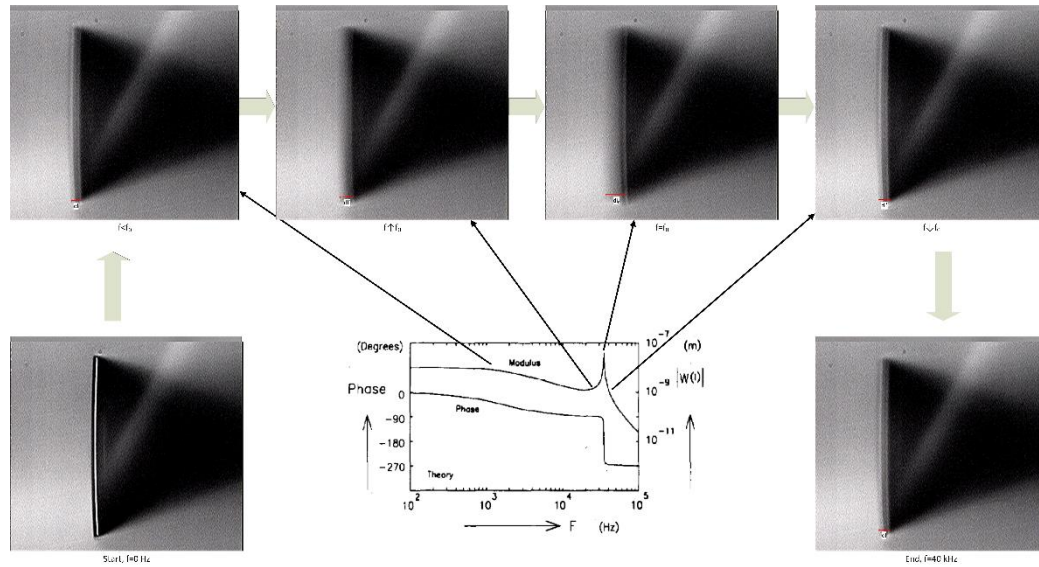


Figure 51: Result of the experiment to determining the feasibility of achieving mechanical resonance through thermal excitation. The frequency response graph is typical for an electrothermal actuator and is taken from [54]. From bottom left to bottom right: $f = 0$ (stationary) – oscillating cantilever for $f < f_0$ (it appears oscillating/blurry because the camera was not focused on the tip) – oscillating cantilever for f in resonance proximity – oscillation at resonance – oscillation at proximity of resonance – $f > f_0$ (tip appears stationary).

Beginning the sweep, we observed a noticeable drop in the amplitude of oscillations at 50 Hz and a complete recession of oscillations at 1.5 kHz, where the bimorph assumed a seemingly steady displacement equivalent to the V_{rms} value. Therefore we assume that the thermal cutoff frequency is $f_{th} = 50$ Hz. The first resonance mode was achieved at 6.89 kHz for sinusoidal input and 6.93 kHz for square wave input, where, not only did the oscillations resume, but also the displacement amplification due to resonance more than compensated the drop in amplitude due to the low thermal cutoff frequency. The measured resonance frequency showed good agreement with the simulated first resonance mode (Figure 50), for the material parameters extracted in this chapter. Continuing the sweep, we attempted to reach higher resonance modes without success. The reason for this is that the heater heats the whole surface area and thus an expansion force is distributed throughout the “hot” layer. This biases the structure to move only in the first resonance mode shape, whereas higher resonance modes are super harmonics of the same motion. Therefore, in order to achieve higher modes, we should set up the structure to move in the shape of the mode we intend to select.

In Table 17 we present the fundamental resonance frequency observed for different input voltages. During operation, the bimorph cantilever was biased to oscillate around the displacement of the V_{rms} value of the electrical input. A small shift to lower frequencies is observed for decreasing actuation voltage. The amplitude of the oscillation was optically assessed and it was also found to monotonously decrease with voltage. This behavior is expected, as the stiffness of the bimorph depends on the displacement of its tip, with higher cantilever deflection causing an increase in its apparent stiffness, which in turn shifts the resonance frequency to higher values.

Table 17

V_{pp} (V)	1.7	1.3	1	0.8	0.6	0.4	0.2
f_0 (kHz)	7.06	6.96	6.89	6.84	6.82	6.8	-

4.8. Conclusion

The properties of polycrystalline poly-SiC make it an appealing option for use in bimorph actuators. It has high Young modulus (≈ 250 GPa), mediocre TCR (-0.0018 1/K), acceptable CTE (4.3 ppm/K), ability to work in very high temperatures ($600^\circ\text{C} - 800^\circ\text{C}$), high thermal conductivity (360 W/mK) and perhaps most important of all, can be deposited conformably in trenches, which allows us to build in-plane actuators. We have observed that in doped SiC, the Young modulus is decreasing with increasing DCS/ACS ratio while the residual stress decreases. DCS/ACS ratio also seems to influence the CTE parameter, although more investigation on this should be carried out before any definite conclusions can be drawn. Doping can be adjusted to produce resistivities in the range of $10^2 - 10^{-3} \Omega\cdot\text{cm}$. Thus we can achieve a wide range of mechanical and electrical properties by adjusting the deposition conditions of the layer and therefore tailor it to the application at hand.

Concerning the application of SiC in bimorphs, experiments showed that mechanical resonance is easily achievable through thermal actuation, even though the thermal domain is inherently slower. Therefore we conclude that SiC based bimorphs are feasible and can perform adequately in the role of MEMS scanner actuators. In the next chapter we propose a novel, high temperature bimorph actuator design.

First generation device failure post-characterization analysis

When surpassing their yield strength σ_F under static loading, brittle materials like silicon, silicon carbide and ceramics in general, fracture rather than plastically deform. Fracture can occur even for stress values below the material theoretical σ_F , as defects such as dislocations and irregularities in the geometry of a structure cause high stress concentrations which accelerate crack growth. Therefore it is no surprise that the strength value of brittle materials shows significant scatter in the literature, showing dependence on defect size, loading mode, specimen size, orientation and test method. Values vary between 1 GPa – 20 GPa for crystalline silicon, with a value of 7 GPa typically reported ([55]), and 3 GPa – 5 GPa for its polycrystalline counterpart.

A way to deal with this variation is to study fracture in a probabilistic framework and model the fracture probability as a weakest-link, Weibull distribution ([56]). An alternative to this is to first determine the material fracture toughness K_{IC} , which is the critical value of the stress intensity factor¹ in mode I loading K_I at a crack tip², required for the crack to begin to grow and propagate to fracture, as described in [58]. K_{IC} values are independent of the microstructure and dopants of the material and thus appear in the literature with much less variation (see table Table 18). Knowing K_{IC} , we can calculate σ_F as a function of the critical defect (crack) size through the equation: $K_{IC} = Q\sigma_F\sqrt{\pi a_c}$, where Q is a geometric factor close to unity and a_c is the critical crack size. A plot of this for single crystal Si and polycrystalline 3C-SiC is shown in Figure 52.

¹ The stress intensity factor K is a concept used in fracture mechanics to predict fracture of brittle materials. For a detailed discussion the reader is invited to read Chapter 9 in [61].

² Loading modes are different ways to apply stress to an existing crack in a structure. There are three linearly independent cracking modes that are commonly used in fracture mechanics. Mode I corresponds to directly “opening” a crack by pulling the crack surfaces apart and is the most common load type encountered in engineering design. The stress intensity factor for mode I is designated as K_I .

Table 18: Fracture toughness of several materials. Data is extracted from [57], [58] and [60].

Material	K_{Ic} (MPa m ^{1/2})
Si (single crystal)	0.7 – 1.3
Si (polycrystalline)	1
3C-SiC (polycrystalline)	2.8 – 3.4
SiO ₂	0.8 – 1
Aluminum	14 – 28

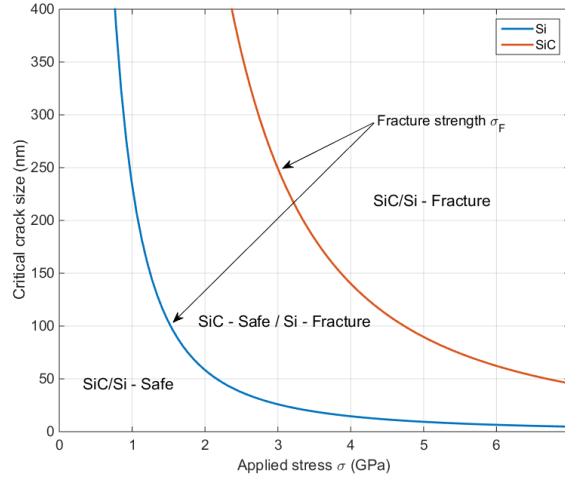


Figure 52: Fracture (yield) strength, as a function of the critical crack size. SiC is theoretically more robust as, given a crack of size a_c , it can tolerate much higher applied stress. Inversely, given a fracture strength value, it can tolerate much bigger cracks before fracture.

Nevertheless, in crystalline Si MEMS structures, failure stresses of as low as 300 MPa are reported, which is significantly lower than even the most conservative estimates for Si yield strength. The cause of this discrepancy was tracked down by Sooriakumar et al. ([59]) to the introduction of sharp edges from anisotropic dry etching, which act as strong stress concentrators. Environmental conditions such as humidity also play a role to reduce single crystal K_{Ic} through the thickening of the native oxide. Furthermore, micron-scale Si thin films show susceptibility to delayed fracture under cyclic loading (fatigue) caused by the native oxide. Early fracture can occur at $\approx 50\%$ of the fracture strength and therefore an oxide formation suppression layer may be called for ([58]). On the other hand, such corrosion effects are not observed on SiC as it practically does not form a native oxide ([60]).

A post-characterization examination of the stress distribution of the 1st generation device immediately after release is necessary to assess its risk of fracture. The material parameters were set to the values extracted from the sections above. We assumed the worst case scenario in which the stress after release of the device has already led to crack formation. First, we used the von Mises yield criterion as an indicator of location of those initial cracks. Subsequently we studied whether those formed cracks posed a fracture risk to the device. For this, we calculated the stress intensity factor K through the J-integral method ([61]) and compared it with the corresponding material fracture toughness.

As can be seen in Figure 53, stress after release of the device concentrates on regions with sharp corners: A) at the connection to the anchor and B) at the point where SiC is joined with Si to make the bimorph. In both cases, we have noted possible cracking trajectories, following the direction of

maximum stress. In region A, the stress intensity for one corner was found to be $\approx 0.61 \text{ MPa m}^{1/2}$ which is below Si fracture toughness. If, however, we take into account both corners at the same time, the stress intensity K becomes $\approx 1.08 \text{ MPa m}^{1/2}$ which is a value that can result in fracture. In region B, in the side of SiC, $K \approx 3.4 \text{ MPa m}^{1/2}$ which corresponds to fracture of SiC. In the Si side, $K = 2.78 \text{ MPa m}^{1/2}$, which also means fracture.

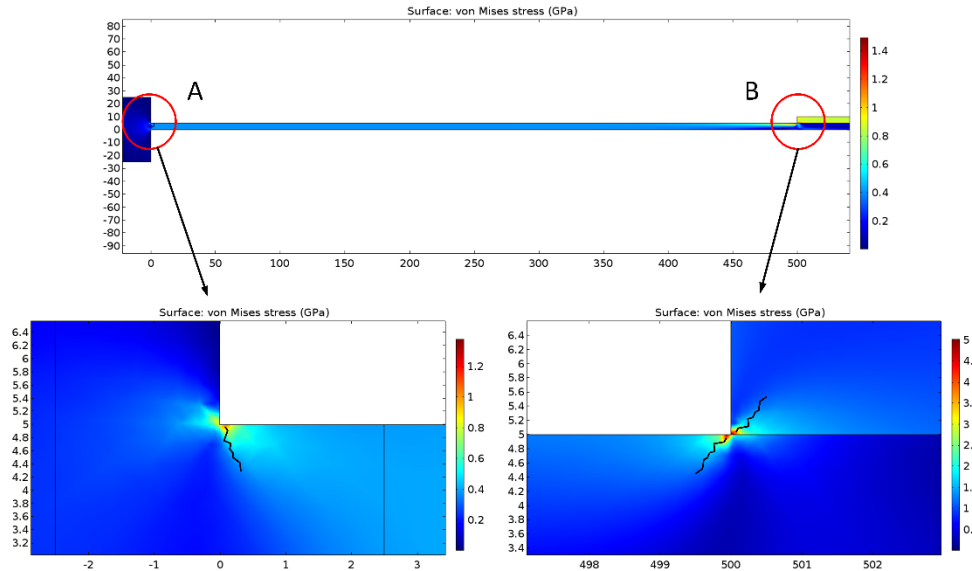


Figure 53: Fracture analysis of the fixed-fixed actuator. The structure is symmetric, thus only one side is shown. High stress concentrations can lead to crack creation and subsequently fracture.

Only one out of all these fracture possibilities is enough to cause breaking of the actuator, with the exception of SiC fracture, which will plausibly only cause a loss of electrical contact. The fact that we observed fracture predominantly near the anchor region indicates that the crack propagation there is faster. Once fracture has occurred, stress redistributes and thus no further fracturing occurs. We should also note that this analysis has not taken into account any sharp edges on the Si layer, caused by the dry etching process. This will further exacerbate the fracture danger. Therefore we conclude that this design suffers from high fracturing danger due to stress concentration areas caused by sharp edges.

5. Improved in-plane thermal actuator design

5.1. Basic design architecture

A new architecture for a bimorph actuator is proposed that aims at preventing the shortcomings of the original design and builds on the strengths of SiC. The top view of the spring bimorph is seen in Figure 54. The SiC “hot” layer is placed on the inside (concave), whereas the “cold” SiO₂ is on the outside (convex) surface. The SiC layer also acts as a heater, as it can be doped to acceptable resistivity levels. The initial thermal tensile stress in the SiC layer will cause the spring geometry to contract in the structure axial direction, where it is most compliant. Increasing the temperature will cause the bimorph to assume its original shape, effectively producing an in-plane displacement on the y direction at the actuator tip. The structure anchor is placed on the side in order to cancel out any offset in the perpendicular direction. Reversing the layer order will result in the opposite offset behavior, making this actuator suitable for push-pull architectures. The spring structure can be contacted directly with a high temperature resilient metal, such as platinum, or indirectly by extending a SiC connection over the anchor and using aluminum for a contact there, on this safe, low temperature region.

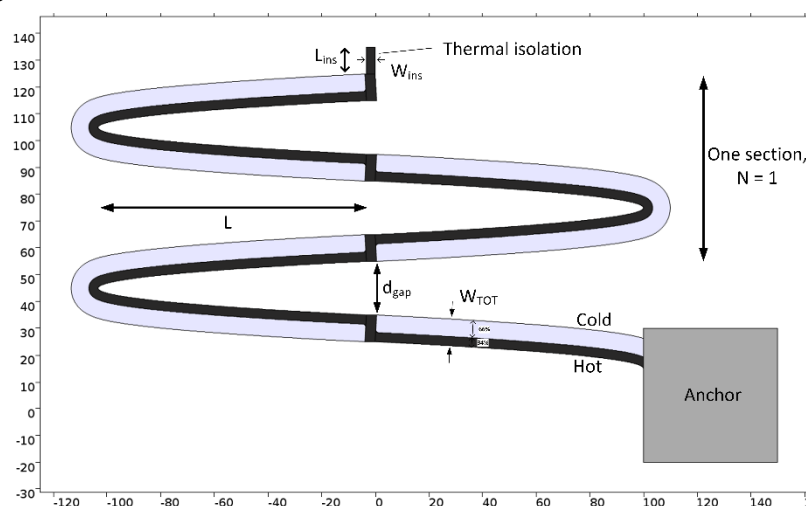


Figure 54: Top view of spring actuator. SiC is the dark layer, while SiO₂ is the white layer.

To quickly evaluate the spring bimorph in relation to the previous fixed-fixed design (Figure 55), a figure of merit that takes into account only static characteristics was defined as: $FOM = \frac{d_{max}}{A_{eff}}$, where d_{max} is the maximum zero load tip displacement and A_{eff} is the effective top view area occupied by the actuator. Comparing the proposed spring actuator to the original fixed-fixed design yields a $\approx 2.3x$ increase in this FOM, under identical actuating conditions¹. A discussion on the motivation of our general design choices follows.

The spring geometry maximizes the thermal stress generating contact area between the “hot” and “cold” layers in a given volume and it is also scalable, which allows it to be tailored to the specific displacement requirements at hand (for example a configuration with 8 sections is shown in Figure 55). Moreover, it is compliant in the x and y directions and it has at least two resonance modes along

¹ Sweeping the design parameters, such as the number of sections, led to only a very minor change in the FOM, and that positive. Hence the comparison is valid for the two designs in general and not just for one instance of their parameters.

the direction of motion. The structure can act as a support for loads, especially in push pull configurations, because of its high stiffness in the z direction. In order to avoid stress concentration we have minimized sharp angles wherever possible. Lastly, many devices can be placed in a folded fashion, laterally close to each other, allowing us to scale the applied force if necessary.

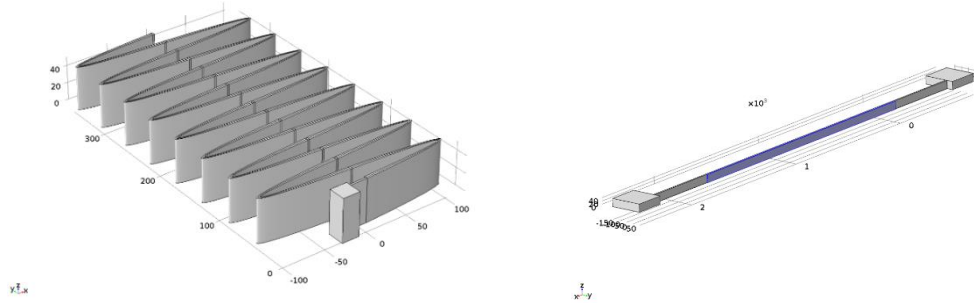


Figure 55: 3D model of the proposed spring actuator in 8 sections configuration (left) and the original fixed-fixed design with the SiC layer highlighted.

5.2. Performance

An analysis of the influence of the design parameters L , d_{gap} , number of sections N and W_{total} on static and dynamic characteristics is presented below. For the rest of this chapter we will use the abbreviations shown in Table 19. The COMSOL models are two dimensional to reduce the computation time, as a comparison between 2D and 3D models showed negligible differences. They include thermal conduction and convection and SiC material parameters as measured in the previous chapter. The maximum actuation temperature was set to $T_{max} = 600^{\circ}\text{C}$. The geometric parameters are set to $L = 100\text{ }\mu\text{m}$, $W_{TOT} = 10\text{ }\mu\text{m}$, $d_{gap} = 20\text{ }\mu\text{m}$, height $h = 50\text{ }\mu\text{m}$, insulation thickness $W_{ins} = 10\text{ }\mu\text{m}$ and $N = 8$ unless otherwise stated.

Table 19

Parameter	Notation
Displacement	D
Force	F
Average von Mises stress	σ
Max von Mises stress	σ_{max}
Area	A
Frequency	f_0
Time to D_{max}	t_{on}
Time to 0	t_{off}

Static

The static simulations show the ultimate capabilities of the actuator concerning the maximum displacement and force and we will, therefore, start our discussion from there. When the device is activated, the temperature increases and hence the stress, which is primarily composed by the thermal component as described in 4.2, reduces. Thus the device is in most danger from breaking at

rest and therefore, the stress experienced at this state is a good indicator of device yield. In the plots below, we analyze the impact of the main geometric parameters on D , F , σ and σ_{max} . Finally, we examine the thermal profile of the device as a function of the thermal insulation thickness at the tip and the actuating current.

The straight regions in the spring act as a displacement amplifying mechanism and hence it is logical that the displacement is mostly influenced by L (Figure 56). Concerning F , increasing L triggers two counteracting mechanisms: The stiffness of the spring decreases but the force generating area increases. The output force as seen in Figure 57 appears to decrease with increasing L and thus we assume that stiffness plays a more major role. Similarly, the number of sections N sets the length of the spring actuator and thus higher N translates to less stiff actuator but more force generating area (Figure 59). As expected it also leads to higher displacement (Figure 58). The total thickness W_{TOT} is a bit more different: higher W_{TOT} is associated with higher stiffness but not higher contact area between the layers. Thus we expect it to be inversely proportional to D but proportional to F , which is indeed observed in Figure 60 and Figure 61.

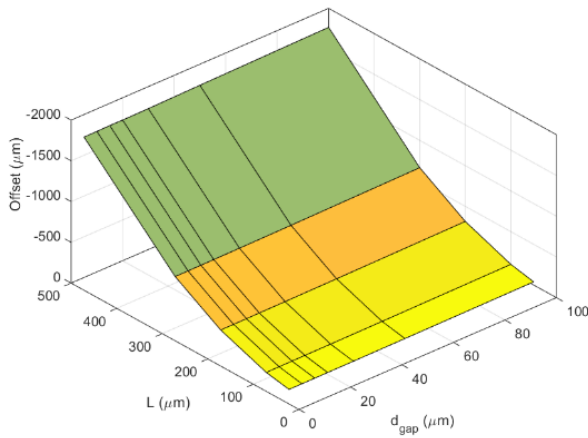


Figure 56: D vs L , d_{gap} . Offset is most sensitive to L .

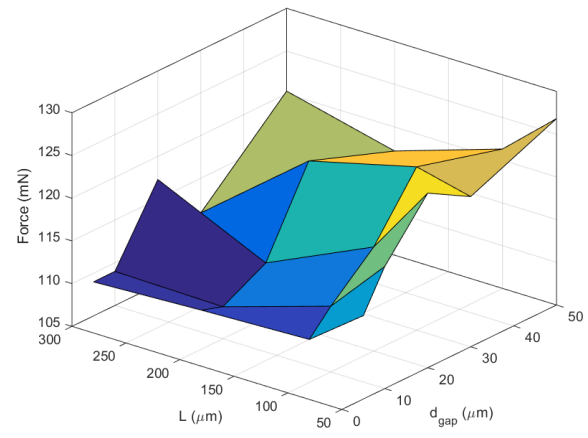


Figure 57: F vs L , d_{gap} . The behavior is more complex but it still shows an inverse relation between L and F .

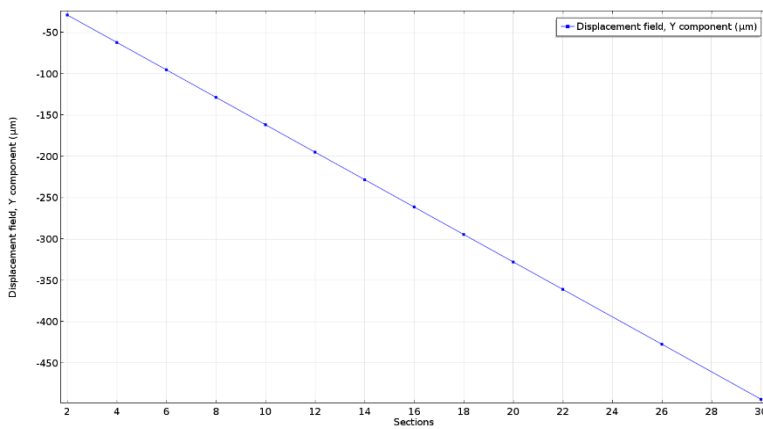


Figure 58: D vs N . The spring actuator shows linear offset scaling with the number of sections.

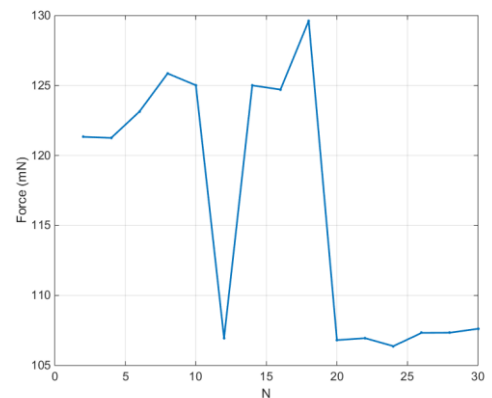


Figure 59: F vs N . The simulation includes non-linear effects such as stress stiffening which may explain the fluctuations.

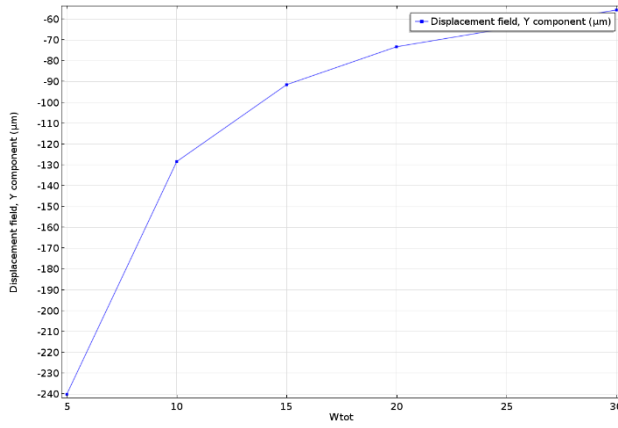


Figure 60: D vs W_{TOT} . Higher W_{TOT} means stiffer structure but more robust actuator and easier fabrication.

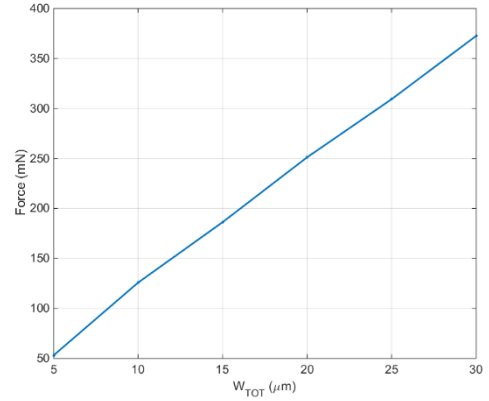


Figure 61: F vs W_{TOT} .

On the other hand, d_{gap} showed no consistent correlation for F or D . However, it appears it has a more prevalent role in the distribution of stress: higher values are associated with higher average stress in the actuator while smaller values mean higher stress concentration at the spring curves (Figure 62 and Figure 63). Therefore an intermediate value is best. L appears to play a conditional role, showing correlation only above or below certain values of d_{gap} for σ and σ_{max} respectively.

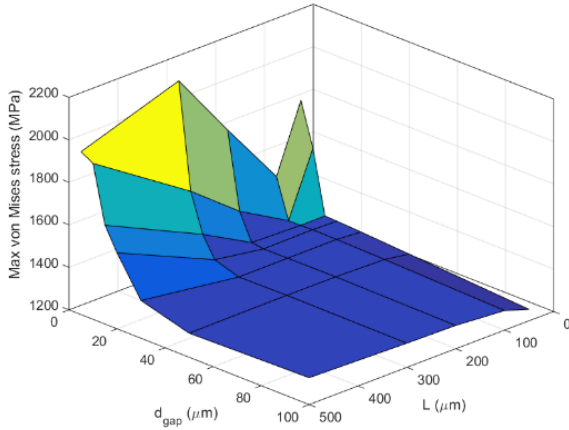


Figure 62: σ_{max} vs d_{gap} , L in the actuator at rest. Areas with high curvature and sharp contacts show highest stress concentrations. Therefore, d_{gap} is unsurprisingly the most influential parameter in that regard.

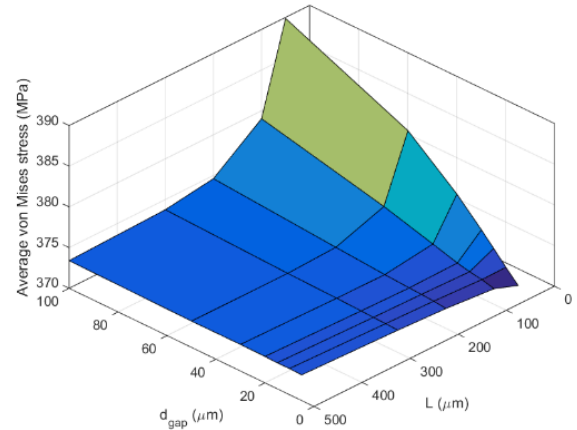


Figure 63: σ vs d_{gap} , L at rest. The average von Mises stress in the actuator is increasing slightly the more the actuator is "stretched" in the axial direction.

It is apparent that we have three parameters (L , N , W_{TOT}) that all influence the actuator D , F and A in a similar way. It is thus a question of how D , F and A scale with each one and whether there are other factors that weigh in the decision for each parameter. Table 20 presents the correlations we have found from our simulations. Based on this, we have decided to first fix W_{TOT} to 10 μm , taking into account the device fabrication difficulty and F requirement. Subsequently we set N to 20 to get the required D of 250 μm . L and d_{gap} are set to 100 μm and 20 μm respectively, keeping in mind primarily to keep a small device footprint and avoid excessive stress buildup. The parameters of choice are summarized in Table 21.

Table 20

	D	F	A	$\sigma_{\max}$	σ
L	s^2	s^{-x}	s	-	$d_{\text{gap}}s^2$
N	s^2	-	s	-	-
W_{TOT}	s^{-1}	s	-	s^x	s^{-x}
d_{gap}	-	-	s	s^{-x}	LS^x

The insulation thickness W_{ins} can have a pronounced effect on the thermal behavior of the spring. Assuming that the load has very large thermal capacity compared to the actuator, its temperature will remain relatively unchanged during the actuator 'on'/'off' cycles. Thus good thermal insulation is necessary for fast response and performance stability. Ideally the thermal insulation should be made out of SiO_2 but in order to keep fabrication simple, SiC with reduced contact area may be used as well. Therefore, SiC only contacts the platform at the top of the structure and has a width of $W_{\text{ins}} = 3.5 \mu\text{m}$. The effect of varying insulation length is seen in Figure 64. Compared to just tweaking the actuation current, which increases the maximum temperature, changing the insulation length causes the maximum temperature also to shift towards the tip, which resembles more the case of perfect isolation. Therefore, even though the temperature distribution becomes less uniform, the average temperature itself increases, which translates to improved performance. A value of $L_{\text{ins}} = 20 \mu\text{m}$ is selected to achieve good thermal isolation and load bearing ability.

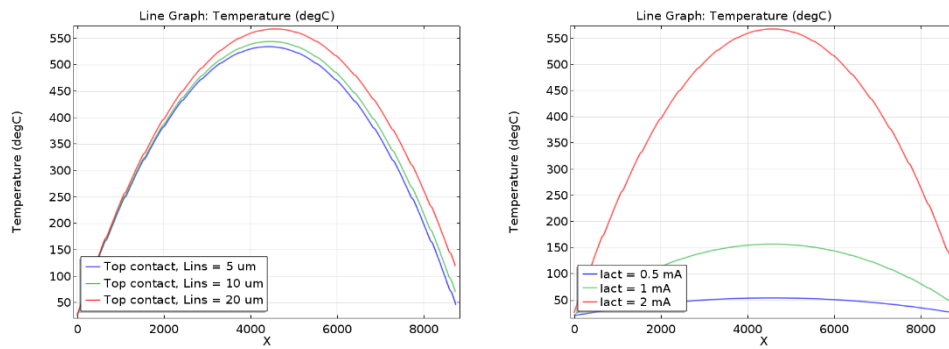


Figure 64: Temperature profile along the local x coordinate of the "hot" layer, from the anchor point ($x = 0$) to the load bearing tip ($x = 8728 \mu\text{m}$). The first graph (left) demonstrates the behavior of varying L_{ins} whereas the second graph (right), of varying current.

Table 21: Overview of design choices and their justification.

Parameter	Value	Reason
L_{ins}	$20 \mu\text{m}$	Tradeoff between load bearing ability and thermal insulation.
W_{ins}	$3.5 \mu\text{m}$	Tradeoff between load bearing ability and thermal insulation.
L	$100 \mu\text{m}$	Relatively small lateral dimension to limit the area footprint.
d_{gap}	$20 \mu\text{m}$	Tradeoff between stress concentration and area footprint.
W_{TOT}	$10 \mu\text{m}$	Thicker layers have higher force output, are more robust and easier to fabricate.
N	20	Number of sections set to reach the displacement requirement.

For these parameters, each actuator has a 0.3 mm^2 area footprint and a maximum static power consumption of 43 mW. Its maximum displacement is estimated at $250 \mu\text{m}$ and its maximum force at 80.9 mN.

Dynamic

The actuator in-plane resonance modes without any load all lie above the 10 kHz range, as can be seen in Figure 65. The second resonance mode is of interest as it is the lowest frequency mode in the direction of motion we are aiming for (Figure 66). We should note that when the actuator is deployed in a platform, we can promote the second resonance mode to first, simply by making it harder for the whole structure to move in the direction of the first mode (increase the direction stiffness by placing extra springs), while making sure the stiffness of the second mode motion stays relatively unchanged. Nevertheless, we should keep in mind that, in these scales, the thermal domain is associated with larger time constants than the mechanical domain (t_{on} , t_{off} in Figure 67) and thus is ultimately the limiting factor in any frequency related discussion.

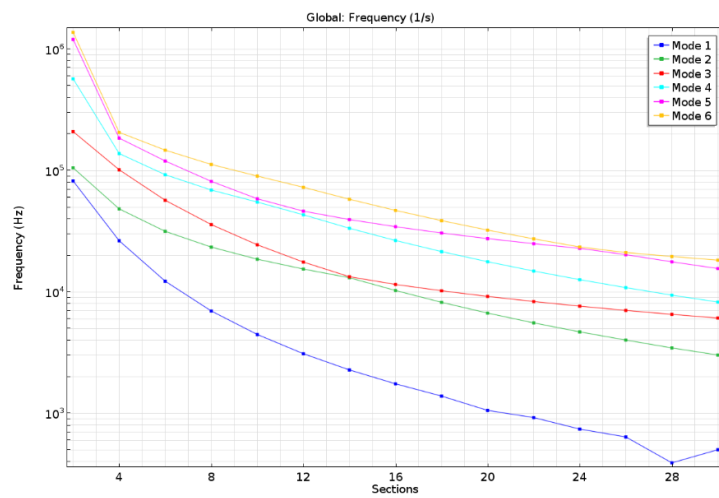


Figure 65: Resonance frequencies as a function of N , for different eigenmodes of oscillation.

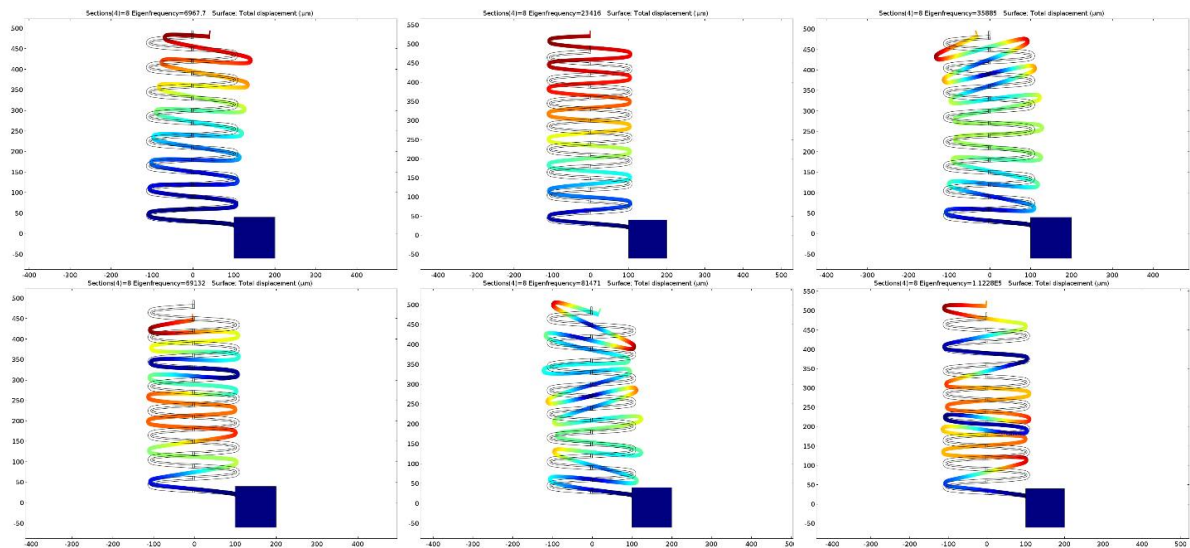


Figure 66: Typical oscillation modes from lowest (natural) to highest - top left to bottom right-.

The time it takes to achieve full extension, t_{on} , and to return to the rest position, t_{off} , is shown for $N = 20$ in Figure 67. The actuation current was set to 2 mA. Slow temperature change is the underlying reason behind the slow reaction times. Overdriving the actuator temporarily with a larger current (for example through a capacitor bank) can accelerate the extension phase at the expense of power consumption.

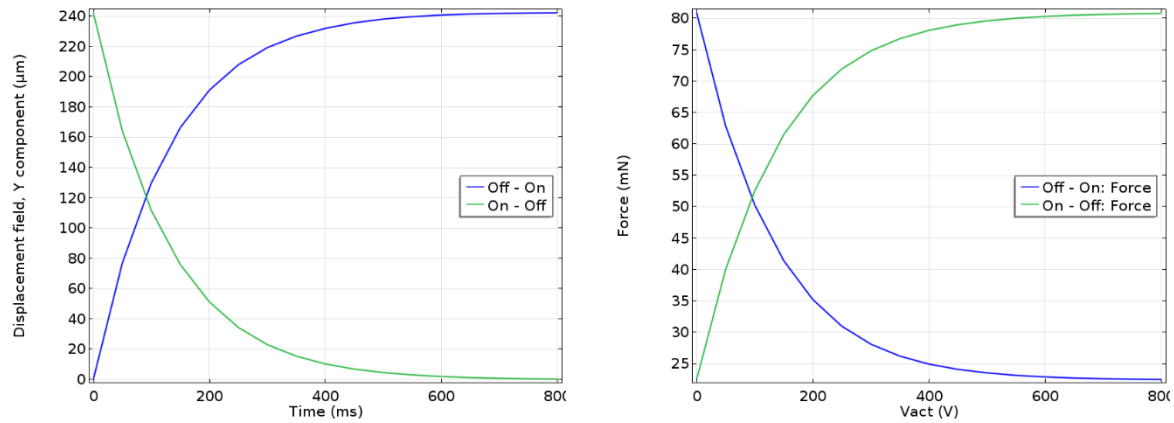


Figure 67: D vs t (left) and F vs t (right), starting from rest position and extended position respectively. The thermal time constants dominates the system.

5.3. Platform architecture

Two proposed platform structures are shown in Figure 68 and Figure 69. In the first platform, an electrical connection is placed at each side of the platform to actuate both push-pull actuators per side. These actuators have inverse layers so that they produce the opposite displacement for the same input. Embedding this platform to another one in a nested fashion can upgrade its translational and rotational capabilities.

A reduced footprint platform is shown in Figure 69, in which, however, the actuators have to overcome the resistance of the actuators on the sides which are perpendicular to the direction of motion. The spring structure is also compliant in this direction and thus this configuration can also produce the desired motions, albeit with less efficiency.

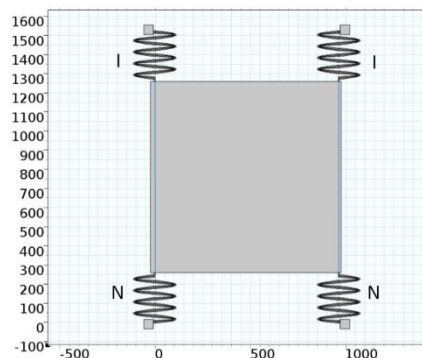


Figure 68: Platform architecture for 1D motion and rotation. It can be expanded in a nested fashion to obtain 2D motion. Inverse actuators are placed opposite to normal actuators. Metal lines transverse the platform to electrically connect the actuators.

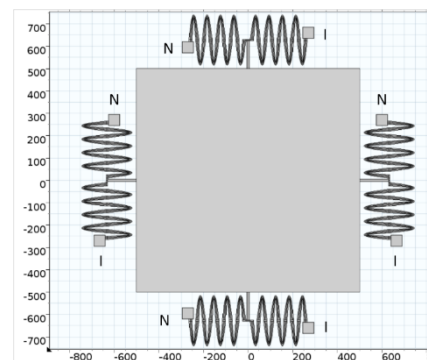


Figure 69: Platform architecture with 2D motion and rotation capability.

5.4. Fabrication

A proposed simplified fabrication flow is presented below. The process begins with the definition and etching of trenches which will act as a mold for the SiC layer (A). The requirement of the aspect ratio for the trench is relaxed as thermally grown SiO₂ will further narrow down the trench width to the desired dimension prior to LPCVD in-situ doped SiC deposition (B). In the next step, the structure is striped from the surrounding Si (C) and any residual SiO₂ left on the SiC outer wall is removed by an immersion to a buffered HF solution (D). Thermal oxide is again grown until the remaining Si part is consumed on the bimorph pillar (E). SiC has a much lower oxidation rate than Si in the same conditions ([62]) and thus it is protected during this step. Finally, the SiO₂ is anisotropically etched to reveal the substrate (F) and the device is released by an isotropic Si plasma etch (G). The flow utilizes only four lithography masks (including platform definition and metallization).

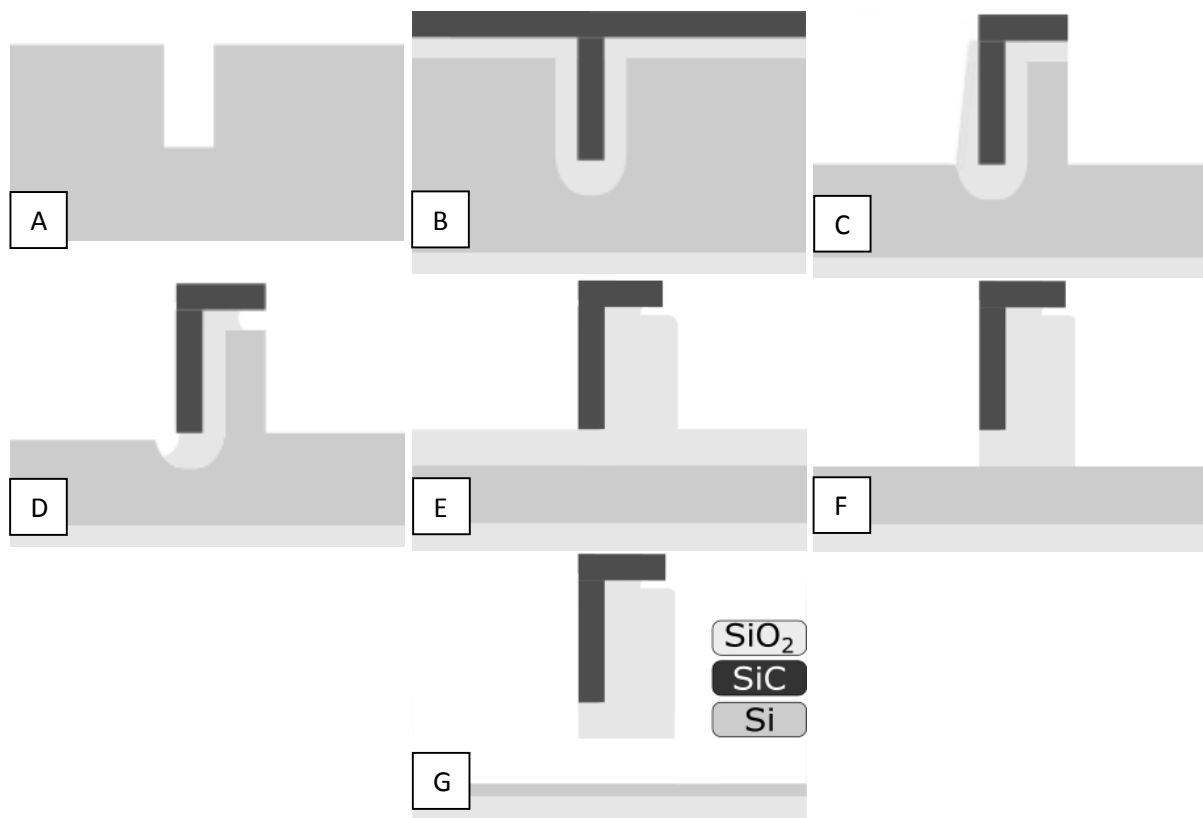


Figure 70: Proposed fabrication flow for the spring bimorph actuator. Substrate thickness is not to scale.

6. Conclusion and Future Work

6.1. Conclusion

In this thesis we reviewed the available MEMS actuator literature for a micro-actuator suitable for OCT applications and we concluded that bimorph electrothermal actuators show the highest promise. Next, we presented a characterization procedure to determine a material properties that are relevant for bimorph actuators and we applied said technique to the characterization of polycrystalline, in-situ doped, LPCVD SiC to evaluate it as potential “hot” material for use in bimorphs. Lastly, we proposed a bimorph design that is based on SiC and fulfills the requirements for deployment in OCT probes.

The contributions of this thesis can be summarized as below:

- a) Characterization of Young modulus, *CTE* and *TCR* of doped polycrystalline SiC.
- b) Refinement of EPI method for accurate cantilever geometry.
- c) Evaluation of material characterization based on simple test structures and automatic streamlined characterization procedure with robust data analysis.
- d) Comparison of different ad-hoc material characterization methods for *CTE*.
- e) Fabrication and rudimentary characterization of basic poly-SiC/SiO₂ based cantilever bimorph.
- f) Actuator design recommendation of poly-SiC/SiO₂ based bimorph that is based on the strong points of SiC.

6.2. Future Work

The following tasks are left to be pursued in the future:

- The implementation and characterization of the proposed design.
- Explore further miniaturization of the SiC bimorphs, essentially giving up large displacement for faster response time. Decreased dimensions will reduce the system heat capacity, surface/volume ratio and mass, resulting in smaller thermal and mechanical time constants. It would be interesting to examine the relative decrease in time constants with the prospect of thermally driven multi-mode mechanical resonance. Furthermore, a large number of bimorphs in-parallel are predicted to reduce total system variation as mismatches caused by fabrication inaccuracies between individual bimorphs will cancel out.
- A thorough study of the thermal resistance across boundaries could lead to improvements in bimorphs, as a temperature gradient has effectively the same effect in strain generation as a difference in *CTE* between the “hot” and “cold” layers. Multilayer stacks comprising of nanometer thick poly-Si, Si₃N₄, SiO₂ or amorphous SiC could enhance the thermal contact resistance and introduce a significant ΔT between “hot” and “cold” layers, which could be verified, for example, with spatial temperature mapping through Raman scattering ([63]). Thus this direction of research is promising and is left for further research.
- Further optimization of the proposed spring geometry can yield better results, in the static as well as the dynamic domains. For example a minimization of the sources of damping

can yield better resonance behavior and an optimization of the spring height can give us a more uniform temperature profile.

- The proposed vertically-aligned bimorph concept can be applied in v-beam electrothermal actuators as well. Bimorphs can substitute the “hot”, expanding v-beams for a potential increase in achievable displacement. This idea warrants further investigation in future iterations of this work.

Appendix

A. Fabrication flows

Cantilevers – V-beams: Combined process flow

Zero layer

Device and σ wafers

1. PHOTORESIST COATING

- Machine: EVG 120 wafer track
- Humidity 48 ± 2 %
- Temperature 21 ± 0.5 °C
- Coating recipe: Co – 3012 - Zero Layer
- Description:
 - i. Thermal treatment with HMDS (hexamethyldisilazane) vapour with nitrogen as a carrier gas at 135 °C for 90 seconds.
 - ii. Cooling down to 20 °C.
 - iii. Spin coating with Shipley SPR#3012 positive photoresist.
 - iv. Soft bake at 95 °C for 90 seconds.
- Final resist thickness: **~1.4 μ m**.

2. LITHOGRAPHY FOR ALIGNMENT MARKERS

- Machine: ASM PAS 5500/80 automatic wafer stepper.
- Mask: COMRUK
- Job: FWAM
- Energy: 120 mJ/cm²
- Focus: 0 μ m

3. DEVELOPMENT

- Machine: EVG 120 wafer track
- Humidity 48 ± 2 %
- Temperature 21 ± 0.5 °C
- Development recipe: 1-Dev - Single Puddle
- Description:
 - i. Post exposure bake at 115 °C for 80 seconds.
 - ii. Cooling down to 20 °C.
 - iii. Spin development with Shipley MF322 developer - single puddle process - 57s
 - iv. Hard bake at 100 °C for 90 seconds.

4. INSPECTION: PHOTORESIST OPENINGS

Visual inspection of the wafers through a microscope: Check the line width of the marks - no resist residues are allowed.

5. PLASMA ETCHING OF CONTACT ALIGNMENT MARKS

- Machine: Trikon Omega
- Temperature: 20 °C
- Etching recipe: URK_NPD

Table 22: Step parameters.

Step	1. breakthrough	2. bulk etch
Pressure	5 mTorr	60 mTorr
Platen RF	60 W	20 W
ICP RF	500 W	500 W
CF4	40 sccm	0
O2	20 sccm	0
Cl2	0	80 sccm
HBr	0	40 sccm
Time	00:10	00:40

6. PLASMA PHOTORESIST REMOVAL

- Machine: Tepla stripper
- Recipe: recipe 1
- Description: Removing photoresist in an oxygen plasma system. Standard resist strip with endpoint detection and 2 minute overetch.
- Oxygen concentration rate: 400 ml/min
- Pressure: 0.8 mbar
- Power: 1000 W

7. REFERENCE WAFER BOW MEASUREMENTS

Only σ wafers

- Tool: Tencor Flexus
- Description: Create separate file for each wafer. Measure each wafer twice with the primary flat oriented first down and then right. Repeat each measurement 5 times.

8. WET WAFER CLEANING AND DRYING

- Wet bench HNO3 (99%)
 - i. 10 minutes in fuming nitric acid at ambient temperature.
 - ii. Rinse in the Quick Dump Rinser with the standard program until the resistivity is 5 M Ω or until time is 5 minutes.
- Wet bench HNO3 (65%)
 - i. 10 minutes in fuming nitric acid at ambient at 110 °C.
 - ii. Rinse in the Quick Dump Rinser with the standard program until the resistivity is 5 M Ω or until time is 5 minutes.
- Semitool "rinser/dryer"
 - Standard program in the white carrier with red dot.
 - *Note: Red dot - silicon, black dot - green metals

Si

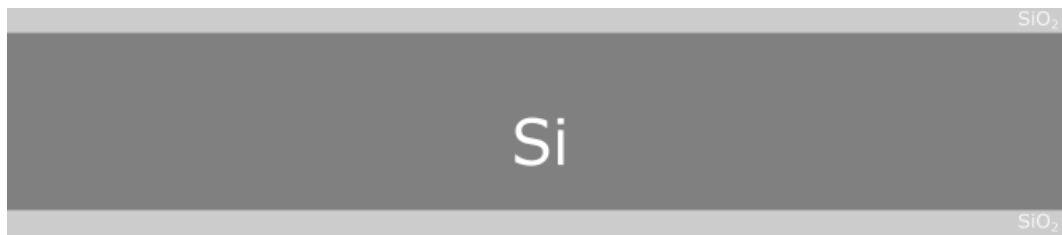
Sacrificial Layer

Device wafers

9. WET OXIDATION

- Furnace: Tempres C1
- Time: 13h:00:00
- Temperature: 1100 °C
- Recipe: WETOXID

- Target thickness: ~2500 nm



10. THICKNESS MEASUREMENT

- Tool: Leitz MPV-SP
- Recipe: Th. SiO2 633nm
- Description: Thermal oxide thickness measurement for thicknesses greater than 50 nm. Note down the results on **Error! Reference source not found..**

11. REFERENCE WAFER BOW MEASUREMENTS WITH SiO₂ ON BOTH SIDES

- Tool: Tencor Flexus
- Description: Create separate file for each wafer. Measure each wafer twice with the primary flat oriented first down and then right. Repeat each measurement 5 times. Substrate thickness is 525 µm.

12. WET WAFER CLEANING AND DRYING

All wafers

- Wet bench HNO₃ (99%)
 - 10 minutes in fuming nitric acid at ambient temperature.
 - Rinse in the Quick Dump Rinser with the standard program until the resistivity is 5 MΩ or until time is 5 minutes.
- Wet bench HNO₃ (65%)
 - 10 minutes in fuming nitric acid at ambient at 110 °C.
 - Rinse in the Quick Dump Rinser with the standard program until the resistivity is 5 MΩ or until time is 5 minutes.
- Semitool "rinser/dryer"

Standard program in the white carrier with red dot.

*Note: Red dot - silicon, black dot - green metals

SiC Layer & patterning

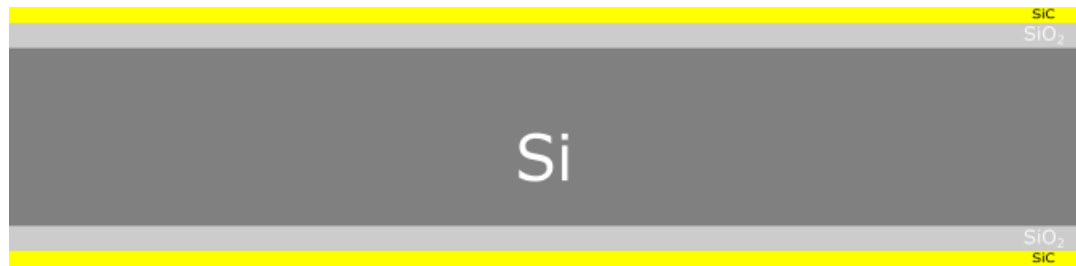
All wafers

13. LPCVD SiC DEPOSITIONS

- Furnace: Tempres E4
- Time: 4h:00:00 (700), 3h:28:00 (300), 3h:41:00 (40)
- Temperature: 860 OC
- Recipe: SIC860VA
- **Gases:**

Table 23: Step parameters.

Wafers	C ₂ H ₂ (ACE)	SiH ₂ Cl ₂ (DCS)	NH ₃
Set 1 (700)	320	80	75
Set 2 (300)	310	90	90
Set 3 (40)	300	100	75



14. THICKNESS MEASUREMENT

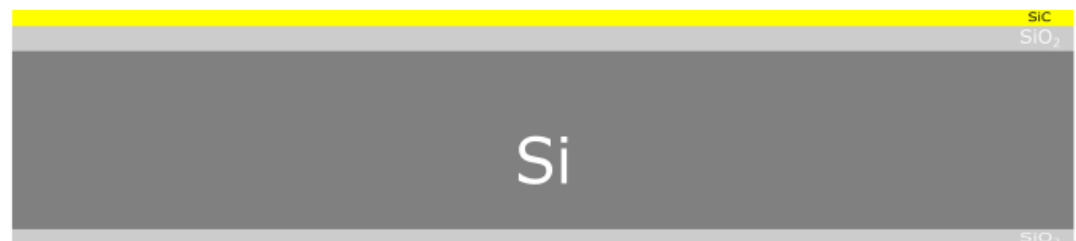
- Tool: Leitz MPV-SP
- Recipe: Silicon Carbide on Si, Silicon Carbide on SiO₂ on Si
- Description: SiC approximate thickness measurement.
- Mean measured value:

15. BACKSIDE SiC ETCHING

- Machine: Trikon Omega
- Time: 12:00

Table 24: Step parameters.

Parameter	Bulk
Temperature	25 °C
Process pressure	5 mTorr
Helium BP	9.5 mTorr
RF1 power	40 W
ICP power	500 W
Cl ₂ flow	30 sccm
HBr	40 sccm



16. SHEET RESISTANCE MEASUREMENTS

- p wafers*
- Tool: CDE ResMap
 - Recipe: Special/Standard 10 point
 - Mean R_{sheet}:

17. WAFER BOW MEASUREMENTS: Stress of SiC

- σ and device wafers*
- Description: Measure each wafer twice with the primary flat oriented first down and then right. Repeat each measurement 5 times. Note down the results in **Error! Reference source not found.**

18. PECVD 3μm TEOS DEPOSITION AS HARD MASK

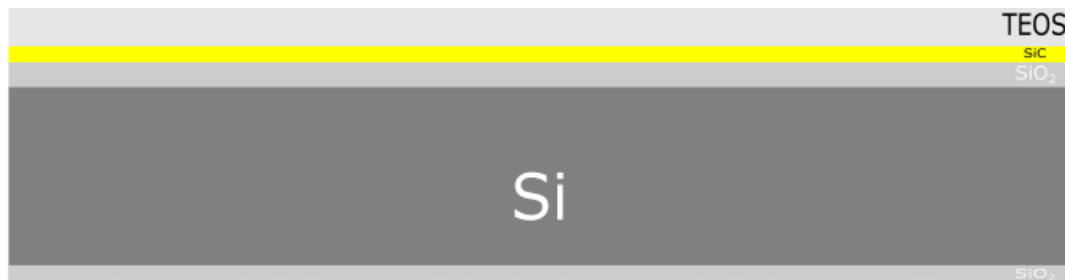
- Device wafers*
- Tool: Novellus Concept One

- Recipe: xxxnmteos
- Temperature: 350 °C.
- Time: 112.3 s
- Deposition rate: 2230 Å/min
- Etch rate: 1000 Å/min
- Power: HF: 0.5 kW, LF: 0.5 kW
- Precoat time: 100 s
- preA delay: 0.2 s
- postA delay: 0.5 s
- TEOS RF delay: 6 s

Table 25: Gas box.

A main fold			B main fold		
A1	N ₂ ON Ar OFF	A2	SiH ₄ ON	B1	SiH ₄ ON Ar OFF
A3	PH ₃ OFF	A4	B ₂ H ₆ OFF	B3	C ₂ F ₆ ON
				B4	NH ₃ ON CH ₄ OFF O ₂ ON

***Note: Do not forget to turn TEOS gases ON!**



19. THICKNESS MEASUREMENT

Device wafers

- Tool: Leitz MPV-SP
- Recipe: Novellus TEOS
- Description: PECVD TEOS hard mask thickness measurement.
- Mean measured value:

20. NEGATIVE PHOTORESIST COATING FOR CANTILEVERS

Device/Cantilever wafers

- Machine: EVG 120 wafer track
- Humidity 48 ± 2 %
- Temperature 21± 0.5°C
- Coating recipe: Co – Nloff - 2 µm
- Final resist thickness: ~2.0 µm.

21. PHOTORESIST COATING FOR V-BEAMS

Device/V-beam wafers

- Machine: EVG 120 wafer track
- Humidity 48 ± 2 %
- Temperature 21± 0.5°C
- Coating recipe: Co – 3012 - 1.4 µm
- Description:
 - i. Thermal treatment with HMDS (hexamethyldisilazane) vapour with nitrogen as a carrier gas at 135°C for 90 seconds.
 - ii. Cooling down to 20 °C.
 - iii. Spin coating with Shipley SPR#3012 positive photoresist.

- iv. Soft bake at 95 °C for 90 seconds.
- Final resist thickness: ~1.4 µm.

22. LITHOGRAPHY, FOR CANTILEVERS (1/2) AND V-BEAMS (1/1)

Device wafers

- Machine: ASM PAS 5500/80 automatic wafer stepper
- Job: g10a-1
- Focus: 0 µm

Table 26: Step parameters.

	Cantilever wafers	V-beam wafers
Mask	HINGES/BOX 460	DIAMONDv1/BOX 441
Energy (mJ/cm²)	150	140

23. DEVELOPMENT, CANTILEVERS

Device/Cantilever wafers

- **X-link bake on a hotplate: 60s at 115 °C.**
- Machine: EVG 120 wafer track
- Humidity 48 ± 2 %
- Temperature 21 ± 0.5 °C
- Development recipe: Dev – DP2 – noPEB

24. DEVELOPMENT, V-BEAMS

Device/V-beam wafers

- Machine: EVG 120 wafer track
- Humidity 48 ± 2 %
- Temperature 21 ± 0.5 °C
- Development recipe: 1-Dev - Single Puddle
- Description:
 - i. Post exposure bake at 115 °C for 80 seconds.
 - ii. Cooling down to 20 °C.
 - iii. Spin development with Shipley MF322 developer - single puddle process - 57s
 - iv. Hard bake at 100°C for 90 seconds

25. INSPECTION: PHOTORESIST OPENINGS

Device wafers

Visual inspection of the wafers through a microscope: Check the line width of line structures - no resist residues are allowed.

26. HARD MASK DRY ETCHING 1

Device wafers

- Machine: DryTek.
- Recipe: STDOXIDE
- Time: 6:00

Table 27: Step parameters.

Step	bulk
Stabilisation time	15 s
Pressure	180 mTorr
RF power	300 W
He pressure	12 Torr
AMN mode	3
C ₂ F ₆	36 sccm
CHF ₃	144 sccm

27. INSPECTION: HARD MASK OPENINGS

Device wafers

Visual inspection of the wafers through a microscope: Check the line width of line structures - no SiO₂ residues are allowed.

28. PLASMA PHOTORESIST REMOVAL

Device wafers

- Machine: Tepla stripper
- Recipe: recipe 1
- Description: Removing photoresist in an oxygen plasma system. Standard resist strip with endpoint detection and 2 minute overetch.
- Oxygen concentration rate: 400 ml/min
- Pressure: 0.8 mbar
- Power: 1000 W

29. WET WAFER CLEANING AND DRYING

- Wet bench HNO₃ (99%)
 - i. 10 minutes in fuming nitric acid at ambient temperature.
 - ii. Rinse in the Quick Dump Rinser with the standard program until the resistivity is 5 MΩ or until time is 5 minutes.
- Wet bench HNO₃ (65%)
 - i. 10 minutes in fuming nitric acid at ambient at 110 °C.
 - ii. Rinse in the Quick Dump Rinser with the standard program until the resistivity is 5 MΩ or until time is 5 minutes.
- Semitool "rinser/dryer"
 - Standard program in the white carrier with red dot.
 - *Note: Red dot - silicon, black dot - green metals

30. PHOTORESIST COATING 2

Device/Cantilevers wafers

- Machine: EVG 120 wafer track
- Humidity 48 ± 2 %
- Temperature 21± 0.5 °C
- Coating recipe: Co – Topo - 3027 - 4 µm
- Description:
 - i. Thermal treatment with HMDS (hexamethyldisilazane) vapour with nitrogen as a carrier gas at 135 °C for 90 seconds.
 - ii. Cooling down to 20 °C.
 - iii. Spin coating with Shipley SPR#3012 positive photoresist.
 - iv. Soft bake at 95 °C for 90 seconds.

- **Inspect backside for residues, clean with IPA if necessary.**
- Final resist thickness: ~1.4 µm.

31. LITHOGRAPHY, FOR CANTILEVERS (2/2)

Device/Cantilevers wafers

- Machine: ASM PAS 5500/80 automatic wafer stepper
- Job: g10a-1
- Energy: 560 mJ/cm²
- Focus: -2 µm

32. DEVELOPMENT, CANTILEVERS

Device/Cantilevers wafers

- Machine: EVG 120 wafer track
- Humidity 48 ± 2 %
- Temperature 21 ± 0.5 °C
- Development recipe: 1-Dev - Single Puddle
- Description:
 - Post exposure bake at 115 °C for 80 seconds.
 - Cooling down to 20 °C.
 - Spin development with Shipley MF322 developer - single puddle process - 57s
 - Hard bake at 100°C for 90 seconds

33. INSPECTION: PHOTORESIST OPENINGS

Device/Cantilevers wafers

Visual inspection of the wafers through a microscope: Check the line width of line structures - no resist residues are allowed.

34. HARD MASK DRY ETCHING 2

Device/Cantilevers wafers

- Machine: DryTek
- Recipe: STD OXIDE
- Time: 6:00

Table 28: Step parameters.

Step	bulk
Stabilisation time	15 s
Pressure	180 mTorr
RF power	300 W
He pressure	12 Torr
AMN mode	3
C ₂ F ₆	36 sccm
CHF ₃	144 sccm

35. INSPECTION: HARD MASK OPENINGS

Device/Cantilevers wafers

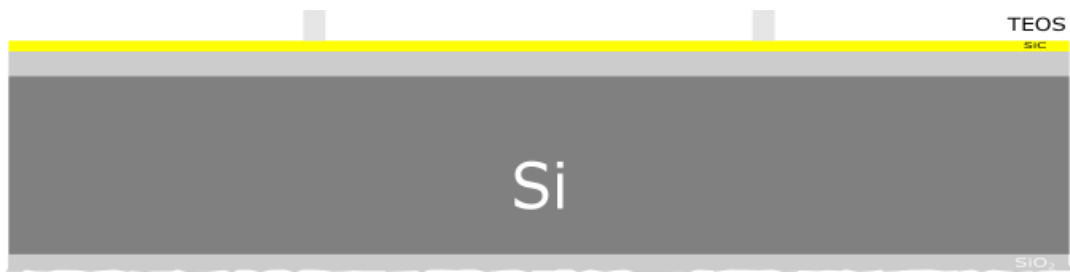
Visual inspection of the wafers through a microscope: Check the line width of line structures - no SiO₂ residues are allowed.

36. PLASMA PHOTORESIST REMOVAL

Device/Cantilevers wafers

- Machine: Tepla stripper
- Recipe: recipe 4
- Description: Removing photoresist in an oxygen plasma system. 15 minutes etching.

- Oxygen concentration rate: 400 ml/min
- Pressure: 0.8 mbar
- Power: 1000 W

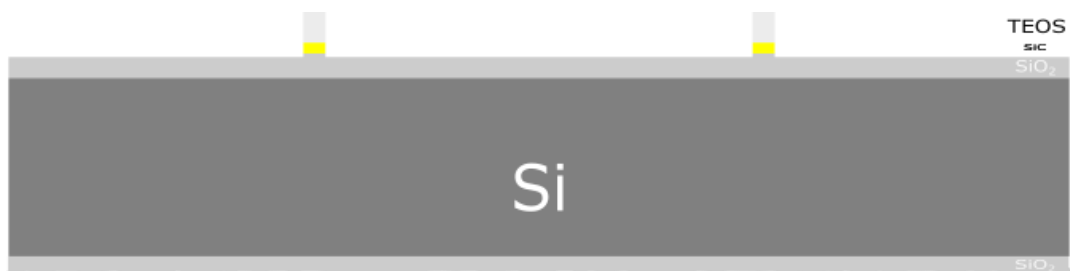


37. SiC PATTERNING ETCH

- Machine: Trikon Omega
- Time: 8:00

Table 29: Step parameters.

Parameter	Bulk
Temperature	25 °C
Process pressure	5 mTorr
Helium BP	9.5 mTorr
RF1 power	40 W
ICP power	500 W
Cl2 flow	30 sccm
HBr	40 sccm

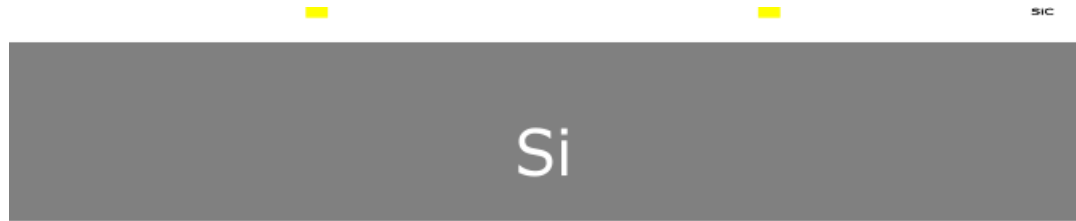


Device Release

Device wafers

38. HF VAPOR ETCHING

- Machine: HF Vapor Etcher Primaxx uEtch HF SPTS
- Recipe: 3
- Cycles: 66
- Minutes/cycle: 7
- Total etching time: 7.7 hours



39. INSPECTION: UNDERCUT AND RELEASE

Device wafers

Visual inspection of the wafers through the **Olympus** microscope: Check the undercut by focusing on the substrate in **Dark Field/max brightness** view of the structures. The undercut must be sufficient for device release. Note down the result:

$L_u =$

B. Wafer die coordinates

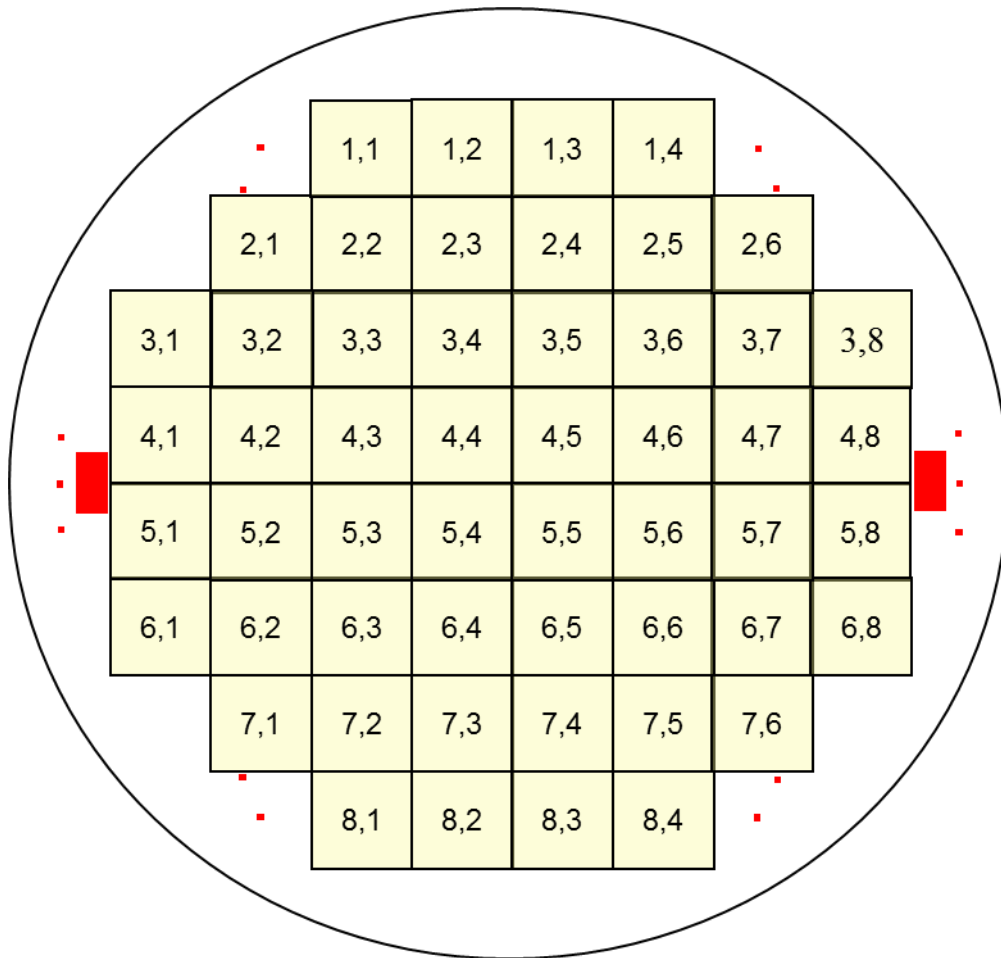


Figure 71: Die coordinates, as used throughout this thesis, for a die size of 10 mm x 10 mm and wafer size of 100 mm (4 inch).

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