Design of Curved Electrodes to Enable Large Stroke - Low Voltage Micro Actuators

by

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Submitted to the Department of Mechanical Engineering on September 3, 2015, in Partial Fulfillment of the requirements for the degree of Doctor of Philosophy in Mechanical Engineering

Abstract
Electrostatic actuators are common in many MicroElectroMechanical Systems (MEMS) devices. These actuators tend to be fabricated as two parallel planar structures, which works well for applications where the motion and electrode spacing are small. To achieve larger displacements, very large voltages are needed. Curved electrodes provide a way to create actuators that achieve large displacements and large forces at much lower voltages than comparable parallel plate designs.

This thesis describes two methods for creating curved silicon membranes that can be easily integrated into practical devices. The first method uses air pressure to plastically deform a silicon membrane at high temperature. During wafer bonding, gas is trapped behind a thin silicon membrane. High temperature annealing causes the gas to pressurize, plastically deforming the silicon membrane. A model predicting the extent of plastic deformation was matched with actual deformation measurements. The second method utilizes the strain resulting from curing epoxy. A diaphragm is formed by etching through the handle of an SOI wafer, leaving a circular silicon membrane. After filling the etched hole with epoxy and covering it, the epoxy shrinks and pulls the membrane into a curved shape.

These curved membranes have been integrated into electrostatic actuators. Pull-in was demonstrated on devices with a wide range circular membranes. The actuation is reliable and requires relatively low voltages. A first order model based on the principle of virtual work was developed that accurately predicts the onset of pull-in. Predictions are compared for several electrode profiles and in every case the critical voltages were lower than for parallel plates.

Finally, the design of a MEMS vacuum pump that incorporates curved electrostatic actuators is analyzed. Factors that affect the base pressure, such as dead volume and pumping volume, are discussed in detail. With low leak rates, base pressures as low as 0.24 Torr could be achieved in these devices in a two stage pump. Guidelines for the design and operation of future MEMS diaphragm pumps are proposed.

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List of Notation

$(r, \theta, z)$: position in cylindrical coordinates
$(x, y, z)$: position in Cartesian coordinates
$\nabla^4$: biharmonic operator, $(\nabla^4 = \nabla^2 \nabla^2 )$
$\alpha$: angle at edge of plastically deforming membrane
$\delta W$: virtual work
$\delta u$: virtual displacement
$\Delta P_{flow}$: pressure difference due to flow stress
$\varepsilon_0$: permittivity of free space (8.854 F/m)
$\varepsilon_1, \varepsilon_2$: relative permittivity of material 1 or material 2
$\varepsilon_{ij}$: strain
$\varepsilon$: strain rate
$\kappa$: system stiffness
$\lambda$: nondimensionalized forcing term
$\lambda_{pi}$: critical forcing term resulting in pull-in
$\mu$: viscosity
$\nu$: Poisson’s ratio
$\rho$: material density
$\rho_e$: electrostatic pressure ($F_e/A$)
$\sigma_f$: flow stress
$\sigma_{\theta\theta}$: normal stress in circumferential direction
$\sigma_{ij}$: stress on $i$ face in $j$ direction
$\sigma'_{ij}$: deviatoric stress, $\sigma'_{ij} = \sigma_{ij} - \frac{1}{3} \sigma_{ll}$
$\sigma_{\text{max}}$: largest absolute stress in body
$\sigma_{rr}$: normal stress in radial direction
$\sigma_{r\theta}$: shear stress in r-$\theta$ plane
$\sigma_u$: ultimate tensile stress
$\sigma_y$: yield stress
$\tau_p$: pumping cycle time
$\tau$: shear stress
$\phi_i$: mode shapes for displacement approximation
$a$: membrane radius
$A$: area of electrostatic actuator
$c$: air damping
$C$: capacitance
$d$: depth of anti-stiction bumps
$D$: flexural rigidity, $D = Et^3/(1 - v^2)$
$E$: Young’s modulus
$F_e$: electrostatic force
$F_e(\omega)$: electrostatic forcing function
$f_i$: body force in $i$th direction
$F_s$: spring force
g: electrode gap
$g_{eff}$: effective gap
$h$: height from neutral axis ($h = t/2$)
$H(r)$: Heaviside function or unit step function
$k$: Boltzmann constant $1.381 \text{ J/K}$
$k$: spring constant
$k_{eff}$: system stiffness, including electrostatic spring softening
$Kn$: Knudsen number
$m$: anti-stiction pump period
$n_i$: normal vector in $i$th direction
$n_{rem}$: gas removed each pumping cycle
$n_{leak}$: gas leaking in each pumping cycle
$P_{vacuum}$: base pressure of a vacuum pump
$P_0$: ambient pressure, usually 760 Torr
$P_1, P_2$: pressure at state 1 and state 2
$q$: distributed load
$q_e$: charge
$Q_{leak}$: leak rate
$r$: non-dimensional radial coordinate
$R$: gas constant $8.314 \text{ J/mol-K}$
$R_h$: hydraulic resistance
t: time
$t$: membrane thickness
$t_2$: thickness of material 2
$T$: temperature
$T$: membrane tension
$T_1, T_2$: temperature at state 1 and state 2
$U$: activation energy for dislocation motion
$U_c$: stored energy in capacitor
$U_{ps}$: stored energy in power supply
$u_i$: displacement in $i$th direction
$V$: applied voltage
$V_0$: initial system volume
$V_1, V_2$: volume at state 1 and state 2
$V_{bumps}$: dead volume due to anti-stiction bumps
$V_{cap}$: volume of spherical cap
$v_d$: dislocation velocity
$V_{dead}$: dead volume
$V_{geom}$: volume derived from geometric constraints
$V_{pumped}$: volume pumped in one cycle
$V_{pf}$: critical pull-in voltage
$V_{vent}$: dead volume due to venting channels
$w$: deformation
$ar{w}(r)$: non-dimensional membrane displacement
$ar{w}_i$: scaled displacement of mode shape $\phi_i$
$ar{w}_{pf}$: central deflection resulting in pull-in
$w_{as}$: maximum displacement between anti-stiction bumps
$w_{ox}$: oxide bow
$w_{max}$: maximum deflection of membrane; $w_{max} = w(r)|_{r=0}$
$w_{vent}, h_{vent}, l_{vent}$: width, height and length of venting channel
$W$: shape of curved membrane
$\bar{W}$: non-dimensional curved electrode shape
$\bar{W}_{combined}$: non-dimensionalized combined shape of epoxy and oxide
$\bar{W}_{epoxy}$: non-dimensionalized epoxy contraction membrane shape
$W_e$: external work
$W_{eff}$: effective deformation magnitude
$W_i$: internal work
$W_{max}$: curved deformation magnitude
$z_c$: critical height for pull-in instability
$z$: deflection of pressurized membrane, while deforming
$z_0$: initial electrode height (gap spacing)
Chapter 1
Introduction

1.1 Motivation
Microscale sensors and transducers are becoming increasingly common in everyday life. Modern smartphones have many internal sensors, including inertial sensors, gyroscopes, compasses, and miniature microphones. Inertial sensors also appear in applications as varied as guided munitions to satellites [1]. Many safety features of modern automobiles rely on accelerometers and gyroscopes, such as the automatic braking system (ABS), electronic stability control, and crash detection for rapid airbag deployment [2]. Small pressure sensors can be embedded in the tire wall and wirelessly alert the driver to low tire pressure [2]. Full tires decreases rolling resistance and improved fuel efficiency. Long used in office projectors, DMD chips are now appearing in many other applications such as active headlights that direct light away from other drivers eyes [3], [4]. Mechanical resonators are used for applications as varied as filtering telecommunication signals for smartphones to providing a stable timing frequency for microchips [4]. Infrared cameras are usually composed of an array of miniature bolometers, which are sensors that absorb infrared radiation [5].

The functional elements of all of these devices are MicroElectroMechanical Systems (MEMS), which are small mechanical elements that move or vibrate based on electrical inputs. These devices are fabricated in a cleanroom using many of the same processes found in the manufacturing of integrated circuits, enabling very small planar structures to be created with sub-micron precision. These techniques have enabled very precise, small, low power sensors to be created relatively cheaply.

Many of these sensors operate in a low pressure environment. MEMS switches and mirrors need low pressures to remove squeeze film damping effects. Vacuum enables them to move quickly without fighting air resistance. Accelerometers need low pressures to reduce noise from molecular motion. To sense the minute motions of the proof mass, very low pressures are required. Resonators and gyroscopes need low pressures to reduce air damping and increase quality factor. Thermal devices, such as bolometers or thermal actuators, need vacuum to remain thermally isolated from their environment.

Low pressures in the devices mentioned above is enabled by packaging the chips in vacuum at the foundry. This is only possible when the device is self-contained or the signal being sensed can pass through the walls of the package. Radiation (such visible light for mirrors or infrared radiation in bolometers) can pass through transparent windows. External forces and motion
(accelerometers, gyroscopes) can pass through the package by shaking an internal proof mass. Electrical signals required for most devices (resonators, switches, actuators) can pass through a sealed package through vias or wire bonds.

However, there are many potential devices that are not currently feasible because they require active vacuum pumping to operate. Consider mass spectrometry, one of the cornerstones of chemical analysis. This tool ionizes small amounts of a material and subjects the ions to electric and magnetic fields. The time of flight varies based on the particle properties, and ions are separate by mass to charge ratio and measured. Many molecules can be identified this way, making this technique critical to fields as varied as environmental monitoring, chemical weapons detection, food safety and disease diagnosis [6].

Unfortunately, there is not a portable mass spectrometer. This is often the case in portable analytics. Mass spectrometer [6], [7], gas chromatographs [8], [9], and many other chemical sensors both to need both a low pressure sensing environment and a method for bring the chemicals inside the device. This means that they cannot have a hermetic seal, and must have some method of active pumping to bring the pressure of the sensor down internally. Currently, it is possible to scale the sensors down to fit in a backpack [6], but for a truly portable chemical sensing platform, a small, low power vacuum pump is required.

The main reason that small vacuum pumps have not been successful yet is that it is very challenging to achieve a large compression ratio (for a displacement pump, this is the ratio between pumped volume and unpumped “dead” volume) in a small device. It is difficult to find an actuator that combines low power with high pumping volumes and minimizes dead volume.

This thesis will argue that curved electrostatic actuators provide a way to meet all of these criteria and could be an enabling technology for miniaturized vacuum pumping. Electrostatic actuation is inherently not dissipative and thus low power. The curved electrode increases the size of the pumping volume, and careful device design can minimize the dead volume.

Curved electrostatic actuators would be useful for many other applications. Using curved electrodes provides a way to generate large displacements with low voltages and power dissipation. This could be useful for a wide range of microscale devices that are currently limited to the narrow gap between parallel electrodes.

1.2 Previous Work at MIT

Work on a MEMS vacuum pump began with a displacement pump developed Vikas Sharma’s vacuum pump [10]. The pump was developed for the Micro Gas Analyzer project. The design was a displacement pump consisting of three elements: one large piston for the pump chamber and two smaller pistons for the valves. The pistons were thick pieces of silicon attached by flexible tethers to the rest of the substrate. The device was actuated pneumatically by applying
pressure or vacuum to the external side of the pump or valve. This device was able to achieve a base pressure of 502 Torr.

This initial pump provided proved that this basic design could be used to create vacuum, and pointed to several areas for improvement. Hui Zhou continued to develop the pump for the Chip Scale Micro Vacuum Pump program [11]. The new pump was still actuated pneumatically, but design improvements allowed the pump to reach a base pressure of 164 Torr [12]. At the time, it was the lowest base pressure for any micromachined vacuum pump. The design increased the pump stroke and decreased the dead volume in order to reach lower base pressures. Additionally, Hui improved upon the valve sealing to decrease the pump leak rate.

The initial pump created by Vikas was quite large: 50 mm x 30 mm x 2mm (3 cm³). The second generation device built by Hui was significantly smaller, at only 1.14 cm³. However, both of these pumps required an external source of pneumatic actuation. This was very helpful for testing a proof of concept device, but it limits the pump to laboratory testing. In order to create a truly portable system, the next generation of the pumps need to have integrated actuation.

1.3 Curved Electrostatic Actuators

Several qualities are desired for integrated actuators. These are:

1. Small size: The solenoids and motors that drive many commercial pumps consume most of the device volume. For a truly portable pump, smaller actuators are desired.
2. Low power: The more power the pump consumes, the larger the power supply needs to be.
3. Easy integration: A reasonable actuator must be easily integrated into standard cleanroom processing techniques.
4. Large volume displacement: In order to reach low pressures, pumps need to be able to achieve large compression ratios. For mechanical displacement pumps, this can be done by increasing the pumping volume and decreasing dead volume.

In this next section, it will be shown that electrostatic actuation with curved electrodes can meet all of these criteria.

Electrostatic actuators operate by applying an electric potential between two electrically isolated conductive surfaces. As the surfaces charge up, the electric charges create an attractive force between the two objects. The electrostatic force (for parallel plates) is given by:

$$F_e = \frac{\varepsilon \varepsilon_0 A}{2g^2}$$  (1.1)

where $\varepsilon_0$ is the permittivity of free space, $\varepsilon$ is the relative permittivity of the medium between the electrodes, $A$ is the area of the electrodes, and $g$ is the gap between the electrodes. This is a highly nonlinear force that is much larger in regions with a narrow gap.
Electrostatic actuation is common in MEMS because it provides a simple way to apply forces and transduce signals without the need for transduction to other physical domains (such as thermal, piezoelectric, etc). Power usage is very low because the actuator is essentially a large capacitor; it stores most of the applied energy during actuation, and releases it again when the applied field is removed. There is no dissipation other than small resistive losses due to the displacement current.

When the gap between the conductors is small, the forces generated can be quite large. However, the nonlinear relationship to the gap \( g \) means that electrostatic force drops off precipitously when the gap is large. This means that electrostatic actuation is not well suited for large stroke actuators with a constant gap.

Electrostatic actuators, in geometries as varied as such as cantilevers, comb drives, or diaphragms, tend to be flat and fabricated in the plane of the substrate. This is because the structural layers are either intrinsic to the starting wafers or deposited on a flat wafer surface. One common method for forming devices starts with a Silicon-on-Insulator (SOI) wafer. These wafers consist of a three layer: a thick silicon base layer called a handle, a dielectric layer (usually oxide), and a thin upper silicon layer called the device layer. If the insulator is oxide, the insulator is referred to as the Buried OXide (BOX) layer. Small, thin shapes, such as cantilevers, can be etched out of the device layer and released by etching away the BOX layer. These structures can be actuated laterally in the plane of the wafer. However, larger structures, such as diaphragms, cannot be cut out vertically. They must be formed by stacking layers on the surface of the substrate and removing a sacrificial layer. For this reason, larger structures tend to only be found with constant gaps between the plates.

Zipping provides a way out of the tradeoff between large forces and large displacements by introducing a variable gap height. For cantilevers, it has been demonstrated that by creating one curved electrode, pull-in can propagate along the beam at much lower voltages than for comparable flat beams [13].

A similar technique can be used to zip a membrane down onto a curved electrode, generating large forces and displacements. The curved shape creates a variable gap, with a narrow air gap at the edge and a much wider gap in the center of the membrane. For our geometry, this means that there are large forces near the edge of the membrane, pushing the edge down. This creates adjacent areas with narrow gap, causing the membrane to “zip” from the edge to the center.

Because the force is nonlinear, at a critical voltage the membrane will snap down completely. This is called pull-in, with the critical voltage being the pull-in voltage. This process is shown in Figure 1.
Figure 1: Stages of zipper actuation for a circular membrane above a rigid curved electrode (shown in cross-section). With no voltage applied the upper electrode is flat (top). When a voltage below the critical pull-in voltage is applied, the membrane displaces, with contact starting near the edges (middle). When the critical pull-in voltage is reached, the flexible membrane snaps down onto the lower electrode (bottom). The black bumps represent anti-stiction bumps etched into the insulating oxide between the two electrodes. These bumps prevent the surfaces from sticking together.

Using a curved electrode has significant benefits both for achieving low pressures and for pumping at low voltages.

First, consider the electrostatic properties. The variable gap allows us to reach the pull-in instability at a much lower critical voltage than if we used parallel plates with comparable spacing. A full analysis of parallel plates versus different curved geometries is presented in Section 4.6.

Now consider the curved actuator as a pumping element. The curved shape allows the membrane to displace a much larger volume of air than comparable parallel plate actuators. The curved electrode can be much deeper than a comparable parallel plate electrode and still have low voltage pull-in. Additionally, the smooth surfaces between the two membranes mean that they will contact conformally, with very few air pockets. This limits the dead volume between the membranes, lowering the base pressure.
We can place several of these actuators in series together to create a diaphragm pump. By actuating these membranes in the correct order, we can move air from the inlet (device) side to the outlet (atmosphere). The cycle is as follows:

1) Start with all the membranes closed
2) Open the inlet valve and pump chamber
3) Close the inlet valve
4) Open the outlet valve and close the pump chamber

These steps can be repeated to create vacuum on the inlet side. A diagram of this process is shown in Figure 2.

![Diagram of diaphragm pump cycle](image)

Figure 2: Method for creating vacuum using a diaphragm pump, shown with curved pump chamber and valves. Black bumps on lower surface are intended to prevent friction between surfaces.

To create low pressures, the pump membranes need to displace as much volume as possible. Conversely, the valves only need to actuate far enough to allow air to flow through them, so the curved electrode for the valves can be much shallower.
1.4 Thesis Outline

The focus of this thesis will be on developing methods for integrating curved electrostatic actuators into MEMS devices, specifically vacuum pumps. Two methods for creating curved membranes, high temperature plastic deformation and epoxy induced contraction, will be discussed in Chapter 2. Deformation results will be presented along with a model for predicting the extent of plastic deformation. Chapter 3 presents methods for integrating these curved diaphragms into simple electrostatic actuators and testing the resulting devices to find the pull-in voltage. Chapter 4 describes a first order model for estimating the pull-in voltage for curved electrodes, and compares the predictions to experimental results. The performance of other curved electrode shapes are compared as well. In Chapter 5, the performance of a diaphragm pump utilizing curved electrostatic actuators is analyzed. Factors that affect the dead volume are considered in depth. Estimates of the base pressure are compared for a variety of curved electrode shapes. The contributions from this thesis as well as future work are discussed in Chapter 6.
Chapter 2
Creating curved silicon membranes

2.1 Summary of curved electrode literature
There are several examples in the literature of other microsystems incorporating curved electrostatic actuators. In this section, several of them will be described in more detail. Selected diagrams and figures from these examples are shown in Figure 3.

Branebjerg and Gravesen created an early curved electrostatic electrode with a similar geometry to our plastically deformed membranes [14]. A silicon membrane was bonded to a glass plate to form a cavity, and a pressure was applied between the two electrodes to push out the membrane. Applying a voltage between the silicon membrane and gold layer on the plate causes the membrane to pull in and contact the plate. While the voltages achieved are low, a pressurized electrode makes this design less useful for vacuum pumping applications.

Jeahyeong Han et al. created a diaphragm flow pump using curved circular electrostatic actuators [15]. The curved electrode was created using a dimpling machine with a diamond slurry to create dimples in the silicon substrate, followed by CMP to smooth out the surface roughness. While their device was optimized for fluid flow rather than vacuum generation, this method could provide another alternative for making curved electrodes in a vacuum pump.

Resist reflow can be used to create curved structures as well. The lower electrode was formed by depositing a LPCVD film on a silicon wafer, then etching through the substrate to release the membrane. Resist is patterned on top of the membranes, and then exposed to hot solvents to cause the resist to reflow into a curved profile. Using gold as a seed layer, nickel is electroplated on top of the resist to form the upper electrode. Using this method, Yee et al. fabricated microphones using an electrostatic actuator with one curved electrode, perforated for air flow [16], [17]. The curved electrode lowered the pull-in voltage significantly: for a 1.3mm square actuator, the pull-in voltage decreased from 337V for a flat electrode to 240V with a curved electrode (with comparable spacing between the electrodes).

Wagner et al. created an electrostatically actuated microvalve where both the lower and upper electrodes were curved [18]. Silicon dioxide on the upper electrode caused the membrane to buckle. The lower electrode was formed either with an isotropic wet etch or using grey-tone lithography. Isotropic etches form hemispherical cavities, which are not ideal for electrostatic actuation for several reasons. Formation of curved structures depends on a carefully timed etch; variations in timing can lead to different cavity sizes and depths, and thus variable actuator performance. Additionally, the edge of the etched cavity has a sharp edge, which increases the
voltage for pull-in. Grey tone lithography can produce arbitrary resist profiles across a wafer, and transfer these shapes into the underlying substrate using an reactive ion etch (RIE) [19]. To do this, the features on a standard quartz/chrome mask are broken into square pixels, and the size of each pixel is modulated to control the amount of light that reaches the resist for exposure. Light from adjacent areas blur together; after developing the pattern, resist is left with height proportional to the size of the pixels. However, the process must be carefully calibrated to match the grey scale exposure time and RIE resist selectivity in order to create the desired geometry.

McIntosh et al. created pressure sensors utilizing curved trench electrodes [20]. The electrode curvature enabled them to create a sensor with a linear capacitance to pressure relationship over a large range. They used an electrical discharge machine (EDM) to cut curved trenches in glass wafers, and anodically bonded a flexible silicon membrane to form the upper electrode.

DRIE (Deep RIE) is known to have variable etch rate, known as DRIE lag, depending on feature size and density. This normally leads to undesirable nonuniformity, but it can also be used to make variable curved geometries. Chou and Najafi [21] created very tightly spaced trenches with varying widths, utilizing DRIE lag to etch them to varying depths. A subsequent isotropic etch (wet or dry) removed the walls between the trenches and smoothed the curved feature. This process can create arbitrary geometries, but requires a careful characterization of tool and feature dependent DRIE lag.

At the mesoscale, curved electrodes can be fabricated by injection molding [22].
2.2 High Temperature Plastic Deformation

Many materials exhibit elastic plastic deformation behavior. This means that under small loads it behaves elastically, and will return to their original state once unloaded. Above a critical stress, called the yield stress ($\sigma_y$), the material will start to permanently deform. The permanent deformation is called plastic deformation.

The ultimate tensile strength ($\sigma_u$) of a material is the maximum amount of stress that a material can withstand before breaking. Ductile materials deform plastically before reaching $\sigma_u$. Brittle materials, like silicon, crack and fail at $\sigma_u$ without any plastic deformation. Another way to think about this is that the ultimate tensile strength is reached before the yield stress. However, at elevated temperatures, the yield stress drops. At high enough temperatures, silicon becomes ductile and can plastically deform before reaching the fracture stress.
Unlike ductile metals, above the yield stress, single crystal silicon has a much lower flow stress \( \sigma_f \). This means that once silicon starts to deform, much less stress is required to continue straining the material than was required to reach yield. An example of this constitutive relation is shown in Figure 4.

![Graph showing yield stress and flow stress over temperature](image)

**Figure 4:** Above 600 °C, the yield stress for silicon is below the fracture stress, and the material can start to flow. As the temperature rises, the yield and flow stress drop. (Left) Data taken from [23] with a calculated best fit line. (Right) Plot adapted from [23], showing constitutive behavior for dislocation free silicon at 815 °C with a crosshead displacement rate of 5 x 10^-3 cm/min.

Some planes in a crystal, called slip planes, have less resistance to motion. When a crystalline material plastically deforms, the slip planes start to slide past one another. Even in these preferential directions, the crystal does not slide all at once. Breaking all the bonds at once and reforming them in the new position would require a massive amount of energy. Instead, dislocations, or defects in the normally ordered structure of a crystal, allow individual bonds to break and reform, allowing crystal planes to flow past each other one atom at a time.

Both the flow stress and yield stress have been shown to be of the form [24]:

\[
\tau \propto \dot{\varepsilon}^{1/n} \exp \left( \frac{U}{kT} \right)
\]

where \( \tau \) is the resolved shear stress along the dislocation's direction, \( \dot{\varepsilon} \) is the strain rate, \( n \) is an experimentally determined fitting parameter, \( U \) is the activation energy for dislocations, and \( k \) is the Boltzmann constant. This expression shows that the flow stress and yield stress increase with larger strain rates, and decrease at elevated temperatures. These properties have been carefully measured [23], [24] and modelled [25].

Since the annealing process slowly heats at -5 °C/min, the load is applied gradually. This means that the membranes will strain slowly. If the strain occurs uniformly over the ramping process, the largest strain rate experienced by any device would be 1.67 x 10^-3 cm/min. For our
predictions, we will be using $\sigma_f$ and $\sigma_y$ measured with $\dot{\varepsilon} = 5 \times 10^{-3}$ cm/min [23], the lowest strain rates available, even when our strain rate was actually lower. At high strain rates, variations in $\dot{\varepsilon}$ leads to large changes in the flow and yield stresses, but at lower strain rates the changes are smaller. For this reason, it is reasonable to use $\sigma_f$ and $\sigma_y$, $\dot{\varepsilon} = 5 \times 10^{-3}$ cm/min, even though it is larger than the expected strain rate.

The yield and flow stresses are very sensitive to the concentration of dislocations in the material. The velocity of each dislocation is proportional to the applied shear stress [26]:

$$v_d \propto \tau^m \tag{2.2}$$

where $v_d$ is the velocity of the dislocation. At low dislocation densities, plastic flow is limited because only a few dislocations exist. At a constant strain rate, to achieve an incremental displacement each defect must move further. This requires larger stresses to create the necessary dislocation velocities (2.2). As the material deforms, additional dislocations are created and the velocity of individual dislocations drops. This causes the stress needed for further straining to decrease. Eventually enough dislocations have formed that they start to interfere with each other, decreasing the benefit of higher dislocation densities, and the stress needed flattens out into the flow stress ($\sigma_f$). Dislocations themselves create a localized stress strain field. Once enough of them have formed, these fields inhibit further plastic flow, and the flow stress starts to increase again. This is called stress hardening.

2.2.1 Summary of Deformation Process

By controlling the location and magnitude of the applied stress, the deformation in the silicon can be used to create a variety of curved structures. Several examples of this exist in the literature. Selected diagrams and figures from these examples are shown in

Large parabolic "parasols" for use as X-ray lenses were formed by pressing a curved quartz mold into a silicon wafer [27]. Scanning micromirrors supported on thin tethers were permanently rotated around their supports. This was done by applying a load to the edge of the mirror at elevated temperatures in a furnace [28] and using localized resistive heaters [29]. When actuated, these out of plane comb drives rotated around the pivot point. Cantilevers and membranes can be bent out of the wafer plane by applying a load to the end of a cantilever [30], [31].

Most relevant to the current work is the research by Huff [32]. He used plastically deformed membranes to create a pressure control switch [33] and developed a model to predict the extent of plastic deformation in circular silicon membranes [34]. We will build on his work in the following chapter.
The devices modelled in this section are circular membranes that are plastically deformed during wafer bonding. A precise volume is etched into the device layer of an SOI wafer (henceforth referred to as the deformation volume and deforming wafer). A circular membrane is formed by etching through the handle wafer. The deforming wafer is contacted in a nitrogen environment to another wafer. During the high temperature anneal that fuses the wafers together, the $N_2$ expands, pressurizing the membrane and causing it to plastically deform. The expansion of the membrane decreases the pressure, lowering the stress and causing the deformation to terminate at the desired magnitude.

These steps are shown in Figure 6. A more detailed process for device fabrication is discussed in Section 2.2.3.
1. Pattern lower wafer to form membrane and deformation volume

2. Align and contact wafers in N₂ at 1 atm.

3. Anneal bonded pair at high temperature to deform

Figure 6: Basic steps for creating plastically deformed membranes. The blue layer is the device layer of an SOI wafer.

2.2.2 Modeling plastic deformation

In this section a model will be presented that predicts the extent of plastic deformation for a circular membrane undergoing a high temperature anneal with a defined volume of air on the back side.

First, a few assumptions are necessary about the constitutive relationship of the deforming material.

Silicon is a material with cubic symmetry. This means that three independent stiffness components are needed to completely describe elastic behavior and the plastic yield depends on the orientation of the applied stresses. Material properties, such as Young’s modulus (E), vary widely with direction: E has a maximum of 188 GPa in the <111> direction and a minimum of 130 GPa in the <100> direction.

However, we only need to consider the material properties in the plane of the deforming membrane because the diaphragm is thin enough to be in plane stress (σ₃₃ = 0). For a 100 wafer, within the plane E ranges from 130 GPa in the <100> direction to 169 GPa in the <110>
direction. Poisson's ratio ranges from $v = 0.064$ in the $<110>$ direction and $v = 0.28$ in the $<100>$ direction [35].

To simplify the analysis, we will assume that the material behaves isotropically with a Young's modulus ($E$) and Poisson ratio ($v$) of 150GPa and .17, respectively. This is the average value for both parameters in the plane of the membrane, and it will be seen that this approximation provides a reasonably fit for the experimental data.

If a crystal starts with a significant dislocation density, the initial yield stress can be depressed. In cases with very high dislocation density, the yield stress $\sigma_y$ can disappear entirely, allowing the membrane to flow as soon as $\sigma_f$ is reached. RIE etches can cause damage (and create dislocations) on the surface [36]. For our devices, all etches stopped on an oxide etch stop and did not directly etch the silicon device layer. For this reason it is believed that the starting wafers had a low dislocation density, but without knowing the effect of processing steps, both defect free (high $\sigma_y$) and high defect density ($\sigma_y = \sigma_f$) will be considered.

Predicting the extent of plastic deformation is done in two steps. First, the elastic response is calculated to determine the stresses in the membrane. If stresses satisfy the yield criterion, then the magnitude of the permanent plastic deformation is calculated. Otherwise, the membrane does not deform.

First, consider the elastic response. As the temperature rises, the gas inside the membrane expands and the pressure increases. For a circular plate under a uniform load, the shape of the membrane is:

$$w(r) = \frac{q}{64D}(a^2 - r^2)^2$$

(2.3)

where $q$ is the pressure load, $D = Et^3/(1 - v^2)$ is the flexural rigidity, and $a$ is the membrane radius. It should be noted that $q$ is not the absolute pressure inside the membrane, but the difference between internal and external pressures. (2.3) can be integrated to find the volume due to the pressurized geometry.

$$V_{geom} = \frac{\pi}{3} a^2 \left( \frac{q a^4}{32 D} \right) = \frac{\pi}{3} a^2 w_{max}$$

(2.4)

where $w_{max} = w(r)|_{r=0}$ is the maximum deflection of the membrane. Thermodynamics provides another equation for the volume. From the ideal gas law,

$$V_2 = V_1 \frac{P_1 T_2}{P_2 T_1}$$

(2.5)

$P_1, V_1$ and $T_1$ are the initial conditions, and $T_2$ is set by the annealing temperature. Thermodynamics must agree with the geometric volume, so we can set equations (2.4) and (2.5) equal to each other and solve for $P_2$ and $V_2$. Using this final pressure, we can calculate the amount of elastic deformation; this is shown in Figure 7.
The plate is thin enough that the body can be considered to be in plane stress \((\sigma_{3i} = 0)\). The other components are:

\[
\sigma_{rr} = -\frac{3qz}{4t^3} [(1 + \nu)a^2 - (3 + \nu)r^2]
\]  
\[
\sigma_{\theta\theta} = -\frac{3qz}{4t^3} [(1 + \nu)a^2 - (1 + 3\nu)r^2]
\]  
\[
\sigma_{r\theta} = 0
\]

This leads to a maximum stresses:

\[
\sigma_{rr}\big|_{z=t/2, r=a} = \frac{3qa^2}{4t^2}
\]  
\[
\sigma_{\theta\theta}\big|_{z=t/2, r=0} = \frac{3qa^2}{8t^2} (1 + \nu)
\]

(2.3) is only valid for small deflections because the derivation only considers plate bending. At larger deflections, the plate starts to stretch as well. When the central deflection reaches half the thickness \((w(0) \leq t/2)\), (2.3) overestimates the deflection by \(\sim 11\%\) [37]. For the devices used in this work, large deformations formulations are largely unnecessary.

Throughout this analysis, two deformation volumes will be used to drive the plastic deformation. Both volumes are etched 42 \(\mu\)m deep (40 \(\mu\)m device layer plus 2 \(\mu\)m thick oxide). The smaller deformation volume is 1mm \(\times\) 2mm on a side, while the larger volume is 2mm \(\times\) 2mm. As seen in Figure 7, membranes with the smaller deformation volume displace less than \(t/2\) (40/2 = 20 \(\mu\)m) for most radii. However, the larger deformation volume leads to displacements well above the cutoff, and requires the use of the large deformation form. At higher temperatures or with thinner deforming membranes, the large deformation form would be needed for both cases.

The calculated elastic deformation is shown in Figure 7. For small radii, the displacement is low, so the volume change is small. This means that the pressure is almost constant. From (2.3) \(w(r) \propto a^4\). As the radius increases, the volume change becomes more significant and the pressure starts to drop as the membrane displaces. At large radii, this is the dominant effect and the deformation decreases with increasing radius.
Figure 7: Central deflection (elastic) for 40 \( \mu \)m thick circular membranes at 900°C using two different deformation volumes. Calculated using small deformation theory (2.3).

For large deflections, it is better to use the following expression for center displacement and stress from Henckey:

\[
\begin{align*}
    w(r)|_{r=0} &= 0.662a^3 \frac{q\alpha}{Eh} \\
    \sigma_{rr}|_{r=0} &= 0.423 \sqrt{\frac{Eq^2a^2}{h^2}} \\
    \sigma_{rr}|_{r=a} &= 0.328 \sqrt{\frac{Eq^2a^2}{h^2}}
\end{align*}
\]

Assuming that the shape of the membrane is close to the small deformation case, (2.11) can be substituted into (2.4) to find the volume, and the same calculation equating the volumes can be done to find \( P_2, V_2 \).

The simplest yield criterion is to compare the yield stress with the maximum stress in the body:

\[ \sigma_{max} \geq \sigma_y \]  

(2.14)
This is accurate for bodies in uniaxial loading. The von Mises criterion takes more time, but is more accurate for more complicated stress states. Yield occurs if:

\[ \sqrt{\frac{3}{2}} \sigma'_{ij} \sigma'_{ij} \geq \sigma_y \]  

(2.15)

where \( \sigma'_{ij} = \sigma_{ij} - \frac{1}{3} \sigma_{ii} \) is the deviatoric stress. In plane stress, this simplifies to:

\[ \sqrt{\sigma_{rr}^2 - \sigma_{rr} \sigma_{\theta \theta} + \sigma_{\theta \theta}^2} \geq \sigma_y \]  

(2.16)

For comparison, in uniaxial tension, (2.16) reduces to the simpler yield criterion (2.14). The two yield conditions are compared in Figure 8.

![Figure 8: Comparison of uniaxial and von Mises yield criterions for 40 \( \mu \)m thick circular membranes at 900 °C with a deformation volume of 1 mm x 2 mm x 42 \( \mu \)m.](image)

This analysis allows us to predict the temperature of yield for the circular membranes studied here. Predictions for both yield conditions and two deformation volumes are shown in Figure 9. The von Mises criterion predicts yield at lower pressures (and thus lower temperatures) than the basic uniaxial yield criterion.
It is clear that there are two competing factors limiting yield.

- For small membranes, the change in volume as the temperature increases is small compared to the size of the deformation cavity, meaning the pressure is effectively constant for these small radii. However:
  \[ \sigma_{rr}, \sigma_{\theta\theta} \propto qa^2 \]
  This leads to an increase in stress as the membrane as the radius increases, and a corresponding lower temperature for initial yield.

- For large membranes, the membranes displace easily. Increased temperature leads to large changes in the volume and a much smaller pressure increase. This results in lower stresses.

At both extremes (large and small radii), higher temperatures, and the associated increase in pressure and drop in yield stress, are needed to cause these membranes to plastically deform.

Next, the plastic deformation will be considered. If the membrane has not yielded, then no plastic deformation will happen and the diaphragm will remain flat. Above \( \sigma_y \), the material is allowed to flow freely at the flow stress, \( \sigma_f \). This causes the membrane to inflate, increasing the
internal volume and decreasing the pressure. Eventually, this feedback causes the stress to drop below $\sigma_f$ and the deformation stops. We are trying to find this new equilibrium point where the internal pressure balances the flow stress.

We will assume that the diaphragm behaves as a membrane with zero bending stiffness and a tension equal to the flow stress. This is actually a reasonable assumption, because at larger deflections, plates behave as membranes. This hemispherical shape is shown with relevant variables defined in Figure 10.

![Diagram](image)

**Figure 10:** Geometry for plastic deformation model. The colors correspond to increasing temperature.

The flow stress balances the internal pressure, allowing the membrane to support a pressure difference. The internal pressure can easily be calculated from a force balance (shown in Figure 11).
Figure 11: Balance of forces between internal pressure and flow stress. At the edge, $\sigma_f t$ is a force/length acting on the entire circumference.

Pressure acts on the area of the membrane, while the stress is resolved over the thickness and around the perimeter of the membrane. In equilibrium, this can be written as:

$$\Sigma F_z = 0 = \Delta P (\pi a^2) - \sigma_f t (2\pi a) \sin(\alpha)$$  \hspace{1cm} (2.17)

Rearranging the terms:

$$\Delta P_{flow} = \frac{2\sigma_f t}{a} \sin(\alpha)$$  \hspace{1cm} (2.18)

This phenomenon, described by the Young-Laplace equation, is common to any surface subject to tension, such as water droplets. The angle term $\sin(\alpha)$ can be rewritten in terms of design parameters $a$ and $z$:

$$\sin(\alpha) = \frac{2za}{a^2 + z^2}$$  \hspace{1cm} (2.19)

The volume of a spherical cap is:

$$V_{cap} = \frac{\pi}{6} h (3a^2 + z^2)$$  \hspace{1cm} (2.20)

When solving for the elastic deformation, we had an explicit relationship between the pressure and the deformation shape (2.3). During plastic deformation, our assumed shape does not depend explicitly on the pressure, instead, it introduces a third unknown variable – the deformation magnitude $z$. Thermodynamics (2.5) can be used to relate the $p$ and $V$, geometry (2.20) can be used to relate $V$ to $z$, and force balance (2.18) can be used to relate $p$ to $z$. These three equations can be solved to find the three unknowns. The final plastic deformation is plotted in Figure 12 below.
Figure 12: Plastic deformation predictions. Solid lines are for the small deformation volume (1 mm x 2 mm x 42 μm), dotted lines are for the large deformation volume (2 mm x 2 mm x 42 μm).

As with the elastic deformation, the plastic deformation is not monotonic.

- At large radii, the deformations volume limited. The flow stress plays a small role, meaning that increases in deformation volume translate directly into increase plastic deformation. For the highest radii modelled, it is clear that doubling the volume leads to doubling the magnitude of deformation.
- At small radii, the deformation is flow stress limited. Consider two membranes, one larger than the other, that deform the same amount. The angle $\alpha$ at the edge will be bigger for the small membrane, leading to a larger resolved flow stress at the edge. This supports a larger pressure difference across the membrane. This effect is plotted in Figure 13. This pressure difference is required for equilibrium, and leaves less pressure to drive the plastic deformation. For the smallest membranes modelled (0.5 mm radius), a pressure difference of >2.5 atm is required across the membrane.
Figure 13: The flow stress can support a pressure difference across the membrane during plastic deformation. The pressure difference is plotted here vs temperature for a range of membrane radii. Smaller membranes have larger flow stresses. Solid lines are for the small deformation volume (1 mm x 2 mm x 42 μm), dotted lines are for the large deformation volume (2 mm x 2 mm x 42 μm).

This modelling suggests that small membranes are very difficult to control using plastic deformation for three reasons. It takes a high temperature to cause them to yield (Figure 9). Since it takes a high temperature to initiate plastic deformation, they can only deform large amounts (Figure 12). Finally, the flow stress makes the small membranes much less sensitive to changes in deformation cavity volume.

It is important to note the differences between the current analysis and the model presented by Huff [32].

- First, the geometry is different. In both cases there is a cavity, called the deformation volume, driving the deformation. Huff etched a deep cavity directly behind each membrane using a timed KOH etch. The etch depth was measured after the etch, and the volume calculated in order to predict the plastic deformation. Our devices have a separate cavity connected to the membrane via a venting channel. The deformation volume is precisely 22 μm deep. The depth is controlled by etching through the 20 μm thick handle wafer and removing the 2 μm thick BOX layer at the bottom. Using a
deformation volume that is separate from the deforming membrane allows for more precise control over the quantity of air driving the deformation process.

- Huff used large deformation theory to predict the stresses (and yield) of the membranes. This was appropriate for his devices, but it was shown in Figure 7 that small deformation theory is more appropriate for many of the devices tested here. For this work, small deformation displacements were checked first to see if the central deflection was above \( t/2 \), (corresponding to 11% overestimate of the stress), and if necessary large deformation stresses were used.

- A more accurate yield criterion was used. The Mises yield criterion captures the multiaxial stress state in the membrane better than the uniaxial criterion used by Huff. For the membranes studied here, there is a difference of approximately 25 °C between the two yield conditions (shown Figure 8).

- Huff focused on devices with a single membrane diameter. This work studied the effect of membrane radius on the plastic deformation process.

2.2.3 Plastic Deformation Experiments

Plastic deformation was initially demonstrated while fabricating electrostatic actuator test structures (see section 3.1.1). However, the results had a lot of variability. The magnitude of deformation was quite variable, and many dies did not deform at all. It is believed that two major factors contributed to the variance. First, in the short loop tests, the device layers of two SOI wafers are bonded together. They have opposite curvature bowed away from the bond interface (bow of each was around 150 μm), leading to a low quality bond. Second, the bonded area between deforming membranes was as little as 200 μm. If the two wafers did not contact perfectly between membranes, pressurized air may have leaked between adjacent membranes, changing the effective deformation volume.

In order to understand the process more fully, a new process based on controlling only plastic deformation were designed. Design changes were included to address both of the bonding problems, as well as investigate the effect of membrane radius and deformation volume on the process.

The fabricated devices will not be complete actuators. Instead, the design only includes four masks to create the essential features for plastic deformation:

1. Define the membrane that will deform
2. Define fixed volume to drive the deformation
3. Create a channel between the membrane and the deformation volume
4. Guide lines for cleaving the finished wafer.

Plastic deformation is driven by a controlled volume, called the deformation volume. For this process, two volumes were used. The smaller deformation volume was 1mm x 4mm on a side, while the larger volume was 2mm x 4mm. Both were etched through the device layer of the 20
μm wafer. The deformation volume is connected to the back side of the deforming membrane via a narrow channel.

All microfabrication was carried out at the Microsystems Technology Laboratories (MTL) at MIT. The starting substrates, both 150mm diameter, are a double side polished (DSP) prime wafer and an SOI wafer. For this process, the SOI wafer (henceforth called the deforming wafer) has a 40 μm device layer, 2 μm BOX, and 400 μm thick handle.

Immediately out of the box, the SOI wafer, or deforming wafer, has 100nm of LPCVD nitride layer deposited followed by a 2.5 μm PECVD oxide layer on the handle side to compensate for the stress caused by the BOX layer. DSP wafers have effectively no bow, and after compensation the SOI wafer had approximately 40 μm of bow.

It has been shown that residue from photoresist and other cleanroom processes can decrease bond quality [38]. The LPCVD nitride on the SOI wafer protects the surface from etches and chemicals prior to bonding, and will be stripped immediately before bonding. The DSP wafer will be taken straight from the box for bonding (no processing) and does not need the nitride layer.

A top view and cross-sections of the devices are shown in Figure 14 and Figure 15. The processing steps are shown in Figure 16.

![Cross-section Path](image)

Figure 14: Top view formed by overlapping masks for a single die. Cleavage guides are used to break the wafer and release pressure after deformation. The black dotted line is used for the cross section in Figure 15.
Figure 15: Cross section showing critical features of the plastic deformation test devices following the dotted line in Figure 14.

1) Deposit LPCVD nitride, PECVD oxide on back to compensate stress

2) Etch pump and connection channel through nitride and 100nm into silicon on device side, remove nitride and oxide on pump and cleavage lines on back

3) DRIE etch deformation cavity

4) DRIE through wafer, BOE to strip BOX from membrane

5. Strip nitride and fusion bond to blank DSP Si wafer

6. Cleave

Figure 16: Processing steps for plastic deformation short loop.
Processing on the deforming wafer

On the device side, membrane and connection channel are etched through nitride film, continuing 200nm into the silicon. This prevents the membrane surface from contacting, and bonding to, the other wafer. On the handle side, the membrane and cleaving lines are etched through both oxide and nitride films.

Next, a deep reactive ion etch (DRIE) is used to form the deformation cavities and cleavage guide lines. The etch stops at the BOX layer, leaving a 40 μm deep deformation cavity.

DRIE is used to etch through the handle, leaving free-standing 40 μm thick membranes and forming the cleavage guides. Buffered oxide etch (BOE) is used to strip the exposed oxide from the membranes. Oxide would cause the membranes to bow, complicating the measurement of plastic deformation. This step also removes the oxide from the base of the deformation cavity, increasing the depth to 42 μm.

Processing on both wafers

Removing that residue and preparing the surface for bonding involves several cleaning steps. The deforming wafer is ashed followed by a piranha clean to remove any residual resist and other contaminants. Next, the nitride surface layer is stripped in 165°C phosphoric acid. This leaves the bare wafer on the device layer, which has not seen any wet processing yet, and is as close to a virgin surface as we can get. To further clean and hydrolyze the surface for bonding, an RCA clean was performed without the HF dip on both wafers (SOI and DSP).

Immediately following the cleaning steps, the wafers are placed in a new, clean wafer box and transported to the bonding area. The upper and lower wafer are aligned and clamped in an EV 620 aligner and contacted in an EV 501 bonder. After pressing overnight, the wafers are annealed in a diffusion furnace at 900 °C, causing them to plastically deform.

After bonding, the wafers are inspected under an IR camera to find defects. Silicon is transparent to infrared wavelengths, but defects show up as interference patterns on the camera.
The plastic deformation increases the volume of the deformed cavity, but the quantity of air is constant. At high temperature, the membrane is in equilibrium, but as the wafer is brought back to room temperature, the internal pressure decreases inside the membrane. To release the pressure, the wafer is cleaved into dies using the cleavage guides.

2.2.4 Measurements
The process designed for plastic deformation was much more reliable than the fabrication process incorporating electrostatic actuators. A larger proportion of the membranes that were predicted to yield successfully deformed, and the yield behavior was more uniform.

Due to a mask mistake, the cleavage guides intersected the deformation volume for four dies, preventing them from deforming. Four additional dies did not deform when the model predicted they would. These undeformed dies were all 3mm diameter or less. Three 8mm diameter dies deformed when the model did not expect them to. This is less of a surprise than the undeformed membranes, since 8mm membranes are predicted to yield around 925 °C, slightly above the targeted anneal temperature of 900 °C. A wafer map comparing the yield behavior to the model is shown in Figure 18.
Figure 18: Comparison of the deformed dies to the model. The die labels refer to the membrane radius and large/small deformation volume, i.e. 3L is a 3mm diameter membrane with a large deformation volume. Green and blue dies match the model’s predictions for yield, while red and yellow do not. O dies refer to oval test structures. Dies with red outlines indicate did not have sealed deformation volumes.

The center of the wafer deformed as expected, with most of the unexplained undeformed dies near the edge. Wafer handling can cause defects and particles to accumulate at the edges, and bond quality is usually worse at the edge as well. This could explain the undeformed membranes.

The reliability of these tests is summarized in Table 1. Of the 36 dies expected to deform, only 3 failed to deform. This is an overall predicted deformation yield of 91.7%. Additionally, 3 of the 24 dies not expected to yield did deform, for an overall unpredicted deformation yield of 12.5%. Across all membranes, the model predicted yield for 54 of 60 dies, or 90%.
Table 1: Summary of plastic deformation reliability. The yield numbers and yield percent exclude membranes without sealed deformation volumes. The color code matches Figure 18.

<table>
<thead>
<tr>
<th>Membrane Radius [mm]</th>
<th>(deformed)/(total)</th>
<th>Yield [%]</th>
<th>(deformed)/(total)</th>
<th>Yield [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.5</td>
<td>0/4</td>
<td>0</td>
<td>0/4</td>
<td>0</td>
</tr>
<tr>
<td>1</td>
<td>3/4</td>
<td>75</td>
<td>2/3</td>
<td>67</td>
</tr>
<tr>
<td>1.5</td>
<td>3/3</td>
<td>100</td>
<td>3/4</td>
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<td>2.5</td>
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<td>2/4</td>
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</tr>
<tr>
<td>5</td>
<td>0/4</td>
<td>0</td>
<td>0/4</td>
<td>0</td>
</tr>
</tbody>
</table>

After venting the internal pressure, the magnitude of plastic deformation was measured using a WYKO white light interference microscope. Most of the membranes were too large to fit in a single frame, so a larger image of the membrane was stitched together using multiple measurements. As measured, the images were tilted. To flatten them, a plane calculated from the height at the edge of the membrane was subtracted off of the data.
The model does a reasonable job predicting the trend of the deformation and the relative magnitudes. However, it is clear that it underestimates the magnitude of the deformation. This is likely due to additional creep during the long anneal. It has been shown that at similar temperatures, silicon will slowly creep [39]–[41]. Of particular significance, Juan Ren et al. plastically deformed 2.5 mm diameter membranes at 900 °C under 1 atm of pressure. Deformation was measured after annealing for 1 hour (resulting in mostly plastic deformation) and again after 10 hours (resulting in plastic deformation and creep) [39]. The creep introduced an additional 20% strain to the membranes, which is consistent with the results measured here.

Several other factors could have affected the magnitude of the deformations. During the modeling, isotropic elasticity and plasticity were assumed for the silicon membranes. Images of the deformed membranes (shown in Figure 20) show that the plastic deformation was not perfectly symmetric. There are flatter regions on the top, bottom and sided, with more deformation between these areas. This is likely due to the fact the yield condition is not isotropic. Some planes, like those oriented at 45° from the flat, require less shear before slipping than others. This may lower the onset of plastic deformation for some angles, leading to an angular dependence in the yield temperature.
Figure 20: Optical microscope image of a 2mm plastically deformed membrane. Slip lines are visible on the membrane surface aligned with the wafer flat.

Additionally, variation in the design parameters could lead to more or less deformation. The annealing temperature during fusion bonding can have a large impact on the magnitude of the deformation. The furnaces used have a set point and three thermocouple measurements on the furnace tube itself (from back to front they measure the source, central and load zones). The thermocouple readings can vary from each other by as much as 10 °C within the furnace tube, and the maximum temperature may vary by as much as 15 °C from the set point. This can lead to significant variations in the intended anneal temperature. As seen in Figure 21, a change of 25 °C can change the magnitude of deformation.
Figure 21: Effect of variation in annealing temperature on the deformation magnitude.

The thickness of the SOI device layer can also vary by +/- 1 µm. This affects both the thickness of the deforming membrane and the depth of the deformation volume. These two effects act in opposition (a thicker membrane would also have a deeper deformation volume driving the plastic flow). In the end, variations in membrane thickness cause only slight shifts in the plastic deformation magnitude. This is shown in Figure 22.
The deformation volume is large enough that small changes in the dimensions are unlikely to dramatically change the deformation behavior. For instance, if resist was overexposed and the deformation cavity was 20 μm wider in both directions, the deformation volume would increase by only 2.5%. This would have a very small impact on small membrane radii, and increase the deformation for large radii by approximately 2.5%.

2.3 Epoxy Induced Contraction

2.3.1 Summary of deformation process

In this next section, the process for using epoxy to deform silicon membranes will be laid out.

Epoxy contracts as it cures. In this process, a membrane is formed by etching through the handle of an SOI wafer, and epoxy is placed in the cavity. A glass cap is placed on top to seal the epoxy. As it cures, the epoxy shrinks and pulls the membrane down with it. This is illustrated in Figure 23.
Epoxy 301 epoxy was used in this work. This is a two part epoxy that is compatible with silicon and glass. The material is not viscous and wicks well, allowing it to easily wet the surfaces of the membrane cavity. Additionally, the epoxy meets the NASA ASTM E595 standard for low outgassing. This means that after 24 hours at 125 °C, the material loses less than 1% of its starting mass (usually water) and produces less than 0.1% of condensable volatile compounds. Low outgassing makes the epoxy ideal for use in vacuum applications.

Bubbles in the epoxy can cause problems for the devices. Since bubbles are a lack of epoxy, in regions with large bubbles, the membrane does not deform as much as expected. Voids near the edges can lead to asymmetric curvature as well. Additionally, since the bubbles displace epoxy in the deformation volume, even small bubbles in sufficient quantities can decrease the overall deformation magnitude. Finally, the bubbles have an internal pressure near atmosphere. If the other side of the membrane is in vacuum, a large enough bubble could deform and affect the actuator performance. Assuming an approximately circular bubble, the displacement of the center of the bubble is given by:

$$w_{max} = \frac{\Delta P a^4}{64D}$$

A moderately sized bubble can have a big impact. A 1.51 mm bubble with a 1 atm pressure difference would have a 10 μm deflection, potentially interfering with the moving electrode prior to pull-in. This might lower the pull-in voltage, but it would also decrease the pumping volume of the device.

To prevent bubbles from being trapped in the epoxy, a specialized vacuum chamber was designed to cap the epoxy in vacuum. A slot is cut in the base to orient and restrain the device. A ¼” plastic rod is inserted through a compression fitting in the base. The compression fitting allows the rod to be raised and lowered while the chamber is under vacuum. The glass cap is balanced between the compression fitting rod and a metal pin, with several additional pins along sides ensure that the glass slide remains aligned with the chip. It is important that the glass slide not touch the epoxy until it is in perfect position. The surface tension of the epoxy is high.
enough that the slightest contact will cause the epoxy to wet the glass slide and pull the chip out of alignment. A cross-section of the design is shown in Figure 24.

![Diagram of the vacuum chamber and specialized jig used for applying epoxy.](image)

**Steps for application of epoxy**
1. Mix the two parts of the epoxy slowly. This minimizes bubbles entrained by the stir rod.
2. Degas mixed epoxy in vacuum chamber.
3. Place device in vacuum chamber.
4. Using micropipette, insert epoxy totaling 110% of cavity volume into the cavity. The epoxy will wick into the gap between the glass and silicon, the extra 10% ensures the chamber stays full.
5. Degas device with epoxy. Larger bubbles should pop, but a few small bubbles may remain.
6. Open chamber again, and balance glass cap over the epoxy reservoir.
7. Pump down, and pull out pin quickly. The epoxy has enough capillary force to pull the entire chip out of alignment if the glass cap is not dropped rapidly.
8. After the epoxy wets the perimeter of the cavity, bring chamber back to atmosphere. The increase in pressure causes any remaining bubbles to shrink before they cure.

**2.3.2 Deformation Results**
The shape created by epoxy contraction is quite different from that due to high temperature plastic deformation. Most of the curvature is near the edges, and the center of the device is largely flat. While the shape was flat, it was not always level with the wafer surface. It is believed that this is due to imperfect settling of the glass cap. The glass cap was not very heavy; it is not surprising that it did not always squeeze the epoxy uniformly and settle flat. Future designs could add weight to the glass after applying the epoxy or simply clamp the surfaces together to prevent the uneven profile.
Both the flat shape and uneven settling are shown in Figure 25. The flat slope varies by ~2 μm, or 10% of the overall deformation. With uniform epoxy strain, that could correspond to glass cap that was 40 μm higher (10% of the handle thickness) on the left side of the image.

Figure 25: White light interference image showing the characteristic flat shape of the epoxy contracted membranes. A 6 mm membrane is shown here. The shape is not perfectly level due to imperfect settling of the glass cap. At the edge of the membrane the slope is high enough that not enough light is reflected back to the microscope objective to register as a measurement.

The epoxy deformation is consistent. 100% of the devices which had epoxy applied deformed, with fairly repeatable results compared to plastic deformation. A summary of the deformation results is shown in Figure 26.

For 6 mm diameter membranes, the mean deformation was 17.8 μm with a standard deviation of 1.4 μm. For 2.5 mm diameter membrane, the mean deformation was 7.6 μm with a standard deviation of 1.4 μm. It should be noted that this is a relatively small sample size – (10) 6mm membranes and (17) 2.5 mm membranes.
2.3.3 Comparison to plastic deformation

The different fabrication methods both have benefits and difficulties. Understanding these issues can help designers choose the correct deformation method.

One of the biggest factors for curved actuator performance is the reliability of the deformation process. From the plastic deformation short loop test, the yield across membranes predicted to deform is ~90%. This is unfortunate for testing actuators, and but would have a more serious impact on pump testing. If a single membrane is not deformed, the entire device is useless; for a 2 stage vacuum pump with 6 diaphragm actuators, which leads to a yield of 53% for a single step. This is acceptable yield for a research setting, but would need to be significantly improved in a production environment.

On the other hand, epoxy is a much lower risk process. The yield for epoxy was 100%. Additionally, epoxy is applied at the die level after processing is complete. This means that any risk is spread over only a single die, rather than allowing a single bad deformation process ruin an entire wafer worth of devices. It avoids a high temperature process, which could be valuable for other devices with heat sensitive materials, such as polymer structures or certain high mobility metals.

In addition to deformation yield, the magnitude of the plastic deformations is less predictable than for epoxy. As can be seen in Figure 19, even in a process designed for high quality plastic
deformation, the magnitude varies by as much 10 µm from the average value, while the epoxy
has a standard deviation of 1.4 µm.

Plastic deformation does bring several benefits as well. The magnitude of the deformation can
be controlled by varying the deformation volume and annealing temperature, but the only way to
change the magnitude of epoxy contraction is to increase the size of the well. This can be done
by using an SOI with a thicker handle. Performing the deformation at the wafer level in batches;
this could be much more efficient than applying epoxy to each individual die one at a time. If
the deformation yield and variability could be controlled, this process would likely be a better
candidate for commercial production.

The two shapes perform differently as electrostatic actuators. Test results will be compared in
Section 3.2.3, and modeling results will be presented in Section 4.5.2.

2.3.4 Controlling the curved electrode shape
The order that the epoxy cures matters to the deformation. Does it set first, then shrink? Or does
the contraction happen throughout the process while the epoxy cross-links? At room
temperature, the epoxy requires 24 hours before it is ready for testing. After 12 hours, no
deformation is visible, but the glass slide was well attached to the device. After 24 hours, the
full deformation was visible. The epoxy first hardens, then contracts.

This opens up the possibility of shaping the electrode further. Until this point, we had only
added epoxy to the membrane cavity. If we add other filler material, we may be able to achieve
locally decreased deformation. This was demonstrated experimentally by adding 400 µm ball
bearings to the epoxy in the cavity. The surface profile is shown in Figure 27.

![Surface Profile](image)

**Figure 27:** Surface profile (left) of a 6 mm membrane deformed using epoxy with two 400 µm ball bearings added. The bearings are clearly visible in the surface map and the cross section (right).

This is desirable because we may be able to tailor the electrode so that the narrow gap regions
are in more desirable locations. In the current devices, the edges have the largest forces but are
also rigidly attached to the bulk of the wafer. If we could create a narrow gap region in the center of the wafer, the pull-in voltage could be reduced even further. A diagram showing this is shown in Figure 28.

![Diagram showing normal curvature vs. controlled curvature](image)

Figure 28: Diagram showing the benefits of controlling the curved electrode shape. In the normal curved shape, the forces drop off rapidly away from the edge. In the controlled curvature shape, large forces are possible in the center as well.

With the current method, spherical ball bearings create a small point with less deformation. This could be done with more sophisticated filler or a mold. Features could be patterned into the glass cap, removing the need for filler entirely.
Chapter 3

Electrostatic Actuation

For curved electrodes to be useful, it must be possible to integrate them into useful devices. In this next section, methods will be presented to fabricate and test electrostatic actuators using curved electrodes. The device design and fabrication process will be presented first, followed by test results.

3.1 Incorporating curved membranes into actuators

In this first section, the design of a MEMS electrostatic actuator with a curved electrode will be presented, followed by the fabrication procedure.

3.1.1 Curved Actuator Process Design

The full MEMS vacuum pump design (discussed in Section 5.1.3) involves 5 wafers and 14 separate masks. In the MIT cleanroom, it takes at least 2 months to fabricate. In order to test the pumping mechanism (the curved electrostatic actuators) more rapidly, a new design with fewer steps was needed. The modified devices would only include the elements necessary to verify the fabrication process and test the electrostatic actuators. Instead of five wafers and 14 masks used in the full pump design, the new process uses only two wafers and five masks. Instead of two months, the process would take two weeks in MIT’s cleanroom.

Plastic deformation is driven by a controlled volume, called the deformation volume. For this process, two volumes were used. The smaller deformation volume was 1mm x 4mm on a side, while the larger volume was 2mm x 4mm. Both were etched through the device layer of the 20 μm wafer. The deformation volume is connected to the back side of the deforming membrane via a narrow channel.

This process is discussed in greater detail in Section 3.1.2. It went through several iterations before reaching this final point; several design improvements are described below.

In these devices, two conductive electrodes are positioned on top of each other with an insulating layer between them. This creates a capacitor with a gap that varies as the plates attract each other. In the ideal case, electrical isolation is perfect – applying a voltage between the plates results in an attractive force but no current between the layers. In actual devices, however, electrical isolation proved to be a recurring issue during electrostatic testing. Working devices have a large resistance (on the order of 10 MΩ), but the isolation could fail (and the resistance would drop) for two reasons.
1. **Shorting due to contamination:** Many of the early devices were shorted (resistance ~ 1 kΩ) before any voltage was applied. While the two electrode layers are separated by an oxide layer, shorting can easily occur across the oxide at the edge of the device. For a 1cm x 1cm chip, there is a 4cm perimeter. For the chip to short, a single particle (dust, particles from dicing, etc.) needs to bridge the 1 μm of oxide insulation. Even etching through the device layer to separate the actuators from the edge does not prevent shorting – a particle can bridge the gap between the device layer and the handle, with a separate particle bridging the handle and the other device layer. Only by etching through both the device layer and handle can the chip be safely protected from electrical shorting. This is illustrated in Figure 29 below.

2. **Shorting to dielectric breakdown:** Other devices shorted during testing, after a moderate voltage was applied. As the voltage was increased, eventually the resistance across the device under test would disappear (dropping to < 1 kΩ). The breakdown strength of oxide is ~900 V/μm [42]. Early devices used 100 nm of oxide on only one of the electrodes, leading to dielectric breakdown as low as 90 V. The oxide thickness was increased to 500nm to prevent breakdown, but this does decrease the actuation force and increase the necessary voltage for pull-in.
Figure 29: Illustration of shorting paths for three potential devices. The isolation trenches surround the diaphragm, isolating it from the wafer edge. Three designs are shown: no isolation (top), device layer isolation (middle) and complete device and handle isolation (bottom).

For the first four short loop tests, actual pump masks were used which had 6 mm diameter pump membranes and 2.5 mm diameter valve membranes. Both sizes plastically deformed, but only the 6 mm diameter membranes pulled in. An applied voltage caused the 2.5 mm diameter membrane to displace, but pull-in was not observed. The stiffness of a circular membrane is $\propto a^4$, so decreasing the diameter from 6 to 2.5 mm increases the stiffness by a factor of more than 33. Deforming the 2.5 mm membranes less would create a narrower gap that might be able to overcome this high stiffness. However, as discussed in Section 2.2, it is very difficult to achieve stresses large enough to plastically deform a small membrane without causing large deformations.
It was clear that the diameter of the membrane has a large impact on the stiffness and pull-in voltage, but with a single data point (6mm membranes) it was hard to know the magnitude of the impact. To investigate this, a new mask set was designed that incorporated several variations:

- Eight membrane diameters: 1, 2, 3, 4, 5, 6, 8, 10 (all in mm) were spread across the wafer.
- Two deformation volumes were used. They had the same depth (22 μm) but different etched areas (2 or 4 mm²). These will lead to different deformation magnitudes.

There are 16 combinations of membrane diameters and deformation volumes. Each combination appears 4 times on a wafer. This created a range of membrane radii and deformation magnitudes, and allowed for a more complete exploration of pull-in voltages.

Due to the similarity with the plastic deformation experiments in 2.2.3, many of the masks were reused for this process. The cleavage lines that were used to separate the dies for the earlier experiments were not used when creating electrostatic actuators. Instead, the wafer was diced.

A summary of the problems encountered while testing devices created using the actual pump masks and the corresponding design changes included in the new masks are summarized in Table 2 below.

<table>
<thead>
<tr>
<th>Problem</th>
<th>Design improvement</th>
</tr>
</thead>
<tbody>
<tr>
<td>Membranes stick after pull-in</td>
<td>Increase anti-stiction bump density</td>
</tr>
<tr>
<td>Devices shorted before testing</td>
<td>Etched insulation trenches through handle and device layer</td>
</tr>
<tr>
<td>Devices shorting at moderate voltages</td>
<td>Increase oxide thickness at bond interface from 100nm to 500nm.</td>
</tr>
<tr>
<td>Large wafer bow affecting bond quality</td>
<td>Added PECVD oxide to compensate BOX stress</td>
</tr>
<tr>
<td>Small membranes do not pull in</td>
<td>New masks with a wide range of membrane diameters</td>
</tr>
</tbody>
</table>

3.1.2 Fabrication Process

In this next section the fabrication process for the curved electrode short loop will be discussed. Images of the device are shown in Figure 30, with a cross-section in Figure 31.

The process is very similar for high temperature plastic deformation and epoxy contraction. The following description will be specifically for plastic deformation. Changes needed for epoxy contraction will be described at the end.

All microfabrication was carried out at the Microsystems Technology Laboratories (MTL) at MIT. The starting substrates are both 150mm diameter SOI wafers (Ultrasil). Both wafers have a 400 μm thick handle and a 2 μm BOX layer. One wafer (henceforth referred to as the
deforming wafer) has a 40 μm thick device layer, while the other wafer (called the flexible wafer) has a 20 μm thick device layer.

Figure 30: Top views of actuator design. The cleavage features were used for plastic deformation tests in 2.2.3, and are not used in these devices. (Left) Die schematic formed by overlapping masks. Anti-stiction bumps have been omitted for clarity. (Right) Top view showing a 10 mm diameter flexible membrane. The color variation on the membrane and via surfaces are due variations in the oxide thickness resulting from etch non-uniformity.

Figure 31: Cross-section of short loop electrostatic actuator design.

The fabrication process is outlined in Figure 32 and Figure 33 below.
Flexible wafer (20 μm device layer)

1A) Grow 0.5 μm thermal oxide and 100nm nitride on both sides, 2.5 μm PECVD oxide on back for stress compensation.

2A) Pattern and etch anti-stiction bumps.

3A) Pattern pump (handle only) and deformation cavities and isolation trenches (both sides), dry etch through oxide and nitride.

4A) Spin thick resist on both sides, pattern deformation cavities and isolation trenches on front. DRIE to BOX, use BOE to strip exposed oxide.

Deforming wafer (40 μm device layer)

1B) Grow 0.5 μm thermal oxide and 100nm nitride on both sides, 2.5 μm PECVD oxide on back for stress compensation.

2B) Roughen ox on membrane surface.

3B) On back, pattern membrane and via, dry etch through oxide and nitride, DRIE through handle, use BOE to strip BOX from membrane.

Figure 32: Fabrication steps before bonding for both the 20 μm flexible wafer and 40μm deforming wafers.
Both Wafers

1) Strip nitride, fusion bond to plastically deform

2) Pattern isolation, via, membrane, DRIE to BOX

Silicon  Nitride  Oxide

Figure 33: Fabrication steps including wafer bonding and final DRIE etch.

Wafer preparation, (both wafers)

Immediately out of the box, a 550 nm thermal oxide layer is grown on both wafers, followed by a thin 100 nm thick nitride layer. The oxide will be the bonding surface and provide dielectric isolation for the silicon electrodes, while the nitride layer protects the bond surface from scratches and chemical residue during processing.

The wafers have a large wafer bow of ~160 μm out of the box due to the thick buried oxide (BOX) layer. This high curvature makes achieving a good quality bond very difficult. To compensate for the bow, a 2.5 μm oxide layer is deposited on the back side of the wafer prior to any etching. This lowers the wafer bow to ~40 μm, which is safe for wafer bonding [43]. Before and after the deposition of the oxide, the wafer bow was measured using a Tencor FLX 2320 wafer curvature measurement system.

Wafer 1 (40 μm SOI, Lower fixed electrode)

To make hermetically sealed actuators, we need to be able to control which portions of the wafer bond and which do not. It is well known that smooth surfaces bond well, while rough surfaces do not. With this in mind, a dry etch was used on the device side to remove the nitride and slightly roughen the oxide from surface of the pump membrane. The goal is not to etch the oxide, but to roughen it with plasma and ion bombardment so that it is unbondable.
On the handle side, the membranes on the deforming wafer are defined before the bond. The oxide and nitride layers can be patterned with dry etches. This is followed by etching through the handle with DRIE, stopping on the BOX layer. Finally, BOE is used to strip the exposed BOX layer from the back side of membranes. Resist is used to protect the oxide on the device side.

**Wafer 2 (20 µm SOI, Upper flexible electrode)**

As discussed above for wafer 1, a dry etch of the membrane surface will remove the nitride and slightly roughen the oxide. To further decrease the contact area between surfaces during actuation, for wafer 2 we are dry etching 50 nm into the oxide to create 10 µm diameter, 35 µm pitch bumps across the circular membrane. These raised bumps decrease the contact area between the two membranes during actuation. These bumps are shown in Figure 34.

![Figure 34: Anti-stiction bumps were included on the deforming membranes to prevent stiction. Variations in coloration between the two images are largely due to the angle at which the oxide is viewed.](image)

Following the anti-stiction bumps, a dry etch is used to remove the nitride and oxide layers from the vias, isolation trench and deformation cavity features on both sides of the wafer as well as the membrane shape on the handle side. A second dry silicon etch of the venting channel is needed to increase the depth to 3 µm. This creates a 3 µm x 200 µm cross section channel between the circular membrane and the deformation volume.

Finally, using a DRIE we can create the deformation cavities by etching through the device layer. BOE is used to remove the oxide layer at the bottom of the deformation cavity. This creates a deformation cavity 22 µm deep (including oxide thickness).

**Both Wafers**

To prepare for bonding, the wafers are thoroughly cleaned, followed by stripping the nitride in hot phosphoric acid, and finally an RCA clean (without the HF dip, which would roughen the
oxide). The upper and lower wafer are aligned and pressed in a wafer bonder. If the wafers are to be plastically deformed, they are contacted in nitrogen; otherwise they are contacted in vacuum. After pressing overnight, the wafers are annealed in a diffusion furnace for a variable time and temperature (see section 2.2.1).

DRIE is used to etch through the handle using double thick resist. The initial fabrication process used a single coat of thick (12µm) resist, which etched away around 400 µm into the through wafer etch. DRIE tools improve the selectivity of their etches by cooling the wafer. To do this, they rely on a combination of a water cooled chuck and a pocket of helium between the chuck and wafer to conduct heat away from the wafer. Unfortunately, the deep features on the back side of our wafer prevent a good seal and allow helium to leak out. To mitigate this, blue tape is applied to the back side of the wafer before the etch to provide a better seal. However, even with blue tape on the back side the wafer, it does not fully cover the lip seal and some helium leaks out. This leakage, along with the lower conductivity of the blue tape itself, leads to poor thermal contact, a hot wafer, and terrible resist selectivity, necessitating two coats and approximately 20 µm of resist.

After this final etch, the channel to the actuator chamber is open. Any liquid that gets into the inner channel is likely to wick into the actuator chamber. As the liquid dries, surface tension can pull the two membranes into contact, causing them to irreversible stick together. This phenomenon is called stiction. For this reason, no wet etches or solvents can be used after this point.

Following the etch, the wafers are heated (5 minutes) to soften the tape, allowing it to be easily removed. Any additional resist is removed in O2 plasma in the asher. Prior to dicing, UV release dicing tape is applied to both sides of the wafer. Great care must be taken to remove any air bubbles from between the tape and wafers, because these air pockets create channels for dicing liquid and particles to enter the devices. Finally, the wafers are diced using the isolation trenches as guide lines.

Images of the final wafer stack and dies are shown in Figure 35. A cross-section of the deformed structure is shown in Figure 36.

More details on the fabrication steps, as well as mask diagrams, are shown in .

3.1.3 Variations for Epoxy Induced Contraction
Using epoxy induced contraction to create curved electrodes uses almost the same process as was used to create plastically deformed membranes. The critical difference is that for epoxy contraction the bonding should be done in vacuum, while for plastic deformation the wafer bonding needs to be done in nitrogen. This prevents plastic deformation from affecting the final membrane shape.
For the devices tested Section 3.2.2, the wafers were bonded before the two DRIE through wafer etches. Waiting until after bonding decreases the risk that the thin membranes will rupture during processing. This is possible because the epoxy contraction happens at the end of the process, not during the high temperature anneal.

![Figure 35: (Top) Processed wafers for electrostatic actuation tests. (Bottom) 10 mm diameter membrane dies are shown on the bottom.](image-url)
3.2 Electrostatic Testing

In this section, the experimental results for electrostatic pull-in using curved electrodes will be presented. Results for electrostatic actuation at atmospheric pressure and low pressure will be presented, and the reliability of the actuators will be shown.

3.2.1 Experimental Set-up, Atmospheric Actuation

In order to make electrical contact to silicon, metal contact pads are typically deposited and annealed to form a silicide eutectic layer. This layer decreases the contact resistance and ensures that low voltage signals can pass into the semiconductor. If a conductor is placed directly onto the silicon surface, a Schottky barrier is formed and signals passing through the interface are rectified. However, throughout testing, the current through the device was quite low. The power supply was limited to 20 mA, and rarely was above 1 mA. This means that any rectification was minimal, and the voltage drop across the Schottky barrier was quite small. Additionally, the silicon used is sufficiently conductive (0.01 – 1 Ω-cm) that resistive losses are negligible. This means that the electrostatic actuator provides a majority of the impedance in the circuit, and thus the almost the entire voltage drop is across the actuator, without needing metallization on the contact pads. As such, we have simplified the process and not included metal contact pads on these devices.

Instead, conductive epoxy (MG Chemicals 8331 silver conductive epoxy) was used to make contact with the wafers. A large bead of epoxy was placed in the via, with the epoxy rising out of the hole. To connect the device to the power supply, an alligator clip with one side insulated using Kapton tape was attached to the chip, oriented so that the teeth contacting the epoxy bead.

Voltage was applied using a Stanford Research Systems PS310 high voltage power supply. As voltage increased, many chips started to short or break down. This was monitored using a separate resistor in series with the device under test. At higher voltages, some of the actuators started to “leak”, allowing current through. By subtracting the voltage across the measurement
resistor \( R_m \) from the applied voltage, the actual potential across the actuator can be calculated. A circuit diagram for the testing layout is shown in Figure 37.

![Circuit Diagram](image)

Figure 37: Electrical layout for pull-in testing. The measurement resistor \( R_m \) allowed accurate measurement of the voltage across the device.

Actuation and pull-in were monitored using a white light interference microscope (ZYGO NewView 600). This tool can monitor the interference fringes created by reflecting white light off of the sample. Using a piezoelectric stage to move the objective vertically, changes in the fringe position can be converted into the surface profile of the substrate. In a few seconds, the moving objective will scan fringes across the entire surface, creating an elevation map of the sample.

Once a chip has been placed on the stage and levelled, even slight changes in the actuation voltage cause displacements that are visible as shifts in the fringes. After the pull-in voltage is reached, there is a dramatic shift in the fringes as the upper membrane snaps down to the curved electrode. During testing, voltage was increased in small increments. The surface map at each step as well as the critical pull-in voltage was recorded.

This method can only be used to measure actuation in atmospheric pressure. At lower pressures, the device must be completely enclosed to maintain a pressure drop, and the interference microscope cannot focus through another surface. Even if the enclosure is transparent, fringes are only seen on the top surface of the chamber and not on lower reflective surfaces of the device. A different method for monitoring pull-in in vacuum will be discussed in Section 3.2.2.

At higher voltages, it became evident that with a constant applied DC voltage the pull-in voltage was slowly increasing with time. Switching the polarity of the voltage caused the pull-in voltage to revert to its initial value. This can be seen in Figure 38 below.
This is likely due to charging of the dielectric. A constant high voltage drives electrons into the dielectric. These trapped charges in the SiO₂ effectively decrease the electric field in the device, requiring additional voltage to achieve the same force needed for pull-in. Reversing the polarity allows the charges to dissipate. In actual operation, using a square wave centered at 0 V would solve this problem. Since the electrostatic force is proportional to \( V^2 \), the force would be unchanged, but charging would be minimized.

### 3.2.2 Actuation at vacuum conditions

When actuating in atmosphere, the actuator must push the air out of the actuation volume during pull-in and suck it back in each cycle. The venting channels are quite narrow by design – this lowers the dead volume in a vacuum pump allowing lower base pressures. However, the fluidic resistance of the venting channel limits the actuation speed of the curved electrostatic actuator. To get a sense for the limits of the device, it is necessary to test it in a vacuum environment where the fluidic resistance is negligible.

The custom built vacuum chamber built for degassing epoxy was redesigned for vacuum testing. The chamber lid is machined from acrylic, the walls are an aluminum tube, and base is machined aluminum. Electrical connections are fed through a \( \frac{1}{4}'' \) plastic tube, which is then filled with epoxy (Epo-tek 301) to seal the tube. The tube is pushed through a compression fitting (Swagelock Ultra-Torr, \( \frac{1}{4}'' \) NPT) to make a vacuum tight seal. Alligator clips can be attached to the end of the wires after they have been threaded into the chamber. While this design does not reach high vacuum, it can easily achieve pressures of 10 Torr and can be disassembled for rapid testing of multiple samples. A diagram and picture of the chamber are shown in Figure 39.
Pressure Sensor Clamp holds electrical connection chip level to back side

To Vacuum Pump

Electrical connection to front side

Figure 39: Vacuum chamber used to test electrostatic actuators at reduced pressure.

As a membrane is actuated in the chamber, the reflection off the surface changes. Most membranes start slightly convex, which causes them to slightly magnify and distort a reflected image. As it displaces and eventually pulls in, the shape becomes more and more concave. This means the reflected image shrinks and blurs. This can be viewed as a mirror with large aberration because the shape is concave/convex but not close to a parabola.

In order to more clearly visualize this, a mesh grid printed on a piece of paper was placed on the lid of the chamber. An illustration of how this works is shown in Figure 40, and images showing how the actuation affects the reflection is shown in Figure 41.

Figure 40: Illustration of how grid pattern is reflected and distorted off of the actuator surface.
Figure 41: These images show the reflection off of a 5mm membrane with $W_{eff} = 15 \mu m$, actuating in vacuum. With no applied voltage (left), the grid pattern can easily be seen. Some initial bow due to the uneven oxide thicknesses causes the reflection to warp slightly. (Right) After applying the pull-in voltage the image becomes quite distorted.

As discussed before, applied DC causes the pull-in voltage to slowly increase. For the tests in vacuum, a square wave (centered at 0 V) was used to prevent charging. Using the same high voltage DC power supply and four PVT412 Photovoltaic relays in an H Bridge configuration (shown in Figure 42), a high voltage square wave was generated. A Hewlett Packard 3314A function generator with a 5V square wave output was used to switch the relays. The terminals (ground and $A = \pm 5$ V) are flipped on two of the relays so that they pass current out of phase of the other two.

At frequencies above $\sim 100$Hz, the relays cannot switch fast enough to form a clean square wave. However, the highest frequency needed for testing these devices was $\sim 2$Hz. This high frequency degradation is shown in Figure 43.
Figure 42: H Bridge configuration using 4 optical relays. This circuit creates a square wave across the device under test (DUT) of magnitude $V_{app}$ centered at 0V. The control voltage, $A = \pm 5V$, is a separate square wave that sets the switching frequency.
Figure 43: Square wave created by optical relays in H Bridge configuration. At higher frequencies above ~100Hz, the relays cannot switch fast enough to generate a sharp square wave. Actuation remained below 10 Hz throughout testing.

Testing at low pressures confirms that damping was one of the limiting factors for pumping speed at atmospheric conditions. To operate even faster, larger voltages could be used to speed up pull-in. Thicker membranes would help the membrane spring back faster, but would also increase the pull-in voltage.

3.2.3 Actuation results
In this section, results from the short loop tests will be discussed. Devices with plastically deformed electrodes will be presented first, followed by devices with epoxy contracted electrodes.

Early wafers only had 2.5 mm diameter valve and 6 mm diameter pump membranes. As discussed before, the 2.5 mm membrane did not pull in, but the results from the 6mm membranes are shown in Figure 44. Two rounds of fabrication had different oxide thicknesses, allowing us to compare the pull-in voltage for 100nm and 500nm effective gaps ($g_{eff}$).
A separate round of fabrication used a mask set that varied the membrane radius and deformation volume. After fabrication, it became clear that the "flat" membrane was actually significantly bowed. This was due to different oxide thickness on the two membrane surfaces. Oxide films have a compressive residual stress; mismatched in oxide layer thickness can cause membranes to bend. The lower surface had 0.45 μm remaining after etching the anti-stiction bumps. The upper surface has the BOX layer, which starts at 2 μm. The mismatch resulted in a very large bow. The BOX layer was etched back, but etch non-uniformity led to variations between 0.4 and 0.6 μm in the final BOX, and some bending of the membrane remained.

To correct for this, we will introduce an effective curvature magnitude, which is simply the difference between the measured plastic deformation and the oxide bow.

\[ W_{eff} = W_{max} - W_{oxide} \]
Figure 45: A comparison of the intended geometry (left) and the fabricated geometry for tested electrostatic devices.

This is a reasonable approximation because even though one electrode is curved, they are both still relatively flat. In the devices tested for this thesis, the largest angle in any of the curved membranes was 1.8° for a 3mm diameter, 30μm plastically deformed membrane. As such, the electrostatic force is reasonably approximated by the parallel plate form, with the gap varying locally based on the curvature. The oxide bow simply changes the curvature slightly.

The pull-in voltages from these devices, compared with the effective magnitude $W_{\text{eff}}$, are shown in Figure 46. Larger diameter membranes clearly have lower pull-in voltages, and devices with more plastic deformation require larger pull-in voltages. In Section Chapter 4, a model will be developed to predict pull-in for these devices and other curved electrode geometries.
Figure 46: Pull-in voltages for electrostatic actuators with one plastically deformed electrode compared to the magnitude of curvature. These devices all have an effective gap \( (g_{eff}) \) of 0.43 \( \mu \)m. The effective gap is derived in Section 4.1.

Epoxy deformed devices involved slightly different testing procedures. The epoxy devices came from a separate round of fabrication than the plastically deformed devices. The devices vent properly while the membrane is up, but when actuated, the venting channel does not rapidly vent the device. Testing in air and in vacuum showed that the pull-in voltage was identical.

With this in mind, the epoxy devices were all tested in vacuum. This way they were vented prior to actuation, and a more accurate pull-in voltage could be determined. Pull-in was repeated several times to verify the minimum pull-in voltage was found.

As with the plastically deformed devices, the oxide thicknesses on both sides of the flexible membrane were not perfectly balance, leading to curvature in the flexible membrane. The lower surface had 0.45 \( \mu \)m of oxide remaining after etching the anti-stiction bumps. The upper surface has the BOX layer, which started at 0.5 \( \mu \)m thick. However, the BOX layer was the etch stop for the final DRIE etch. The DRIE etch rate varies over the membrane surface. This meant that in order to clear the entire membrane of silicon, the edges were over-etched. This created a variable oxide thickness, where some parts were as thin as 0.4 \( \mu \)m, and other regions were at...
exactly 0.5 μm. The magnitude of flexible membrane curvature was measured using a white light interference microscope.

In order to measure the magnitude of epoxy deformation, the upper membrane had to be removed. To do this, the edge of the membrane was scored with a scribe and tape was used to rip the flexible membrane off of the device. The membranes were sturdy enough to resist tape without scratching the surface. After a majority of the membrane was removed, the surface was measured using the white light interference microscope.

In many cases, some of the flexible membrane remained at the edge. In this case, a measurement was taken including the flexible membrane, and the extra height was subtracted off of the resulting data to ensure an accurate measurement of the deformed electrode shape.

The pull-in voltages from devices with epoxy contracted electrodes plotted against the effective magnitude $W_{\text{eff}}$, are shown in Figure 47. Larger diameter membranes clearly have lower pull-in voltages, and devices with more plastic deformation require larger pull-in voltages. In Chapter 4, a model will be developed to predict pull-in for these devices and other curved electrode geometries.

![Figure 47: Pull-in voltages for electrostatic actuators with one epoxy contracted electrode compared to the magnitude of curvature. These devices all have an effective gap ($g_{\text{eff}}$) of 0.23 μm. The effective gap is derived in Section 4.1](image-url)
3.2.4 Reliability
To test the reliability of the actuators, the levels of the square wave were shifted from +/- 50 V to 0 – 50 V. At higher frequencies, the actuator could not release, but around 1 Hz the membrane was able to pull in and release once per period.

The membrane was cycled continuously for an hour (>3600 pull-in events) with no decrease in the pull-in voltage or stiction. Several days later, the test was repeated with no change in the device performance.

Some charging would be expected for actuation with the same polarity over long periods of time. However, for this device, no charging was observed. If charges were being trapped in the dielectric, the pull-in voltage would have increased over time. However, pull-in was observed at 50 V throughout the test. This is likely due to the low voltages used. Charging was much more noticeable above ~100 V (see Figure 38).
Chapter 4
Electrostatic Modeling

In our devices, we are using the pull-in instability to zip one flexible electrode onto a thicker (and thus stiffer) curved electrode. This is been done before using flat electrodes of various geometries [44], and less commonly with curved electrodes [13]–[15].

Both Li et al. and Legtenberg et al. fabricated curved cantilever actuators by using DRIE to cut precise cantilever shapes through the device layer of an SOI, and released by undercutting the buried oxide layer. Li demonstrated that curved cantilevers dramatically lowered the pull-in voltage [45]. Legtenberg designed beams that had a polynomial curvatures ($w(x) \propto x^n$). They demonstrated that curved beams with $n \leq 2$ would pull in, while higher powers would not [13].

To intelligently design an electrostatic actuator and appropriate experimental, it is necessary to have understanding of the underlying physics. In the next several sections, we will discuss the origins of the electrostatic force and pull-in, and then review the principle of virtual work and how it can be used to find approximate solutions to solid mechanics problems. Finally, we will develop and use a first order model utilizing the principle of virtual work to predict the pull in instability in curved geometries, and compare these predictions to the experimental results.

Before proceeding further, a few comments should be made about notation used in this chapter. Lowercase $w$ will be used to refer to the flexible membrane, while uppercase $W$ refers to the rigid (usually curved) membrane. A tilde over a variable means it has been non-dimensionalized. As an example, $\tilde{W}(r)$ is the non-dimensionalized rigid electrode shape.

4.1 Electrostatic Force and Pull-in for Parallel Plates
Parallel plates with a generalized spring are a useful approximation for many real situations. In this section, two situations (shown in Figure 48 below) will be considered.
In the first case, two rigid parallel plates are separated by a fluid with uniform relative permittivity $\varepsilon_1$ and with $z_0$ denoting the space between the solid surfaces. In the second case, there is an additional dielectric layer of thickness $t_2$ and relative permittivity $\varepsilon_2$. A voltage is applied using an external power supply, which creates an electrostatic force between the two charged plates. If the upper plate moves by a small increment ($z \rightarrow z + \Delta z$) while keeping the voltage constant, work is done against the electrostatic force. As $z$ increases, the capacitive energy decreases and the power supply absorbs charge (and energy) from the system.

\[ F_e \Delta z = -\Delta U_c + \Delta U_{ps} \]  \hspace{1cm} (4.1)

Where $F_e$ is the electrostatic force, $U_c$ is the energy stored in the capacitor, and $U_{ps}$ is the energy provided by the power supply. We can divide by $\Delta z$ and take the limit as the displacement goes to zero:

\[ F_e = -\frac{dU_c}{dz} + \frac{dU_{ps}}{dz} \]  \hspace{1cm} (4.2)

The electrostatic energy stored in a capacitor is given by:

\[ U_c = \frac{1}{2}CV^2 \]  \hspace{1cm} (4.3)

The energy provided by the power supply is related to the charge required to maintain a constant voltage across the plates:

\[ U_{ps} = q_e V \]  \hspace{1cm} (4.4)

Charge stored in a capacitor at a given voltage is $q_e = CV$. Using this we can combine the two energy terms in:

\[ F_e = \frac{1}{2}V^2 \frac{\partial C}{\partial z} \]  \hspace{1cm} (4.5)

With a uniform gap (first case in Figure 48), the parallel plate capacitance is $C(z) = \varepsilon\varepsilon_0 A/(z_0 + z)$, where $\varepsilon$ is the relative permittivity of the material in the gap. Therefore the electrostatic force is:
The negative sign means that the force is attractive between the two plates.

In many real devices, the gap is partially filled with two materials, such as air and a dielectric material like silicon dioxide (SiO2) (this corresponds with the second case in Figure 48). Again, let us define the space between the solid surfaces as \( z_0 \) (meaning the space between the conductors is \( z_0 + t_z \)). The dielectric causes the capacitance to change, but the calculation is mostly the same:

\[
C(z) = \frac{\varepsilon_0 A}{t_2/\varepsilon_2 + (z_0 + z)/\varepsilon_1} \tag{4.7}
\]

\[
F_e = -\frac{2\varepsilon_1(t_2/\varepsilon_2 + (z_0 + z)/\varepsilon_1)^2}{\varepsilon_0 A V^2} \tag{4.8}
\]

This reduces to (4.6) as the thickness of the dielectric goes to 0. Without a dielectric, the force is proportional to an actual gap between the conductive planes, \( g = z_0 + z \). With a dielectric, the force is proportional to an effective gap between the conductors:

\[
ge_{eff} = z_0 + z + \left(\frac{t_2}{\varepsilon_2}\right) \tag{4.9}
\]

From a force perspective, the only effect of adding a dielectric layer is that it changes the force by linearly changing the effective distance between the two plates.

Now consider the force balance between the electrostatic attraction and the spring stiffness. At a stable equilibrium point,

\[
\Sigma F_z = F_s + F_e = kz - \frac{\varepsilon_1\varepsilon_0 A V^2}{2(z_0 + z)^2} = 0 \tag{4.10}
\]

The stiffness of the system can be found by taking the derivative of the sum of forces:

\[
k_{eff} = \frac{d(\Sigma F_z)}{dz} = k - \frac{\varepsilon_1\varepsilon_0 A V^2}{(z_0 + z)^2} \tag{4.11}
\]

When the voltage is 0, the effective stiffness reduces to the spring constant. As the voltage increase, the system stiffness goes down. When the system stiffness becomes negative, the parallel plates are unstable and will snap together. This can be found easily by solving for \( k z \) in (4.10) and substituting into (4.11):

\[
0 = k - \frac{\varepsilon_1\varepsilon_0 A V^2}{(z_0 + z_c)^2} \tag{4.12}
\]

where \( z_c \) is the critical height of \( z \) where the system is unstable. It is clear that \( z_c = z_0/3 \). Substituting this value back into (4.10), we see that the required voltage is:
\[ V_{pl} = \frac{8kz_0^2}{27\varepsilon_1\varepsilon_0 A} \quad (4.13) \]

For parallel plate actuators with an additional dielectric, \( z_0 \) is replaced by

\[ g_{eff}\big|_{z=0} = z_0 + \frac{\varepsilon_1}{\varepsilon_2} \quad (4.14) \]

### 4.2 Beyond the Parallel Plate Model

In this next section, we will relax several of the assumptions used to calculate the electrostatic force and pull-in voltage for two rigid parallel plates.

First, consider the upper moving plate. In realistic devices, it is not a rigid, flat plate that stays flat as it translates up and down. The plate deforms under the electrostatic load, and must be attached in some way to a support for the structure to spring back after loading. Using \( w(r) \) to define the deflection of the flexible electrode, we can account for plate bending by using the Kirchoff-Love bending equations (valid for small deflections). The full dynamic equation (for small displacements) for our plate is:

\[ \frac{1}{a^2} \frac{\partial^2 w}{\partial t^2} + 2c \frac{\partial w}{\partial t} + D\nabla^4 w - T\nabla^2 w = \rho_e \quad (4.15) \]

where \( \rho \) is the membrane density, \( h \) is the thickness, \( w \) is the membrane displacement, \( t \) is time, \( c \) is the damping coefficient, \( D = Et^3/(1 - v^2) \) is the flexural rigidity, \( T \) is the membrane tension, and \( \rho_e \) is a distributed electrostatic load over the surface. The first term is the inertia of the moving membrane, the second term is the damping force, the third is the membrane stiffness resisting out of plane motion, and the fourth term corresponds to in plane stretching.

We will consider actuation in the static limit, where inertial effects of the membrane can be ignored. This is reasonable because in a vacuum pump, operation will largely occur while pumping the test volume out, and air damping will prevent large velocities. Additionally, we will assume that the residual stress and displacements of the actuator are low enough to ignore the membrane tension. In this simplified form, the equation can be rewritten as:

\[ D\nabla^4 w = \rho_e \quad (4.16) \]

In the devices we are considering, the plate is rigidly fixedly fixed at the edge \((r = a)\), leading to the following boundary conditions:

\[ w(a) = 0 \quad (4.17a) \]

\[ \frac{\partial w}{\partial r} \bigg|_{r=a} = 0 \quad (4.17b) \]

Throughout this analysis, it is assumed that symmetric boundary conditions lead to stable symmetric displacements of the plate/membrane. However, variations in the starting wafers and fabrication processes can lead to small differences in the geometry which are accentuated by the
nonlinear \((1/\text{geff})^2\) actuation force. Even without geometric variation, it has been shown theoretically that electrostatic actuation can lead to asymmetric solutions at large voltages [46], [47].

In actual operation the actuators need to push out the air between the two plates; this leads to a temporary pressure load on the back side of the membrane. The pressure decreases closer to the outlet, meaning it is not radially symmetric and could also contribute to non-symmetric actuation if pull-in occurs rapidly. However, this pressure should be quickly vented and the membrane should return to the electrostatically loaded shape that is being modeled in this chapter.

To simplify the calculations, we will assume that the lower electrode remains rigid. In all of the devices tested for this thesis, the lower electrode was at least twice as thick as the upper electrode, which means it was at least eight times stiffer.

Another significant change is that the lower electrode, \(W(r)\), does not need to be flat. A curved lower electrode means that the gap between the two plates varies with position. This means that the parts of the actuator membrane with narrow gaps will have much higher electrostatic force, and will likely pull-in first. As these regions pull in, it narrows the gap in the neighborhood of the original pull-in site, and the instability propagates across the membrane. This propagation phenomenon is known as zipping [45]. Zipping allows curved membranes to achieve significantly lower pull-in voltages than be realized with comparable parallel plate structures. This will be discussed in more detail later in section 4.6.

While the gap varies across the electrode, we will assume the two electrodes are close enough to parallel locally. In the devices tested for this thesis, the largest angle in any of the curved membranes was 1.8° for a 3mm diameter, 30μm plastically deformed membrane. If we divide the two membranes up into infinitesimally small differential elements as shown in Figure 49, each pair of small elements is approximately parallel.
Figure 49: Geometry for curved electrode actuation showing differential elements. The spacing between upper and lower electrodes has been greatly exaggerated.

This suggests that we can use the same form for the electrostatic force as a parallel plate for each element.

\[
dF_e = \frac{\varepsilon_0 \varepsilon V^2}{2(w(r) - W(r))^2} dA
\]  \hspace{1cm} (4.18)

Dividing by the area element and letting the differential element go to zero, we can write this as a distributed load on the membrane that depends on the position.

\[
dF_e = p_e(r) = \frac{\varepsilon_0 \varepsilon V^2}{2(w(r) - W(r))^2}
\]  \hspace{1cm} (4.19)

Substituting (4.19) into (4.16):

\[
\nabla^4 w(r) = \frac{\varepsilon_0 V^2}{2} \frac{1}{(w(r) - W(r))^2}
\]  \hspace{1cm} (4.20)

Equation (4.20) can be non-dimensionalized:

\[
\nabla^4 \tilde{w}(\tilde{r}) = \frac{\lambda}{(\tilde{w} - \bar{W})^2}
\]  \hspace{1cm} (4.21)

where:

\[
\tilde{r} = \frac{r}{a} \hspace{1cm} (4.22a)
\]
\[
\tilde{w} = \frac{w}{W_{max}} \hspace{1cm} (4.22b)
\]
\[ \dot{W} = \frac{W}{W_{\text{max}}} \]  
\[ \lambda = \frac{\varepsilon_0 V^2 a^*}{2DW_{\text{max}}^3} \]  

4.3 Summary of modeling literature for pull-in calculations

Unfortunately, (4.21) has no known solution in closed form. In this section, several previous methods for approximating the result will be discussed and compared.

One common technique for modeling pull-in voltage is to use a reduced order model, such as Rayleigh-Ritz or Galerkin methods [44], [48]–[51]. This technique assumes a function for the displacement:

\[ w = \sum_{i}^{n} a_i \phi_i \]  

where \( a_i \) are constant fitting parameters and \( \phi_i \) are the assumed mode shapes. The full sum covers the complete range of displacement possibilities and will guarantee an accurate solution, but a reduced set of functions (or even a single well-chosen mode shape) can provide an excellent estimate of the displacement. A first order approximation uses one mode \((n=1)\), increasing the order (increasing \( n \)) increases the accuracy of the calculation. This technique will be discussed in later sections, and will be the basis for the model developed to predict pull-in with curved electrodes.

Bertarelli et al. used a first order Galerkin approximation to find an approximation for clamped circular parallel plates. Using the uniform loading displacement \( w(r) \propto (1 - r^2)^2 \) as a mode shape, they found a solution for the point of instability (pull in) in closed form [48]. It should be pointed out that this is a closed form solution to the simplified first order problem, not a solution to the full electrostatic differential equations. Bertarelli expanded this analysis to consider the same clamped circular plate under two modified control schemes: charge control and multiple ring electrodes with different voltages [49], finding that these methods can extend the actuation range before pull-in occurs. They also considered the post pull-in behavior and stiction [52].

Chao et al. compared first through fifth order Galerkin approximations for a rectangular plate, factoring in the effect of residual stress [44]. They show that the first order approximation is sufficient for estimating the required pull-in voltage.

Vogl and Nayfeh compare first through fifth order approximations for a circular plate, incorporating residual stress and a large deflection plate theory [50].

Another common technique is to find equilibrium positions by minimizing the energy of the system. In the electrostatic actuators studied here, actuation is a balance of the electrostatic potential energy due to the applied voltage and the internal strain energy of the membrane/plate.
François and Dufour compared several actuation mechanisms (electrostatic, pneumatic, piezoelectric) and used energy minimization to calculate the pull-in voltage for both square plates and membranes [53].

Saif used an energy minimization model to predict zipping behavior and pull-in for membranes in two curved geometries (parabolic and annular lower electrodes) [54]. Zhang added membrane tension to Saif’s model, and used this to analyze a bidirectional domed cavity structure where a flexible membrane was sandwiched between two parabolic domes [55]. In both cases, the authors anticipated their devices being used as compressors to generate high pressures between the membranes. This leads to large puffed up membranes, and if the deflections are large enough then stretching in the membrane becomes more important than bending (allowing the authors to justify their assumptions). However, for vacuum applications, the actuators will not be sealed and any pressure built up will quickly vent away. Therefore the membrane deflections we are interested in have much lower magnitudes, and we cannot ignore the bending stiffness of the membrane.

Pull-in voltage depends on all of the material and geometric parameters of a design, so it is not a very helpful metric when comparing the results between different models. Instead, the non-dimensionalized form of the governing equation (4.21) provides two general metrics for comparison. Boundary conditions have not been enforced at this point, so this equation can apply to any plate structure. \( \dot{\lambda} \) is a non-dimensionalized forcing term that incorporates the actuation voltage, and \( \tilde{w}(0) \) is a scaled displacement for the center of the membrane.

At pull-in, we will define the pull-in voltage \( V_{pl} \), \( \lambda_{pl} \), and \( \tilde{w}_{pl}(0) \). A comparison of these previous modeling results, along with the appropriate non-dimensional numbers, is shown in Table 3.
Table 3: Summary of previous plate modeling results

<table>
<thead>
<tr>
<th>Geometry</th>
<th>Method</th>
<th>Source</th>
<th>$\tilde{w}_{PL}(0)$</th>
<th>$\lambda_{PL}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lumped element</td>
<td>Closed form solution</td>
<td></td>
<td>0.3333</td>
<td>-</td>
</tr>
<tr>
<td>Square membrane</td>
<td>Energy Minimization</td>
<td>Français and Dufour [53]</td>
<td>0.36*</td>
<td>0.67**</td>
</tr>
<tr>
<td>Square plate</td>
<td>Energy Minimization</td>
<td>Français and Dufour [53]</td>
<td>0.042*</td>
<td>10.8</td>
</tr>
<tr>
<td>Square plate</td>
<td>2nd-5th order Galerkin</td>
<td>Chao et al. [44]</td>
<td>0.481-NA</td>
<td></td>
</tr>
<tr>
<td>Circular plate</td>
<td>1st order Galerkin</td>
<td>Bertarelli et al. [48]</td>
<td>0.4633</td>
<td>14.24</td>
</tr>
<tr>
<td>Circular plate</td>
<td>5th order Galerkin</td>
<td>Vogl and Nayfeh [50]</td>
<td>0.53*</td>
<td>14</td>
</tr>
<tr>
<td>Circular plate</td>
<td>Shooting method</td>
<td>Batra et al. [47]</td>
<td>0.4365</td>
<td>0.7890**</td>
</tr>
<tr>
<td>Circular plate</td>
<td>Meshless local Petrov-Galerkin (MLPG)</td>
<td>Batra et al. [47]</td>
<td>0.4433</td>
<td>0.7915**</td>
</tr>
<tr>
<td>Annular membrane (fixed at $a$ and $0.1a$)</td>
<td>Meshless local Petrov-Galerkin (MLPG)</td>
<td>Batra et al. [47]</td>
<td>0.393</td>
<td>1.485**</td>
</tr>
</tbody>
</table>

* Estimated from figures
** Membranes are governed by different differential equations and have a different non-dimensionalized forcing term, $\lambda$

The lumped element solution (derived in Section 4.1, yielding the classic gap/3 result) is the lowest pull in voltage of any method. This is because the entire plate translates while remaining parallel. This leads to an electrostatic force that is constant across the actuator, all governed by the maximum displacement $\tilde{w}(0)$. For real membranes, the fixed boundary means that the gap between the two electrodes will vary, with the narrowest gap in the center and a wider gap at the edge. The electrostatic pressure (electrostatic force/area) of the two systems is only the same at the central position $\tilde{w}(0)$, and is smaller everywhere else.

While there is some variation between the different methods, circular parallel plates pull-in in a narrow range of $\tilde{w}(0) = [0.43-0.53]$. Membrane models pull-in earlier, likely because lacking bending stiffness leads to a softer spring constant.

4.4 Overview of the Principle of Virtual Work

In this next section, we will discuss how the principle of virtual work can be used to approximate the actuation of curved electrostatic actuators. This explanation is adapted from Reddy [56].

Consider a particle subject to $n$ forces. The particle is in equilibrium, therefore:

$$\sum_{n} F_i = 0 \quad (4.24)$$

Now consider a small, virtual displacement for the particle $\delta u$. The virtual work due to this displacement by the $n$ forces is:
If the particle is still in equilibrium, the sum of the forces is zero and \( \delta W = 0 \). Alternatively, if \( \delta W = 0 \) and \( \delta u \) is arbitrary, it implies that

\[
\sum_i F_i = 0 \Leftrightarrow \delta W = 0
\]  

(4.26)

The particle is in a stable equilibrium position if and only if the virtual work for arbitrary virtual displacements is zero. This is the principle of virtual displacement.

Conversely, if after a virtual displacement the particle is no longer in equilibrium, the original position was not stable. As in an inverted pendulum, a slight perturbation causes the particle to leave the stable neighborhood near the original point.

We can expand on this idea and apply it to a more general situation. Conservation of energy requires that work done on a deformable body by external forces, \( W_E \), is balanced by an increase in internal energy, \( W_I \), of the body. Writing this for the total energy in the system:

\[
W_{\text{total}} = W_E + W_I = 0
\]  

(4.27)

This increase in internal energy can be heat, strain, or many other effects. In our model, we will consider an elastic object, and the internal energy will be identical to the strain energy caused by the external loads. Elastic materials are conservative - external forces cause strain in the body without any losses (heat, phase changes, etc). When the load is removed the material returns to its initial state.

Consider an elastic body at equilibrium subject to arbitrary body forces \( f_i \) and surface tractions \( t_i \). These external forces do external work, \( W_E \), on the object.

\[
W_E = -\int_V f_i u_i dV - \int_S t_i u_i dS
\]  

(4.28)

where \( u_i \) is the displacement field in the \( i^{th} \) direction. \( S \) is the surface and \( V \) is the volume of the body. The internal energy increase is assumed to be solely due to strain energy

\[
W_I = \int_V \sigma_{ij} \epsilon_{ij} dV
\]  

(4.29)

From conservation of energy, \( W_E + W_I = 0 \). Work done to the body by external forces and tractions becomes internal strain.

The condition for equilibrium of a solid body is:

\[
\sigma_{ij,j} + f_i = 0
\]  

(4.30)

The definition of strain is:
For this example, consider small displacements and neglect the last term. The same result can be shown for large displacements [56].

As before, we can introduce a small virtual displacement \((u_i \rightarrow u_i + \delta u_i)\) from the equilibrium position. The virtual work due to this displacement is:

\[
\delta W_E = -\int_V f_i \delta u_i dV - \int_S t_i \delta u_i dS
\]

\[(4.32)\]

\[
\delta W_I = \int_V \sigma_{ij} \delta \varepsilon_{ij}
\]

\[(4.33)\]

Stress is symmetric \((\sigma_{ij} = \sigma_{ji})\), so using (4.31), the integrand of (4.33) can be rewritten:

\[
\sigma_{ij} \delta \varepsilon_{ij} = \sigma_{ij} \delta u_{i,j}
\]

\[(4.34)\]

Using integration by parts, we can rewrite (4.33) as:

\[
\delta W_I = \oint_S n_j \sigma_{ij} \delta u_i dS - \int_V \sigma_{ij,j} \delta u_i dV
\]

\[(4.35)\]

where \(n_j\) is a unit normal to the surface \(S\). Combining (4.32) and (4.35):

\[
\delta W_I + \delta W_E = -\int_V (\sigma_{ij,j} + f_i) \delta u_i dV + \oint_S (n_j \sigma_{ij} + t_i) \delta u_i dS
\]

\[(4.36)\]

The portion of the volume integral in parentheses is the equilibrium condition (4.30), similarly the portion of the surface integral in parentheses is a balance of forces across the interface. If the new position is in equilibrium, then these will be zero. Since the virtual displacement was arbitrary, a stable position is equivalent to saying:

\[
\delta W_I + \delta W_E = 0
\]

\[(4.37)\]

This is the principle of virtual displacement. Along with the law of conservation of energy (4.27), this principle allows us to find approximate equilibrium position of a system strictly from the governing equation, without the need for an explicit analytical solution. This is the basis of many energy based approximation schemes, such as the Rayleigh Ritz method, Galerkin and others.

### 4.5 First order model for predicting pull-in

We will use the principle of virtual displacement in a first order model to find pull in voltage for a circular plate with a fixed support at the edge. This derivation builds on a similar calculation by Bertarelli et al. [48]. Their work assumed flat electrodes \((W(r) = \text{constant})\). We will expand on this work to consider the more general case where \(W(r)\) is curved.
Equation (4.21) cannot be solved analytically in closed form. In order to approximate the critical pull-in voltage, we can write the deflection as a sum of mode shapes $\phi_l(\vec{r})$ with undefined coefficients $\hat{w}_l$.

$$\hat{w}(\vec{r}) \approx \sum_{l=1}^{n} \hat{w}_l \phi_l(\vec{r})$$  \hfill (4.38)

For this work, a first order approximation with a single mode shape is sufficient.

$$\hat{w}(\vec{r}) \approx \hat{w} \phi(\vec{r})$$  \hfill (4.39)

The mode shape $\phi(\vec{r})$ must match the boundary conditions of the actual displacement field (4.17a), and the closer the assumed mode shape is to the real displacement the more accurate the estimated result will be [57]. For this analysis, we will use the same mode shape used by Bertarelli [48], which is the non-dimensionalized shape of a plate under a pressure load:

$$\phi(\vec{r}) = (1 - \vec{r}^2)^2$$  \hfill (4.40)

This is shown in Figure 50 below.
Figure 50: Assumed mode shapes are shown with increasing \( \hat{w} \) compared to a curved electrode with a shape defined by \( W = (1 - r^2)^2 \), which is a close approximation of the plastically deformed membrane shape. The curved electrode is shifted up slightly due to an assumed air gap at the edge.

Substituting this mode shape into the governing equation (4.21):

\[
\hat{w} \nabla^4 \phi = \frac{\lambda}{(\hat{w} \phi - W)^2}
\]

The equation is effectively a non-dimensionalized force balance per unit area. The left hand side of this equation is the restoring force due to the straining of the membrane. The right hand side is the external electrostatic force.

In order to use the principle of virtual displacement, we need to convert this to internal and external work. Multiplying both sides of (4.41) by the assumed mode shape \( (\hat{w} \phi) \) yields work per area, and finally integrating over the membrane area yields an equation that equates the virtual work done by the plate moving according to \( \hat{w} \phi(\hat{r}) \) and the energy due to the electrostatic force:

\[
(strain\ energy) = \kappa \hat{w} = \lambda p_F \phi(\hat{w}) = (external\ work)
\]
where the $\kappa$ is the system stiffness and $F_e$ the electrostatic forcing function, defined by:

$$\kappa = 2\pi \int_0^1 \rho \phi(\tilde{r}) \phi(\tilde{r}) d\tilde{r}$$  \hspace{1cm} (4.43)

$$F_e(\tilde{\omega}) = 2\pi \int_0^1 \frac{\phi(\tilde{r})}{\left(\tilde{\omega} - \phi(\tilde{r})\right)^2} \tilde{r} d\tilde{r}$$  \hspace{1cm} (4.44)

Conservation of energy:

$$W_{total} = \kappa \tilde{\omega} - \lambda_{pl} F_e(\tilde{\omega}) = 0$$  \hspace{1cm} (4.45)

This is redundant information, already provided by the governing equation. However, the principle of virtual displacement yields:

$$\delta W = \kappa - \lambda_{pl} \left. \frac{dF_e(\tilde{\omega})}{d\tilde{\omega}} \right|_{\tilde{\omega}_{pl}} = 0$$  \hspace{1cm} (4.46)

This gives us two equations for two unknowns ($\lambda_{pl}$ and $\tilde{\omega}_{pl}$).

This is very similar to the parallel plate case, with $\kappa$ (system effective stiffness) acting as the system stiffness and a nonlinear forcing term that depends on the displacement ($\tilde{\omega}$). We can solve (4.45) for $\lambda_{pl}$ and substitute into (4.46). With some manipulation, this yields:

$$F_e(\tilde{\omega}) = \tilde{\omega} \left. \frac{dF_e(\tilde{\omega})}{d\tilde{\omega}} \right|_{\tilde{\omega}_{pl}}$$  \hspace{1cm} (4.47)

This can be solved numerically to find the critical $\tilde{\omega}$, and substituted back into (4.45) to find $\lambda_{pl}$. $\tilde{\omega}_{pl}$ does not depend on $\lambda$, and is therefore independent of the material parameters contained in $\lambda$ such as thickness, membrane radius, or any material properties. It only depends on $F_e$, which is determined solely from the curved membrane function $W$ and the assumed mode shape $\phi(\tilde{r})$ for the moving membrane.

Additionally, there is a single $\lambda_{pl}$ for the chosen electrode shape. This constant can be used to solve for the pull-in voltage for a large number of geometries. From (4.22a)

$$V_{pl} = \sqrt{\frac{2\lambda_{pl} DW_{max}^3}{\varepsilon_0 a^4}}$$  \hspace{1cm} (4.48)

4.5.1 Comparison of model to data, plastic deformation

It is important to verify that the model accurately predicts pull-in for real devices. We have data from plastically deformed electrodes, let us solve for the predicted pull in voltage and compare to the data from Section 3.2.2.

For the plastically deformed membranes, the curvature can be approximated the 4th order polynomial $= (1 - r^2)^2$.
In addition the shape of the curved electrode, we need to consider the effect of the oxide at the interface and the constant air gap etched into the surface of the membranes. As derived earlier (4.9), the effective gap between two parallel plates is \( g_{\text{eff}} = z_0 + z + \left( t_2 \frac{\varepsilon_1}{\varepsilon_2} \right) \).

As seen in Figure 51 below, for our model \( z_0 = W(r) \), \( z = w(r) \), and \( t_2 = t_{\text{ox},1} + t_{\text{ox},2} \). As discussed in Section 3.1.2, the thermal oxide at the interface was grown to 550nm. To prevent fusion between the two membranes during bonding (and to roughen the surfaces to prevent stiction), both sides are etched back 100nm. This means that for devices, both \( t_{\text{ox},1} \) and \( t_{\text{ox},2} = 0.45 \, \mu m \), and the air gap, \( t_{\text{gap}} = 0.2 \, \mu m \). For air and silicon dioxide, the relative permittivities are: \( \varepsilon_1 = 1.0005 \) (air) and \( \varepsilon_2 = 3.9 \) (oxide). This yields an effective gap of \( 0.9/3.9 + 0.2 = 0.43 \) \( \mu m \).

To accurately account for the gap, we need to add it to our shape function for the rigid electrode. Before non-dimensionalizing this is the new shape:

\[
W(r) = W_{\text{max}} (1 - r^2)^2 + g_{\text{eff}}
\]  

(4.49)

To non-dimensionalize this function, we simply divide by the magnitude of the curvature \( W_{\text{max}} \). However, this yields a different added constant \( (g_{\text{eff}}/W_{\text{max}}) \) for each value of \( W_{\text{max}} \); each \( g_{\text{eff}}/W_{\text{max}} \) pair must be solved as a separate shape. This means that when accounting for the gap, we will get a different \( \lambda_{ij} \) for each \( W_{\text{max}} \) we are interested in.

Additionally, as previously discussed in Section 3.2.2, we will be using the effective curvature magnitude \( W_{\text{eff}} = W_{\text{max}} - w_{\text{ox}} \).

Now that the model fully captures the geometry that was tested, we can compare our predictions to the experimental data.
As can be seen in Figure 52, the model captures the trends in the experimentally measured data quite well. However, the match is not perfect. Much of the error in the model can be attributed to variations in the devices themselves. The pull-in voltage is sensitive to the effective gap between the two surfaces, the device radius, and especially the membrane thickness.

As was discussed above, the intended dimensions that define the effective gap are $t_{ox,1}$ and $t_{ox,2} = 0.45 \mu m$, and the air gap, $t_{gap} = 0.2 \mu m$. The etch recipe used has a listed non-uniformity of 5% over the wafer; if a device is over (underetched) by 5%, this changes the effective gap by +/- 1.73%. Even if the initial thermal oxide varies as well, the effective gap should never be more than 5% larger (smaller) than target. The effect of varying the effective gap by 5% can easily be modelled, with the results shown in Figure 53. It is clear that variations in the gap height (at...
least on the order of fabrication variability) have a minimal impact on the pull-in voltage.

Another fabrication concern is variation of the actual radius of the moving membrane. As designed, the DRIE etch that defines the membrane size would have perfectly straight sidewalls. However, SEM images (Figure 36) show that the sidewalls angle outwards, yielding a larger membrane radius than expected.

Assuming that the measured increase is indicative of the variability experienced during these etches, we can use the model to predict how the pull-in voltage will vary if the membrane radius varies by +/- 50 μm. From the model, we know that $V_{P_I} \propto a^2$. This is shown in Figure 54 below.
Another dimension that will vary from die to die is the device layer thickness. The SOI wafers used are specified to have a device layer 20 \( \mu \text{m} \pm 1 \, \mu\text{m} \) thick. From conversations with the vendor, this means that the wafers are measured at 5 points, and wafers do not pass the test if the thickness varies by more than one micron. The pull in model tells us that the pull in voltage \( V_{\text{PI}} \propto t^{3/2} \). This dependence is shown in Figure 55.

Figure 54: Variation in pull-in voltage due to variability in the membrane diameter.
Finally, in the worst case where all three of these dimensions combine to soften (stiffen) the membrane we can predict what the expected variation would be. This is shown in Figure 56 below.
Figure 56: Worst case scenario where all of the potential dimension variations combine to make the membrane softer or stiffer. This shows the full range of expected pull in voltages due to device variation.

### 4.5.2 Comparison of model to data, epoxy induced contraction

In this next section we will apply the same analysis to the epoxy contracted devices. The shape of the epoxy contracted membranes is discussed in 2.3.2. Most of the curvature is near the edges, and the center of the device is largely flat. This is approximated using the following function, which has been non-dimensionalized to span a domain and range of $[0, 1]$.

$$
\bar{W}_{\text{epoxy}}(r) = 1 - H(r - 0.5) \left( 1 - \left(1 - \left(2(r - 0.5)\right)^2\right)^2 \right) - 0.5H(0.5 - r) \left( 1 - \left(1 - \left(2(r - 0.5)\right)^2\right)^2 \right)
$$

where $H(r)$ is the Heaviside or unit step function:

$$
H(r) = \begin{cases} 
0 & r < 0 \\
0.5 & r = 0 \\
1 & r > 0 
\end{cases}
$$

This assumed shape is flat over $[0, 0.5]$ and smoothly curves to match boundary conditions over $[0.5, 1]$. 

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For many of the devices, the oxide curvature of the flexible electrode is comparable to the epoxy contraction induced deformation of the deformed electrode. The two shapes are quite different (see Figure 57) so new non-dimensionalized shape function will be used that weights the contributions from the oxide and epoxy shapes:

$$\bar{W}_{combined}(r) = \frac{W_{max, epoxy} \bar{W}_{epoxy}(r) + W_{max, ox} \bar{W}_{ox}(r)}{W_{max, epoxy} + W_{max, ox}} \quad (4.51)$$

$$W_{epoxy}(r) = (W_{max, epoxy} + W_{max, ox}) \bar{W}_{combined}(r) + g_{eff} \quad (4.52)$$

To non-dimensionalize this function, we simply divide by the magnitude of the curvature ($W_{max, epoxy} + W_{max, ox}$). This yields a different added constant for each value of $W_{max}$; each $g_{eff}/(W_{max, epoxy} + W_{max, ox})$ pair must be solved as a separate shape. This means that when accounting for the gap, we will get a different $\lambda_{pi}$ for each device we are interested in.

Several devices were cleaved and imaged using an SEM. The DRIE etch did not create straight sidewalls leading to larger membrane diameters than designed. Larger membranes had a larger undercut. A best fit line was fit to the amount of undercut, and the relation between measured and actual membrane size was found to be:

$$a_{actual} = 1.05a_{design} + 100 \mu m \quad (4.53)$$

This leads to a softer flexible membrane, and lower pull-in voltages.
Both of these non-idealities (oxide bow, larger radii) were accounted for in the modeling calculations. The predictions are compared with the measured pull-in voltages in Figure 58.

![Diagram](image)

**Figure 58:** Measured pull-in data (○) compared to predicted pull-in values (x).

### 4.6 Varying the curved electrode shape

Until this point, we have assumed that our curved electrode shape is offering some improvements over the more common parallel plate design. In this next section, I will compare several different curved electrode geometries both in their ability to reach pull in at low voltages and the amount of volume they displace.

Using our model, we can easily predict how different curved membrane geometries would affect vacuum pumping. For each membrane shape \( W(r) \), we can calculate a specific \( \lambda_{pl} \), and from there find the pull-in voltage for a particular device geometry.

Several characteristic shapes were tested. Parallel plates provide a baseline. Flat plates are by far the easiest shape to fabricate, so any curved structure must perform better than a parallel plate to be useful. Both shapes created for this thesis (via plastic deformation and epoxy contraction) are approximated with a simple function.
Two hypothetical shapes are used to test other limits. A cone is linear in \( r \), and has a linearly increasing gap. This provides an interesting comparison with both the plastic deformation and epoxy contraction shapes, where the electrode is concave near the boundary where pull-in is expected to start. The final annulus shape has the same form as the epoxy approximation for \( r > 0.5 \), but the gap narrows again in the center. This could have significant benefits by providing large forces at the center of the membrane where they generate the largest moment.

The functional forms are given in Table 4, and are plotted in Figure 59 below.

Table 4: Mathematical functions used for modeling.

<table>
<thead>
<tr>
<th>Name</th>
<th>( W(r) )</th>
</tr>
</thead>
<tbody>
<tr>
<td>Parallel Plates</td>
<td>( 1 )</td>
</tr>
<tr>
<td>Cone (linear)</td>
<td>( 1 - r )</td>
</tr>
<tr>
<td>Plastic Deformation ( (r^4) )</td>
<td>( (1 - r^2)^2 )</td>
</tr>
<tr>
<td>Epoxy</td>
<td>( 1 - H(r - 0.5)(1 - (1 - (2(r - 0.5))^2)^2) )</td>
</tr>
<tr>
<td>Annulus</td>
<td>( 1 - H(r - 0.5) \left(1 - \left(1 - (2(r - 0.5))^2\right)^2\right) )</td>
</tr>
</tbody>
</table>

\( - 0.5H(0.5 - r) \left(1 - \left(1 - (2(r - 0.5))^2\right)^2\right) \)

Where \( H(r) \) is the Heaviside function.

Figure 59: Non-dimensionalized shapes used as curved electrodes. The dotted lines show where the curves overlap.
For parallel plates, it is common to list the pull in voltage for a given plate separation. A similar metric for curved plates is the magnitude of deformation, $W_{\text{max}}$.

![Graph showing pull in voltage vs volume displaced for different shapes.]

**Figure 60**: Predictions of pull in voltage for the shapes considered. All electrodes are shown for both an effective gap $g_{\text{eff}} = 0.5 \, \mu\text{m}$ (solid line) and $g_{\text{eff}} = 0.1 \, \mu\text{m}$ (dashed line), with a 6mm membrane.

Of the shapes considered, parallel plates require the largest voltage to actuate to pull-in, while our plastically deformed shape requires the lowest voltage.

It is also clear that increases in $g_{\text{eff}}$ have a much larger impact on some shapes than others. The plastic deformation, epoxy and annulus geometries have a pronounced decrease in pull-in voltage with a smaller effective gap. This is due to the large region near the edge of the membrane with a narrow gap due to the flat, zero slope condition enforced at the edge. The cone shape does not have a convex region with narrow gap at the edge, and is much less affected by the 5x decrease in gap thickness. Finally, the parallel plate geometry is almost unaffected by changes in $g_{\text{eff}}$ — and it should not be. Increasing the gap in for parallel plates simply changes the spacing between the plates; there is no region with a narrow gap at all.

These results are summarized Table 5 below, using the non-dimensionalized parameters $\tilde{w}_{pl}(0)$ and $\lambda_{pl}$.

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Table 5: Summary of pull-in for curved electrodes. Values vary with the membrane gap and curvature magnitude combinations.

<table>
<thead>
<tr>
<th>Shape</th>
<th>$g_{eff} = 0.1 \mu m$</th>
<th>$g_{eff} = 0.5 \mu m$</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$\bar{W}_{pl}(0)$</td>
<td>$\lambda_{pl}$</td>
</tr>
<tr>
<td>Parallel Plates</td>
<td>0.48 0.47</td>
<td>15.11 14.32</td>
</tr>
<tr>
<td>Cone (linear)</td>
<td>0.38 0.38</td>
<td>1.832 1.475</td>
</tr>
<tr>
<td>Plastic Deformation ($r^4$)</td>
<td>0.43 0.41</td>
<td>0.927 0.244</td>
</tr>
<tr>
<td>Epoxy</td>
<td>0.61 0.69</td>
<td>7.093 3.361</td>
</tr>
<tr>
<td>Annulus</td>
<td>0.34 0.38</td>
<td>3.967 1.970</td>
</tr>
</tbody>
</table>

However, for vacuum pumping applications, a low pull in voltage is not the only metric that is desired. We want an actuator that can also pump out a large volume of air.

![Graph](image)

Figure 61: Displaced volume compared to pull in voltage. Curves with lower pull-in voltages for a given volume displaced are better for vacuum pumping. All electrodes are shown for both an effective gap $g_{eff} = 0.5 \mu m$ (solid line) and $g_{eff} = 0.1 \mu m$ (dashed line).

For pumping applications, it becomes clear that some geometries are inherently better than others. A cone shape, which is both hard to make and requires large voltages to displace a
reasonable volume, is not a great design for a vacuum pump. However, the plastic deformation and annulus geometries can all improve on the standard parallel plate system, at least for some arrangements.

With a 0.1 μm air gap, plastic deformation and annulus geometries are always displace more volume at a given voltage than the comparable parallel plate design. For the plastic deformation shape, with a 0.5 μm air gap, we need to reach large cavity depths before the volume displaced becomes comparable to the parallel plate geometry.

These calculations demonstrate several things. First, it provides analytical validation for using a curved geometry for electrostatic actuation. These out of plane electrodes can achieve much lower pull-in voltages as well as displacing larger volumes than a comparable parallel plate actuation. Second, it allows a designer to make informed decisions about device geometry when it is clear what the pull in voltage depends on. This allows reasonable approximations to be made before fabricating devices. Finally, it shows that we can model more complicated geometries, such as the annulus shape, that are quite challenging to fabricate. This provides insight into which future shapes are worth pursuing.
Chapter 5
Performance of a Vacuum Pump with Curved Electrodes

In this chapter, the curved electrostatic actuators will be applied to microscale vacuum pumping. Previous work will be discussed, and a micropump design will be presented and analyzed.

5.1 Pump Design

5.1.1 Design Specifications for a portable vacuum pump
Before designing a miniature vacuum pump, it is important to understand what is necessary for the pump to be useful for in portable sensing applications.

1) Low power
A portable device needs to minimize the power consumption of all components. If the device is small, but the pump needs a large power source to operate, it is no longer portable. The targeted application is a device that can be powered off of a small battery, similar to that used in a smartphone. If the entire sensing system uses ~ 1 W, the vacuum pump must use a fraction of that. We consider 0.25 W a reasonable power consumption.

2) Small size
The smaller the pump is, the smaller the overall sensor package can be (or it leaves more room for other sensors/batteries). Our target is volume of 1 cm³.

3) Sufficient vacuum and flow rate
A portable device is useless if it takes too long to operate. It is clear that the device will not have the same performance as a macroscale lab vacuum pump, but the final system still needs to be functional. To act as a backing pump for a high vacuum device, a micropump would need to achieve a base pressure of 10 Torr, and it should reach this pressure in a few minutes.

5.1.2 Summary of other vacuum pumps in literature
There have been several previous miniature vacuum pumps. However, none of the previous attempts have met all of the requirements for a portable source of vacuum. They were too large, used too much power, did not reach low enough pressures, or had low flow rates. Several of these pumps will be described in this section. These pumps are summarized in Table 6.

The Knudsen number is the ratio of the mean free path of the gas over the characteristic length for a fluid channel. In viscous flow (Kn < 0.1), gasses follow the Navier-Stokes equations, and
flow follows pressure gradients. At low pressures (rarified flow, Kn > 1), the molecules are not close enough to interact with each other. Pressure gradients have minimal impact, instead diffusion dominates. Temperature gradients are by definition due to differences in particle velocities, leading to net flow away from higher temperatures. This means that fluid flow can follow temperature gradients, and can flow against a pressure gradient.

Knudsen pumps utilize thermal transpiration to create vacuum without moving parts. To do this, narrow channels (with low Kn) are created and heaters are used to create a local thermal gradient. This causes the gas to flow against the pressure gradient, and creates a slight pressure drop [58]. Cascading many of these stages allows the device to create vacuum in a small package [59]. Pressures as low as 0.9 Torr have been reported using 162 stages [60]. However, pumping down to these pressures takes a long time (~20 hours in this case), and needs to constantly use power to heat each stage (0.39 W). The long pump out time makes these pumps more useful as a method to maintain vacuum, rather than a method to create vacuum on a chip.

Sputter ion pumps provide a good way to maintain vacuum on a chip [61]. In this pump, a titanium layer is patterned into two planar electrodes with a narrow air gap in between. A voltage is applied to create an arc discharge between the anode and cathode. This ionizes a small volume of air. The ions are attracted to the titanium cathode, sputtering titanium around the chip and exposing fresh un-oxidized titanium. The bare Ti easily absorbs molecules from the air, reducing the pressure in the chamber. The discharge process slowly destroys the two electrodes, and will eventually short them together. The limited lifetime makes it more applicable to vacuum maintenance than creation of vacuum on a chip.

Small vacuum pumps have been created using macro-machined components. Son et al. created a diaphragm pump driven by a solenoid and using hydraulic amplification to create large forces. However, this pump was larger than is desired (22.8 cm$^3$), and only reaches 206 Torr [62]. This is likely due to the dead volume created by using macro-machined plastic membranes, which are not as smooth as silicon wafer surfaces.

Most relevant to this work is a MEMS diaphragm pump designed by Zhou et al. [11], [12]. The design consisted of two valves and a pump diaphragm that were actuated pneumatically using external vacuum and high pressure. The device was able to reach pressures as low as 164 Torr using a single stage. The base pressure was limited by the pneumatic actuation mechanism. Motion of the diaphragm requires a pressure difference between the internal pressure and the applied vacuum or high pressure. As the test volume is evacuated, the pressure difference from applied vacuum dropped, until the pump was not returning to the initial open state. This decreased the pump volume, and lowered the base pressure.

The pump design presented in this chapter incorporates several improvements over the previous work in [12]. The dead volume has been reduced and curved electrostatic actuators have been integrated into the device.
Table 6: Summary of previous vacuum pumps. The colors designate whether a device matches the portability goals laid out in section 5.1.1 (red = not portable, yellow = borderline, green = satisfactory). For the current work, conservative estimates are presented here. Predictions for base pressure and pumping time are presented in later in this chapter.

<table>
<thead>
<tr>
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<tbody>
<tr>
<td>2-stage diaphragm pump</td>
<td>Diaphragm pump</td>
<td>48 stage Knudsen pump</td>
<td>2 part, 162 stage Knudsen pump</td>
<td>Diaphragm pump</td>
<td>Sputter Ion Pump</td>
<td></td>
</tr>
<tr>
<td>Type of Actuation</td>
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<td>Pneumatic</td>
<td>Thermal</td>
<td>Thermal</td>
<td>Electro-magnetic hydraulic</td>
<td>Electric Discharge</td>
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<tr>
<td>Integrated actuation?</td>
<td>Yes</td>
<td>No*</td>
<td>Yes</td>
<td>Yes</td>
<td>Yes</td>
<td>Yes</td>
</tr>
<tr>
<td>Minimum Pressure from 1 Atm</td>
<td>&lt;30 Torr</td>
<td>164 Torr</td>
<td>50 Torr</td>
<td>0.9 Torr</td>
<td>206 Torr</td>
<td>592 Torr</td>
</tr>
<tr>
<td>Power consumption (for min pressure)</td>
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<td>Low</td>
<td>1.35W</td>
<td>0.44 W</td>
<td>3 W</td>
<td>Not given</td>
</tr>
<tr>
<td>Time to reach min pressure</td>
<td>&lt;1 hours</td>
<td>4 hours</td>
<td>Not given</td>
<td>20 hours</td>
<td>5 minutes</td>
<td>&gt;50 hours</td>
</tr>
<tr>
<td>Size</td>
<td>1.15 cm³</td>
<td>1.15 cm³*</td>
<td>0.06 cm³ **</td>
<td>0.09 cm³ **</td>
<td>22.8 cm³</td>
<td>6.33 cm³</td>
</tr>
</tbody>
</table>

* This device requires an external vacuum pump to operate; volume calculation excludes external vacuum source.
** These pumps are surface micromachined on a single wafer; volume calculations assume 0.5 mm thick wafer.

5.1.3 Comparison with commercial vacuum pumps

There are many vacuum pumps on the market with many different pumping mechanisms. Large pumps can achieve incredibly large flow rates and achieve very low base pressures. However, to achieve our goal of portable, low power vacuum, we need to consider the smallest available pumps. These pumps are compared in Figure 62.
The small vacuum pumps that are commercially available do not have the necessary characteristics to provide vacuum for MEMS devices - they are all too large, or cannot achieve very low pressures. Many of them appear targeted at medical applications, but cannot generate sufficiently low pressures for MEMS sensors. The smallest pump is more than 8 cm$^3$. This is an impressive piece of engineering, but this tiny device can only reach ~0.5 atm.

If we instead look for the smallest pump that can achieve the pressures necessary to act as a roughing pump for a smaller ion pump, we see that the necessary pumps are all quite large. The smallest commercial pump that can achieve $<10$ Torr is a 1000 cm$^3$ diaphragm pump. This is not a portable pump in any reasonable handheld system. Creare has created a scroll pump for the Mars Curiosity mission that has better performance and is only 350 g and about the size of a D cell battery. This is approximately 100 times larger than the target volume of 1 cm$^3$, but this pump has been used in backpack sized system for mass spectrometry [6].
5.1.4 Scaling of the base pressure in vacuum pumps

The base pressure for a displacement pump (such as a diaphragm or rotary vane pump) is given by [11]:

$$P_{\text{vacuum}} = \frac{P_0 V_{\text{dead}} + P_0 Q_{\text{leak}} \tau_p}{V_{\text{dead}} + V_{\text{pumped}}}$$  \hspace{1cm} (5.1)

where $P_{\text{vacuum}}$ is the ultimate base pressure, $P_0$ is the outlet pressure (usually 1 Atm = 760 Torr), $Q_{\text{leak}}$ is the leak rate (volumetric flow rate), $\tau_p$ is the pumping cycle time, $V_{\text{dead}}$ is the pump dead volume, and $V_{\text{pumped}}$ is the volume pumped each cycle.

The total volume inside a pump includes both the pumping volume and the dead volume. The pumping volume is the quantity displaced by the pumping element during a pump cycle. For a diaphragm pump, this is the volume displaced by the moving diaphragm. The dead volume is all the unused volume, which can come from gaps between the membranes, as well as the connection and venting channels necessary for a working pump. These two components are shown in Figure 63 below.

![Diagram showing pumped and dead volumes for a diaphragm actuator](image)

Figure 63: Diagram showing dead volume and pumped volume for a diaphragm actuator. Dead volume includes all of the unpumped volume between the inlet and outlet ports on the left and right, while the pumped volume is the volume displaced during actuation. In this example, dead volume has been increased due to a bubble trapped under the membrane.

The physical interpretation becomes clearer in an ideal pump with no leaks. The compression ratio is given by the ratio of total pump volume over the unused dead volume:

$$\frac{P_0}{P_{\text{vacuum}}} = \frac{V_{\text{dead}} + V_{\text{pumping}}}{V_{\text{dead}}}$$  \hspace{1cm} (5.2)

This is simply an expression of the ideal gas law at constant temperature and with a constant number of particles. A pump designed to achieve low base pressure needs a large compression ratio while minimizing the leak rate.
5.1.5 Our vacuum pump design

Our device is a two stage diaphragm vacuum pump utilizing curved electrostatic actuators. Formation of curved electrodes was discussed in Chapter 2. Methods to integrate curved electrodes into real devices as well as a demonstration of electrostatic actuation using curved electrodes were described in Chapter 3.

Several additional factors need to be considered when designing the full vacuum pump.

Electrostatic actuation can only pull. This is due to the squared voltage in the forcing term – both positive and negative voltages are attractive. Three terminal layouts have been used to enable pull-pull actuation [66]. For our design, each actuator will have three electrical terminals: the flexible membrane, a lower curved electrode, and the upper common electrode. Instead of relying on the membrane to spring back, this enables electrostatic pulling in both directions; this increases the frequency of the device.

One concern with two very smooth surfaces contacting each other is that they will stick, and not come apart again. This is called stiction, and it can be mitigated by roughening the contacting surfaces. Our design incorporates two levels of roughness. First, the surfaces are slightly roughened using a dry plasma etch. Second, a shallow etch is used to create small bumps on the surface of the moving membrane. The bumps decrease the amount of contact area between the two surfaces, and the area that is in contact is rough.

Images of the pump dies are shown in Figure 64, and a schematic showing the critical features is shown in Figure 65.
Figure 64: Pictures of micropump. The internal pump features (top). The top side (showing electrical vias) and bottom side (with fluid connections) are shown on the bottom.
Figure 65: (Top) The layout for a MEMS vacuum pump. The large circular membranes are the pump chambers, while the smaller membranes are valves. There are eight circular vias that allow electrical contact to the actuator layers. (Bottom) Cross-section (following the red path in the upper diagram) showing the critical features of the pump.

5.2 Base Pressure

In this section, we will estimate the dead volume and leak rate for the proposed micropump, and assess how various design modifications would affect the ultimate base pressure.

Calculating the pump volume requires a few assumptions about the actuators.

*Rigid lower electrode:* Epoxy deformed electrodes are held in place by a block of epoxy with a Shore D hardness of 85 (E = 2.5 MPa). This is very close to a rigid electrode. On the other hand, the plastically deformed electrodes are not perfectly rigid. In this design, the curved electrode is twice as thick as the flexible zipping electrode. Plate deflection:

\[ w(r) \big|_{r=a} \propto t^3 \quad (5.3) \]

This means the lower curved electrode is 8 times stiffer than the upper electrode. This motion decreases the size of the chamber at full actuation by \(1/9 = 11\%\).
In this analysis, epoxy contracted pumps will be assumed to be rigid, and the volume for other shapes will be reduced by 11%.

**Perfect contact:** In an ideal pump, after actuation the membranes would make perfect contact with each other, leaving no air between the two surfaces. In a real device, this will not be the case.

Imagine trying to press plastic wrap on to a smooth surface. Bubbles and wrinkles form between the surfaces after initial contact. To get rid of these areas, one must squeeze the air out. Two thin membranes electrostatically actuated into contact behaved the same way. Air can easily be trapped between the two surfaces, and channels are needed to allow the air to vent out. However, these channels remain filled with air after pull-in. The design of a diaphragm pump needs to balance the need to efficiently remove the bubbles with the desire to minimize the dead volume.

In this analysis, the dead volume due to anti-stiction bumps and venting channels will be calculated explicitly, and we will assume that the venting channels are venting at removing any bubbles that form.

### 5.2.1 Dead volume calculations

First, consider the dead volume created by the anti-stiction bumps. The purpose of the bumps is to decrease the amount of surface area between the two membrane surfaces. This requires that a small volume will not be pumped. The simplest approximation is to assume the flexible membrane remains parallel to the lower electrode, but the bumps keep the two membranes separated. This approximation provides a dead volume of:

\[
V_{\text{bumps}} = \pi a^2 d(1 - n) \tag{5.4}
\]

where \(d\) is the depth of the bumps and \(n\) is the percent of pump area covered by bumps. However, this is an overestimate of the dead volume, which leads to higher base pressure predictions. A more accurate approximation takes into account the electrostatic force on the membrane. The electric field will cause the membrane to deflect into the gaps between the bumps, decreasing the dead volume between the membranes. Approximating the shape of the membrane as a sinusoidal function in \(x\) and \(y\) provides a much better estimate of the dead volume than parallel plates. The anti-stiction bumps are laid out in a rectangular grid with \(x\) and \(y\) pitches of \(m\), respectively. The membrane shape is approximately:

\[
w(x, y) = \frac{w_{as}}{2} \left[ \cos \left( \frac{2\pi x}{m} \right) \cos \left( \frac{2\pi y}{m} \right) - 1 \right] + d \tag{5.5}
\]

where \(w_{as}\) is the displacement at the midpoint \((x, y) = (m/2, m/2)\) between the bumps. If we consider a square cell around one bump, the dead volume is:

\[
V_{\text{bumps}} = \int_0^m \int_0^m \left( \frac{w_{as}}{2} \left[ \cos \left( \frac{2\pi x}{m} \right) \cos \left( \frac{2\pi y}{m} \right) - 1 \right] + d \right) dx \, dy \tag{5.6}
\]

The sinusoidal terms are integrated over a whole period, so they disappear. This leaves:
\[ V_{\text{bumps}} = \left( d - \frac{w_{as}}{2} \right) m^2 \]  \hspace{1cm} (5.7)

\( m^2 \) is the area of the unit cell, so the dead volume of the entire membrane is simply:

\[ V_{\text{bumps}} = \left( d - \frac{w_{as}}{2} \right) \pi a^2 \]  \hspace{1cm} (5.8)

The dead volume due to the venting channels is simply:

\[ V_{\text{vent}} = w_{\text{vent}} h_{\text{vent}} l_{\text{vent}} \]  \hspace{1cm} (5.9)

where \( w_{\text{vent}} \) is the width of the channel, \( h_{\text{vent}} \) is the depth if the channel, and \( l_{\text{vent}} \) is the total length of venting channels across the membrane. The connection channels have the same form, but with their own width, depth, and length.

First we will calculate the base pressure for the most recent micropump design shown in Figure 65, followed by a discussion of the main contributing factors and potential design changes.

The current pump has channels that are 4 \( \mu \text{m} \) deep by 200 \( \mu \text{m} \) wide. The connection channels are 2 mm long. There are 6 venting channels in each actuator laid out radially in a spoke pattern, with a combined length of 6a. The anti-stiction bumps are etched 100 nm into the oxide, have a diameter of 10 \( \mu \text{m} \) and are spaced with a pitch of 100 \( \mu \text{m} \). The 2.5 \( \mu \text{m} \) diameter valves contribute to the dead volume as well due to the anti-stiction bumps and venting channels present on the valve surface.

The magnitude of the deflection past the anti-stiction bumps, \( w_{as} \), can be estimated using the multiphysics modelling software COMSOL. When the membrane is completely pulled in, the remaining gap is just the oxide thickness plus a thin film of air surrounding the bumps. For this analysis, consider an electrode with 100 nm anti-stiction bumps etched into an originally 500 nm thick oxide layer. This creates an effective gap of \( g_{\text{eff}} = (400/3.9 + 100) = 202.6 \text{nm} \). After pull-in, under 75 V, the membrane would experience an electrostatic pressure of 606.9 kPa, or almost 6 atm. Anti-stiction bumps are modelled as rigid circles on the lower surface, and a uniform pressure of 6 atm is applied to the upper surface. Periodic boundary conditions are enforced at the edge of the domain. An example of this calculation is shown 150 \( \mu \text{m} \) pitch in Figure 66.
Figure 66: Surface map for membrane after pull-in, shown for 10 μm diameter anti-stiction bumps with 150 μm pitch under 6 atm of pressure (equivalent to 75 V across an effective gap of 202.6 nm. The domain modelled is 450 μm x 450 μm.

Results from a range of anti-stiction bump pitches are shown in Figure 67, along with a fourth order polynomial best fit line. This shows that the deflection between the bumps is proportional to the pitch to the fourth power. In the pressure ranges tested, the deflection is also linear with the applied pressure load \( q \).

\[ w_{as} \propto q m^4 \]  \hspace{1cm} (5.10)
Figure 67: Deflection between anti-stiction bumps based on bump pitch. Estimated using COMSOL with 6atm of pressure, equivalent to 75V across a constant gap of $g_{\text{eff}} = 202.6$ nm corresponding to 100nm tall anti-stiction bumps. A fourth order polynomial best-fit line is shown, with $R^2 = 0.999998$.

With the device dimensions and an estimate of $w_{\text{as}}$, the dead volume for the device as designed can be calculated. Assuming minimal leaking, the base pressure can be calculated from equation (5.1). For a 6 mm diameter pump, the base pressures are shown in Figure 68 below.

Multiple pump stages can be used to increase the compression ratio and decrease the base pressure.
5.2.2 Design Variations

There are two ways to increase the compression ratio and lower the base pressure of a micropump. The pumping volume can be increased or the dead volume can be decreased, but each of these introduces tradeoffs in pump performance. These will be summarized below.

Increasing pump volume:

Magnitude of curvature: Deforming the curved electrode more increases the pump stroke and lowers the base pressure. However, this also requires a larger pull-in voltage. The upper bound is the breakdown strength of the dielectric.

Membrane radius: Increasing the radius of the membrane increases the pump volume and lowers the pull-in voltage. However, it also increases the overall size of the micropump die, and makes the pump membrane more fragile.

The base pressure as a function of these two dimensions is shown in Figure 69.
Figure 69: Variations in plastic deformation magnitude and membrane radius, with the dead volume calculated from the micropump design (shown for a single pump stage).

**Dead Volume:**

*Venting channels:* Reducing the size and number of venting channels would reduce the dead volume, but would slow down the air escaping from the actuator each cycle. Fewer vent channels would lead to a lower base pressure but a longer pump down time.

*Space surrounding anti-stiction bumps:* Shorter bumps would decrease the dead volume, but if they are too short the membrane will contact the lower surface and permanently adhere.

The contributions from each component of dead volume, as a fraction of the total dead volume, are shown in Figure 70.
Figure 70: Proportion of dead volume from four major sources: space around anti-stiction bumps after pull-in (both pump and valves), venting channels, and connection channels. The dead volume is independent of the curved electrode shape.

This shows that the primary contribution to dead volume in these devices is the venting channels, while the space surrounding the anti-stiction bumps contributes a small amount as well. This suggests that a good way to improve the pump base pressure would be to decrease the size of the venting channels, and to a lesser degree decrease the volume surrounding the anti-stiction bumps.

Before reducing the size of the venting channels, it is important to understand how the base pressure and pumping speed are related. The actuator needs to fully vent each cycle, and increasing the number and venting channels helps with this. However, additional venting channels (or larger channels) increase the dead volume of the device. It is critical to use this dead volume in the most effective way possible to facilitate rapid venting.

As the membrane pulls in, the venting channels can be modelled using a hydraulic resistance:

\[ \Delta P = R_h Q \] (5.11)
where $R_h$ is the hydraulic resistance and $Q$ is the volumetric flow rate through the channel. A rectangular pipe with a hydraulic resistance of:

$$R_h = \frac{12 \mu L}{wh^3(1 - 0.630h/w)} \quad (5.12)$$

Clearly, the height of the channel has a larger impact on the resistance than the width or length. A channel that is twice as tall but half as wide has the same volume, but has a flow rate four times higher for a given pressure. Alternatively, we can cut the width down by a factor of 8, while doubling the depth and the flow will be the same. This would reduce the dead volume by 75%.

All of the air flows out of the device at the same exit channel. If the device has constant flow rates along the other 5 venting channels, there would be 6 times as much air flowing through the final channel and out of the device (see Figure 71). If all 6 venting channels are the same size, the exit channel will become a bottleneck and slow down the venting from the device.

![Figure 71: All of the air leaving an actuator must pass through the outlet venting channel. If it is the same size as the other channels, it will reduce the flow out of the device.](image)

While decreasing fluidic resistance is important for venting the actuation chamber, it should not be taken too far. Even shallow venting channels can affect the shape of the curved electrodes can have a significant impact on the shape of the membrane. A single channel was etched into the deforming electrodes used for electrostatic testing in Section 3.2.2, and the asymmetry induced is clearly visible in Figure 72.
Additionally, slow venting is only a problem at elevated pressures. As the pressure decreases, large bubbles are less likely to form, and venting is not needed for fast actuation. At low pressures, the channels are adding to the dead volume and increasing the base pressure. This means that only the channels that are needed should be added to the membrane.

Next, the dead volume due to anti-stiction bumps will be discussed. As can be seen in Figure 67, the current bump height is very conservative. 10 μm diameter bumps with a pitch of 100nm lead to a 3.14% contact area between the electrodes, and the flexible membrane deflects only 7nm between 100nm tall anti-stiction bumps.

Pull-in for an idealized parallel plate actuator on a linear support (see Section 4.1) occurs at one third of the initial gap. Electrostatically actuated plates pull-in at higher fractions of the gap (Section 4.3). If we restrict the predicted deflection between the bumps to 20% of the bump height, we can be assured that it will not pull in.

From equation (5.10), we can relate a change in the gap to a change in the deflection. This deflection occurs with a bump height of 70.5 nm. This can be improved further by decreasing the pitch between the bumps. The electrostatic force is inversely proportional to the gap, so equation (5.10) implies that at constant voltage:

$$w_{as} \propto \frac{m^4}{g^2}$$  \hspace{1cm} (5.13)

Shorter spacing leads to less deflection of the membrane, and thus shorter bumps can be used. However, if there are too many bumps, the surface of the bumps will stick to the moving membrane. The best solution is to use smaller, shallow bumps that are spaced with a narrow pitch. This prevents the membrane for making contact on either the upper or lower surfaces, and decreases the dead volume surrounding the bumps.
Decreasing the size of the bumps is limited by the resolution of the lithography and etching steps. The anti-stiction bumps can easily be reduced to 5 μm diameter, which allows for a 2x reduction in the pitch in both x and y directions while keeping the same contact area. This decreased spacing, along with the 20% deflection criterion, allows for 28nm anti-stiction bumps. This would decrease the dead volume surrounding the bumps by 74.6%.

With both modifications (narrow, deep venting channels and shorter anti-stiction bumps), the dead volume falls significantly. For a 6mm membrane, the dead volume decreases to 25.2% of the current value. This is true for all shapes and deformation magnitudes. For a 25 μm plastically deformed electrode, and the base pressure decreases from 87 (9.9) Torr to 24 (0.75) Torr for a single (two) stage vacuum pump.

5.3 Membrane Shape

In this section, we will compare several membrane geometries, and attempt to provide guidelines for the design of curved electrostatic actuators.

The dead volume for the vacuum pump design is not affected by different curved electrode shapes. The curvature does have an impact on the pumping volume. The base pressure for each curved electrode shape is compared for 1 and 2 stage pumps in Figure 73.

The volumes used for these calculations all assume that the electrodes come into perfect contact after pull-in. This is not completely true, because the flexible membrane is attached to a rigid support at the edge, and the membrane must bend down to reach the lower electrode. However, this is still reasonable approximation for the curved shapes, because the gap at the edge is small. For parallel plates, this leads to a significant overestimate of the pumped volume and an underestimate of the dead volume.

To compensate for that, additional dead volume was added to the parallel plate devices. This is because parallel plates cannot pump the entire volume between the plates. The upper membrane is rigidly attached at the edge, and bends over a finite distance to contact the lower electrode. This distance was estimated by calculating the un-pumped volume between a membrane under 6 atm of pressure, which is the electrostatic load between the plates at pull-in, and the lower electrode. This is an underestimate because the actual shape at the edge should have zero slope on both pinned points.
Figure 73: Base pressure predictions for several membrane shapes, using the dimensions from a recent micropump design. The deformation magnitude, $W_{max}$, is 25 $\mu$m for all calculations shown. The plastically deformed and cone shapes have the same volume, and thus the same base pressure.

Parallel plate electrodes produce the lowest pressures because they displace the largest volume for a given actuator magnitude. However, they also require the largest actuation voltages.

Another, perhaps more useful, metric would be to compare the base pressure to the required pull-in voltage. This shows that cone shaped electrodes produce the worst base pressure to pull-in voltage ratio, followed by epoxy deformed electrodes. Plastically deformed devices with thin oxide layers also compare favorably with parallel plates. This can be seen in Figure 74.
Figure 74: Comparison of pull-in voltage to base pressure for several curved electrode geometries. Additional dead volume was added to the flat electrodes because parallel plates do not pump all the way to the edge.

For the 2-stage diaphragm pump proposed in this chapter base pressures and pull-in voltages are summarized below in Table 7. Device dimensions are assumed to be: 6mm pump membranes, 0.5 μm oxide layer + 100nm air gap with 28nm anti-stiction bumps ($g_{eff} = 0.256 \mu m$), 25 μm deformation magnitude.

Table 7: Summary of base pressures for 25 μm curved electrodes with $g_{eff} = 0.256 \mu m$.

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<tr>
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<td>Plastically Deformed</td>
<td>23.92</td>
<td>0.75</td>
<td>52.2</td>
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<tr>
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<td>0.24</td>
<td>163.3</td>
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</table>
5.4 Guidelines for designing a MEMS vacuum pump

Vacuum pumping does not scale favorably to small scales (see section 5.1.4). Simply scaling down larger pumps will not yield the desired base pressures, as is shown by miniature commercial pumps (Section 5.1.3). In order to create a viable microscale vacuum pump, the design must find a way of overcoming these limitations, either by decreasing the dead volume or increasing the pumping volume.

Microfabricated pumps can leverage incredibly smooth semiconductor wafers and create features with submicron precision using photolithography. This allows pumping surfaces come together each other with very little unused space between them, decreasing the dead volume. Curved electrostatic actuators provide an excellent way to increase the pumping volume while maintaining a low power, low voltage device. Together, low dead volume and high pumping volume create a device that can achieve very low base pressures.

With the suggested changes in the previous section, a vacuum pump with curved electrodes could achieve base pressures well below current commercial pumps and any comparable micropumps reported in the literature. A two stage pump with plastically deformed electrodes could reach a base pressure of 0.75 Torr using a just over 50 V for actuation. A two stage pump with epoxy contracted electrodes could achieve a lower pressure of 0.24 Torr but requires a significantly higher actuation voltage of almost 163 V. Both of these pumps would provide sufficient vacuum to act as a roughing pump for many sensing applications.

Several additional suggestions for the operation of a micropump are worth mentioning.

_Membrane Size:_ In the current micropump design the valves smaller than the pump diaphragms. This allows the pumps and valves to be packed together more efficiently, decreasing the size of the overall device. However, it also means that the actuation of the smaller and larger membranes need to be optimized separately. The valves are much stiffer due to the smaller radius, and in order to pull in at a comparable voltage to the pumps, the magnitude of the curved electrode must by much shallower for the valves.

Assuming the valves are half the diameter, they will by 8 times stiffer. Based on the electrostatic force relation, this would require approximately $2\sqrt{2} = 2.83$ times narrower gap to pull in at around the same voltage. With plastic deformation it is challenging to create small shallow membranes at the same time as larger deep membranes (see section 2.2.2). This difficulty can be avoided by using the same size membranes for both valves and pumps, but it does increase the size of the overall device.

_Pull-pull actuation:_ To operate at a reasonable frequency, it is critical to be able to actuate the membrane up as well as down. Especially at large membrane radii, the stiffness in the membrane is not sufficient to return the membrane to the original position in a reasonable
amount of time. This is especially true at higher pressures, when the actuator must pull air back in.

Electrostatic actuators can only pull the membrane. Three electrodes, with the flexible electrode in the middle, allow for pull-pull actuation. The lower electrode can be used to pull the flexible electrode down, and the upper electrode actuates it back up.

**Frequency ramping:** At high pressures, the limitation on pump flow rate is the time to evacuate the pump chamber each cycle. As the pressure decreases, the venting time drops as well. To maximize the pump flow rate, the frequency of actuation should increase as the pressure drops. Frequency ramping is also a useful way to speed up pumping and minimize the detrimental effect of a leaky pump.

Pumping with and without frequency ramping can be approximated with a simple iterative calculation. Starting with the outlet valve closed, we assume that the entire system has a volume \( V_0 \) and initial pressure \( P_0 \). The number of moles of gas in the system is:

\[
    n_{total} = \frac{RT}{P_0 V_0}
\]  

(5.14)

Each cycle, the pump compresses the gas remaining in the system (with both valves closed). When the outlet valve opens, the pump evacuates gas until the internal pressure matches the ambient air. The valve closes and the cycle repeats. Each period, some air is pushed out and some leaks back in. The change in the total number of molecules in the device is:

\[
    n_{i+1} = n_i - n_{rem} + n_{leak}
\]  

(5.15)

The amount of gas removed each cycle \( (n_{rem}) \) is given by:

\[
    n_{rem} = \frac{V_{dead}}{RT} \left( P_i \frac{V_{pumped} + V_{dead}}{V_{dead}} - P_o \right)
\]  

(5.16)

where \( P_i \) is the internal pressure during the \( i \)th cycle. The amount that leaks back into the device \( (n_{leak}) \) is given by:

\[
    n_{leak} = \frac{P_o Q_{leak} \tau_p}{RT}
\]  

(5.17)

After each cycle, the pressure can be recalculated from \( n_i \). If frequency ramping is used, the cycle time is updated based on the new pressure. Eventually, the system asymptotes at the base pressure predicted in (5.1).

Assume that the volume being pumped out has a volume \( V_0 = 1 \, \text{cm}^3 \). This is much larger than the volume of the pump chamber and valves \(-0.1 \, \text{mm}^3\), depending on the pump shape, deformation magnitude, and radius). At atmospheric pressure, it is possible to actuate the pump at around 1 Hz. The resonant frequency of a 6mm pump membrane is around 1kHz, which suggests that actuation at 100 Hz \( (10\% \text{ of resonant frequency}) \) would be possible. For simplicity assume a linear transition between the operation frequency at atmospheric and vacuum actuation.
The results of linear frequency ramping and constant frequency operation are shown in Figure 75.

![Graphs showing the comparison of pump rate with frequency ramping (left) and constant cycle times (right).](image)

Figure 75: Comparison of pump rate with frequency ramping (left) and constant cycle times (right). For this calculation, the pump was evacuating a 1 cm³ volume. Previous work [11] found a leak rate of 3x10⁻³ sccm across the pump valves.

It is clear that increasing the pumping frequency dramatically speeds up pump rate and mitigates the effect of leak rates. As shown in (5.1), pump leakage raises the base pressure by a factor proportional to \( P_0 Q \text{leak} \tau_p \). The period \( \tau_p \) used is the cycle time at the base pressure. With frequency ramping, \( \tau \) can be very small, thus minimizing the impact of pump leakage.

**Power Consumption**

We can estimate the power consumption by considering the work done to compress the gas each cycle. The fundamental limit on how much energy the vacuum pump uses is the thermodynamic work due to changing volume/pressure. One way to estimate this energy is to consider the entire pumped volume at once. The act of pumping out the volume reduces the pressure. We can estimate the work using:

\[
W = P \Delta V
\]  
(5.18)

To pump out a 1 cm³ volume at 1 atm, ~100mJ of work is needed. However, the work done is path dependent, so we can improve on this initial estimate by choosing a reasonable path. Silicon has a high thermal conductivity, so the substrate will rapidly conduct generated heat away from the gas. For this reason the compression is assumed to be isothermal. The work done in this case is:

\[
W = \int_{V_1}^{V_2} PdV = nRT \ln \left( \frac{V_2}{V_1} \right)
\]  
(5.19)

In addition to considering the path, the work done on the gas depends on how the gas is vented. One simple method for pumping would be to close both inlet and outlet valves, then compress...
the pump chamber, and finally open the outlet to vent out the compressed air. However, this requires more compression work to achieve these large pressures.

Instead, assume that the outlet valve opens as soon as the pump reaches 1 atm. This is the minimum pressure needed to prevent backflow into the pump chamber. This way all additional compression causes the air to vent out with minimal pressure increases. This venting method is shown schematically in Figure 76 and on P-V plots in Figure 77.

![Diagram of venting process](image)

**Figure 76:** Steps for efficient venting of a diaphragm pump with two diaphragm valves. (Left) air fills pump chamber from the open inlet valve. (middle) The pump chamber is compressed until the pressure reaches 1 atm. (Right) The outlet is opened and the pump chamber vents out.

![Pressure-volume plots](image)

**Figure 77:** Two compression schemes are plotted here. (Left) Pump chamber remains sealed and compresses the air from $(V_1, P_1)$ to $(V_2, P_2)$ isothermally. (Right) The pump chamber compresses up to 1 atm, then vents the air without further pressure increase.

Each cycle, work is done to compress the air to 1 atm and vent it out of the device. Power can be determined by adding up the energy used each cycle and dividing by the pump time. This is shown in Figure 78.
This leads to a power estimate of approximately 200 μW when using frequency ramping (10 minutes) and 6 μW when using a constant 1 Hz actuation frequency (8.3 hours for pump down). The difference in power consumption comes down differences in pumping time. Both methods use similar amounts of energy, but frequency ramping uses it in a shorter period of time.

In addition to thermodynamic energy costs, the actuation method and drive circuitry will contribute to the power consumption. Electrostatic actuation dissipates very little power, but to compare our electrostatic diaphragm pump to other miniature vacuum pumps, published or commercial, we want to know how much power it consumes. Without a working pump, we cannot explicitly measure the power use, but we can estimate the magnitude.

Each actuator has three states: unactuated, under voltage before pull-in, and under voltage after pull-in. When the actuator stays in a given state (with constant voltage), no power is consumed because no current is flowing. The application of voltage draws a small current that charges up the electrodes, and during pull-in the capacitance changes, causing current to flow.

First, we will consider the simplest method for operating the pump. The actuators draw the current needed and dump the charge back to ground after each charging event. This is what was done in Section 3.2 while testing the curved electrostatic actuators.

The amount of charge that build up each cycle is given by:

\[ q = CV \]  \hspace{1cm} (5.20)

The average current needed to charge this capacitor is:

\[ i = \frac{q}{\tau_p} \]  \hspace{1cm} (5.21)
Then the resistive losses in the device would be:

\[ P = iV = \frac{CV^2}{T_p} \]  

(5.22)

For this calculation, assume that the actuation voltage is 100V. The device layer surrounding the diaphragm is also electrically connected to the membrane, and contributes to the capacitance. This leads to a capacitance of around 10 nF for the device (based on the design in Figure 65). Each cycle uses approximately 100 µJ. With frequency ramping, this leads to a power consumption of 5 mW for 10 minutes, while with constant 1 Hz operation, the device uses 100 µW for 8.3 hours.

Power can be reduced in several ways. First, the actuation voltage can be decreased. At 50 V, the pump would use 4x less power. Second, the area surrounding the actuators could be decreased. In the current design, the actuators take up less than a quarter of the area charged each cycle. Some of this area is necessary for the deformation cavity and electrical contact, but much of it could be reduced.

These estimates are consistent with experiments. During testing, a 10 kΩ resistor was always placed in series with the device under test (see Figure 37). The voltage across this resistor was used to calculate the current through the device. The voltage was always very low for moderate applied voltages. With 50V across the actuators, less than 1 mV (the measurement accuracy of the voltmeter used) was observed across the resistor. This would correspond to <50 µW, compared to 25 µW calculated above.

With such small thermodynamic and electrical losses, it is expected that the drive circuitry will be the dominant the power consumption of these devices. This can be mitigated partially by implement a resonant circuit. In this design, charge oscillates between the actuators and a separate charge storage component, so that the energy is not lost each cycle. It should be possible to design the control circuitry to achieve power consumption below 100 mW. With this in mind, we can compare this MEMS pump to other commercial miniature vacuum pumps in Figure 79.
Figure 79: Comparison of power consumption with other commercial vacuum pumps.

**Pump Performance Tradeoffs**

Throughout this analysis, we have considered aspects of the pumping performance in isolation. In actuality, most of these design parameters are not independent. It is important to understand the tradeoffs when making design decisions.

We have focused on designing a low power, small volume diaphragm pump throughout this section. This leads to lower flow rates and longer pump times because the pumping chamber cannot be very large. Increasing the pump radius would improve the pump rate as well as the base pressure, but yield a larger pump. Increasing the chamber depth (perhaps via more plastic deformation) would increase the pumped volume, increasing flow rate and improving the base pressure, but the operation voltages and power would increase.

There are also tradeoffs in the design of the venting channels. Higher flow rates require moving more air out of the pump chamber. This can be done easily by increasing the size and number of venting channels, and especially by widening the exit channel. This greatly increases the dead volume and limits the base pressure of the device. However, if pump rate is more important for a given application, larger venting channels would assist in this goal.
Chapter 6
Further Work

In this chapter, the thesis contributions will be summarized, along with ideas for future investigations.

6.1 Thesis Contributions
We have developed two methods for fabricating curved silicon membranes and demonstrated their utility as MEMS actuators. The contributions of this thesis include:

1. Developed two methods for forming curved silicon membranes
   a. High Temperature Plastic Deformation
      i. Design of a fabrication process to test the dependence of the process on critical design parameters, such as membrane radius and deformation cavity volume
      ii. Improved on previous modeling work to predict the onset of yield and magnitude of plastic deformation. Changes included using both large and small deformation theory to calculate stresses, using the Mises yield criterion instead of uniaxial yield, and expanding the analysis to consider a wider range of device dimensions.
   b. Epoxy Induced Contraction
      i. Demonstrated for the first time that capped epoxy can be used to create curved electrodes.
      ii. Devised a process for applying the epoxy under vacuum that minimized air bubbles and improved reliability.
      iii. Created a method for controlling the shape epoxy deformation further by adding additional filler material.

2. Integrated both types of curved electrodes into electrostatic actuators
   a. Demonstrated electrostatic actuation and pull-in using both plastically deformed and epoxy contracted electrodes
   b. Tested devices at atmospheric pressure and in vacuum
   c. Actuation proved repeatable and reliable for over 30 minutes of constant cycling.

3. Developed a model based on the principle of virtual work to predict the pull-in instability for arbitrary curved electrodes with an air gap
a. Model requires no fitting parameters to accurately predict the pull-in voltage for devices with plastically deformed electrodes and epoxy contracted electrodes.
b. Model inputs have been adjusted to account for membrane curvature due to unbalanced oxide thicknesses.
c. Calculated the effect of varying critical dimensions on plastic deformation magnitude.

4. Analyzed a proposed two-stage diaphragm pump using integrated curved electrostatic actuators.
   a. Estimated the dead volume for the proposed devise and offered suggestions to improve it further.
   b. Pumping performance for several curved electrode shapes is compared.
   c. With a leak free pump, the new design with plastically deformed electrodes should be able to achieve 24 Torr with the original design, and 0.75 Torr with the suggested improvements.
   d. Unified diaphragm sizes, pull-pull actuation, and frequency ramping are all suggested to speed up design and improve pump performance.

6.2 Further Work

The most obvious next step is to utilize the results presented in this thesis to create an operational MEMS vacuum pump with integrated electrostatic actuators. While the models predict pressures below 1 Torr for two-stage pumps, it would be valuable to demonstrate this in a working device.

Methods were developed to study arbitrary curved electrodes. It would be worthwhile to analyze additional shapes, and investigate which properties of these shapes are most beneficial for large stroke – low voltage actuation. Initial results suggest that one of the primary factors in which shapes achieve better performance is the fraction of the electrode that has a narrow gap. This basic criterion correctly predicts that plastic deformation would require the lowest pull-in voltages and flat electrodes would require the largest pull-in voltages.

Once promising curvatures are found, further research would be needed to create these shapes. Methods such as DRIE lag and gray scale lithography provide a way to create arbitrary electrode shapes, but both of these methods are difficult to characterize and use reliably.

Controlling the epoxy contraction deformation may provide a third method for creating arbitrary shapes. It has already been demonstrated that small amounts of filler, such as miniature ball bearings, can change the shape of an epoxy contracted membrane. Using different filler shapes could enable considerable control over the electrode shape. It is conceivable that truly arbitrary curvatures could be created by macro-machining a disk with variable thickness using a mill or EDM, and adding this to the epoxy cavity. This could greatly reduce the pull-in voltage, and produce pumps with an even lower voltage to pumped volume.
References


Appendix A

This appendix will describe the fabrication process in more detail.

Select tool descriptions

Throughout this fabrication process, photolithography is performed using two types of positive resist:

1. OCG 825-20 thin positive resist developed in MF CD-26 developer
2. AZ 4620 thick resist developed in AZ405

Resist was exposed using an Electronic Visions EV620 Mask Aligner.

Dry etching was done entirely using two tools:

1. RIE (Reactive ion etching) performed using an AME P5000 (RF magnetically coupled etching system). Ideal for shallow etches
2. DRIE (Deep RIE) was performed using an ST Systems ICP (Inductively Coupled Plasma) tool. Used extensively for deep etches.

The full fabrication process is presented step by step in Table 8.

Table 8: Full fabrication process for electrostatic actuators.

<table>
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<tr>
<th>Feature</th>
<th>Step#</th>
<th>Lab</th>
<th>Process step</th>
<th>Machine</th>
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40 um SOI

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The masks used by the plastic deformation tests are shown in Figure 80 through Figure 82. The masks reused these same three masks plus those shown in Figure 83 through Figure 85.

![Die outline]

Figure 80 - Deformation cavity mask, with 150mm wafer outline (left), with a callout to an individual die (right).
Figure 81 - Pump membrane and cleavage line mask, with 150mm wafer outline (left), with a callout to an individual die (right).

Figure 82 - Venting Channel Mask, with 150mm wafer outline (left), with a callout to an individual die (right).
Figure 83: Electrical via mask, with 150mm wafer outline (left), with a callout to an individual die (right).

Figure 84 - Electrical isolation mask, with 150mm wafer outline (left), with a callout to an individual die (right).
Figure 85: Anti-stiction bump mask, with 150mm wafer outline (left), with a callout to an individual die (right). Bumps are 10 μm across with a pitch of 35 μm.