DEVELOPMENT OF A HYBRID THERMOPLASTIC FORMING PROCESS FOR THE MANUFACTURE OF MULTI-FACET AND CURVILINEAR SURGICAL BLADES FROM BULK METALLIC GLASS

BY

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THESIS

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Abstract

Bulk metallic glasses (BMGs) are multi-component alloys that have formed an amorphous atomic structure. This class of materials exhibit a unique combination of high strength/hardness and high elastic limit that is ideal for the formation and retention of a sharp edge. At high temperatures above the glass transition temperature, BMG begins to exhibit softening behavior and transitions to a supercooled liquid regime. This supercooled state allows the use of economically-viable thermoplastic forming techniques that are typically not available to metals and other hard materials. BMG has therefore been identified as an alternative material for precision surgical blades with the potential of tremendous cost savings.

A hybrid thermoplastic forming processing involving sequential micro-molding and micro-drawing operations is developed to manufacture the multi-facet/curvilinear geometries found on most surgical blades. This is accomplished through an oblique drawing technique, i.e. drawing with a non-zero inclination angle. By applying time-varying force profiles during the drawing operation, a wide range of complex blade geometries are possible. A manufacturing testbed has been designed, assembled, and automated based on the oblique drawing concept. To facilitate a smooth drawing operation, a supervisory control algorithm has been specifically developed to switch between force-feedback and velocity-feedback controllers.

Experiments have exhibited positive results across several multi-facet and curvilinear blade geometries. Manufacturing process capabilities are quantitatively evaluated and experimental results have measured cutting edge radii to be consistently less than 15 nm, rake face surface finish $R_a$ to be on the order of 20 nm, and edge straightness deviations to be less than 5 µm RMS. Measurements are made at several locations along blade samples as well as across various blade geometries. A high degree of repeatability of edge radius, surface finish, and straightness are found both within a single sample as well as among different blades.
Acknowledgements

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Thank you to all the friends and family who have provided love and encouragement throughout the years. Thank you to all the colleagues that I have met along the way who have profoundly impacted my professional development.

Whitney, you gave me the strength.

Hannah, you give me the reason.

Lord, you have and always will give me the ability.

“For nothing is impossible with God.” Luke 1:37
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Chapter 1

Introduction

1.1 Background and Motivation

While a wide variety of precision surgical blades exist on the market, there is a clear trade-off between performance and cost benefits. In corneal surgery, small incisions with higher quality blades result in a shorter recovery period, less trauma to the eye, and reduced possibility of surgical complication [1]. With high hardness and the ability form exceptionally sharp edges (< 10 nm edge radius), single crystalline diamond has become the preferred material for precision surgical blades. The cost of diamond blades, however, can range between $2000-$3000 [2]. Although the diamond material itself is expensive, the high cost is primarily dictated by the time consuming planarization and polishing processes used to manufacture the blade’s edge radius. Sapphire blades can achieve similar edge radii (20-40 nm) but only maintains its edge for less than 100 incisions and uses the same time consuming manufacturing process as diamond blades [3]. Stainless steel blades can be manufactured more efficiently through coining and electrochemical polishing, but yields a large edge radius (~300 nm) resulting in inferior cutting performance and longer patient recovery. Alternatively, silicon blades can also be manufactured efficiently through an etching process and achieve much smaller edge radii (~40 nm), thus nearing the cutting performance of diamond. However, both stainless steel and silicon blades do not possess the durability to sustain their cutting edge over multiple incisions [3].

Bulk metallic glass (BMG) has been identified as an economically viable alternative material for precision surgical blades. Vitreloy-1, developed by Liquid Metal Technologies [4], is a zirconium-based bulk metallic glass with the chemical formula: Zr_{41.2}Ti_{13.8}Cu_{12.5}Ni_{10.0}Be_{22.5}. During material processing, rapid cooling (1 K/s) during solidification from a melt allows the material to reach a metastable condition in which the amorphous structure that existed in its
liquid state is retained at room temperature. Vitreloy-1 is ideal for use as a surgical blade material due to its amorphous structure, high strength (1900 MPa), and high hardness (534 HV) [4]. Both the strength and hardness are equivalent to standard tool steels and are ideal for the formation and retention of a sharp edge. Vitreloy-1 also reaches 2% elongation before failure at room temperature, which is greater than similar high strength brittle metals and ceramics. Zirconium-based alloys have been shown to be fully biocompatible [5,6] and unlike many plastic and silicon instruments, can be sterilized with any current medical method.

At high temperatures above the glass transition temperature (~625 K for Vitreloy-1), BMG begins to exhibit softening behavior and transitions to a supercooled liquid regime. This supercooled state allows the use of several thermally-assisted manufacturing methods that are not typically available to metals and other hard materials (i.e. thermoplastic forming). The availability of these highly economical manufacturing processes combined with BMG’s superior mechanical properties enable the potential to produce blades of higher performance than current metal alloys at a price point significantly lower than precision diamond blades.

Recently, a novel hybrid thermoplastic forming process combining thermally-assisted micro-molding and thermally-assisted micro-drawing was developed to create precision surgical blades with edge radii < 30 nm [7]. Micro-molding was chosen for its ability to quickly and efficiently manufacture the geometry of the blade while micro-drawing was used to create the nano-scale cutting edge through the inherent necking of the material. Through testing it was found that a good balance between necking and elongation could be achieved during drawing, creating a strong, stable cutting edge [7]. Although this hybrid thermoplastic forming process demonstrates tremendous potential, it is currently limited to a 1D configuration. Furthermore, the current manufacturing concept is not directly applicable to more complex geometry such as multi-facet or curvilinear blade designs. It is therefore essential to develop a new, highly flexible system that can extend the capabilities of the process to encompass a wide range of blade geometries.
1.2 Research Objectives, Scope, and Tasks

1.2.1 Research Objectives and Scope

The objective of this research is to investigate new manufacturing technology in the field of precision surgical blades. In particular, a hybrid thermoplastic forming process will be developed to specifically take advantage of the unique processing characteristics of BMG. Development of the novel manufacturing method will be tailored for the production of complex multi-facet and curvilinear blade geometries. Requirements for the manufacturing process are as follows:

1. The process must be economically viable for mass production of surgical blades.
2. The process must be able to produce blades with edge radii less than 50 nm.
3. The surface finish on the blades must be below 20 nm $R_a$.
4. Precision surgical blades are available in a variety of shapes and sizes. Figure 1.1 categorizes the various different blade geometries into four general categories. The novel manufacturing process must be able to accommodate all different blade shapes.

To this end, it is necessary to develop a variation of the original hybrid thermoplastic forming process [3] that specifically improves the flexibility of the drawing process. The scope of this research revolves around the design and analysis of a custom testbed that expands the manufacturing capability to include blades with complex multi-facet/curvilinear geometry. To demonstrate feasibility, several multi-facet and curvilinear blade types will be manufactured and experimentally characterized through a series of performance measures. Although the
hybrid thermoplastic forming process [7] is applicable to any class of metallic glass, experimental work will be limited to a commercially available BMG, Vitreloy-1b ($\text{Zr}_{44}\text{Ti}_{11}\text{Cu}_{10}\text{Ni}_{10}\text{Be}_{25}$), developed by Liquid Metal Technologies [4].

1.2.2 Research Tasks

Objectives will be met in four phases and are outlined in the following tasks.

Task 1: Development of Hybrid Thermoplastic Forming Technique for Multi-Facet/Curvilinear Blades

- A variation of the original hybrid thermoplastic forming process will be investigated. Specifically, the drawing operation will be performed in an oblique direction such that the method for applying the drawing force is highly flexible.
- The manufacture of a wide range of blade geometries will be possible by controlling the drawing force as a function of the oblique inclination angle formed by the drawing direction and the edge geometry.

Task 2: Hybrid Thermoplastic Forming Process Testbed Design

- Based on the oblique drawing concept, a custom testbed will be designed to produce complex multi-facet and curvilinear blade geometries.
- The design of the custom testbed must include mechanisms to sequentially perform the molding operation followed by the oblique drawing operation. A set of modules will be developed to address the stringent requirements for both high accuracy and repeatability required by the hybrid thermoplastic forming process.
- Critical components in both the molding platform and the drawing platform will be analyzed in detail to ensure proper tolerances are achieved.

Task 3: Automation and Controller Development

- Sensors will be integrated in the custom testbed for both active control over the individual components as well as process monitoring.
• Automation, temperature control, and motion control requirements for the process will be programmed on Labview compactRIO hardware.
• A supervisory control algorithm will be developed to address the requirements of the oblique drawing process. Specifically, this supervisory controller will be designed to switch between force-feedback and velocity-feedback controllers with an emphasis on ensuring a smooth drawing profile.

**Task 4: Performance Evaluation of Various Blade Geometries**

• The custom testbed will be used to experimentally explore the oblique drawing process and its ability to produce blades with complex geometry.
• Several blade geometries will be evaluated based on the requirements set forth in Section 1.2.1. In particular, experiments will be carried out to assess the capabilities of this testbed based on oblique drawing.
• In addition, blade quality will be studied as a function of process parameters. These experiments will provide insight for understanding general trends of the process such that process parameters can be chosen to produce high quality blades. Repeatability will be investigated once a set of suitable input parameters has been identified.

### 1.3 Outline of Thesis

The remainder of this thesis is organized as follows.

Chapter 2 provides a review of relevant literature pertaining to the processing of BMG. The first section overviews the mechanism that drives the formation of the amorphous structure as well as crystallization thermodynamics and kinetics. The general stress-strain behavior of BMG materials is then described over a wide temperature range including the glass transition temperature along with several techniques to model the constitutive behavior. This is followed with a discussion on the major manufacturing techniques typically applied to BMG materials including near net shape processing as well as machining processes. Finally, the work of Krejcie et al. [7] is analyzed in detail as a direct predecessor to the current work.
Chapter 3 proposes a conceptual outline of an oblique drawing process that is capable of manufacturing complex blade geometries. Based on the oblique drawing process requirements, a testbed design is formulated as three separate modules, namely the lever arm modules, the molding module, and the drawing module. Mechanism design and the associated coordinate transformations are analyzed to obtain a full description of machine kinematics. For each module, critical components are analyzed individually to ensure proper tolerances are met. In the next section, the automation architecture is described along with details on implementation. A customized supervisory control algorithm for the drawing operation is explained.

Chapter 4 describes a series of experiments used to characterize the hybrid thermoplastic forming process with regards to multi-facet and curvilinear blade manufacture. This chapter initially investigates the local crystallization during edge formation through a TEM selected area diffraction study. This is followed by a preliminary investigation of bi-axial drawing technique on a 90° lancet multi-facet blade. Once feasibility is shown, extensive testing is performed on 45° lancet multi-facet blade to investigate the effect of varying process parameters. Once process parameters that produce high-quality blades are identified, repeatability of the process is quantitatively evaluated through the included angle tolerance, force/velocity profile, drawing distance, edge radius, rake face surface roughness, and blade straightness. In addition, several qualitative observations of the overall blade geometry and tip formation are made. Finally, the capability to produce curvilinear blade geometry is demonstrated through the manufacture of a crescent blade. These crescent blades are again similarly characterized in terms of edge radius and surface roughness.

Chapter 5 provides several conclusions based on the work accomplished. Recommendations for future work are proposed.
Chapter 2

Literature Review

2.1 Introduction

Conventional metallic alloys typically form polycrystalline micro-structures when cooled from a liquid state. This phase transformation occurs naturally as thermodynamic forces drive atomic reconfiguration into its lowest energy state, i.e. a crystalline lattice structure. Grain boundaries in a polycrystalline material represent areas of less than optimal atomic packing and act as initiation sites for both fracture and corrosion. Within each grain, crystalline dislocations propagate in the presence of stress resulting in permanent plastic deformation at much lower strengths than theoretical maximums [9].

Metallic glasses are a unique class of materials epitomized by their amorphous atomic structure. With no crystalline defects, metallic glasses are a particularly attractive class of materials due to their unique combination of high strength/hardness, high elastic limit, superior strength-to-weight ratio, and high corrosion and wear resistance [9–18]. As outlined by Fig. 2.1, metallic glasses provide a combination of high strength and elasticity that is not available in many traditional materials.

The first reported case of metallic glass originated in 1960 by Klement et al. [19], wherein a silicon-gold alloy was rapidly quenched from 1300 °C to room temperature. Through this rapid quenching procedure, the process of nucleation and growth of crystalline phases was kinetically bypassed, resulting in the vitrification of the liquid melt. The silicon-gold alloy retained its amorphous structure in a metastable configuration with no long-range order in its atomic structure. Since the discovery of metallic glass, intensive research has been conducted to improve the formability of larger samples, known as bulk metallic glass (BMG) [14,20–26].
Figure 2.1: Comparison of structural properties of metallic glass alloys with traditional materials [9]

2.2 Formation of Metallic Glasses

Metallic glasses are formed through the rapid quenching of a liquid mixture of metallic elements. As the metallic glass is cooled, the amorphous structure of the liquid state must be stabilized while suppressing crystallization during solidification [12]. The glass forming ability (GFA) gauges an alloy’s aptitude for suppressing of crystallization during solidification and ultimately dictates the critical cooling rate that is required to form the metastable amorphous atomic structure. Above this critical cooling rate, thermally-driven atomic movement rearranges the atomic structure into a thermodynamically stable crystalline state [18].

Turnbull proposed a ratio called the reduced glass transition temperature as a measure for the GFA of an alloy [27]. The reduced glass transition temperature is defined as [18]


\[ T_{rg} = \frac{T_g}{T_m}, \tag{2.1} \]

where \( T_g \) is the glass transition temperature and \( T_m \) is the melting, or liquidus, temperature. Turnbull’s criterion [25] proposed that an alloy possessing \( T_{rg} > 2/3 \) will exhibit sluggish crystallization kinetics and therefore only crystallize within a narrow temperature range. This is a well-known criterion for assessing the ability of a liquid to form a glassy structure at reasonably low cooling rates. It has also been one of the key principles guiding the development of various metallic glass systems [18].

It is a common observation that the GFA in BMG materials tend to increase as more components are added to the alloy, which is known as the confusion principle [28]. As additional element components are added to a BMG alloy, the formation of competing crystalline phases destabilizes during solidification [18]. For multi-component metallic alloys, Inoue [29–31] has empirically identified three major guidelines that encourages high GFA [12].

1. Multi-component alloy consisting of three or more elements.
2. Significant atomic size mismatch of over 12% among the three primary elements.
3. Negative heat of mixing for the compound.

Multi-component metallic glasses following the three empirical rules proposed by Inoue [12] have also been shown to exhibit three characteristic features. First, these alloys will typically possess a dense, randomly packed structure, which in most cases is energetically favorable [32]. The dense atomic packing reduces the average atomic free volume thereby decreasing the atomic mobility facilitating crystallization kinetics [23]. Second, the atomic structure will contain new local atomic configurations that are significantly different than corresponding crystalline phases. Finally, the amorphous phase will have long range homogeneity with attractive interactions [13,30]. These characteristics lead to an increase in reduced glass transition temperature. The mechanism for stabilizing a supercooled liquid originating from Inoue’s three empirical rules for improving GFA [12] are summarized in Fig 2.2.
Research into several BMG material systems have been heavily investigated in the past. Table 2.1 provides a list of common bulk glassy alloy systems along with the date of first publication [12,33].
Table 2.1: Typical bulk metallic glass systems with the year of first publication [12,33]

<table>
<thead>
<tr>
<th>Non-ferromagnetic alloy systems</th>
<th>Year</th>
<th>Ferromagnetic alloy systems</th>
<th>Year</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mg–Ln–M (Ln = lanthanide metal, M = Ni, Cu, Zn)</td>
<td>1988</td>
<td>Fe–(Al,Ga)–(P,C,B,Si,Ge)</td>
<td>1995</td>
</tr>
<tr>
<td>Ln–Al–TM (TM = Fe, Co, Ni, Cu)</td>
<td>1989</td>
<td>Fe–(Nb,Mo)-(Al,Ga)-(P,B,Si)</td>
<td>1995</td>
</tr>
<tr>
<td>Zr–Al–TM</td>
<td>1990</td>
<td>Fe–(Zr,Hf,Nb)–B</td>
<td>1996</td>
</tr>
<tr>
<td>Pd–Cu–Ni–P</td>
<td>1996</td>
<td>Fe–(Cr,Mo)–(C,B)</td>
<td>1999</td>
</tr>
<tr>
<td>Pd–Ni–Fe–P</td>
<td>1996</td>
<td>Ni–(Nb,Cr,Mo)–(P,B)</td>
<td>1999</td>
</tr>
<tr>
<td>Ti–Ni–Cu–Sn</td>
<td>1998</td>
<td>Co–Ta–B</td>
<td>1999</td>
</tr>
<tr>
<td>Ca–Cu–Ag–Mg</td>
<td>2000</td>
<td>Fe–Ga–(P,B)</td>
<td>2000</td>
</tr>
<tr>
<td>Cu–(Zr,Hf)–Ti</td>
<td>2001</td>
<td>Ni–(Nb,Ta)–Zr–Ti</td>
<td>2002</td>
</tr>
<tr>
<td>Cu–(Zr,Hf)–Ti–(Fe,Co,Ni)</td>
<td>2002</td>
<td>Fe–Si–B–Nb</td>
<td>2002</td>
</tr>
<tr>
<td>Cu–(Zr,Hf)–Al</td>
<td>2003</td>
<td>Co–Fe–Si–B–Nb</td>
<td>2002</td>
</tr>
<tr>
<td>Cu–(Zr,Hf)–Al–(Ag,Pd)</td>
<td>2004</td>
<td>Ni–Nb–Sn</td>
<td>2003</td>
</tr>
<tr>
<td>Pt–Cu–Ni–P</td>
<td>2004</td>
<td>Co–Fe–Ta–B–Si</td>
<td>2003</td>
</tr>
<tr>
<td>Ti–Cu–(Zr,Hf)–(Co,Ni)</td>
<td>2004</td>
<td>Ni–Pd–P</td>
<td>2004</td>
</tr>
<tr>
<td>Au–Ag–Pd–Cu–Si</td>
<td>2005</td>
<td>Fe–(Cr,Mo)–(C,B)–Ln (Ln = Y, Er, Tm)</td>
<td>2004</td>
</tr>
<tr>
<td>Ce–Cu–Al–Si–Fe</td>
<td>2005</td>
<td>Co–(Cr,Mo)–(C,B)–Ln (Ln = Y, Tm)</td>
<td>2005</td>
</tr>
<tr>
<td>Cu–(Zr,Hf)–Ag</td>
<td>2005</td>
<td>Ni–(Nb,Ta)–Ti–Zr–Pd</td>
<td>2006</td>
</tr>
<tr>
<td>Zr–Cu–Al–Ag–Pd</td>
<td>2007</td>
<td>Fe–(Nb, Cr)–(P,B,Si)</td>
<td>2010</td>
</tr>
<tr>
<td>Ti–Zr–Cu–Pd</td>
<td>2007</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Ti–Zr–Cu–Pd–Sn</td>
<td>2007</td>
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<td></td>
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</tbody>
</table>

2.3 Crystallization of Metallic Glasses

Since the amorphous state of BMG is only metastable, the precipitation and growth of crystalline phases are important to understand due to their effects on material behavior. The complete crystallization of a sample results in a highly brittle and weak material lacking the unique properties of the BMGs and therefore must be avoided. During the solidification and
processing of these metallic glass materials, crystallization is governed primarily by two competing mechanisms. Below the melting temperature of a metallic glass, the amorphous atomic structure is thermodynamically driven towards competing crystalline phases [24,34,35]. As temperature is decreased, the atomic mobility is further restricted due to kinetic constraints, which is correlated to the temperature-dependent viscosity of the material [36]. Taking into account these two contributions, it is observed that BMG materials typically exhibit unusually high viscosities as well as low thermodynamic driving forces towards crystallization [24].

2.3.1 Thermodynamic Driving Force

The driving force towards crystallization is measured by the Gibbs free energy difference, $\Delta G$, between the supercooled liquid (i.e. the amorphous phase) and the competing crystalline phases [24,34,35]. $\Delta G$ is experimentally determined from the enthalpy of fusion, $\Delta H_f$, contribution and the integration of the difference in specific heat capacity, $\Delta c_p(T)$, between the liquid and crystalline phases. Specifically, $\Delta G(T)$, is calculated as [37]

$$
\Delta G(T) = \Delta H_f - \Delta S_f T_0 - \int_T^{T_0} \Delta c_p(T')dT' + T \int_T^{T_0} \frac{\Delta c_p(T')}{T'} dT',
$$

(2.2)

where $\Delta H_f$ is the enthalpy of fusion, $\Delta S_f$ is the entropy of fusion, $T_0$ is the temperature where Gibbs free energy of the crystal is equal to the that of the liquid, and $\Delta c_p$ is the difference in specific heat capacity between the liquid and crystalline phases.

Figure 2.3a shows examples of the specific heat capacity, $c_p$, of several glass-forming alloys within the supercooled liquid regime. In Fig. 2.3b, the calculated Gibbs free energy is plotted for a selection of BMG systems. Both Fig. 2.3a and Fig. 2.3b are plotted against a temperature scale that is normalized to the alloy’s melting temperature.
Figure 2.3: Thermodynamic data for various BMG alloys in the supercooled liquid regime (a) specific heat capacities (b) difference in Gibbs free energy between the liquid and crystalline states (critical cooling rates are listed beneath composition labels) [24]

As temperature is decreased, it can be seen from Fig. 2.3a that the difference in specific heat capacity between the supercooled liquid and crystalline phases increases further. This trend is
correlated to a decrease in free volume and most likely a gain of short-range order in the alloy melt that is experienced as temperature is decreased [24,37]. From Fig. 2.3b, the asymptotic behavior of Gibbs free energy as the liquid melt is cooled towards the glass transition temperature is attributed to this increasing difference in specific heat capacity [37].

From thermodynamics considerations, bulk metallic glass materials naturally exhibit a low driving force for crystallization, i.e. low $\Delta G$, in the supercooled liquid regime [18]. This primarily originates from BMG systems having smaller entropies of fusion and therefore smaller gradients in $\Delta G$ at the melting point [18,24]. The low driving force results in low nucleation rates and therefore improved GFA [37].

2.3.2 Crystallization Kinetics

It has been observed that the glass transition temperature measurement of a BMG depends on the experimental heating or cooling rate. Phase transitions for these alloys therefore cannot be purely described from a thermodynamic perspective [18]. To characterize the crystallization kinetics, the viscosity of the liquid melt is an important parameter that will significantly influence the GFA of an alloy system. A variety of techniques have therefore been applied to measure viscosity over a wide range of temperatures between the glass transition temperature and the melting temperature [18,21,36,38]. Regardless of metallic or non-metallic composition, as a glass former is cooled through the supercooled liquid regime, the viscosity will increase as a result of a net decrease in atomic mobility [37,39]. Figure 2.4 compares the temperature dependent viscosities of several BMG alloys with a selection of non-metallic glass formers [40].
Temperature-dependent viscosity for glass forming materials is well described by the following Vogel–Fulcher–Tammann (VFT) relation [37]

\[
\eta = \eta_0 \exp \left( D \frac{T_0}{T - T_0} \right),
\]

(2.3)

where \(\eta\) is the viscosity, \(\eta_0\) is the viscosity at infinite temperature, \(D\) is a measure of the kinetic strength of the liquid (i.e. inverse of kinetic fragility), and \(T_0\) is the Vogel–Fulcher temperature.

It is observed that strong glass formers, such as SiO\(_2\), show near-Arrhenius behavior, as indicated by the straight line in Fig. 2.4, as well as possess relatively high viscosities. Conversely, kinetically fragile liquids, such as o-terphenyl, are highly temperature-dependent and exhibit a relatively large drop in viscosity at temperatures just above the glass transition temperature. In addition, fragile liquids have viscosities that are up to eight orders of magnitude
lower than SiO$_2$. For comparison, the kinetic strength parameter from the VFT relation, D, is on the order of 2 for highly fragile glass formers whereas D can reach 100 for a strong glass former like SiO$_2$ [24].

Strong glass formers possess high viscosity in the supercooled liquid regime, which corresponds to sluggish crystallization kinetics. Thermodynamic driving forces naturally favor crystalline phases in most metallic glass systems. However, due to the poor atomic mobility in these BMG formers, the precipitation of stable nuclei and crystal growth is severely limited [18,36,41,42]. The combination of low thermodynamic driving forces and sluggish kinetics inhibits crystal formation and ultimately leads to excellent GFA and high thermal stability in the supercooled liquid state [18].

### 2.3.3 Time-Temperature-Transformation Diagram

For metallic glasses, a time-temperature transformation (TTT) diagram illustrates the time span prior to crystallization for a given temperature in isothermal conditions. Although constant-cooling conditions may be more realistic during processing, it has been shown that both constant-cooling curves and TTT diagrams appear similar in shape [15,43–45].

Figure 2.5a provides an example of an experimentally determined TTT diagram for a BMG commercially known as Vitreloy-1 with the chemical composition of Zr$_{41.2}$Ti$_{13.8}$Cu$_{12.5}$Ni$_{10.0}$Be$_{22.5}$ [24]. Figure 2.5a includes data obtained through electrostatic levitation (red) [46] and high purity carbon crucibles (blue) [47] as well as calculated crystallization times. As described in Section 2.3.1 and Section 2.3.2, decreases in temperature correspond to an increase in thermodynamic driving force towards crystallization along with an overall decrease in atomic mobility within the supercooled liquid regime. The characteristic nose shape observed in the TTT diagram represents a local maximum in crystallization rate as a result of these two competing mechanisms [24].

Figure 2.5b depicts the experimentally determined TTT diagram of Vitreloy-4 with chemical composition of Zr$_{58.5}$Nb$_{2.8}$Ni$_{12.8}$Cu$_{15.6}$Al$_{10.3}$ [15]. Unlike the data provided in Fig. 2.5a, the TTT diagrams have been generated from both cooling from the melting temperature as well as heating from ambient temperature [43,48–51].
It is evident from Fig. 2.5b that either heating or cooling results in an asymmetry in the TTT diagrams for Vitreloy-4. It is observed that the two datasets coincide for low temperatures, however, begin to diverge as annealing temperature is increased. In general, crystallization can be avoided by either preventing crystalline phase nucleation or suppressing crystal growth. The temperature dependent rates for these two mechanisms are given in Fig. 2.5c. It is illustrated that each mechanism exhibits a peak, where the maximum nucleation rate occurs at a much lower temperature compared to the maximum crystal growth rate [49].

In the case of heating, a large number of nuclei will initially form as the sample is heated past the temperature range of maximum nucleation rate. If heating is continued, the large number
of nuclei will be exposed to the temperature range with maximum growth rate resulting in rapid increase in the volume fraction of crystalline phases. In contrast, liquid metallic glass will possess little to no nuclei as the sample is cooled past the temperature range of maximum growth rate. The nuclei that eventually form as the sample is cooled past the maximum nucleation rate experience a dramatically lower growth rate [18,52,53]. This overall sequence of crystalline growth followed by nucleation inhibits the formation of large volumes of crystalline phase and contributes to the excellent GFA of BMG formers. It has been concluded that this asymmetric crystallization mechanism is present in all metallic glass systems [49], and therefore has significant ramifications for design of BMG manufacturing processes and the selection of process parameters.

2.3.4 Deformation-Induced Crystallization

Besides thermal annealing, it is widely known that mechanical deformation in metallic glasses contributes to the formation of nano-crystals with sizes on the order of 5-20 nm [54]. Mechanically-induced nano-crystallization has been observed in a number of studies involving ball end milling [55–57], bending tests [58,59], tension tests [60–65], rolling [66], nano-indentation [67,68], and application of hydrostatic pressure[69,70].

The degree of crystallization is found to correlate with the amount of strain imposed on a sample [62,64]. Differential scanning calorimetry (DSC) scans of deformed and undeformed sections of tensile test specimens with varying strains are provided in Fig. 2.6a and Fig. 2.6b, respectively. It is clearly seen that the crystallization peaks shift in temperature with increasing strains. Using the heat flow collected during DSC, it was possible to estimate the degree of crystallization for a series of tensile test specimens corresponding to the first crystallization peak. Specifically, crystallized volume fraction is estimated as follows [64]

$$V_c = 1 - \frac{\Delta H}{\Delta H_0},$$  

(2.4)

where $V_c$ is the estimated crystallized volume fraction, $\Delta H$ is the heat flow collected prior to the first crystallization peak during a DSC measurement, and $\Delta H_0$ is the heat flow of an as-cast BMG sample.
Figure 2.6: DSC scans with a heating rate of 0.67 K/s for Cu$_{47}$Ti$_{33}$Zr$_{11}$Ni$_6$Sn$_2$Si$_1$ metallic glass tensile specimens at varying strains (a) undeformed section (b) deformed section [64]

The crystallized volume fraction is plotted as a function of strain for the deformed and undeformed sections of the tensile test specimens in Fig. 2.7. Since the entire sample maintains a uniform temperature, the increasing gap between the deformed and undeformed sections implies a direct effect of strain on the degree of crystallization.

Figure 2.7: Volume fraction of the first crystalline phase as a function of strain for Cu$_{47}$Ti$_{33}$Zr$_{11}$Ni$_6$Sn$_2$Si$_1$ BMG at T = 477 °C and strain rate $\dot{\varepsilon} = 2E-3$ s$^{-1}$ [64]

While deformation in the presence of high temperature enhances the rate of nano-crystallization [61,64,66], it has also been cited that nano-crystal formation is observed even in the absence in heating. In several nano-indentation experiments, significant precipitation of nano-crystalline agglomerates were observed in the indented region [67,68]. With quasi-static
While complete crystallization results in a highly brittle material, the presence of nanocrystalline phases creates a composite structure and can significantly affect the mechanical properties of a metallic glass. At room temperatures, it can lead to significant plasticity and strain hardening [54,65]. Within the supercooled liquid regime, it has been associated with non-Newtonian behavior and an increase in the flow stress [18,54,64,65,71,72].

2.4 Bulk Metallic Glass Mechanical Behavior

BMGs have a wide range of potential applications owing to their unique combination of material properties, such as superior strength (up to 5 GPa [73]) and hardness [74,75], excellent corrosion [76] and wear resistance [77], and low magnetic energy loss [78]. The material properties for Vitreloy-1, a commercially available BMG, are given in Table 2.2.

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Elastic strain limit</td>
<td>~2%</td>
</tr>
<tr>
<td>Tensile yield strength</td>
<td>1.9 GPa</td>
</tr>
<tr>
<td>Young’s modulus</td>
<td>96 GPa</td>
</tr>
<tr>
<td>Shear modulus</td>
<td>34.3 GPa</td>
</tr>
<tr>
<td>Poisson’s ratio</td>
<td>0.36</td>
</tr>
<tr>
<td>Vickers hardness</td>
<td>534 VHN</td>
</tr>
<tr>
<td>Fracture toughness, $K_{IC}$</td>
<td>55-59 MPA-m$^{1/2}$</td>
</tr>
<tr>
<td>Thermal expansion coefficient</td>
<td>10.1E-6 K$^{-1}$</td>
</tr>
<tr>
<td>Density</td>
<td>6.11 g/cm$^3$</td>
</tr>
</tbody>
</table>

An Ashby plot is a material selection device used to compare a wide range of materials based on two or more properties. Figure 2.8 provides an Ashby plot based on yield strength and Young’s modulus that contains a several common BMG alloys along with many traditional metals, alloys, and metal-matrix composites. The metallic glass alloys are unique outliers at
the upper edge of the plot, approaching the boundary of theoretical strength. The theoretical strength is represented by the shaded region bounded by the line

$$\sigma_y = \frac{E}{20},$$  \hspace{1cm} (2.5)

where $\sigma_y$ is the yield strength and $E$ is the Young’s modulus. The high strength for a given Young’s modulus implies that metallic glasses possess exceptionally high elastic strain limit and therefore can store large elastic energy per unit volume [10]. These unique properties are a result of the random atomic structure that lacks the dislocations and associated slip planes found commonly in crystalline materials.

Figure 2.8: Ashby chart plotting elastic limit against Young’s modulus for a selection of BMG alloys along with traditional materials [10]

Although BMGs exhibit excellent elastic properties, exceeding the yield threshold typically causes macroscopic brittle fracture at ambient temperatures [15,16]. Unlike crystalline materials whose plasticity is grounded on dislocation-based crystallographic slip, metallic glasses undergo inhomogeneous deformation where strain is highly localized into a thin shear
band [17]. The strain localization is generally attributed as a direct consequence of strain softening and subsequent deformation instability. Once an increment of strain that is applied to a local volume exceeds the yield threshold, softening causes continued localized deformation with increasing strain rates, ultimately causing instability and brittle fracture [16].

2.4.1 High Temperature Mechanical Behavior

Generally speaking, the deformation of metallic glasses can be classified into two modes [80].

1. Homogeneous Flow: Strain is distributed over the entire specimen volume and emulates macroscopic viscous flow.
2. Inhomogeneous Deformation: Linearly elastic material behavior is followed by inelastic strain localization into thin shear bands.

Homogeneous flow is typically experienced with combinations of high temperature and low stress (or strain rate) whereas inhomogeneous deformation is experienced at lower temperatures and higher stresses (or strain rates). A deformation map illustrating the transition between these two modes is given in Fig. 2.9. The two deformation modes are separated by the thick solid line, which indicates the maximum operational strength of the metallic glass prior to strain localization for a given temperature.

Figure 2.9: BMG stress-temperature deformation map [16]
Within the homogeneous regime, flow can be characterized as either Newtonian or non-Newtonian. The values of steady-state flow stress for Vitreloy-1 are plotted in Fig. 2.10 with respect to strain rate for various temperatures [75].

![Graph showing steady-state flow stress as a function of strain rate for various temperatures](image)

**Figure 2.10:** Vitreloy-1 steady-state flow stress as a function of strain rate for various temperatures [75]

Flow stress initially exhibits a linear relationship with strain rate, however, as stress (or strain rate) is increased, the experimental relationship exhibits a decrease in strain rate sensitivity. This nonlinearity signifies a transition into non-Newtonian flow and a corresponding decline in flow stability [16,75]. In addition, non-Newtonian flow is accompanied with a transient stress overshoot phenomenon, which is seen explicitly in the stress-strain curves provided in Fig. 2.11. It is seen that decreases in temperature and increases in strain rate have similar effects on the stress-strain response. The stress overshoot is considered to be the result of rate-limited structural relaxation. During Newtonian flow, stress-induced free volume is quickly redistributed via rearrangements of local atoms thereby allowing perfectly plastic behavior. As the stress is increased, structural relaxation is unable to counteract free volume creation and therefore causes a drop in load bearing capacity. Nieh et al. [65] suggested alternatively that non-Newtonian type flow could be attributed to nano-crystallization during deformation of a BMG.
Further increases in strain rate or decreases in temperature restrict diffusional rearrangement of local atomic structure. Dynamic equilibrium between stress-induced free volume creation and diffusional relaxation is unable to be achieved thereby leading to localized strain softening and formation of intense shear bands [80]. This deformation mode is typically the behavior BMG materials exhibit at room temperature and corresponds to the stress-strain curves that show little to no inelastic behavior in Fig. 2.11.

The strain rate-temperature deformation map distinguishing Newtonian, non-Newtonian, and shear localization behavior for Vitreloy-1 is illustrated in Fig. 2.12.
2.4.2 Deformation Mechanisms

It is difficult to identify a unit process of deformation in a metallic glass due to the lack of long-range order. Although the exact nature of local atomic motions during inelastic deformation is not confirmed, a number of mechanistic theories have been proposed that correspond well with the mechanical properties of metallic glass observed over a wide range of temperatures and strain rates. There are two proposed mechanisms that have received general acceptance as suitable deformation mechanisms.

1. Shear Transformation Zone (STZ): The STZ model was proposed by Argon [81] where a cluster of several dozen atoms rearrange to accommodate an applied shear stress, as illustrated by Fig. 2.13a.

2. Single Atomic Jump: Spaepen [80] proposed a model based on the redistribution of free volume where a single atom jumps from an area of low free volume to an area of higher free volume, as illustrated by Fig. 2.13b.
Figure 2.13: Atomistic deformation mechanism in metallic glass [16] (a) STZ model proposed by Argon [81] (b) Single atomic jump proposed by Spaepen [80]

These mechanisms share the fact that these atomistic distortion events are thermally activated and biased in the direction of shear stress. The evolution of both mechanisms can be modeled as simple rate laws for activation as a function of state variables, such as stress, temperature, and local structural order parameters (i.e. free volume) [82].

Shear Transformation Zone

STZ theory proposes the existence of local clusters of atoms that undergo cooperative rearrangement in the presence of shear strain resulting in inelastic deformation. Argon quantified STZ behavior in the context of an Eshelby-type inclusion [83] where an STZ operation occurs within the elastic confinement of the surrounding glass matrix. The free energy associated with STZ activation is calculated as

$$
\Delta F = \left[ \frac{7 - 5\nu}{30(1 - \nu)} + \frac{2(1 + \nu)}{9(1 - \nu)} \beta^2 + \frac{1}{2\gamma_0} \frac{\tau_0}{\mu(T)} \right] \mu(T) \gamma_0^2 \Omega_0, \tag{2.6}
$$

where $\nu$ is the Poisson’s ratio, $\tau_0$ is the athermal shear stress at which the STZ transforms, $\mu(T)$ is the temperature dependent shear modulus, $\beta$ is the ratio of the dilation to the shear strain, $\gamma_0$
is characteristic strain of an STZ that usually on the order of ~0.1, and $\Omega_0$ is the characteristic volume of an STZ usually on the order of ~100 atoms.

STZs have been widely observed in the form of concurrent and collective motion of several dozens of atoms in both 2D and 3D atomistic simulations [84–102]. Due to the transient nature of STZs [16], it is difficult to measure the size of these events. It is estimated that these local clusters of atoms are ~30-100 atoms in size with a volume of ~0.5-3.7 nm$^3$ [82].

The connection between microscopic STZ events and macroscopic yield at room temperature is summarized by Lund and Schuh [103] as follows:

1. STZs nucleate in the presence of applied shear strain [81].
2. STZ events cause localized distortion in the surrounding amorphous matrix and produce free volume. The associated reduction in strength leads to shear localization and the formation of large planar bands of STZs [80,104].
3. Inelastic distortion produces adiabatic heating in these regions [58,105–107].
4. Thermally-enhanced, strain-induced nucleation of nano-crystallites occurs in the shear band [58,63,68,108].
5. Nucleation of nano-voids appears in the shear band [109,110].
6. Subsequent coalescence of nano-voids leads to final failure [111,112].

**Single Atomic Jump**

An alternative mechanism based on the classical free-volume model [113,114] was specialized for glass deformation by Spaepen [80]. As opposed to the redistribution of dozens of atoms in STZ theory, the activation energy in the free volume model corresponds to a highly localized, discrete atomic jump into a vacant site in the glass structure, as illustrated by Fig. 2.13b. The presence of a shear stress gradient causes the atomic jumps to be biased in the direction of stress, which leads to microscopic plastic shear and the redistribution of free volume in the glass [17].

The concept of tracking the net result of a shear-induced creation and the subsequent diffusion-controlled annihilation introduced a convenient state parameter, free-volume. This model has
gained popularity because of the ease of implementation in a constitutive framework, particularly from a computational perspective. However, it should be noted that a single atomic jump cannot relax local shear stress and is therefore lacking a physical basis [82].

Similar to STZ theory, high shear stress conditions initiate the accumulation of free volume and weakens the specimen locally by decreasing the effective density at an atomic jump site [80]. Localized shear bands form due to the generation of free volume [115–117] and the subsequent decrease in macroscopic viscosity of the glass [80,107,115,118,119]. It has also been alternatively hypothesized that viscosity can decrease locally in the shear band due to local adiabatic heating above the glass transition temperature [58,105,106].

2.4.3 Constitutive Relations

BMG has a wide range of behavior that is highly sensitive to both temperature and strain rate. Because of the unique mechanical deformation characteristics exhibited by BMG, specialized constitutive equations are required to properly capture the stress-strain response. In particular, an elastic-viscoplastic constitutive framework specifically developed for thermoplastic forming process is adapted for von-Mises plasticity [120] and provided as an example for implementation in a finite element (FE) framework. This model is limited for isothermal situations without temperature gradients. In addition, the pressure-sensitivity of flow and hence, the internal coefficient of friction are assumed to be negligible.

Kinematics

Thermoplastic forming processes inherently involve large strains. Therefore the kinematics are derived within finite deformation theory.

Motion is first described as a smooth bijective mapping

\[ x = \chi(X,t), \tag{2.7} \]

where \( X \) is an arbitrary point in the reference configuration and \( x \) is a point in the current configuration and \( \chi \) is the bijective mapping of the motion.
The deformation gradient is defined as

$$F = \nabla \chi(X, t), \quad (2.8)$$

where $F$ is the deformation gradient.

The total deformation gradient is split via multiplicative decomposition (i.e. Lee decomposition)

$$F = F^e F^p, \quad (2.9)$$

where $F^e$ and $F^p$ represent the elastic and plastic parts of $F$, respectively.

The elastic deformation gradient, $F^e$, is split via polar decomposition

$$F^e = R^e U^e, \quad (2.10)$$

where $R^e$ and $U^e$ represent the elastic rotation and right stretch tensor, respectively.

The elastic right stretch tensor, $U^e$, is split via spectral decomposition

$$U^e = \sum_{\alpha=1}^{3} \lambda^e_{\alpha} r^e_{\alpha} \otimes r^e_{\alpha}, \quad (2.11)$$

where $\lambda^e_{\alpha}$ are principal stretches and $r^e_{\alpha}$ are the eigenvectors.

Logarithmic elastic strain is defined in terms of the principal stretches from Eq. 2.11

$$E^e = \sum_{\alpha=1}^{3} \ln(\lambda^e_{\alpha}) r^e_{\alpha} \otimes r^e_{\alpha}, \quad (2.12)$$

where $E^e$ is the logarithmic elastic strain measure.
Hyperelastic Strain Energy Density Function

The model is posed in a hyperelastic framework with the following simple strain energy density function

$$\psi = G |\mathbf{E}_0^e|^2 + \frac{1}{2} K (\text{tr}(\mathbf{E}^e))^2,$$  

(2.13)

where $G$ and $K$ are the shear and bulk moduli, respectively, and $\mathbf{E}_0^e$ represents the deviatoric part of the logarithmic elastic strain.

The shear and bulk moduli are defined as functions of the experimentally-fit, temperature-dependent Young’s modulus and the Poisson’s ratio using standard homogeneous, linearly elastic isotropic material relations

$$G(\theta) = \frac{E(\theta)}{2(1 + \nu(\theta))},$$

$$K(\theta) = \frac{E(\theta)}{3(1 - 2\nu(\theta))},$$

(2.14)

$$E(\theta) = \frac{1}{2} (E_{gl} + E_{sc}) - \frac{1}{2} (E_{gl} - E_{sc}) \tanh \left( \frac{1}{\Delta_E} (\theta - \theta_g) \right) - k_E (\theta - \theta_g),$$

$$\nu(\theta) = \frac{1}{2} (\nu_{gl} + \nu_{sc}) - \frac{1}{2} (\nu_{gl} - \nu_{sc}) \tanh \left( \frac{1}{\Delta_E} (\theta - \theta_g) \right),$$

where $G(\theta)$ is the shear modulus, $K(\theta)$ is the bulk modulus, $E(\theta)$ is the Young’s modulus, $\nu(\theta)$ is the Poisson’s ratio, $\theta$ is the temperature, and $E_{gl}, E_{sc}, \Delta_E, \theta_g, k_E, \nu_{gl}, \nu_{sc}$ are material parameters provided in [120] that are fit to experimental data.

The stress conjugate to logarithmic elastic strain $\mathbf{E}^e$ is given as

$$\mathbf{M}^e = \frac{\partial \psi}{\partial \mathbf{E}^e} = 2G \mathbf{E}_0^e + K (\text{tr}(\mathbf{E}^e)) \mathbf{1},$$  

(2.15)

where $\mathbf{M}^e$ is also known as the Mandel stress tensor.
Cauchy stress is therefore defined as
\[ T := J^{-1} \left( R^e M^e R^{e^t} \right), \]  
(2.16)
where \( J^e = \det(F^e) \).

**Evolution Equations and Flow Rule**

Two internal state variables, a microstructural order parameter \( \varphi \) and a resistance to plastic flow parameter \( S \), introduce coupling effects in the evolution equations to better reproduce the stress-overshoot and strain softening observed in BMG compression tests [75]. The coupled evolution equations of the internal state variables are given below

\[
\dot{\varphi} = \beta \nu^p \quad \text{where } \varphi(X, 0) = \varphi_0,
\]

\[
\beta = g(\varphi^* - \varphi),
\]

\[
\varphi^* = \begin{cases} 
\varphi_s \left( 1 - \left( \frac{\theta - \theta_0}{\theta_s} \right)^p \right)^q & \text{if } (\theta - \theta_0) \leq \theta_s, \\
0 & \text{if } (\theta - \theta_0) > \theta_s,
\end{cases}
\]

(2.17)

\[
\varphi_s = k_1 + k_2 \ln \left( \frac{\nu^p}{\nu_{\text{ref}}} \right),
\]

\[
\theta_s = l_1 + l_2 \ln \left( \frac{\nu^p}{\nu_{\text{ref}}} \right),
\]

\[
\dot{S} = h \nu^p \quad \text{where } S(X, 0) = S_0,
\]

\[
h = h_0 (S^* - S),
\]

(2.18)

\[
S^* = b(\varphi^* - \varphi),
\]

\[
b = \begin{cases} 
b_{\text{dil}} & \text{if } \varphi < \varphi^*, \\
b_{\text{com}} & \text{if } \varphi > \varphi^*,
\end{cases}
\]

where \( S_0, h_0, b_{\text{dil}}, b_{\text{com}}, g, \theta_0, p, q, k_1, k_2, l_1, l_2 \), and \( \nu_{\text{ref}} \) are fitted material parameters taken from [120].
The flow function is a modified version of the Spaepen free-volume model [80] and is defined as

$$
\nu^p = \nu_0 \exp(-1/\zeta) \exp\left(-\frac{\Delta F}{k_B \theta}\right) \sinh\left(\frac{\tau_{eff}V}{2k_B \theta}\right),
$$

$$
\tau_{eff} = \sigma_f - S,
$$

$$
\Delta F(\theta) = \frac{1}{2}(\Delta F_g + \Delta F_s) - \frac{1}{2}(\Delta F_g - \Delta F_s) \tanh\left(\frac{1}{\Delta F}(\theta - \theta_g)\right),
$$

$$
V(\theta) = \frac{1}{2}(\Delta V_g + \Delta V_s) - \frac{1}{2}(\Delta V_g - \Delta V_s) \tanh\left(\frac{1}{\Delta V}(\theta - \theta_v)\right),
$$

$$
\zeta(\theta) = \frac{1}{2d_1} \left[\theta - \theta_{ref} + \sqrt{(\theta - \theta_{ref})^2 + d_2 \theta}\right],
$$

where $\nu^p$ is the plastic shearing rate adapted from the Spaepen one-dimensional free-volume model, $\tau_{eff}$ is the effective stress driving inelastic distortion, $\sigma_f$ is the flow stress, $\Delta F$ is a fitted activation energy, $V$ is a fitted activation volume, and $\zeta$ is the temperature-dependent equilibrium free volume that has been adopted from Cohen and Grest [121]. $\nu_0$, $k_B$, $\Delta F_g$, $\Delta F_s$, $\Delta F$, $\Delta V$, $\theta_V$, $d_1$, $\theta_{ref}$, and $d_2$ are fitted material parameters taken from [120].

For the time integration procedure, the evolution of $F^p$ is given as

$$
\dot{F}^p = D^p F^p,
$$

$$
N^p = \frac{3 M^e_0}{2 |M_0^e|} = \frac{3 M^e_0}{2 \sqrt{M_0^e : M_0^e}},
$$

$$
D^p = \frac{3}{\sqrt{2}} \nu^p N^p,
$$

where $M^e_0$ is the deviatoric part of the Mandel stress tensor, $N^p$ is the plastic flow direction tensor, and $D^p$ is the plastic rate of deformation tensor.
Yield Function

The yield function is simplified to a von-Mises yield criteria, which is given as

\[ G = \tau - \sigma_f = \tau - \tau_{eff} - S, \]

\[ \tau = \sqrt{\frac{3}{2} |M_0^e|} = \sqrt{\frac{3}{2} M_0^e : M_0^e}, \quad (2.21) \]

where \( \tau \) is the equivalent von-Mises stress.

2.5 Processing of Metallic Glasses

The primary challenge encountered during processing of BMG is avoiding crystallization of the metastable amorphous structure. Prior to the discovery of BMG material systems, production of metallic glass was limited to thin samples due to the high rate of heat transfer required to prevent crystallization. Thin-film processing was restricted to techniques such as splat quenching or melt spinning [19]. With the advent of BMG formers with critical cooling rate of < 10 K/s, near-net shape manufacturing has become the preferred processing strategy and will typically follow two different paths [122], as shown in Fig. 2.14.

The first path is direct casting wherein a molten BMG alloy fills a mold cavity through either die casting or suction casting. While filling the mold, the BMG alloy must simultaneously achieve the critical cooling rate to avoid crystallization as well as maintain adequate flow to avoid cold shuts and low porosity. Even with careful selection of processing parameters, the range of achievable geometries are limited due to these inherently conflicting objectives [123,124].

The second path involves reheating BMG samples to the glass transition temperature and performing thermoplastic forming (TPF) techniques in the supercooled liquid regime. Above the glass transition temperature, BMG exhibits considerable softening with viscosities between \( 10^{12} \) Pa-s and \( 10^5 \) Pa-s while at the same time retaining its metastable amorphous structure for relatively long periods of time prior to complete crystallization [36].
In many applications where certain features are unable to be cast or formed, it is common to perform post-processing steps to finalize part geometry. The machinability of BMG is also reviewed as a secondary manufacturing process to provide complementary capabilities.

2.5.1 Direct Casting

During direct casting, BMGs must be rapidly cooled during solidification to avoid crystallization. However, to obtain high-quality castings, slow cooling and small thermal gradients are desired. The conflicting nature of these requirements makes casting of complex geometries a challenging task [126,127]. Processing parameters such as mold design, casting temperature, and mold temperature and pressure must be carefully controlled to achieve acceptable quality [122].

Both suction and die casting methods have been applied to BMG net-shape fabrication processes [15]. While suction casting often yields higher quality parts with low porosity [128], die casting is easily scalable to high volume production of small- and medium-sized parts. Die casting has been recently pursued commercially by Liquidmetal Technologies using aluminum dies for high-volume production of Zr-based BMG parts. Several examples of cast parts are shown in Fig. 2.15.
BMG castings are able to maintain excellent dimensional stability and tolerances because of the absence of a first-order phase transformations. Vitreloy-1b, for example, experiences only a 0.4% shrinkage due to thermal contraction [122] compared to crystalline materials that exhibit almost 5% shrinkage due to phase transition [129]. In spite of the order of magnitude reduction in shrinkage, the gap that forms due to contraction significantly lowers the rate of heat transfer between the part and the die [130]. It has been shown that heat transfer through the gap can vary drastically depending on whether a gas atmosphere or vacuum is applied. The choice in environmental conditions can become the rate-limiting factor in achieving the critical cooling rate required for vitrification.

2.5.2 Thermoplastic Forming

TPF involves reheating BMG into the supercooled liquid regime to induce thermal softening so that complex shapes can be created through the viscous flow of the material. With appropriate temperatures and strain rates, Zr-based BMG samples have been observed to undergo elongations exceeding 300% [131]. The amorphous structure of BMG is thermally
stable over a wide temptation range such that forming processes are able to occur on a practical experimental time scale [132]. TPF of BMG has become a highly feasible processing option because of its ability to decouple the high cooling rate required for vitrification from the formation of part geometry.

Compared to casting techniques, TPF is performed at lower temperatures resulting in shrinkage rates as low as 0.2%. By decreasing the cooling rate, thermal stresses can be also be reduced to negligible levels. In addition, there are less stringent requirements on environmental control because oxidation only has a minor influence on the crystallization kinetics near the glass transition temperature for most BMG alloys [133]. Although TPF requires an additional processing step compared to direct casting, the higher tolerances and less stringent requirements may result in lower overall production costs.

TPF processes have been explored through a variety of techniques including micro-/nano-molding [122,125,134–138], injection molding [139], blow molding [140], powder compaction [141], extrusion [142,143], rolling [144], foaming synthesis [145], and writing/erasing [146,147]. Figure 2.16 provides examples of a diverse range of surface patterns and 3D parts and geometries that have been created through TPF techniques.
Figure 2.16: TPF examples (a) compression molding of 10-50 µm diameter micropillars Pt$_{57.5}$Cu$_{14.7}$Ni$_{5.3}$P$_{22.5}$ [148] (b) blow molding of Zr$_{44}$Ti$_{11}$Cu$_{10}$Ni$_{10}$Be$_{25}$ rectangular perfume bottle [15] (c) 3D micro-gears created by molding and planarization [149] (d) extruded Zr$_{44}$Ti$_{11}$Cu$_{10}$Ni$_{10}$Be$_{25}$ 2 mm wire [150] (e) thinning and micro replication during rolling [144] (f) surface smoothening through surface-tension driven flow on Pt$_{57.5}$Cu$_{14.7}$Ni$_{5.3}$P$_{22.5}$ [146]

BMGs possess excellent replication ability because they are not limited by a heterogeneous grain structure. At the micro-scale, complex geometry has been imprinted with high fidelity in multiple hot-embossing studies [122,125,134–138]. Feature replication capabilities have been demonstrated across a wide range in length scales from 250 µm to 100 nm with high accuracy.
and reproducibility, as shown in Fig. 2.17. It has even been shown that nano-replication of porous alumina rods can be scaled down to 13 nm features and can be potentially applied to non-planar surfaces when applied to blow molding [149].

Figure 2.17: Micro- and nano-replication ability across different length scales (a) electroplated nickel mold (b) silicon mold (c) Pyrolized SU-8 mold (d)-(f) porous alumina mold [148]

Discrete 3D micro-parts have been produced by combining molding techniques with planarization or scraping techniques [137]. Several examples of complex gear geometries and micro-tools are shown in Fig. 2.18a-f with a length scale on the order of 300 µm. These techniques can similarly be scaled down to the nano-scale to create 150 nm free standing rods. Fig. 2.18g shows an example of BMG nano-rods formed by molding against porous alumina. The remaining BMG reservoir is scraped off and the alumina is etched to produce free-standing parts [149].
Figure 2.18: Discrete 3D micro part examples (a) complex Zr₃₅Ti₃₀Cu₈.₂₅Be₂₆.₇₂ gear (b) spiral Zr₃₅Ti₃₀Cu₈.₂₅Be₂₆.₇₅ spring with 20 µm thickness (c) micro-tweezers (d) sharp micro-scalpels with 1 µm radius of curvature (e) rolled BMG mesh to form a stent (f) BMG membrane with 50 µm pores (g) free standing 150 nm nano-rods [148]

2.5.3 Machinability

In many manufacturing processes, high aspect ratio holes and secondary finishing machining operations are required. Many of these post-processing operations on the near-net shaped parts require the use of mechanical subtractive processes such as drilling, milling, and turning [151–154]. Unfortunately, the high heat generation associated with these machining processes
compounded with the poor thermal conductivity of BMG increases the heat localization at the tool-chip interface. This is further exacerbated by the risk of crystallizing the BMG due to the high temperatures and large strains experienced during machining, thereby diminishing all of the desirable material properties.

When studying the thermal characteristics during machining, one parameter of particular interest is the Péclet number. This is a dimensionless number that expresses the ratio between mass transport to thermal conduction. In terms of metal cutting, mass transport can be represented by the product of cutting speed and chip load whereas thermal conduction can be represented by thermal diffusivity of the workpiece [155]. Several macro-scale turning studies on BMG, performed by Bakkal et al. and Fujita et al. [151,152], observed an exothermic oxidation reaction in the form of light emission. These turning tests were performed with high Péclet numbers on the order of approximately 15-30, which indicate relatively slow thermal wave propagation into the workpiece in comparison to the speed of the tool. In this situation, a majority of the cutting energy was contained in the chip [155]. The heat localization in the BMG chip was hot enough to trigger an exothermic oxidation reaction, resulting in estimated chip temperatures in the range of 2400-2700 K [151].

At the micro-scale, Johnson et al. performed micro-milling tests with ball endmills [153]. For micro-scale machining processes, chip load must be reduced in order to compensate for the low rigidity of the tool, resulting in Péclet numbers on the order of approximately 0.5-1.5. The heat localization in the BMG chip was not severe enough to trigger an oxidation reaction in these tests. Due to the relatively slow mass transport in cases where the Péclet number is small, a comparatively large portion of the thermal energy generated from plastic deformation and friction conducts into the workpiece [155]. Unfortunately, the increased heat transfer into the workpiece raises the possibility of crystallizing of the surrounding BMG material. This study also revealed the importance of the minimum chip thickness (MCT) phenomenon as well as the thermal softening-induced viscous flow mechanism that affected machining response at higher cutting speeds [153].

For the case of drilling, the extreme temperatures encountered deteriorate hole quality and accelerate tool wear and often cannot be cooled effectively due to the inaccessible cutting zone
in drilling. Limited research work has been done on micro-scale drilling of BMG. To the best of the author’s knowledge, the most relevant research was performed by Bakkal et al., who studied various machining responses of macro-scale drilling of BMG [154]. Unfortunately, many critical machining responses, such as hole quality and tool wear, were not studied comprehensively and were lacking in quantitative measurement. Furthermore, the unique process mechanics that dominate at the micro-scale are not captured in this study. An in-depth investigation in which the machinability of BMG is quantitatively evaluated through a series of micro-drilling experiments is provided in detail in Appendix A.

2.6 Hybrid Thermoplastic Forming Process for Precision Blade Manufacture

A novel hybrid thermoplastic forming process combining thermally-assisted micro-molding and thermally-assisted micro-drawing was previously developed to create precision knife cutting edges with edge radii < 30 nm [3]. Micro-molding was chosen for its ability to quickly and efficiently manufacture the geometry of the BMG blade while micro-drawing was used to create the nano-scale features of the cutting edge through the inherent necking of the material. Through testing it was found that a good balance between necking and elongation could be achieved during drawing, creating a strong stable cutting edge [3].

Figure 2.19 schematically outlines the hybrid thermoplastic forming process developed in [3]. Steps 1-3 involve molding the rake face and initial geometry of the blade. The dies are heated to a pre-specified temperature within the supercooled liquid regime of BMG prior to the beginning of the micro-molding process. At step 3, the die positions are fixed to maintain a gap of approximately 20 μm. Steps 4-6 show a detailed view of the subsequent micro-drawing operation. The BMG sample is drawn to the right, initiating large plastic deformation along with the associated necking of the sample. Micro-drawing is continued until material failure whereon a sharp edge is produced.
2.6.1 BMG Edge Formation Characteristics

Based on the hybrid micro-molding and micro-drawing process, a specialized testbed was developed to evaluate edge formation characteristics as a function of drawing feedrate and temperature [7]. The overall testbed layout, which consists of a molding actuator, a drawing actuator, and temperature-controlled dies, is shown in Fig. 2.20. Tests were performed at three temperatures (650, 660, and 670 K) to study the deformation behavior of Vitreloy-1 within its supercooled liquid regime. Tests performed at temperatures above 670 K experienced difficulties due to temperature-induced crystallization whereas tests performed at temperatures below 650 K exhibited strain-localization behavior that resulted in non-homogeneous edge formation. For these experiments, five drawing feedrates relevant to edge formation were tested (100, 250, 500, 750, and 1000 μm/s). During the micro-molding operation, the dies had a fixed mold gap distance of 20 μm for all tests, as defined in Fig. 2.19.
Edge formation is qualitatively analyzed by observing blade shape and material flow on a JEOL 6060LV SEM as both temperature and federate are varied. Fig. 2.21 provides an oblique view of the edge formation so that the edge quality can be compared for the different process parameters. Figure 2.21a provides a low magnification image of an example blade manufactured during this experiment. Figures 2.21b-p show detailed views of the edge formation generated during drawing. In each image, the dotted line marks the start of deformation corresponding to the point of thinnest cross-section during molding.
Figure 2.21: SEM images of edge deformation [7] (a) entire blade with box of detailed area (b) [T=650 K, v=100 μm/s] (c) [T=650 K, v=250 μm/s] (d) [T=650 K, v=500 μm/s] (e) [T=650 K, v=750 μm/s] (f) [T=650 K, v=1000 μm/s] (g) [T=660 K, v=100 μm/s] (h) [T=660 K, v=250 μm/s] (i) [T=660 K, v=500 μm/s] (j) [T=660 K, v=750 μm/s] (k) [T=660 K, v=1000 μm/s] (l) [T=670 K, v=100 μm/s] (m) [T=670 K, v=250 μm/s] (n) [T=670 K, v=500 μm/s] (o) [T=670 K, v=750 μm/s] (p) [T=670 K, v=1000 μm/s]
From these tests, three distinct types of edge formation were observed [3].

- Type 1 deformation is generally exhibited by tests with a combination of high temperature and low drawing feedrate and is characterized by low drawing forces and high elongation. This type of deformation is distinguished by significant elongation resulting in a thin, flimsy edge, as shown in Fig. 2.21g,h,l,m,n.

- Type 2 deformation is generally exhibited by tests with a combination of low temperature and high drawing feedrate and is characterized by a high peak drawing force and low elongation to failure. This type of deformation is distinguished by sharp necking over a short distance prior failure. Due to the high rate of necking, tests that follow this type of deformation tend to fail in a manner that creates a jagged uneven edge, as shown in Fig. 2.21d,e,f,k.

- Type 3 deformation is generally exhibited by tests with combinations of intermediate temperatures and drawing feedrate relative to type 1 and type 2 deformation. This type of deformation is ideal for cutting edge formation since it maintains a balance between elongation and necking to produce a stable, consistent edge, as shown in Fig. 2.21b,c,i,j,o,p.

Each deformation type exhibits also exhibit a distinct drawing force profile, which is shown in Fig. 2.22.

**Figure 2.22:** Force response of tests exemplifying each type of blade formation [7]
Based on these experimental results, edge formation was qualitatively assessed as either good or poor. Figure 2.23 shows a plot of temperature versus feedrate for all tests and shows a linear relation that defines the loci of temperature and feedrate combinations that produce good edges. It is seen that good edge formation in general occurs with type 3 edge deformation.

![Figure 2.23: Temperature-feedrate edge formation map showing ideal processing conditions with corresponding deformation type (adapted from [7])](image)

2.6.2 Performance Evaluation of BMG Edge Formation

The initial tests using a constant feedrate for the drawing process exhibited inhomogeneous deformation that caused irregular material flow and inconsistencies in the blade deformation. Upon further investigation, it was determined that applying an additional constraint on maximum drawing force improved the blade straightness and edge radius. The constraint on maximum drawing force limits the amount of free volume generated when drawing is initiated [156], eliminating the inhomogeneous necking characteristic of type 2 deformations. By limiting this free volume generation, homogeneous deformation is encouraged and defects and inconsistencies in the blade are reduced. Force was indirectly constrained by limiting the allowable current to the drawing motor while issuing typical feedrate commands. Together the
force and feedrate limitations can be used to create a blade with sufficient necking to generate a strong stable edge while remaining completely within the homogeneous deformation regime.

Figures 2.24 shows a collection of tests run with a current saturation of .083 A, a temperature of 685 K, and a feedrate limit of 500 μm/s. The blades shown in Fig. 2.24a maintain a straight edge across the entire length with no evidence of curling or defects. Figure 2.24b shows magnified images of the deformation in each blade. These images show homogeneous deformation in all the tests with gradual necking and no sign of defects. All the tests exhibited similar amounts of elongation in the range of 50-80 μm. Figure 2.24c shows magnified images of the side-view of each edge. As observed, sharp edges with small edge radii are formed and there are again no observed defects.

**Figure 2.24:** Repeatability tests conducted with current saturation of .083A, temperature of 685K, and federate limit of 500 μm/s [3]
Straightness is characterized by examining edge profile deviations from a straight line in both a top-view (XY-plane) and a side-view (XZ-plane), as shown in Fig. 2.25a and Fig. 2.25b, respectively. It was found that the maximum deviation of the edge was 10 μm in the XY-plane whereas the maximum deviation of the edge was only 3 μm in the XZ-plane over the 2 mm blade width.

![Figure 2.25: Edge profile straightness](image)

The edge radius produced by the force-limited drawing process was investigated on an FEI Dual Beam 235 FIB. Focused ion beam machining was used to etch a cross-section on the BMG blade sample to further analyze the edge radius. Three cross-sections were performed at random locations across the width of the blade and edge radii measured between 14-26 nm, as
shown in Fig. 2.26. The lightest color corresponds to a Pt coating followed by a darker gold plating layer for edge protection during ion milling.

![Figure 2.26: FIB cross-section of BMG blade for edge radius measurement [3]](image)

### 2.6.3 Curvilinear Blade Manufacture Through Lorenz-Force Drawing

Prior work in the development of the hybrid thermoplastic forming process for creating precision surgical blades out of BMG was found to be capable of molding and drawing straight edges with radii < 30 nm. However, due to the unidirectional mechanical drawing nature of the process, it is not capable of producing the curvilinear or multi-facet edges found on most surgical blades. In order to create curvilinear blade geometry there needs to be a way to create a force perpendicular to the cutting edge at all locations. If the edge is a single straight edge this can be done with a single linear actuator. However, if the profile is, for example, a crescent, the drawing force cannot be recreated using a collection of linear forces.

To produce these multi-facet/curvilinear blade geometries, Krejcie [3] proposed using a magnetic field and a current to generate a multi-directional Lorentz force to actuate the drawing operation. Figure 2.27a shows an example of a current path (red line) within a magnetic field (into the page) that can be used to generate a drawing force perpendicular to the direction of the current.

In the hybrid thermoplastic forming process, the current path is electrically isolated in the cross-hatched region shown in Fig. 2.27b. Each mold die is coated with a 500 nm SiO₂ electrical insulation layer using plasma-enhanced chemical vapor deposition (PECVD). The small mold gap formed during molding also acts as an electrical resistance that prevents the
current from leaking into the blade. The electrical current is able to follow curvilinear blade edge profiles and generate a perpendicular Lorentz force in the presence of a magnetic field to perform the drawing operation.

Figure 2.27: Lorenz force drawing for curvilinear blade manufacture (B-magnetic field and F-force) [3] (a) forces generated by current running along a circular profile in a magnetic field (b) modification to drawing process

Feasibility testing for Lorenz force-based drawing has been performed. A C-shaped electromagnet was used to generate a magnetic field of 0.9 T across a 500 μm gap. Current was varied between 10 A and 40 A. During testing, it was ultimately found that the force generated in these conditions were too low. With a maximum current of 40 A, it is estimated that a maximum stress of 35 kPa could be generated across a 1 μm mold gap, which is far below the necessary levels to induce flow. Figure 2.28 shows one example of magnetic drawing. Evidence of material flow can be seen, however, the Lorenz force generated seems to be incapable of overcoming any amount of friction between the BMG sample and the dies. To increase drawing force, a much higher magnetic field must be applied, possibly from a superconducting magnet.
Figure 2.28: Lorentz force-based drawing test result showing evidence of material flow in path of electrodes, but no edge failure (arrows denote contact locations for electrodes) [3]

2.7 Gaps in Knowledge

Since the discovery of bulk metallic glass formers with low critical cooling rates < 10 K/s, significant research has been focused on understanding both the crystallization process and the material behavior for a wide range of BMG compositions. Comparatively, there is a significant dearth in literature related to the processing and manufacture of BMG components with real application.

For the manufacture of surgical blades, Krejcie et al. [7] demonstrated excellent edge formation capability in a 1D configuration. However, when testing was extended to curvilinear geometries using Lorentz force-based drawing, the forces generated were too small to feasibly generate edge failure. The need for an improved hybrid thermoplastic forming process capable of creating multi-facet/curvilinear blade geometries is still a challenge that must be further investigated. In addition, testbed improvements must be made in terms of thermal stability as well as component tolerances to improve overall blade quality and repeatability.
Chapter 3

Development of Hybrid Thermoplastic Forming Process for Multi-Facet/Curvedlinear Blades

Recently, Krejcie et al. [7] developed a hybrid thermoplastic forming process for manufacturing precision surgical blades. This process, however, is limited to a 1D configuration and lacks the ability to generalize to typical multi-facet/curvilinear blade geometries. This chapter focuses on investigating an enhanced hybrid thermoplastic forming process that is capable of manufacturing complex blade geometries.

To extend the process, an oblique drawing process is proposed to allow for greater flexibility in the application of drawing force. Based on this new drawing configuration, a testbed design for both multi-facet and curvilinear blades is described in detail. The implementation of the automation system is explained followed by a description of a specialized supervisory controller developed to ensure smooth drawing operation.

3.1 Oblique Drawing Concept

Previous efforts in hybrid process development for BMG blade manufacture [7] focus on applying a perpendicular force along the blade geometry edge, as shown in Fig. 3.1a. In order to directly apply previous test results to multi-facet/curvilinear blade manufacture, a method must be developed to apply a perpendicular force to the cutting edge at all locations along the edge. This requirement imposes stringent constraints that result in an intricate drawing process to accommodate the various curvilinear and multi-facet blade geometries. Conversely, the
ability to draw in an oblique direction to create a sharp, stable edge allows for greater flexibility in the application of a drawing force, $F_{\text{draw}}$. Figure 3.1b shows the general configuration for an oblique drawing operation.

The major difference between single-facet (Fig. 3.1a) and oblique (Fig. 3.1b) drawing is the presence of an inclination angle, $\theta$, which is explicitly defined in Fig. 3.1b. With a non-zero inclination angle, oblique drawing will exert both a shear force and a normal force along the edge of the blade.

![Figure 3.1](image.png)

**Figure 3.1**: Top view schematic of micro-drawing process (a) single-facet drawing (b) oblique drawing

Figure 3.2a shows an SEM image of a blade created through oblique drawing with a constant inclination angle of 45°. During the micro-drawing process, edge formation is strongly correlated to necking evolution, which is typically associated with tensile behavior. In the preliminary tests for oblique drawing evaluation, it is observed that good edge formation is dependent on the strain rate only in the normal direction. As indicated in [7], good edge formation typically occurs within a Newtonian flow regime wherein strain-rate is proportional to stress. Although material flow due to shear stresses/strain rates are apparent from the image, these effects do not seem to have a significant impact on either the necking evolution or the edge formation. Figure 3.2b shows a TEM image of a blade cross-section created from the oblique drawing process. With an edge radius of approximately 15 nm, oblique drawing is
shown to yield similar performance compared to single-facet blade results reported in [7]. To draw at different inclination angles, the drawing federate, which is correlated to strain rate, must be altered such that the component of feedrate in the normal direction matches the federate parameter used in single-facet drawing.

**Figure 3.2**: Oblique drawing characteristics (a) evidence of shear stresses, dotted line marks the start of drawing deformation (b) TEM cross-section of oblique drawing cutting edge

### 3.2 Oblique Drawing of Curvilinear Blades

For curvilinear blades, such as the blade geometries shown in Fig. 3.3, the inclination angle can be constantly varying as a function of location along the blade edge. To account for non-symmetrical blades, micro-drawing can be reasonably be split up into two directions, offset at 45° from normal, without significant complications.
For a crescent blade in this configuration, the inclination angle can vary as much as ±45° for each drawing direction. Assuming that the edge of the blade can be divided up into a set of differential elements, the range of normal stress that will be experienced during oblique drawing is

\[
\sigma_{norm} = \frac{dF_{draw} \cdot \cos(\theta)}{dA},
\]

where \(\sigma_{norm}\) is the normal stress, \(dF_{draw}\) is the differential drawing force, and \(dA\) is the differential cross-sectional area of the necked region.

Equation 3.1 indicates that the normal stresses can differ up to 30% across the blade edge when drawing curvilinear blades. To accommodate the changing normal stress across the blade’s edge, transients during the drawing operation must be analyzed. Figure 3.4a shows a diagram of half of the crescent blade split down the line of symmetry with the applied drawing force. When drawing in an oblique direction with varying inclination angle, it is assumed that the rate of necking is accelerated at the point of maximum normal stress (\(\theta=0^\circ\)). For the present discussion, failure is defined locally along the blade edge as a region of substantial necking resulting in a negligible cross-sectional area. Note that in this definition, complete material separation does not necessarily occur. Figure 3.4b shows the point of failure initiation corresponding to the point of accelerated necking as well as the direction of the failure propagation.
As the inclination angle increases (θ increases as failure propagates), the normal stress being applied at the point of failure begins to decrease, as described by Eq. 3.1. To maintain a constant normal stress as the failure propagates along the blade’s edge, drawing force/feedrate should increase accordingly as a function of time. This can be achieved through applying a time-varying drawing force profile, such as a ramp, exponential, etc.

![Diagram showing oblique drawing of crescent blade](image)

**Figure 3.4:** Oblique drawing of crescent blade (a) force diagram (b) failure initiation and propagation

### 3.3 Hybrid Thermoplastic Forming Testbed for Multi-Facet/Curvilinear Blades

Requirements for the hybrid thermoplastic forming process are formulated based on the limitations experienced in the first generation testbed developed by Krejcie et al. [3]. Improvements are made in the molding and thermal system while major design modifications are made to accommodate the oblique drawing configuration introduced in Section 3.1 and 3.2. Table 3.1 summarizes the requirements of a new manufacturing testbed for oblique drawing.
Table 3.1: Hybrid thermoplastic forming testbed design requirements for oblique drawing

<table>
<thead>
<tr>
<th>Process</th>
<th>Requirement</th>
</tr>
</thead>
<tbody>
<tr>
<td>Molding</td>
<td></td>
</tr>
<tr>
<td>Repeatability</td>
<td>&lt; ±1 µm</td>
</tr>
<tr>
<td>Molding Force</td>
<td>&gt; 1000 N</td>
</tr>
<tr>
<td>Surface Finish (Rₐ)</td>
<td>&lt; 20 nm</td>
</tr>
<tr>
<td>Drawing</td>
<td></td>
</tr>
<tr>
<td>Drawing Direction</td>
<td>Oblique</td>
</tr>
<tr>
<td>Number of Directions</td>
<td>2</td>
</tr>
<tr>
<td>Force Control</td>
<td>Time-Varying Force Profiles</td>
</tr>
<tr>
<td>Edge Radius</td>
<td>&lt; 50 nm</td>
</tr>
</tbody>
</table>

Based on the proposal for oblique drawing and the requirements listed in Table 3.1, a testbed for implementing the hybrid thermoplastic forming process for multi-facet/curvilinear blades is developed. This testbed has been sub-divided into three primary modules: the lever arm module, the molding module, and the drawing module. Figure 3.5a-c show the general configuration for the various modules.

**Lever Arm Module** – This module is designed to utilize the mechanical advantage of a lever arm design and to improve repeatability and force capabilities of the molding operation.

**Molding Module** – This module consists of a linear air bearing stage that guides the molding operation while ensuring precise alignment of the dies. This stage is connected to the lever arm module through a slider-crank type mechanism.

**Drawing Module** – Drawing is performed in a bi-axial configuration that is applicable to a wide range of multi-facet and curvilinear blade types. The dual voice coil-driven drawing stage is designed to pull in two directions, offset by 90°, with time-varying drawing force profiles.
Figure 3.5: Configuration of hybrid thermoplastic forming testbed for multi-facet/curvilinear blades (a) lever arm module (b) molding module (c) drawing module
3.3.1 Lever Arm Module

The lever arm module contains the primary actuator that facilitates the molding process. This configuration is primarily adopted for the mechanical advantage that it provides, offering a multiplier effect on the effective force and repeatability. This is advantageous because the linear actuator can be specified with lower requirements thereby reducing costs. Although high-performance piezoelectric actuators or linear motors may be used directly without a mechanical advantage to facilitate molding, these options are often cost prohibitive and face limitations such as travel range or duty cycle. The molding actuator is a ball-screw linear stage and is shown together with the lever arm mechanism in Fig. 3.6.

![Figure 3.6: Lever arm variables and constants](image-url)
The molding actuator is connected to the lever arm mechanism, forming a closed-kinematic loop. While this configuration offers an inherently stiff structure, the kinematics of the mechanism must be derived. In particular, the lever arm angle, $\theta_1$, is found following the calculation given by Adair et al. [157]. The analytical formula for the lever arm angle is derived using homogeneous transformation matrices in the Denavit-Hartenberg convention as

$$
\theta_1 = \cos^{-1}
\left[
\frac{a_1^3 d_x - a_1 a_3^2 d_x - a_1 d_3^2 d_x + a_1 d_3^3 + a_1 d_x d_y^2 + \sqrt{-a_1^2 d_y^2 \left(a_1^4 + \left(a_3^2 + d_3^2 - d_x^2 - d_y^2\right)^2 - 2a_1^2(a_3^2 + d_3^2 + d_x^2 + d_y^2)\right)} - 2a_1^2(d_x^2 + d_y^2)}{2a_1^2(d_x^2 + d_y^2)}
\right],
$$

where $a_1$, $a_3$, $d_x$, and $d_y$ are constants and $d_3$ is the molding actuator encoder position. The nomenclature for the lever arm angle calculation is defined pictorially in Fig. 3.6 and nominal values for the testbed design are provided in Table 3.2.

### Table 3.2: Nominal values of constants in lever arm kinematics

<table>
<thead>
<tr>
<th>Constant</th>
<th>Nominal Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$a_1$</td>
<td>480 mm</td>
</tr>
<tr>
<td>$a_3$</td>
<td>201.1 mm</td>
</tr>
<tr>
<td>$d_x$</td>
<td>681.1 mm</td>
</tr>
<tr>
<td>$d_y$</td>
<td>137.975 mm</td>
</tr>
</tbody>
</table>

In this configuration, joint 2 is supported by an air bushing that decouples the lever arm motion from the molding actuator. Any angular misalignment of the molding actuator will have little effect on the motion of the lever arm [157]. The lever arm mechanism is designed with a mechanical advantage of 3, providing an effective maximum force of 2640 N and an effective repeatability of ±1 μm. The lever, however, has one major shortcoming. The linear motion provided by the molding actuator is converted into an arc motion by the lever arm. This arc motion is undesirable, especially when precise alignment of dies is essential. Therefore, a mechanism to convert this arc motion into pure translational motion is required.
3.3.2 Molding Module

The molding mechanism module converts the arc motion of the lever arm into pure translational motion. In addition, the dies that mold the overall geometry of the blade are housed in this module.

Linear Guide Rail Stage

To ensure accurate alignment of the set of dies, a linear guide rail stage is introduced to provide pure translational motion. This linear guide rail stage is connected to the lever arm mechanism to form a slider-crank linkage. As shown in Fig. 3.7c, molding is performed through this linkage mechanism using the mechanical advantage of the lever arm. The following images shown in Fig. 3.7 outline the assembly process of the linear guide rail stage.

Figure 3.7: Assembly process of linear guide rail stage (a) guide rail stage base (b) moving carriage (c) assembled guide rail stage with lever arm
The linear guide rail stage contains a set of three air bushings and their associated rails to guide a carriage platform. A connector rod couples the lever arm motion with this carriage. The connector rod joints are located along the axis of molding force such that there will be minimal torque generated during molding. The molding mechanism module includes cartridge heaters as well as RTD sensors to control the die temperature. Water blocks have also been added to the linear stage to prevent overheating.

For the hybrid thermoplastic forming process, setting the molded gap is an important task. Therefore, inverse kinematics of the linear guide rail stage coupled to the lever arm mechanism is necessary to compute the position of the carriage and thereby the molded gap distance, as defined in Fig. 2.19. Using homogeneous transformation matrices in the Denavit-Hartenberg convention, the molded gap distance is derived as a function of the lever arm angle

\[
d_4 = b_1 \sin(\theta_1 - \theta_{pin}) - d_{yy} - \sqrt{b_2^2 - d_{xx}^2 + 2b_1d_{xx}\cos(\theta_1 - \theta_{pin}) - b_1^2\cos(\theta_1 - \theta_{pin})^2} ,
\]

where \(b_1, b_2, d_{xx}, d_{yy}, \) and \(\theta_{pin}\) are constants. It should be noted that the coordinate frame, \(e\), is a fixed reference frame that is located at the center of the carriage pin when the dies are in contact (i.e. \(d_4\) is the molded gap distance). The nomenclature for the linear guide rail stage position is defined pictorially in Fig. 3.8 and nominal values for the testbed design are provided in Table 3.3.

**Table 3.3:** Nominal values of constants in linear guide rail stage kinematics

<table>
<thead>
<tr>
<th>Constant</th>
<th>Nominal Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>(b_1)</td>
<td>160.503 mm</td>
</tr>
<tr>
<td>(b_2)</td>
<td>31.365 mm</td>
</tr>
<tr>
<td>(d_{xx})</td>
<td>160 mm</td>
</tr>
<tr>
<td>(d_{yy})</td>
<td>44.065 mm</td>
</tr>
<tr>
<td>(\theta_{pin})</td>
<td>4.538°</td>
</tr>
</tbody>
</table>
To verify the correct molded gap, a high resolution LVDT is also installed to provide a direct measurement of the linear displacement of the dies.

**Mold Die Manufacture**

During the hybrid thermoplastic forming process, the molding dies are heated to elevated temperatures as high as 670 K. Many steels will lose a significant amount of strength at these high temperatures. Therefore, H13, a common hot-working tool steel, is chosen for its high hot working strength. H13 boasts an extremely high yield strength of approximately 1000 MPa at 670 K.

The molded gap distance, as defined in Fig. 2.19, is an important parameter that influences the performance of the subsequent micro-drawing operation. Specifically, it is desired to maintain a tolerance of at least 20% for the molded gap distance along the entire edge of the blade, i.e.
gap variation less than 5 µm, to guarantee a straight, uniform edge failure. A specialized manufacturing process for the dies is outlined as follows

1. Machine the outline of die from H13 tool steel.
3. Harden to HRC 50-52 and grind top/bottom surfaces.
4. Perform die-sinking EDM (finishing operation).
5. Polish die surfaces to Ra 20 nm.
6. Grind the top surface to create a flat reference surface.

The final grinding operation must possess a flatness tolerance of 2.5 µm such that when the dies are mated together, the total gap variation between the two edges of the dies remain within the specified tolerance. The width of the flat on the die edges may vary due to the final grinding operation, but this difference should have little effect on the drawing operation. The die manufacturing process is outlined in Fig. 3.9.
Step 1: Machine outline of die from H13 tool steel

Step 2: Micro-machine die cavity (roughing operation)

Step 3: Harden to HRC 50-52 and grind top and bottom surfaces

Step 4: Perform die-sinking EDM (finishing operation)

Step 5: Polish die surfaces to $R_a$ 20 nm

Step 6: Grind the top surface to create a flat reference surface

**Figure 3.9:** Multi-facet mold die manufacture procedure
A mold die for crescent blades has also been developed that will allow for testing of curvilinear drawing techniques. The manufacture of crescent dies follows a similar procedure however the die-sinking operation is performed with a tapered rod, which is outlined pictorially in Fig 3.10a. The final die is shown in Fig. 3.10b.

**Figure 3.10:** Crescent blade mold die (a) die-sinking process (b) final geometry
Mold Die Alignment

As shown below in Fig. 3.11a and Fig. 3.11b, the molding dies are attached to the heated molding die holders by applying a ceramic adhesive at the interface. The current procedure involves applying lateral forces to pull the dies into the yellow blocks mounted to the front of the molding die holders, as shown in Fig. 3.11c. The yellow blocks in Fig. 3.11c laterally align the two molding die edges. Since the ceramic adhesive varies in thickness across the interface, this process also ensures that the two precision ground reference surfaces will be parallel.

Figure 3.11: Alignment procedure of molding dies (a) mold die assembly (b) alignment of mold dies (c) side cross-sectional view of mold die alignment
3.3.3 Drawing Module

The drawing module is composed of a two voice coil-driven linear stages with MicroE SP4800 1.22 nm linear encoder feedback. In addition, each linear stage in the drawing module has been equipped with a Stellar Technology VLC856 button-type load cell with a 222 N capacity to directly measure the applied drawing force. The module is designed to grip the BMG sample and draw in two directions offset by 90°, as indicated in Fig. 3.12. Both multi-facet and curvilinear blades can be accommodated with this drawing mechanism configuration.

![Figure 3.12: Dual linear stage configuration for drawing module](image)

**Figure 3.12**: Dual linear stage configuration for drawing module

**Flexure Clamp**

Each linear stage is equipped with a flexure clamp to grip the BMG blank prior to drawing, as shown in Fig. 3.13. Clamping is achieved by applying a downward force on the flexure via pneumatic actuator, thereby embedding a set of hardened steel gripping teeth into the BMG sample. The resulting gap between the gripping teeth is intended to be five times larger than the molded gap to ensure that failure occurs at the blade edge, as opposed to the flexure clamp. Cartridge heaters and RTD sensors are installed directly in the flexure to control clamp temperature during drawing, as shown in Fig. 3.14.
FEA analyses are performed to analyze flexure performance. This FEA model aims to:

1. Verify that gripping gap is independent of force because it is difficult to control force in the pneumatic press.
2. Find the maximum stress concentration in the gripping clamp flexure.
3. Ensure that drawing dies are aligned adequately.
A static analysis is performed using the FEA model shown in Fig. 3.15. The flexure is modeled as a 2D plane strain structure with a thickness of 10 mm. The force input $F_{in}$, which is a function of air pressure applied to the pneumatic press, is varied between 50 and 300 N to test the robustness of the flexure design.

Three important aspects to this flexure are measured as a function of applied force, namely, the horizontal displacement of the dies, the resulting gripping gap, and the maximum stress due to deflection. These values are tabulated in Table 3.4.

**Figure 3.15: FEA model of gripping clamp flexure**

<table>
<thead>
<tr>
<th>Force Input (N)</th>
<th>Max Stress (Mpa)</th>
<th>x-dist (µm)</th>
<th>gap (µm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>50</td>
<td>87.6</td>
<td>0.542</td>
<td>130.1</td>
</tr>
<tr>
<td>100</td>
<td>88.0</td>
<td>0.359</td>
<td>126.2</td>
</tr>
<tr>
<td>150</td>
<td>88.1</td>
<td>0.198</td>
<td>126.0</td>
</tr>
<tr>
<td>200</td>
<td>88.1</td>
<td>0.076</td>
<td>125.7</td>
</tr>
<tr>
<td>250</td>
<td>88.1</td>
<td>0.175</td>
<td>125.5</td>
</tr>
<tr>
<td>300</td>
<td>88.2</td>
<td>0.580</td>
<td>125.3</td>
</tr>
</tbody>
</table>
A maximum stress of 88.16 MPa was measured for a force input of 300 N (limited by maximum force output of pneumatic press). This is well below the yield strength of normal steels. The average gap formed was 127.7 ± 2.4 μm. The gap size was found to be nearly independent of force input. The maximum misalignment of dies was 0.542 μm, which in this case is negligible.

**Fluid Damper**

Due to the limited bandwidth of the voice coil stages, the drawing process typically experiences a sudden jerk as the BMG sample rapidly necks prior to failure and leads to poor repeatability. An external fluid damper is introduced to the drawing system to dampen this high frequency jerk. In addition, the damper improves stability of the control loops and provides smoother motion, improving overall blade quality.

The fluid damper consists of an Airpot piston submerged in Mobil Velocite #10 Spindle Oil to obtain incompressibility as well as higher damping coefficients. The axis of the linear damper and the axis of the voice coil are aligned so that moments are minimized during drawing. The fluid damper attachment is shown in Fig. 3.16.

![Fluid Damper Diagram](image-url)

**Figure 3.16:** Linear damper for voice coil actuator

The linear damper is enclosed in modified square tube stock with plates that cap each end. This enclosure is mounted behind the voice coil motor in the drawing mechanism. The axis of the
linear damper and the axis of the voice coil are aligned so that moments are minimized. Finally, the moving voice coil stage is attached to the damper piston. The following images in Fig. 3.17 describe the assembly process of the linear damper attachment.

Figure 3.17: Linear voice coil damper assembly (a) damper enclosure (b) damper attached to voice coil stage (c) damper final assembly

**Force Sensor**

Each linear stage in the drawing module has been equipped with a Stellar Technology VLC856 button-type load cell with a 222 N capacity, as shown in Fig. 3.18.
The load cell signals are amplified with Honeywell DV-10 bridge amplifiers and have been calibrated against load cells with known calibration data. The final calibration data set for both the left and the right drawing actuators are provided in Fig. 3.19.

![Stellar Technology Load Cell Calibration](image)

**Figure 3.19:** Load cell sensor calibration curve

The VLC856 button-type load cell inherently measures compression forces and therefore must be mounted in a special configuration to measure the tensile drawing forces directly. A schematic of the mounting system is provided in Fig. 3.20. The load cell is initially un-loaded. When drawing begins, the drawing force load is transferred through the load cell to the voice coil winding. A set of four dowel pins guide this motion and prevent any lateral movement.
3.3.4 Temperature Control and Thermal Management

Accurate temperature control of the mold dies in the molding module as well as the gripping teeth in the drawing module is required to operate within a desired BMG deformation regime. As described in previous sections, cartridge heaters and RTD sensors are installed on the two mold dies and the four gripping teeth, resulting in a total of six locations of temperature control.

Although ceramic insulation is added to the system to isolate these heat sources, excess heat transfer leaks into the surrounding machine structure and actuators. The excess heat must be removed from the system before the machine is allowed to reach extreme temperatures. Water blocks, blocks of metal with water channels, are strategically located around the machine close to the heat sources. Any excess heat that leaked through the insulation plates will be absorbed into the water blocks. The heated water is eventually passed through a large finned radiator to dissipate the heat to the environment. These water blocks will liquid cool the system to reduce thermal expansion-related errors during blade manufacture.
3.3.5 BMG Sample Preparation

The initial BMG sample is a 2.5 X 5.0 X 0.3 mm strip of Vitreloy-1b with a 254 µm wide notch micro-machined in one end of the sample, as shown in Fig. 3.21. The notch is introduced to decrease the excessive forces encountered during bi-axial drawing.

![Initial BMG sample dimensions (mm)](image)

**Figure 3.21:** Initial BMG sample dimensions (mm)

3.3.6 Assembly of Hybrid Thermoplastic Forming Testbed

This testbed has a modular design such that the three major modules can be, for the most part, assembled separately. Once individual modules are assembled, they are attached to the testbed baseplate and connections between the different modules are made. The assembly of the hybrid thermoplastic forming testbed is performed on a coordinate measuring machine (CMM). With a volumetric accuracy of less than 10 µm, the CMM not only provides high accuracy, but also enables a wide variety of measurements including perpendicularity, parallelism, flatness, etc. CMM measurements are taken concurrently with component placement and fastening to ensure parts are properly located. Shimming material is added to correct for any misalignment when parts are secured.

The completed testbed is shown in Fig. 3.22. A list of critical components for the lever arm/molding module, the drawing module, and the electronics are provided in Tables 3.5–3.7, respectively.
Figure 3.22: Completed assembly of the hybrid thermoplastic forming testbed for curvilinear blades

Table 3.5: Lever arm/molding module components

<table>
<thead>
<tr>
<th>Part Description</th>
<th>Part #</th>
<th>Manufacturer</th>
</tr>
</thead>
<tbody>
<tr>
<td>Brushless Motor (Molding)</td>
<td>BE233DQ-KPS</td>
<td>Parker</td>
</tr>
<tr>
<td>Ball Screw Linear Stage</td>
<td>406100XRMS__D2H9 L8C5M61E3B1R1P</td>
<td>Parker</td>
</tr>
<tr>
<td>Tapered Roller Bearing (Joint 1)</td>
<td>32008XA.P5</td>
<td>Applied</td>
</tr>
<tr>
<td>Air Bushing (Joint 2)</td>
<td>S307502</td>
<td>New Way</td>
</tr>
<tr>
<td>Air Bushing Mount</td>
<td>S8075P02</td>
<td>New Way</td>
</tr>
<tr>
<td>Stage Pivot Bearing (Joint 3)</td>
<td>NJ305ETVP2C4QP51</td>
<td>Applied</td>
</tr>
<tr>
<td>Needle Bearing</td>
<td>2423K210</td>
<td>McMaster</td>
</tr>
<tr>
<td>Air Bushing (Linear Mold Stage)</td>
<td>S302001</td>
<td>New Way</td>
</tr>
</tbody>
</table>
### Table 3.6: Drawing module components

<table>
<thead>
<tr>
<th>Part Description</th>
<th>Part #</th>
<th>Manufacturer</th>
</tr>
</thead>
<tbody>
<tr>
<td>Voice Coil Actuator</td>
<td>CVC 40-HF-6.5</td>
<td>PBA Systems</td>
</tr>
<tr>
<td>Recirculating Ball Bearing Linear Guide</td>
<td>MR 12MN ZZ1 V1 P-70L</td>
<td>CPC</td>
</tr>
<tr>
<td>Linear Encoder</td>
<td>Mercury II 4800</td>
<td>Micro E</td>
</tr>
<tr>
<td>Glass Encoder Scale</td>
<td>Mercury II L30A</td>
<td>Micro E</td>
</tr>
<tr>
<td>Miniature Button Load Cell</td>
<td>VLC856-50LBCL-000</td>
<td>Stellar Technology</td>
</tr>
<tr>
<td>Gripping Clamp</td>
<td>13291</td>
<td>Festo</td>
</tr>
<tr>
<td>Linear Damper</td>
<td>Custom</td>
<td>Airpot</td>
</tr>
<tr>
<td>Spindle Oil Medium</td>
<td>Velocite Oil No. 10</td>
<td>Mobil</td>
</tr>
<tr>
<td>Manual Linear Translation Stage</td>
<td>M-TSX-1D</td>
<td>Newport</td>
</tr>
</tbody>
</table>

### Table 3.7: Electronic components

<table>
<thead>
<tr>
<th>Part Description</th>
<th>Part #</th>
<th>Manufacturer</th>
</tr>
</thead>
<tbody>
<tr>
<td>cRIO Controller</td>
<td>NI 9074</td>
<td>NI</td>
</tr>
<tr>
<td>Servo Drive Interface with Encoder Feedback</td>
<td>NI 9514</td>
<td>NI</td>
</tr>
<tr>
<td>Full H-Bridge Brushed DC Servo Drive</td>
<td>NI 9505</td>
<td>NI</td>
</tr>
<tr>
<td>+/-10V Analog Output 100 kS/s 4-ch</td>
<td>NI 9263</td>
<td>NI</td>
</tr>
<tr>
<td>PT100 RTD Analog Input 100 kS/s 4-ch</td>
<td>NI 9217</td>
<td>NI</td>
</tr>
<tr>
<td>+/-10V Analog Input 100 kS/s 4-ch</td>
<td>NI 9215</td>
<td>NI</td>
</tr>
<tr>
<td>8-ch 24V Logic 100 us Sourcing Digital Output</td>
<td>NI 9472</td>
<td>NI</td>
</tr>
<tr>
<td>12V Power Supply</td>
<td>LS150-12</td>
<td>TDK-Lambda</td>
</tr>
<tr>
<td>24V Power Supply</td>
<td>DPP100-24</td>
<td>TDK-Lambda</td>
</tr>
<tr>
<td>Aries Motor Amplifier (Molding)</td>
<td>AR-02A</td>
<td>Parker</td>
</tr>
<tr>
<td>Voice Coil Motor Amplifier (Drawing)</td>
<td>BA20-320</td>
<td>Aerotech</td>
</tr>
<tr>
<td>Load Cell Bridge Amplifier</td>
<td>DV-10</td>
<td>Sensotech</td>
</tr>
<tr>
<td>LVDT Signal Conditioner</td>
<td>DMI-A1</td>
<td>Macro Sensors</td>
</tr>
<tr>
<td>Pneumatic Solenoid</td>
<td>CPE10-M1BH-3GL-QS-4</td>
<td>Festo</td>
</tr>
<tr>
<td>High Amp Relay</td>
<td>2967620</td>
<td>Phoenix Contact</td>
</tr>
<tr>
<td>RTD Temperature Sensor</td>
<td>RTD-2-1PT100K2515-36-G</td>
<td>Omega</td>
</tr>
<tr>
<td>Cartridge Heaters 150 W</td>
<td>3618K729</td>
<td>McMaster</td>
</tr>
<tr>
<td>LVDT Sensor</td>
<td>BBP 315-040</td>
<td>Macro Sensors</td>
</tr>
</tbody>
</table>
3.3.7 Summary of Hybrid Thermoplastic Forming Testbed Capabilities

**Molding** – The lever arm topology adds a multiplication factor of 3 on force and repeatability. This results in a maximum effective molding force of 2640 N and an effective accuracy of ±1 µm. An additional LVDT sensor is located directly on the molding flexure with a resolution of 10 nm. Molding dies can be polished to a surface finish of 20 nm (R<sub>a</sub>). It is expected that the surface finish of the mold dies will transfer to the BMG blade during molding, resulting in a near-equivalent surface finish. Tolerances on the mold dies are set to ensure the molded gap variations remain below 20% across the entire edge.

**Drawing** – A dual drawing stage, offset 90° from each other, is designed to apply force in two directions for multi-facet/curvilinear blade manufacture. Time-varying drawing force profiles are possible with voice coil actuators. To grip the BMG prior to drawing, a gripping clamp flexure has been included in the design. The flexure clamp is designed to be pneumatically actuated while still providing highly repeatable gap control and alignment of drawing dies. A fluid damper is also introduced to improve stability of the voice coil actuators as well as attenuate the sudden jerk encountered during edge failure.

3.4 Hybrid Thermoplastic Forming Testbed Automation and Control

The hybrid thermoplastic forming testbed is required to perform a series of steps to manufacture curvilinear/multi-facet surgical blades. Table 3.8 provides an overview of the operating procedure for the manufacturing process.
Table 3.8: Hybrid Thermoplastic Forming Process Steps

<table>
<thead>
<tr>
<th>Step</th>
<th>Action</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Enable temperature controllers.</td>
</tr>
<tr>
<td>2</td>
<td>Perform homing operation (both molding and drawing actuators).</td>
</tr>
<tr>
<td>3</td>
<td>Calibrate force sensors as a function of position.</td>
</tr>
<tr>
<td>4</td>
<td>Mold the BMG blank with the lever arm module to a pre-specified gap size.</td>
</tr>
<tr>
<td>5</td>
<td>Grip onto the BMG blank using the pneumatic clamps located on the two drawing stages.</td>
</tr>
<tr>
<td>6</td>
<td>Begin the oblique drawing process in two directions.</td>
</tr>
<tr>
<td>7</td>
<td>After edge failure, release the molding dies and remove the BMG blade specimen.</td>
</tr>
</tbody>
</table>

Automation strategies and control algorithms are developed using Labview on a compact reconfigurable I/O (compactRIO) platform developed by National Instruments.

3.4.1 Automation Software Architecture

The automation system can be broken down into a three-layer hierarchy, namely, the host computer, the real-time processor, and the field programmable gate array (FPGA). These three components are tightly integrated with network communication and transfer protocols. The following sections outline the operation of each automation level. Further details of the software implementation can be found in Appendix B.

Host Computer

The host computer primarily acts as a human-machine-interface (HMI). This will be a terminal where a user can start and stop the hybrid thermoplastic forming process. This is also where a user may monitor or alter process variables. Any commands generated from the GUI will be transmitted through a network stream to the real-time processor for further processing. Figure 3.23 provides several control panels that are accessible through the host computer.
Figure 3.23: Automation control panels (a) temperature control panel (b) motion control panel

In addition, the user has access to a set of macros that further automate the process into a simple set of steps that mimic the operating procedure described in Table 3.8. These macros are set-up in a queued state machine that generates a set of instructions and adds them to the command
queue to be transmitted to the real-time processor. The graphical layout of the macros is shown in Fig. 3.24.

**Figure 3.24:** Macros for hybrid thermoplastic forming automation
Real-Time Processor

The real-time processor is embedded in the compactRIO hardware, providing double floating point processing within a deterministic operating system. Commands and process parameters sent by the host computer are received by the real-time processor. These commands are used to drive both temperature control and motion control. Specifically, the variable drawing force profiles are generated as setpoint trajectories and transmitted directly to the FPGA. The real-time processor also acts as an interface to send commands down to the FPGA. In addition, the real-time processor performs several miscellaneous tasks such as signal conditioning for all of the data generated (both RTD sensors and LVDT sensors) as well as transmitting data acquisition signals back to the host computer.

There are six temperature control loops running simultaneously on the real-time processor, namely two for the top and bottom mold dies and four for the drawing flexure clamps. RTD sensors are installed at each temperature controlled location along with cartridge heaters. The temperature controllers are implemented using PID-algorithms with a temperature setpoint feed-forward term. The PID algorithm is used to set the duty cycle of a PWM signal for relay control of the heaters. The control loop had a cycle time of 5 seconds to minimize mechanical wear on the relays.

Field-Programmable-Gate-Array (FPGA)

The FPGA is a reconfigurable chip that implements complex digital computations using a hardware description language (HDL). The FPGA is composed of pre-built logic blocks and a set of programmable routing resources to produce customizable hardware functionality. Labview has a built-in module that builds FPGA configurations directly from standard Labview code. The FPGA architecture allows for true parallel processing as well as high performance based on hardware timing.

For this application, the FPGA is primarily involved with implementing a custom controller for the drawing operation. Control loop cycle times are set to 2kHz and computations are performed using fixed point data. The FPGA directly interfaces with the I/O hardware and provides fast response to incoming signals.
3.4.2 Drawing Actuator Controller Design

Drawing is ideally stress-controlled, however, due to the complex curvilinear geometry and the inherent necking of the hybrid process, it is difficult to estimate stress in the sample in-situ. As an alternative, the drawing process is performed with a form of supervisory control that switches between two local controllers: force-feedback and velocity-feedback PID controllers.

**Supervisory Control**

Supervisory control consists of a network of independent local controllers and a supervisor that monitors feedback/control effort signals and implements discrete logic to generate a switching signal, $\sigma$, to decide which controller to use [159].

During the initial stages of the drawing process, a force-feedback tracking controller is used to implement a variable drawing force profile. As the BMG sample necks and reduces in effective cross-sectional area, the load bearing capacity decreases over time. As a result, the velocity of the drawing stage gradually increases throughout the drawing process. In this application specifically, the supervisor monitors the drawing velocity and compares it to a pre-specified threshold. Once the velocity threshold is exceeded, the supervisor signals a switch to the velocity-feedback controller to suppress the sudden jerk associated with the rapid necking of the BMG immediately prior to failure. This type of control will constrain the maximum velocity of the process and prevent a run-away effect. The general architecture of the controller is given in Fig. 3.25.

![Supervisory control architecture](image)

**Figure 3.25:** Supervisory control architecture
Bumpless Transfer

In general, velocity and force are inherently conflicting objectives, i.e. force and velocity cannot be controlled at the same time. For the drawing process, a smooth transition between force control and velocity control is desired. In typical applications of supervisory control, constant switching between controllers can destabilize the system. To ensure a smooth transfer, the supervisor is configured to perform an interpolation between the two controllers rather than an instantaneous switch [160]. The algorithm implemented by the supervisor is provided in Algorithm 1.

In this algorithm, drawing is initially force controlled with a setpoint, $F_{SP}$, defined by a variable force profile. The supervisor monitors the velocity, $v$, during this initial stage to estimate the switching time, $t_i$, as indicated Fig. 3.26a. At this point, interpolation between the two controllers occurs over a period of $t_d$. The switching time is identified based on extrapolating the instantaneous acceleration over the interpolation period and testing whether the velocity setpoint, $v_{sp}$, is achieved. This estimation assumes the velocity profile is relatively well-behaved, i.e. it is a convex/concave function. It also assumed that as $t_d$ approaches zero, the velocity profile approaches a linear function in the span of $t_d$.

During interpolation, weighting factors for the velocity and force controllers, $\alpha_v$ and $\alpha_f$, are linearly interpolated over the time span $t_d$. In addition, the commanded velocity is ramped from the initial velocity at time $t_i$ to the setpoint velocity over the span of $t_d$. Finally, a bias control effort, $v_{bias}$, is calculated to ensure that the velocity and force controllers initially output the same value. Assuming that $t_d$ is small, the velocity profile should follow a relatively linear profile. This means that both force and velocity should be simultaneously tracked during interpolation, resulting in controllers that are complimentary during the interpolation switch. This scheme thereby reduces the possibility of spikes in the control effort, $u_{out}$, and promotes a smooth transition during the switch. Figure 3.26a provides a representation of the expected responses from this supervisory control algorithm during the drawing operation. Figure 3.26b provides a zoomed in view during the interpolation period. As the interpolation time period approaches zero, the expected error should approach zero as well.
Figure 3.26: Representation of expected response during supervisory control (a) expected response during drawing operation (b) expected response during interpolation step
Algorithm 3.1: Supervisor Interpolation Algorithm

\[ \sigma = 0 \]

while True do
\[ F_{err} = F_{SP} - F_{draw} \]
\[ F_{out} = k_{P,f} F_{err} + k_{I,f} \int F_{err} dt + k_{D,f} \frac{dF_{err}}{dt} \]

if \( \sigma = 0 \) then
\[ u_{out} = F_{out} \]
if \( v + \frac{dv}{dt} \geq v_{sp} \) then
\[ v_{bias} = u_{out} - k_{P,f} \frac{dF_{err}}{dt} \]
\[ v_i = v, \left[ \frac{dv}{dt} \right]_i = \left[ \frac{dv}{dt} \right], \quad t_i = t, \quad u_{out,i} = u_{out} \]
\[ \sigma = 1 \]
end if

if \( \sigma = 1 \) then
\[ \alpha_v = \frac{t - t_i}{t_d} \in [0,1] \]
\[ \alpha_f = 1 - \frac{t - t_i}{t_d} \in [0,1] \]
\[ v_{inter} = v_i + \left[ \frac{dv}{dt} \right]_i (t - t_i) \in [v_i, v_{sp}] \]
\[ v_{err} = v_{inter} - v \]
\[ v_{out} = k_{P,v} v_{err} + k_{I,v} \int v_{err} dt + k_{D,v} \frac{dv_{err}}{dt} + v_{bias} \]
\[ u_{out} = \alpha_v v_{out} + \alpha_f F_{out} \]
end if
end while
3.5 Chapter Summary

This chapter describes a proposal that extends the hybrid thermoplastic forming process capabilities to encompass the multi-facet/curvilinear geometries typically found on most surgical blades. This is accomplished by applying an oblique drawing technique, i.e. drawing with a non-zero inclination angle. By applying time-varying force profiles during the drawing operation, a wide range of complex blade geometries are possible.

A hybrid thermoplastic forming testbed for curvilinear surgical blades has been designed and assembled based on the oblique drawing concept. To achieve desired repeatability and accuracy requirements, analysis is performed on both individual components and machine kinematics. The new machine configuration includes three distinct modules, namely the lever arm module, the molding module, and the drawing module.

Automation and motion/temperature control requirements of the testbed are handled by a compactRIO industrial controller developed by National Instruments. To facilitate a smooth drawing operation, a supervisory control algorithm has been specifically developed to switch between force-feedback and velocity-feedback controllers. This algorithm implements a bumpless transfer technique to facilitate the switch with minimal jerk. These features aim to prevent the occurrence of inhomogeneous deformation during the oblique drawing process thereby improving overall manufacturing performance and repeatability.
Chapter 4

Multi-Facet/Curvilinear Blade Evaluation and Experimental Characterization

4.1 Multi-Facet/Curvilinear Blade Geometry

Three separate blade geometries, as shown in Fig. 4.1, have been successfully manufactured on the testbed described in Chapter 3. Specifically, a 90° lancet blade, a 45° lancet blade, and a crescent blade have been produced. All blade geometries share a common 12° rake face angle. This chapter begins with location-specific evaluation of crystallization at the edge after oblique drawing. After confirming an amorphous atomic structure, testing and evaluation is described for each blade.

Figure 4.1: Manufactured blade geometries (mm) (a) 90° lancet blade (b) 45° lancet blade (c) crescent blade
4.2 Localized Crystallization During Edge Formation

Both high temperature annealing and large strain deformation have been shown to induce the restructuring of the BMG atomic configuration, leading to crystallization of the sample [24,64]. For the oblique drawing process, crystallization during edge formation is of primary concern. Many techniques for determining crystallization, including x-ray diffraction (XRD), are unable to capture localized atomic structure of a sample, but rather, obtain atomic structure information over a broad area. Since the highest strains are experienced at the actual edge of the blade, an alternative transmission electron microscopy (TEM) diffraction technique is used to investigate the localized crystallinity of the edge. In addition, the high resolution TEM images provide an effective method for measuring the edge radius.

After forming a cutting edge with the oblique drawing process with an inclination angle of 45°, a TEM sample is created using a FEI Dual Beam 235 FIB to cut a cross-section. Figure 4.2 provides the general outline of TEM sample preparation. First, the sample is sputter coated with a gold/palladium alloy. On the FIB, ion beam induced deposition of platinum is locally deposited on the edge to protect the nano-scale features during ion-beam milling. The cross-section is then milled from both sides to create the cross-section to be transferred to a TEM grid. Once the sample is welded to the TEM grid, final thinning is commenced resulting in a final thickness of approximately 50 nm.

In TEM, a beam of electrons is transmitted through an ultra-thin sample. As the beam passes through the specimen, the electrons interact the sample and are subsequently focused/magnified on an imaging device (i.e. phosphor screen). This technique is capable of generating high resolution images. In addition, it is possible to focus on the back focal plane of the objective lens to obtain an electron diffraction pattern. This diffraction pattern can be used to identify the atomic structure of a localized region of a specimen, known as selected area diffraction. The diffraction pattern is valuable because it can be used to differentiate between crystalline and amorphous atomic structures.
Figure 4.2: TEM sample preparation procedure (a) selectively plate region of interest (Pt) (b) ion mill both sides to create X-section (c) perform rough thinning (d) transfer sample to TEM grid (e) weld sample to TEM grid (f) perform final thinning

Figure 4.3 shows a high resolution TEM image of the blade edge cross-section with an edge radius of approximately 15 nm. In Fig. 4.4, the selected area diffraction pattern of the BMG material obtained locally at the edge is shown. The rings, which represent average interatomic spacing, clearly indicate that the BMG material retains its amorphous structure after the oblique drawing process. However, looking closely at the image, faint dots are observed around the rings that reveal signs of partial nano-crystallization. The high temperature or strain generated during the oblique drawing process may have induced crystallization effects that are prominent in BMG materials. Since the degree of crystallization appears to be minimal, this slight nano-crystallization may have even been present in the original stock BMG material.
Figure 4.3: High resolution TEM image of edge radius formed with oblique drawing (45° inclination angle)

Figure 4.4: Selected area diffraction pattern of BMG blade edge
4.3 90° Lancet Blade Evaluation

The hybrid thermoplastic forming testbed is outfitted with mold dies to create lancet blades possessing a 90° included angle. For this blade geometry, the inclination angle is zero and therefore drawing is performed in a perpendicular configuration. This blade geometry is tested primarily to investigate the feasibility and performance of bi-axial drawing.

For preliminary experiments, the molding temperature is set to 683 K and the mold gap is set to 30 µm. Following the kinematic relations described by Eq. 3.2 and Eq. 3.3, the mold gap is controlled as a function of molding actuator position. An LVDT sensor measures the mold gap directly and is used to compensate for any thermally-induced errors. Drawing force and velocity setpoint combinations are chosen based on the edge formation trends observed by Krejcie et al. [7]. As discussed in Section 2.6.1, three deformation types are observed, namely

- Type 1 deformation is generally exhibited by tests with a combination of high temperature and low drawing feedrate and is characterized by low drawing forces and high elongation. This type of deformation is distinguished by significant elongation resulting in a thin, flimsy edge.

- Type 2 deformation is generally exhibited by tests with a combination of low temperature and high drawing federate and is characterized by a high peak drawing force and low elongation to failure. This type of deformation is distinguished by sharp necking over a short distance prior failure. Due to the high rate of necking, tests that follow this type of deformation tend to fail in a manner that creates a jagged uneven edge.

- Type 3 deformation is generally exhibited by tests with combinations of intermediate temperatures and drawing federate relative to type 1 and type 2 deformation. This type of deformation is ideal for cutting edge formation since it maintains a balance between elongation and necking to produce a stable, consistent edge.

In these experiments, temperature is kept constant. Therefore, drawing federate is the only process variable in this case. Since deformation will likely occur with Newtonian flow, changes in drawing force has similar effects in changes in drawing federate. For the supervisory
controller, drawing force parameter is chosen to dictate general edge formation characteristics whereas the velocity parameter is set to ensure a smooth drawing profile with reduced jerk. During experimental trials, process parameters are chosen iteratively. Based on the edge formation characteristics of a certain test, force/velocity setpoints are altered based on the following guidelines based on the edge formation characteristics observed by Krejcie et al. [7].

1. If the edge exhibits thin and highly elongated, drawing force is increased.
2. Conversely, if the edge exhibits jagged failure, drawing force is decreased.
3. Velocity setpoints are generally set as high as possible without generating excessive overshoot during the interpolation period.

SEM images are taken of the blades to qualitatively evaluate the edge formation on a multi-facet blade. Figure 4.5 shows the top view of one of the 90° lancet blades manufactured during experimentation. As shown in Fig. 4.5, a sharp cutting edge has been clearly formed during the drawing process. There are no signs of jagged edge failure, which indicates that deformation occurred entirely within the homogeneous flow regime.

![SEM top view of multi-facet edge formation](image)

**Figure 4.5:** SEM top view of multi-facet edge formation, dotted line marks the start of drawing deformation [F=15 N, v=0.2 mm/s]

Several 90° lancet blades are manufactured during this experiment with reasonable repeatability. Figure 4.6 provides an additional example of a multi-facet blade formed during experimentation and shows an oblique perspective view of the overall geometry. Experimental forces and velocity during drawing are provided in Fig. 4.7 along with the interpolation factors from the supervisory controller.
**Figure 4.6:** SEM oblique perspective view of 90° lancet blade, dotted line marks the start of drawing deformation [F=30 N, v=0.5 mm/s]

**Figure 4.7:** 90° lancet blade drawing data from supervisory controller (a) force (b) velocity (c) interpolation factor [F=30 N, v=0.5 mm/s]
Initially, the force-feedback controller tracks a constant drawing force setpoint. During this period, velocity is gradually increasing. The interpolation sequence occurs as the feedback velocity approaches the setpoint. After a period of transients, the controller is able to track the velocity setpoint. During the interpolation, it is noted drawing force initially experiences a drop, but continues to increase as constant velocity drawing continues. This drop in force corresponds to edge failure. The increase in drawing force after edge failure is attributed to the material connected at the base of the notch feature in the BMG sample. As drawing continues, the small piece of connected BMG material at the base of the notch will fail, corresponding to the second drop in drawing force. Further testing is required to determine parameters that allow for gradual edge failure to ensure homogeneous deformation throughout the entire edge formation process.

Following the FIB procedure described in Section 4.2, a FEI Dual Beam 235 FIB is used to cut cross-section in the blade to directly measure the edge radius formed during the hybrid thermoplastic forming process. The blade samples are initially sputter coated with a gold/palladium alloy. The coated samples then have platinum deposited locally on the edge to protect the nano-scale cutting edge during ion-milling. A cross-section is created by direct ion etching on the FIB. The edge radius is directly measured on a Hitachi S4700 High Resolution SEM.

One example of an FIB cutting edge cross-section is provided in Fig. 4.8. Two magnifications (Fig. 4.8a and Fig. 4.8b) are shown to help provide perspective. As can be seen in Fig. 4.8a, a layer of platinum is deposited to protect the edge while ion milling the cross-section. Figure 4.8b is a magnified view of the edge radius and is measured to be 21.6 nm in this example.
Figure 4.8: Example of 90° lancet blade FIB cross-section and edge radius measurement (a) 20,000 X (b) 200,000 X

The surface roughness $R_a$ is measured on the Dektak 3030 profilometer. Roughness measurements are taken along 500 µm line scan measurements that are taken along the molded rake face with a cutoff frequency of 50 µm. Line scan measurements are repeated along various regions on the molded rake face. Repeatability of the surface roughness $R_a$ is measured to be consistently < 30 nm. Since the dies are polished to a surface roughness of approximately 20 nm, these results indicate that the polished finish of the mold dies transfer nearly identically to the BMG material.
Once material separation occurs, the bi-axial drawing operation forms the blade along with two fragments of scrap material. Since the drawing operation is symmetric, an example of one piece of scrap material remaining after the drawing operation is shown in Fig. 4.9. As can be seen from the image, homogeneous deformation from drawing is clearly seen from the SEM image. The micro-machined notch has a drastic effect on reducing the cross-sectional area and thereby forces in bi-axial drawing. Depending on the positioning of the BMG sample in the mold, the region of high cross-sectional area can vary in size. In cases with poor alignment of the BMG sample with the die, this region at the base of the notch can be so large that failure in this region can occur after edge failure, as experienced in Fig. 4.7.

![Figure 4.9: Fragment of BMG scrap material after drawing operation](image)

**Figure 4.9:** Fragment of BMG scrap material after drawing operation

### 4.4 45° Lancet Blade Evaluation

For the 45° lancet blade, an inclination angle of 22.5° exists for each drawing direction. Extensive testing is performed for this blade geometry to fully evaluate the effects of oblique drawing in a bi-axial drawing configuration. Experiments are first conducted by varying process parameters of the drawing process. After identifying a set of parameters that produce high quality blades, the repeatability of the process is studied.
4.4.1 Parametric Study of Oblique Drawing

For the supervisory controller described in Section 3.4.2, force and velocity constitute the two input process parameters that can be varied. For the case of an inclination angle of 22.5°, a constant force and a constant velocity setpoint are used. In the following experiment, the mold gap is set to 50 µm and the mold temperature is set to 683K. Low temperature is desired to prevent thermally-induced crystallization. The force is varied between 30-60 N while the velocity is varied between 0.35-2.1 mm/s. These parameters are chosen such that the three edge deformation types described by Krejcie et al. are observed [7].

Images of the blade samples are taken on the JEOL 6060LV SEM for a wide range of operating conditions and are provided in Fig. 4.10. Overall blade morphology and edge formation is studied and qualitatively compared between the different process parameters. In addition, a magnified view of the edge formation for each set of input parameters is shown in Fig. 4.11.
Figure 4.10: SEM images of 45° lancet blade morphology, dotted line marks the start of drawing deformation (a) [F=30 N, v=0.35 mm/s] (b) [F=30 N, v=0.8 mm/s] (c) [F=30 N, v=1.5 mm/s] (d) [F=30 N, v=2.10 mm/s] (e) [F=40 N, v=0.35 mm/s] (f) [F=40 N, v=0.8 mm/s] (g) [F=40 N, v=1.5 mm/s] (h) [F=40 N, v=2.10 mm/s] (i) [F=50 N, v=0.35 mm/s] (j) [F=50 N, v=0.8 mm/s] (k) [F=50 N, v=1.5 mm/s] (l) [F=50 N, v=2.10 mm/s] (m) [F=60 N, v=0.35 mm/s] (n) [F=60 N, v=0.8 mm/s] (o) [F=60 N, v=1.5 mm/s] (p) [F=60 N, v=2.10 mm/s]
Figure 4.11: SEM images of 45° lancet blade edge formation, dotted line marks the start of
drawing deformation (a) [F=30 N, v=0.35 mm/s] (b) [F=30 N, v=0.8 mm/s] (c) [F=30 N, v=1.5
mm/s] (d) [F=30 N, v=2.10 mm/s] (e) [F=40 N, v=0.35 mm/s] (f) [F=40 N, v=0.8 mm/s] (g)
[F=40 N, v=1.5 mm/s] (h) [F=40 N, v=2.10 mm/s] (i) [F=50 N, v=0.35 mm/s] (j) [F=50 N,
v=0.8 mm/s] (k) [F=50 N, v=1.5 mm/s] (l) [F=50 N, v=2.10 mm/s] (m) [F=60 N, v=0.35 mm/s]
(n) [F=60 N, v=0.8 mm/s] (o) [F=60 N, v=1.5 mm/s] (p) [F=60 N, v=2.10 mm/s]

Test results can be categorized based on the edge deformation types proposed by Krejcie et al.
[7], which are reviewed in Section 2.6.1. The long, highly elongated edges with significant
waviness can be characterized as type 1 deformation, as seen in Fig. 4.10a,b,c,d,e,f,g,h,i,j,m,n
and Fig. 4.11a,b,c,d,e,f,g,h,i,j,m,n. These tests are primarily associated with combinations of
low force and low velocity parameters. For incision purposes, these blades are far from ideal due to their inability to support lateral loads.

When the force or velocity setpoint is increased, the drawing distance decreases and the edge formation shifts from a highly elongated edge to a more stable edge formation classified as type 3 deformation, which is seen in Fig. 4.10k,l,o and Fig. 4.11k,l,o. This edge failure type fails uniformly, resulting in relatively straight cutting edges. Further increases to force and/or velocity result in slightly jagged edge formation, as seen Fig. 4.10p and Fig. 4.11p, and can be classified as type 2 deformation. This jagged edge failure indicates the transition into a strain localization deformation regime resulting in poor edge formation.

As discussed in Section 2.4.1, high temperature mechanical behavior of BMG can be described as either homogeneous flow or inhomogeneous deformation. These deformation regimes are governed by the stress-induced creation and the subsequent temperature-dependent, diffusional annihilation of free volume [80]. In the case of type 2 deformation where high stress and low temperature conditions exist, the creation of free volume outpaces the rate of structural relaxation (i.e. annihilation of free volume). This results in a decrease in the local density and an associated decrease in strength. Localized strain softening causes deformation to become unstable and sharply localize into thin shear bands. This is observed in the form of jagged edge failure in type 2 deformation.

When either temperature is increased or stress is decreased, the diffusional rate of free volume annihilation balances with the rate of free volume creation resulting in the macroscopic flow observed in type 1 deformation. In order for a stable neck to form, some strain hardening mechanism must exist. Krejcie et al. proposed that type 1 deformation is associated with strain-induced nano-crystallization that increases the strength and hardness in the necked region [7]. This creates a strain hardening effect in stabilizes the neck and distributes the stress to the surrounding undeformed regions, thereby increasing elongation.

For the intermediate conditions that cause type 3 deformation, it is believed that the strain softening effect due to the creation of free volume balances with the strain hardening effect of nano-crystal formation. In this situation, deformation is still classified as homogeneous,
however, stress is confined within the necked region. Constrained homogeneous deformation results in rapid necking and the formation of strong, stable edges.

For force setpoints above 60 N, the drawing actuators begin exhibiting instability during the interpolation sequence. It is also observed that for tests with velocity setpoints below 1.5 mm/s, the overshoot during interpolation is negligible. However, as velocity is increased to 2.1 mm/s, there is an increasingly larger overshoot during the interpolation period. This can be seen directly in Fig. 4.12 where the force/velocity profiles for each of the tests are provided. The higher forces/velocities tend to make the switch less ideal and the interpolation process becomes more difficult to control. The inherent time scale for edge formation also becomes much shorter as forces/velocities are increased due to the deformation behavior of BMG. The overshoot, which represents a loss of control over the drawing process, ultimately causes lower repeatability in the edge quality.

There are two primary defects that are observed across a majority of the blades. At the tip of the blade, there is a protuberance that is typically biased to one side, which is particularly notable at low force and low velocity conditions. This defect is likely caused by the complex stress distribution that occurs at the tip of the blade. Unlike the relatively simple loading condition along a single facet, the tip represents an area where the bi-axial drawing forces intersect, creating more variability in the formation of the tip. In addition, variations in the mold gap caused by tilting are present due to the low moment stiffness of the linear guide rail stage, which houses the mold dies. The second defect occurs at either end of the blade where there is a formation of “tail” features. The gripping jaws on the drawing actuators are configured at a 22.5° angle. Because of this angle, the drawing force cannot be directly applied uniformly across the edge. Due to the viscous behavior of the BMG material, as drawing progresses, the scrap material deforms as drawing progresses and therefore inhibits necking behavior at either end of the blade. This causes delayed failure at the ends of the blade and the formation of the “tail” features. Fortunately, as the force and velocity is increased, the blade exhibits minimal “tail” formation and the protuberance at the tip disappear, and instead, form a symmetric tip.
Good edge formation is observed for the force/velocity combination of \([F=50 \text{ N}, v=1.5 \text{ mm/s}], [F=50 \text{ N}, v=2.1 \text{ mm/s}],\) and \([F=60 \text{ N}, v=1.5 \text{ mm/s}].\) Process repeatability is studied by performing multiple repeat tests for the particular case of \([F=50 \text{ N}, v=2.10 \text{ mm/s}].\) To evaluate the performance of the blades, the overall blade geometry and tip formation are first examined. Several quantitative measurements are also discussed, namely, the included angle tolerance,
force/velocity profile, drawing distance, edge radius, rake face surface roughness, and blade straightness.

**Evaluation of Blade Geometry and Tip Formation**

Using force/velocity combination of [F=50 N, v=2.10 mm/s], five 45° lancet blades are manufactured. Images of the five blades are obtained on a JEOL 6060LV SEM and are provided in Fig. 4.13. Qualitatively, the hybrid thermoplastic forming process exhibits excellent ability to reproduce the 45° lancet geometry with good repeatability.

![Images of five lancet blades](image)

**Figure 4.13:** Qualitative blade geometry repeatability for 45° lancet blades, dotted line marks the start of drawing deformation
The included angle of the mold die geometry is nominally $45^\circ$. The ability to reproduce this angle during the hybrid thermoplastic forming process is evaluated. First, coordinate data of the edge location is found from an SEM image, as shown in Fig. 4.14b. Regions around the tip and the extreme edges are disregarded due to the curvature in these areas. Least squares regression is used to fit lines to the two facets and the included angle is calculated, as shown in Fig. 4.14c.

**Figure 4.14:** $45^\circ$ lancet included angle measurement (a) top view SEM image (b) coordinate data of edge location (c) least squares regression fit of coordinate data and included angle measurement
The included angle measurement for the five repeated tests are given in Table 4.1. With a sample size of $N=5$, it is found that the total drawing distance has an average of $42.7^\circ$ with a standard deviation of $0.92^\circ$. This means that on average, the included angle has a 5% error. Since the included angle is relatively repeatable, it should be possible to modify the mold die geometry to shift the included angle average up to $45^\circ$.

**Table 4.1: 45° lancet included angle measurement for repeatability test**

<table>
<thead>
<tr>
<th>Sample #</th>
<th>Included Angle (°)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>42.9</td>
</tr>
<tr>
<td>2</td>
<td>42.4</td>
</tr>
<tr>
<td>3</td>
<td>41.9</td>
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<tr>
<td>4</td>
<td>42.1</td>
</tr>
<tr>
<td>5</td>
<td>42.1</td>
</tr>
</tbody>
</table>

Magnified images of the 45° lancet tip formation are also obtained on the SEM and are provided in Fig. 4.15. Again, the hybrid thermoplastic forming process shows good repeatability of forming a defined tip with minimal protuberances compared to tests with lower force/velocity parameters.
Figure 4.15: Qualitative tip intersection geometry repeatability for 45° lancet blades, dotted line marks the start of drawing deformation

Force/Velocity Interpolation Profiles

The force and velocity profiles of the 45° lancet blade are obtained for five separate repeat tests, which are provided in Fig. 4.16. The overall shape and time-scale of the force/velocity plots is fairly repeatable. While relatively large overshoot is observed in some of the tests, it is
believed that this is a tuning issue rather than a fundamental limitation of the supervisory control algorithm.

**Figure 4.16:** Force and velocity profiles for repeatability for 45° lancet blades \([F=50\ N, v=2.10\ \text{mm/s}]\)

It is important that the elongation during edge formation is relatively repeatable from test to test. The total drawing distance prior to interpolation is calculated by numerically integrating the velocity profile. The total drawing distance is used as an indirect measure of edge formation elongation repeatability. After material failure occurs, the drawn out BMG material is split between the regions of homogeneous deformation on the blade, as shown in Fig. 4.5, and on the scrap material, as shown in Fig. 4.9. Therefore the elongation observed on the blade is only some fraction of the total drawing distance reported here. It should also be noted that the
drawing distance not only represents the elasto-plastic necking of the BMG, but also contains elastic deflections associated with the drawing actuator itself.

The total drawing distance for the five repeated tests are given in Table 4.2. With a sample size of N=5, it is found that the total drawing distance has an average of 705 µm with a standard deviation of 145 µm.

Table 4.2: 45° lancet drawing distance measurement for repeatability tests

<table>
<thead>
<tr>
<th>Sample #</th>
<th>Drawing Distance (µm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>731.9</td>
</tr>
<tr>
<td>2</td>
<td>841.4</td>
</tr>
<tr>
<td>3</td>
<td>480.4</td>
</tr>
<tr>
<td>4</td>
<td>825.2</td>
</tr>
<tr>
<td>5</td>
<td>659.2</td>
</tr>
</tbody>
</table>

Edge Radius

Edge radius is measured on several 45° blades again following the FIB procedure outlined Section 4.2. A cross-section is milled by direct ion etching on a FEI Dual Beam 235 FIB. The edge radius is directly measured on a Hitachi S4700 High Resolution SEM.

One example of an FIB cutting edge cross-section for a crescent blade is provided in Fig. 4.17. Several magnifications of the cutting edge are shown to provide perspective. In this example, the magnified cutting edge is measured to have an edge radius of 10.9 nm in this example.
Figure 4.17: Example of 45° lancet blade FIB cross-section and edge radius measurement (a) 20,000 X (b) 40,000 X (c) 200,000 X
Edge radius measurements for the five repeated tests are given in Table 4.3. With a sample size of $N=5$, it is found that the edge radius has an average of 12.0 nm with a standard deviation of 1.95 nm.

**Table 4.3: 45° lancet edge radius measurement for repeatability tests**

<table>
<thead>
<tr>
<th>Sample #</th>
<th>Edge Radius (nm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>10.9</td>
</tr>
<tr>
<td>2</td>
<td>10.2</td>
</tr>
<tr>
<td>3</td>
<td>10.7</td>
</tr>
<tr>
<td>4</td>
<td>13.8</td>
</tr>
<tr>
<td>5</td>
<td>14.4</td>
</tr>
</tbody>
</table>

**Rake Face Surface Roughness**

The surface roughness $R_a$ is measured on the Dektak 3030 profirometer. Roughness measurements are taken along 500 µm line scan measurements that are repeated along the molded rake face with a cutoff frequency of 50 µm. Line scan measurements are repeated along various regions on the molded rake face.

One example of the surface roughness line scan is provided in Fig. 4.18. In this example, the surface roughness $R_a$ is 17.5 nm.

![Surface Roughness Profile Scan](image)

**Figure 4.18:** Example of 45° lancet blade rake face surface roughness measurement
Surface finish measurements on the molded rake faces are repeated ten times and are given in Table 4.4. With a sample size of N=10, it is found that the surface finish $R_a$ has an average of 21.6 nm with a standard deviation of 4.82 nm.

**Table 4.4**: 45° lancet rake face surface roughness measurement for repeatability tests

<table>
<thead>
<tr>
<th>Sample #</th>
<th>Surface Finish $R_a$ (nm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>22.0</td>
</tr>
<tr>
<td>2</td>
<td>21.5</td>
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<tr>
<td>3</td>
<td>26.1</td>
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<tr>
<td>4</td>
<td>22.3</td>
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<td>5</td>
<td>25.8</td>
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<td>8</td>
<td>17.5</td>
</tr>
<tr>
<td>9</td>
<td>25.7</td>
</tr>
<tr>
<td>10</td>
<td>22.4</td>
</tr>
</tbody>
</table>

**Blade Straightness**

Blade straightness is characterized from both a top view (X-Y plane) as well as from a side view (X-Z plane). The two orientations are shown in Fig. 4.19.

**Figure 4.19**: Blade straightness orientations for evaluation (a) top view (X-Y plane) (b) side view (X-Z plane) [3]
A set of consecutive images are taken at a 200X magnification along the edge of the blade. To avoid the curvature near the edge and tip of the blade, only the center 2mm portion of each facet is considered, as shown in Fig. 4.20.

![Region of interest for blade straightness measurement (mm)](image)

**Figure 4.20: Region of interest for blade straightness measurement (mm)**

Since SEM images retain a large depth of focus, it is possible to take a series of consecutive images and stitch them together. This allows for very wide aspect ratios that allow for measuring straightness over a long distance with high accuracy. Figure 4.21a provides an example of a series of SEM images that have been stitched together to form a 2mm wide top view of a 45° lancet. Coordinate data of the edge location is taken in increments of 10µm along the X-direction, as shown in Fig. 4.21b. Figure 4.21c shows a least squares regression (LSR) line that is fitted to this coordinate data. The residual is calculated based on the minimum distance between a point and line as shown in Fig. 4.21d. The residual provides information on the deviation from a perfectly straight facet. The straightness is defined as the RMS value of the residual data series. This procedure is repeated for the side view (X-Z plane) and an example is shown in Fig. 4.22.
Figure 4.21: Top view (X-Y plane) straightness measurement (a) stitched images (b) coordinate data of edge location (c) least squares regression fit of coordinate data (d) residual straightness error
Figure 4.22: Side view (X-Z plane) straightness measurement (a) stitched images (b) coordinate data of edge location (c) least squares regression fit of coordinate data (d) residual straightness error

This procedure for blade straightness measurement is repeated ten times for the top and side views. Blade straightness RMS measurements for the top (X-Y plane) and the side (X-Z plane) are tabulated in Table 4.5 and Table 4.6, respectively. With a sample size of N=10, it is found that the RMS blade straightness in the top view (X-Y plane) has an average of 4.82 µm with a standard deviation of 1.00 µm and the side view (X-Z plane) has an average of 1.62 µm with a standard deviation of 0.76 µm over the 2 mm blade width.
Table 4.5: 45° lancet top view (X-Y plane) straightness measurement for repeatability tests

<table>
<thead>
<tr>
<th>Sample #</th>
<th>RMS Blade Straightness (µm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>4.19</td>
</tr>
<tr>
<td>2</td>
<td>3.81</td>
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<td>3</td>
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<td>5</td>
<td>3.86</td>
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<td>6</td>
<td>6.56</td>
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<td>7</td>
<td>6.15</td>
</tr>
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<td>8</td>
<td>4.22</td>
</tr>
<tr>
<td>9</td>
<td>4.38</td>
</tr>
<tr>
<td>10</td>
<td>4.73</td>
</tr>
</tbody>
</table>

Table 4.6: 45° lancet side view (X-Z plane) straightness measurement for repeatability tests

<table>
<thead>
<tr>
<th>Sample #</th>
<th>RMS Blade Straightness (µm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1.43</td>
</tr>
<tr>
<td>2</td>
<td>0.91</td>
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<td>3</td>
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<td>4</td>
<td>1.95</td>
</tr>
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<td>1.65</td>
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</tr>
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<td>0.94</td>
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<tr>
<td>8</td>
<td>2.85</td>
</tr>
<tr>
<td>9</td>
<td>1.05</td>
</tr>
<tr>
<td>10</td>
<td>1.42</td>
</tr>
</tbody>
</table>

4.5 Crescent Blade Evaluation

A feasibility study has been performed using the mold dies designed to manufacture crescent blades. For this blade geometry, the inclination angle varies across the edge of the blade. It is therefore necessary to apply time-varying force profiles to control the rate of necking along the edge of the blade during drawing operation. While it is possibly to apply arbitrary drawing force profiles, testing in this section is constrained to linearly ramped drawing force profiles.
Figure 4.23 provides several images of initial crescent blades manufactured. In the following tests, the mold gap is set to 50 µm and the mold temperature is set to 683K. Here, the drawing force ramp rate, \( F \), is varied between 5-10 N/s and a velocity of 1 mm/s.

These preliminary tests show that applying oblique drawing to curvilinear geometry is feasible. Curved facets are clearly formed within the homogeneous deformation regime, however, it is seen that the replication of the crescent blade geometry in these tests are quite poor. Similar to
the 45° blade, a major defect in the form of a protuberance is formed at the intersection of the two drawing actuators. While the drawing distance varies along the edge of the blade in these experiments, a general trend is observed that increasing the drawing force ramp rate decreases the overall drawing distance. To form stable cutting edges, the drawing distances must be kept reasonable short and therefore higher drawing force ramp rates are preferred.

To improve the replication ability of the crescent geometry, the mold gap is decreased to 25 µm. With a smaller gap, the drawing distance correspondingly decreases and therefore the potential for variation is lowered. It is also found that the positioning of the BMG sample notch relative to the mold die is critical to preventing the protuberance defect at the notch location in the BMG sample. A crescent blade manufactured with these considerations is shown in Fig. 4.24.

Figure 4.24: Crescent blade tests with 25 µm mold gap with 7 N/s drawing force ramp rate and 1 mm/s velocity limit, dotted line marks the start of drawing deformation

While the crescent blade possesses a smooth edge profile, the elongation is non-symmetric, i.e. one side of the blade experiences greater draw distance prior to failure. One method to improve the uniformity of elongation along the entire edge is to apply independent force/velocity
parameters to each drawing actuator to ensure elongation is symmetric on both sides of the blade.

The experimental forces and velocity during drawing are provided in Fig. 4.25 along with the interpolation factors from the supervisory controller. The force controller tracks the ramp trajectory profile very well. Once the velocity setpoint is reached, interpolation occurs smoothly with only a small velocity overshoot.

![Figure 4.25: Crescent blade drawing data from supervisory controller \([F=7 \text{ N/s}, v=1.0 \text{ mm/s}]\)
(a) force (b) velocity (c) interpolation factor](image-url)
Edge radius is again measured on several crescent blades following the FIB procedure outlined Section 4.2. A cross-section is milled by direct ion etching on a FEI Dual Beam 235 FIB. The edge radius is directly measured on a Hitachi S4700 High Resolution SEM.

One example of an FIB cutting edge cross-section for a crescent blade is provided in Fig. 4.26. Several magnifications of the cutting edge are shown to provide perspective. In this example, the magnified cutting edge is measured to have an edge radius of 9.9 nm in this example.

![Figure 4.26: Example of crescent blade FIB cross-section and edge radius measurement (a) 20,000 X (b) 40,000 X (c) 200,000 X](image)
Edge radius measurements for the five repeated tests are given in Table 4.7. With a sample size of N=5, it is found that the edge radius has an average of 11.7 nm with a standard deviation of 1.45 nm.

**Table 4.7**: Crescent blade edge radius measurement for repeatability tests

<table>
<thead>
<tr>
<th>Sample #</th>
<th>Edge Radius (nm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>12.9</td>
</tr>
<tr>
<td>2</td>
<td>9.9</td>
</tr>
<tr>
<td>3</td>
<td>11.2</td>
</tr>
<tr>
<td>4</td>
<td>10.9</td>
</tr>
<tr>
<td>5</td>
<td>13.4</td>
</tr>
</tbody>
</table>

The surface roughness $R_a$ measurements are taken along 500 $\mu$m line scan measurements that are repeated along the molded rake face with a cutoff frequency of 50 $\mu$m. These line scan measurements are repeated along various regions on the molded rake face across several crescent blades.

Surface finish measurements on the molded rake faces are repeated ten times and are given in Table 4.8. With a sample size of N=10, it is found that the surface finish $R_a$ has an average of 17.3 nm with a standard deviation of 3.12 nm.

**Table 4.8**: Crescent blade rake face surface roughness measurement for repeatability tests

<table>
<thead>
<tr>
<th>Sample #</th>
<th>Surface Finish $R_a$ (nm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>19.2</td>
</tr>
<tr>
<td>2</td>
<td>17.9</td>
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<td>3</td>
<td>16.9</td>
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<td>8</td>
<td>15.9</td>
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<tr>
<td>9</td>
<td>18.6</td>
</tr>
<tr>
<td>10</td>
<td>17.7</td>
</tr>
</tbody>
</table>
4.6 Chapter Summary

This chapter describes the performance evaluation of the hybrid thermoplastic forming process. Before performing tests on multi-facet/curvilinear blade geometries, TEM selected area diffraction is used to verify the amorphous atomic structure locally at the cutting edge after an oblique drawing operation. While faint traces of nano-crystallization are detected, the bulk of the sample retains an amorphous structure and the effect on material properties is likely minimal.

Three geometries are experimentally characterized, specifically, a 90° lancet blade, a 45° lancet blade, and a crescent blade have been produced. For the 90° lancet blade, drawing is performed in a perpendicular configuration and is tested primarily to investigate the feasibility and performance of bi-axial drawing. Several 90° lancet blades are manufactured during this experiment with homogeneous deformation and reasonable repeatability. Edge radius is measured to be on the order of ~20 nm for a variety of drawing parameters tested. Repeatability of the surface roughness $R_a$ is measured to be consistently < 30 nm. Since the dies are polished to a surface roughness of approximately 20 nm, these results indicate that the polished finish of the mold dies transfer nearly identically to the BMG material.

For the 45° lancet blade, an inclination angle of 22.5° exists for each drawing direction. Extensive testing is performed for this blade geometry to fully evaluate the effects of oblique drawing in a bi-axial drawing configuration. Experiments are first conducted by varying process parameters of the drawing process. Combinations of low force and low velocity results in thin, highly elongated edges that are characterized by significant waviness. For incision purposes, these blades are far from ideal. As force or velocity is increased, the drawing distance decreases and the edge formation shifts from a highly elongated edge to a more stable edge formation. In general, it is found that combinations of increased force and velocity setpoints leads to straight, uniform edge failure as long as deformation remains homogeneous (i.e. no indication of shear localization). Fortunately, the higher forces/velocities also decrease defect occurrence and improve symmetric tip formation.
After identifying a set of parameters that produce high quality blades, the repeatability of the process is studied. Qualitative aspects of the blade formation as well as quantitative measurements of the included angle, drawing distance, edge radius, surface finish, and edge straightness are measured. Summary statistics are provided in Table 4.9.

<table>
<thead>
<tr>
<th>Measurement</th>
<th>Average</th>
<th>Standard. Deviation</th>
<th>N</th>
</tr>
</thead>
<tbody>
<tr>
<td>Included Angle</td>
<td>42.7°</td>
<td>0.92°</td>
<td>5</td>
</tr>
<tr>
<td>Drawing Distance</td>
<td>705 µm</td>
<td>145 µm</td>
<td>5</td>
</tr>
<tr>
<td>Edge Radius</td>
<td>12.0 nm</td>
<td>1.95 nm</td>
<td>5</td>
</tr>
<tr>
<td>Surface Finish (Rₐ)</td>
<td>21.6 nm</td>
<td>4.82 nm</td>
<td>10</td>
</tr>
<tr>
<td>RMS Straightness (XY-Plane)</td>
<td>4.82 µm</td>
<td>1.00 µm</td>
<td>10</td>
</tr>
<tr>
<td>RMS Straightness (XZ-Plane)</td>
<td>1.62 µm</td>
<td>0.76 µm</td>
<td>10</td>
</tr>
</tbody>
</table>

Feasibility has been demonstrated for crescent blade geometry. For this blade, it is necessary to apply time-varying force profiles to control the rate of necking along the edge of the blade during drawing operation. Testing in this section is performed exclusively with linearly ramped drawing force profiles. These preliminary experiments establish that it is possible to obtain satisfactory results by applying oblique drawing to curvilinear geometry. Curved facets are clearly formed within the homogeneous deformation regime, however, several defects are observed. To form stable cutting edges, the drawing distances must be kept reasonable short and therefore higher drawing force ramp rates are preferred. To improve the replication ability of the crescent geometry, the mold gap is decreased to 25 µm. With a smaller gap, the drawing distance correspondingly decreases and therefore the potential for variation is lowered. It is also found that the positioning of the BMG sample notch relative to the mold die is critical to prevent the protuberance defect at the intersection of the two drawing actuators. Crescent blades manufactured in this experiment are measured to possess an average edge radius of 11.7 nm with a standard deviation of 1.45 nm. Average Rₐ surface finish on the crescent rake faces is measured to be 17.3 nm with a standard deviation of 3.12 nm.
Chapter 5
Conclusions and Recommendations

5.1 Conclusions

In this thesis, a novel hybrid thermoplastic forming process combining thermally-assisted micro-molding and thermally-assisted micro-drawing has been developed to manufacture the multi-facet/curvilinear geometries typically found on most surgical blades. Based on an oblique drawing technique, i.e. drawing with a non-zero inclination angle, a hybrid thermoplastic forming testbed for manufacturing surgical blades with complex geometry has been designed, assembled, and automated. Various multi-facet/curvilinear blade geometries are manufactured and experimentally evaluated to demonstrate feasibility and limitations encountered in the process. The following three subsections provide specific conclusions drawn from this work.

5.1.1 Development of Hybrid Thermoplastic Forming Testbed

1. An oblique drawing technique, i.e. drawing with a non-zero inclination angle, is applied to the hybrid thermoplastic forming process. Normal stresses generated during oblique drawing are found to be the dominant factor in the formation of the cutting edge of the surgical blade. By applying time-varying force profiles during the drawing operation, a wide range of multi-facet/curvilinear blade geometries are possible.

2. A hybrid thermoplastic forming testbed for curvilinear surgical blades has been designed and assembled. This testbed has been sub-divided into three primary modules: the lever arm module, the molding module, and the drawing module. This configuration introduces oblique drawing to the hybrid thermoplastic forming process as well as improves repeatability and accuracy capabilities.
3. The molded gap distance is an important parameter that influences the performance of the subsequent micro-drawing operation. Machine structure and kinematics are designed to leverage the mechanical advantage of the lever arm module to improve repeatability and accuracy in setting the mold gap. The lever arm topology adds a multiplication factor of 3 on force and repeatability, providing an effective maximum molding force of 2640 N and accuracy of ±1 µm. An additional LVDT sensor with 10 nm resolution is installed to provide a direct measurement of the linear displacement of the dies to verify the correct molded gap.

4. The consistency of the molded gap distance along the entire edge of the blade must be carefully controlled to guarantee a straight, uniform edge failure. Mold dies are designed to resolve inconsistencies in the gap distances during the molding operation. First, die-sinking EDM is used to create the complex geometry of the blade. This is followed by a surface grinding operation to create a reference surface that can be used to accurately set the mold gap. The final grinding operation must possess a flatness tolerance of 2.5 µm such that when the two symmetric dies are mated together, the total gap variation between the two edges of the dies remain within a 20% tolerance, i.e. total gap variation less than 5 µm.

5. Drawing is performed in a bi-axial configuration that can accommodate both multi-facet and curvilinear blade geometries. The dual voice coil-driven drawing stage is designed to apply force in two directions, offset 90° from each other. Each linear stage is voice coil-actuated and is equipped with a 1.22 nm linear encoder and a 222 N capacity load cell. To grip the BMG prior to drawing, a gripping clamp flexure has been included in the design.

6. Due to the limited bandwidth of the voice coil stages, the drawing process typically experiences a sudden jerk as the BMG sample rapidly necks prior to failure and leads to poor repeatability. An external fluid damper is introduced to the drawing system to dampen this high frequency jerk. In addition, the damper improves stability of the control loops and provides smoother motion, improving overall blade quality.
7. The initial BMG sample is a 2.5 X 5.0 X 0.3 mm strip of Vitreloy-1b with a 254 µm wide notch micro-machined in one end of the sample. The notch is necessary to decrease the excessive forces encountered during bi-axial drawing.

5.1.2 Automation and Controller Design

1. Automation for the testbed is performed on a compactRIO industrial controller developed by National Instruments. The automation system can be broken down into a three-layer hierarchy, namely, the host computer, the real-time processor, and the field programmable gate array (FPGA). These three components are tightly integrated with network communication and transfer protocols.

2. The host computer displays an event-driven graphical user interface (GUI) where the user may input commands to control the hybrid manufacturing testbed. The RT processor is deterministic and therefore handles processes that require precise timing. In particular, the RT processor code performs temperature and molding motor control as well as acts as an interface between the host computer and FPGA. Custom controller design for the drawing actuators is implemented on the FPGA. Since the FPGA architecture is based inherently on hardware, the chip offers both parallel processing and high performance.

3. In order to manufacture the complex curvilinear geometry, variable profile drawing forces must be applied. A supervisory control algorithm has been specifically designed to switch between force-feedback and velocity-feedback controllers. The force-feedback controller can follow several trajectories, such as ramp, exponential, quadratic, etc., to accommodate different blade geometries. In addition, this algorithm implements a bumpless transfer technique to facilitate the switch with minimal jerk. These features aim to prevent the occurrence of inhomogeneous deformation during the oblique drawing process thereby improving overall manufacturing performance and repeatability.
5.1.3 Performance Evaluation of Multi-Facet/Curvilinear Blade Geometries

1. Both high temperature annealing and large strain deformation can contribute to the crystallization of a BMG sample. TEM selected area diffraction is used to verify the amorphous atomic structure locally at the cutting edge after performing an oblique drawing operation. While faint traces of nano-crystallization are detected, the bulk of the sample retains an amorphous structure and the effect on material properties is likely minimal.

2. Early testing of 90° lancet blades has exhibited positive molding results with good edge formation on symmetric multi-facet blades. Edge radius is measured to be on the order of ~20 nm for a variety of drawing parameters tested. Repeatability of the surface roughness $R_a$ is measured to be consistently less than 30 nm. Several blades were manufactured during experimentation with reasonable repeatability, which demonstrates the viability of a bi-axial drawing configuration.

3. For the supervisory controller, combinations of low force and low velocity results in thin, highly elongated edges that are characterized by significant waviness. For incision purposes, these blades are far from ideal. It is found that as force or velocity is increased, the drawing distance decreases and the edge formation shifts from a highly elongated edge to a more stable edge formation with relatively straight cutting edges. In general, it is found that combinations of increased force and velocity setpoints leads to straight, uniform edge failure as long as deformation remains homogeneous (i.e., no indication of shear localization). Fortunately, the higher forces/velocities also decrease defect occurrence and improve symmetric tip formation.

4. After identifying a set of parameters that produce high quality 45° lancet blades, the repeatability of the process is studied. Qualitatively, the hybrid thermoplastic forming process shows good repeatability in forming a defined tip with minimal defects. Multiple 45° lancet blades are manufactured and several measurements are taken to obtain distributions ($\mu$ is the sample mean, $\sigma$ is the sample standard deviation) for
included angle (µ=42.7°, σ=0.92°), drawing distance (µ=705 µm, σ=145 µm), edge radius (µ=12.0 nm, σ=1.95 nm), Rₐ surface finish (µ=21.6 nm, σ=4.82 nm), XY-plane RMS straightness (µ=4.82 µm, σ=1.00 µm), and XZ-plane RMS straightness (µ=1.62 µm, σ=0.76 µm).

5. Feasibility is demonstrated for manufacturing crescent blade with curvilinear edge formation. For this blade geometry, it is necessary to apply time-varying force profiles to control the rate of necking along the edge of the blade during drawing operation. Testing in this section is performed exclusively with linearly ramped drawing force profiles. Curved facets are clearly formed within the homogeneous deformation regime, however, several defects are observed. To form stable cutting edges, the drawing distances must be kept reasonable short and therefore higher drawing force ramp rates are preferred. To improve the replication ability of the crescent geometry, the mold gap is decreased to 25 µm. With a smaller gap, the drawing distance correspondingly decreases and therefore the potential for variation is lowered. It is also found that the positioning of the BMG sample notch relative to the mold die is critical to preventing the protuberance defect at the intersection of the two drawing actuators. Multiple crescent blades are manufactured and several measurements are taken to obtain distributions for edge radius (µ=11.7 nm, σ=1.45 nm) and Rₐ surface finish (µ=17.3 nm, σ=3.12 nm).

6. The molding operation exhibits excellent replication ability with surface finish Rₐ measurements on the order of 20 nm. Across all of the blade geometries, the surface finish of the dies transfer nearly identically to the BMG material. The BMG rake face surface finish is primarily limited by the achievable surface finish in the H13 tool steel dies.

5.2 Recommendations for Future Work

Numerous opportunities exist to further improve the hybrid thermoplastic forming process as well as extend the scope of this research topic. Recommendations for future work are as follows.
1. During experimentation and evaluation, several challenges were encountered. The following testbed modifications are recommended for future tests:

   a. The linear guide rail stage suffered from low moment stiffness exhibited by the porous restricted air bearings. During molding, the low moment stiffness resulted in the stage to tilt, causing an inconsistent mold gap. It is recommended to replace the air bearings with a set of linear ball bearings that can be configured to prevent tilting motions.

   b. To meet the surgical blade surface finish requirements, it is critical to control the surface finish of the mold dies. However, it is observed that the surface finish of the H13 tool steel die gradually degrades as surface oxides form. During the manufacture of the mold dies, it is recommended to either apply a thick chrome plating or an electroless nickel plating prior to the polishing and final surface grinding steps to prevent high temperature oxidation during molding.

   c. In order for BMG material separation to occur in the area near the notched region in the BMG sample, transverse forces must be applied. This generates significant deflections in the drawing stages due to the moment loads induced on the linear guides. It is recommended to place two linear guides in-line with each other in order to improve moment stiffness of the drawing actuators in the XY-plane.

   d. Detailed heat transfer analysis is required to manage thermal aspects of the testbed. With the high temperatures required by thermoplastic forming, heat leaks directly into the machine structure. This induces thermal expansion and generates inaccuracy in setting the mold gap. It is recommended to apply greater thermal management such that thermal expansion is controllable, or at least able to be compensated for.

2. With increasing force and velocity setpoints, the supervisory controller for the drawing actuator experiences greater overshoot and jerk during the switching operation. This is the result of the highly non-linear necking behavior. In particular, the transition between non-Newtonian flow behavior and shear localization causes the rate of edge
failure to increase dramatically. The jerk associated with edge failure is high frequency in nature and lies outside the bandwidth of the drawing actuator. Since the feedback controller is unable to compensate for the high frequency edge failure event, it is recommended to simply increase the mass of the drawing stage to attenuate this disturbance. Increasing damping in the fluid damper may also improve performance. Alternatively, a feed-forward mechanism can be introduced to ensure a smooth drawing profile. An iterative learning controller may be highly beneficial to the system if the drawing process is highly repeatable.

3. It is necessary to evaluate the tissue cutting performance of the manufactured BMG blades. It is recommended to explore the cutting forces as a function of penetration depth as well as blade stiffness in response to transverse loads. In addition, wear studies should be conducted over repeated incisions to explore the reusability of the BMG blade. Testing should also be extended to involve a wider range of blade geometries including both multi-facet and curvilinear blades.

4. Computational modeling within a finite element (FE) framework will be beneficial to study the deformation behavior during the drawing process. In particular, a FE model will provide a method to help optimize force profiles and velocity setpoints without the need for extensive experiments. The model can also provide a basis for understanding the stress distribution in the intersecting region between the two drawing actuators near the notched area in the BMG sample. Due to the complex material behavior near the glass transition region, it is necessary to adopt highly customized constitutive model [120,161,162]. It should be noted that material behavior at the large strain-rates experienced during drawing is currently not available in literature and therefore must be calibrated [75,163,164].

5. The exact mechanism that governs edge formation is still unclear. As the drawing process progresses, the critical dimensions of the necked region approach the nanometer-scale. Quantum size effects due to the significance of discrete atoms may begin to affect the results causing the continuum assumption in many classical mechanics theories to break down. At this scale, shear transformation zones (STZs),
which are described in Section 2.4.2, can be considered as the fundamental unit of deformation and act as a lower limit on sharpness during drawing. Due to the transient nature of STZs, it is difficult to measure the size of these events. It is estimated that these local clusters of atoms are \( \sim 30\text{-}100 \) atoms in size with a volume of \( \sim 0.5\text{-}3.7 \text{ nm}^3 \) [82]. It is recommended that the fundamental STZ mechanism be investigated along with the associated effects on the formation of the nano-scale cutting edge radius on the BMG blades.
References


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Appendix A

Micro-Scale Drilling of Bulk Metallic Glass

A.1 Experimental Design and Procedure

Micro-scale drilling experiments are performed on the Microlution 310-S micro machine tool. The BMG workpiece is mounted to an aluminum pallet using Aquabond thermal adhesive. These aluminum pallets are then kinematically coupled to the micro machine tool via magnets. A Kistler 9018 tri-axial load cell is internally embedded in the X-Y stage of the machine. The aluminum pallet also houses the acoustic emission (AE) sensor, which is spring loaded onto the top of the pallet. Dow Corning high vacuum grease is applied to the interface to improve AE transmission. A thermal IR camera is mounted on top of the micro machine tool and captures a video as the drilling operation occurs. A schematic of the drilling set-up is shown in Fig. A.1.

![Micro-scale drilling configuration](image)

**Figure A.1:** Micro-scale drilling configuration

The BMG investigated in this study is Vitreloy-1 with the chemical formula: $\text{Zr}_{41.2}\text{Ti}_{13.8}\text{Cu}_{12.5}\text{Ni}_{10.0}\text{Be}_{22.5}$. The sample workpiece, obtained from Liquidmetal Technologies,
is an oval BMG plate with overall dimensions of approximately 70 X 40 X 1.2 mm. Holes are drilled through the entire BMG sample and are spaced in increments of 2 mm.

KT-0200-S micro-drills obtained from Performance Micro Tool are used in this experiment to study BMG machinability. These drills are 508 μm diameter uncoated, tungsten carbide drill bits with two flutes and a flute length of 6.604 mm. A schematic of the microdrill [165] is provided in Fig. A.2.

![KT-0200-S drill bit schematic (mm) [165]](image-url)

**Figure A.2:** KT-0200-S drill bit schematic (mm) [165]

The two cutting parameters studied in the first experiment are cutting speed ($x_1$) and chip load ($x_2$), which dictate the revolutions per minute (RPM) and feedrate of a given machining operation. A central composite design (CCD) methodology with five different levels for each factor is adopted. The range of cutting conditions is chosen based on previous literature test conditions [151–154] as well as a preliminary machinability assessment of BMG. This experimental design allows a second-order regression model to be fitted to quantitative response data while performing the minimum number of experiments [166]. Figure A.3 provides the set of cutting conditions that are tested in this experiment.

![Central composite design cutting conditions](image-url)

**Figure A.3:** Central composite design cutting conditions
Since tool wear is a function of cutting conditions, it is crucial to first specify a combination of cutting speed and chip load that considers multiple machinability responses. For experiment 2, the set of cutting conditions is selected using a multi-criteria decision analysis (MCDA). A tool wear study is then conducted by drilling a total of 100 holes in the BMG sample.

Machinability responses selected for this study include machining performance measures as well as hole quality, crystallization, and tool wear.

**Machinability Performance Measures**

Machining performance measures are evaluated to identify the MCT and thermal softening-induced viscous flow mechanism that have been cited by Johnson et al. [153]. These machining performance measures, specifically acoustic emissions, are used to evaluate the effects of higher spindle speeds that are often associated with micro-scale machining processes. The combination of higher frequencies excited by the higher spindle speeds and BMG’s relatively low Young’s modulus may excite drill dynamics and intensify vibrations, leading to oversized holes and reduced overall hole quality.

Axial force is measured using a Kistler 9018 tri-axial load cell. Spindle load, which is used as an indirect measure of drilling torque, is measured from a sensor integrated inside the NSK Astro E500Z electronic spindle controller. Both signals are passed through a low-pass butterworth filter with a cut-off frequency of 300 Hz. Tool vibrations are analyzed via a Physical Acoustics Nano30 acoustic emission (AE) sensor during drilling. AE is defined as vibrations in the frequency range of 20 kHz to 2 MHz [167]. These high frequency vibrations originate from elastic waves generated by the release of strain energy in the form of shearing, ploughing, and rubbing during machining operations [167].

A thermal imaging IR camera, Electrophysics PV-320, is used to measure drilling temperatures by capturing a video of the drilling operation. Camera calibration is performed by fitting a cubic polynomial to calibration data that relates IR pixel intensity to drill temperature. The video capture of the drilling process from the IR camera is decomposed into a series of frames. These frames are scanned for the maximum intensity pixel that is then converted into a temperature using the calibration curve. An example frame is provided in Fig. A.4.
Chips created during the micro-drilling process are collected and subsequently inspected on a JEOL 6060LV SEM. Features such as chip discontinuity, localized shear bands, and chip serrations are inspected.

**Hole Quality**

Final hole quality is evaluated through average entry burr height and hole diameter. These parameters are measured using a Keyence laser displacement sensor that is scanned across the top surface of the drilled hole. The laser displacement sensor has a 1 μm spot size and 10 nm vertical height resolution. As the laser is scanned across the top surface, a 2½D image of the hole is captured. Thresholding is performed to separate the workpiece surface from the bottom of the hole. The laser scan data is then passed through a 2D Gaussian low pass filter to reduce noise during the scan. Figure A.5a shows the original laser scan and Fig. A.5b shows the filtered laser scan.
An outline of the hole is fit using a least-squares algorithm to find the center point and the diameter of the hole. The fitted circle is then segmented into $2^\circ$ pie slices, as shown in Fig. A.6. The maximum burr height is found for each pie slice and then averaged to find the overall average burr height of the hole.

**Figure A.5:** Laser scan profile (a) raw (b) filtered

**Figure A.6:** Entry burr height calculation
Crystallization

Crystallization is detected using x-ray diffraction (XRD) on a Panalytical/Phillips X’pert materials research diffractometer (MRD) system. The holes drilled in experiment 1 are cross-sectioned and a two-theta scan is taken of the inner drilled surface to detect crystallization. Sharp intensity peaks in the two-theta plot signify the degree of crystallization that has occurred in the sample [151].

Tool Wear

Tool wear is quantified using stereo-microscopy techniques to generate 3D models of the drill tip. Stereo-microscopy involves taking an image of a 3D specimen from two different perspectives. Each image is a projection of a 3D object onto a 2D plane. By taking a second image at a tilted angle, it is possible to recover information from the 3rd dimension. An additional 3rd image is used to automatically refine any user input parameters, such as the tilt angle. Images of the drill tool tip are taken on the JEOL 6060LV SEM. The microscope has a built-in tilt stage that is used to generate different perspectives of the drill tip. The three SEM images are loaded into the software package MeX for stereoscopic image processing. Figure A.7 shows the resulting 3D surface that is generated using these three images.

For the tool wear experiment, 3D models of the drill tip are generated in increments after drilling a set number of holes. For each dataset, 2D profile sections are taken at 10, 20, and 40
μm distances from the drill tip. To quantify the tool wear of the drill, the two quantities, flank wear, \( d_f \), and rake wear, \( d_r \), are defined as the distances from the intersection of the rake face and the flank face of an infinitely sharp drill to the corresponding point of tangency. This is a similar characterization scheme for drill wear employed by Park et al. for drilling of composite/titanium stacks [168]. The definition of flank wear and rake wear are illustrated in Fig. A.8.

![Figure A.8: Definition of flank wear and rake wear](image)

A.2 Experiment 1 Results

A.2.1 Quantitative Response Data

Table A.1 provides experimental test conditions and the set of quantitative drilling responses that are measured in this experiment, including temperature (T), axial force (\( F_a \)), spindle load (SL), acoustic emissions (AE), entry burr height (\( h_{\text{Burr}} \)), and hole diameter (\( \phi \)). Note that machining responses at design point 5 are inconsistent with the rest of the data, suggesting that the process mechanics have changed. According to Johnson et al. [153], these inconsistencies
result from the chip load being below the MCT. At low chip loads, there is a shift in cutting mechanics from shearing-dominated regime to a ploughing-dominated regime.

**Table A.1:** Experimental test conditions and responses

<table>
<thead>
<tr>
<th>Design Points</th>
<th>Factor Levels</th>
<th>Responses</th>
</tr>
</thead>
<tbody>
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<td>$x_1$ (m/min)</td>
<td>$x_2$ (μm)</td>
</tr>
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<td>1</td>
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<td>5.75</td>
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<tr>
<td>13</td>
<td>28.73</td>
<td>3.5</td>
</tr>
</tbody>
</table>

*This measurement is an outlier and is discarded.

Data collected are fitted with second-order regression models in order to observe trends and identify key phenomena occurring in the drilling process. The model is reduced by eliminating insignificant terms using statistical significance tests (P>0.1). The response surface contour plots for the adequate models are shown in Fig. A.9.
Figure A.9: Response surface contour plots (a) drill temperature (°C) (b) axial force (N) (c) spindle load (V) (d) acoustic emission (V) (e) average entry burr height (μm) (f) hole diameter (μm)
A.2.2 Analysis of Machinability Responses

Drill Temperature

The drill temperature contours shown in Fig. A.9a indicate that the combination of high cutting speed and high chip load produces the highest drilling temperatures. By far, it seems that cutting speed has the most significant effect on the drill temperature. Higher cutting speeds increase the overall cutting power consumed in the machining operation. With an increased rate of energy dissipation, temperature localization at the tool tip rises due to poor thermal conductivity of the workpiece.

A similar dependence on chip load is observed, but to a lesser degree. The higher chip load results in a larger undeformed chip thickness. The higher cutting force required for material removal increases the normal force on the tool, which increases overall friction. These frictional forces induce greater heat generation and higher temperature gradients.

Axial Force

At higher chip loads, the axial force generally increases due to the increased shear plane area. On the other hand, higher cutting speeds cause axial forces to decrease slightly. When the temperature of the BMG sample exceeds the glass transition temperature, the amorphous structure enters a super-cooled liquid state and begins to soften and exhibit viscous characteristics. Since it has been shown that higher cutting speeds yield higher temperatures, the lower axial forces are attributed to the thermal softening effect.

Spindle Load

Results for spindle load measurements are similar to the axial force measurements. Analogous to the axial force case, spindle load increases for the combination of high chip loads and low cutting speeds.

Figure A.10 shows a comparison of spindle load measurements for three different chip loads. It is observed that each response has two peaks. The first peak occurs at approximately 1.2 mm, which corresponds roughly to the thickness of the BMG plate. After this point the spindle
load slightly decreases, but then spikes again as the tool is retracted at approximately 1.8 mm. When chip load is set at 0.318 μm (design point 5 from Table A.1), it is observed that spindle load does not have a smooth waveform as seen in Fig. A.10. The MCT ratio has been cited to be approximately 0.2-0.3 for micro-machining of BMG [153]. Edge radii of the drills used in this experiment are measured to be in the range of 3-4 μm using stereo-microscopy techniques. This implies a MCT in the range of 0.6-1.2 μm. In the case where uncut chip thickness is 0.318 μm, ploughing dominates the machining response, inducing drilling instabilities and causing an unsteady spindle load response.

Figure A.10: Time-domain spindle load measurements: constant cutting speed (28.73 m/min), varying chip load

Acoustic Emission

The AE RMS response is shown in Fig. A.9d. The data reveals that the higher frequencies generated from higher cutting speed does not increase acoustic emission as expected. Rather, it is seen that the combination of low cutting speed and high chip load produce the highest
acoustic emission. Since lower cutting speeds correlate to lower temperatures, this phenomenon is again attributed to thermal softening at high temperatures.

**Entry Burr Height**

In conventional cutting processes, the entry burrs are typically smaller than the exit burrs, however, at the micro-scale, the opposite is true. This phenomenon is explained by the limitation of cutting edge sharpness on micro-drills. At the micro-scale, edge radius has a tremendous effect on machining due to the MCT ratio. As chip load approaches the MCT, the effective rake angle becomes more negative. With a decreased effective rake angle, the degree of ploughing increases, thereby increasing the entry burr formed [169]. This effect is exemplified for design point 5 in Table A.1, where the chip load is set at 0.318 μm. Employing the equations derived by Jun et al. [170], the effective rake angle is calculated to be approximately -76.7°. In this case, the uncut chip thickness is less than the MCT resulting in a highly negative effective rake angle. The ploughing induced in this test significantly increases the entry burr height formed.

**Hole Diameter**

The oversized holes are a result of the cutting action at the drill’s margin with the sidewalls that dominates at the micro-scale [171]. Error in hole diameter is attributed to both thermal and mechanical effects during drilling. Thermal effects due to heat generation during drilling are minimal because tungsten carbide possesses a low coefficient of thermal expansion. It is expected that thermal expansion accounts for less than 1.5 μm of the oversized hole diameter. One of the most critical problems encountered with micro-drills are their low rigidity. Since the rigidity of a drill is approximately inversely proportional to the fourth power of its diameter [172], wandering during initial penetration into the workpiece causes significant challenges. As a consequence, it is seen that for all of the holes drilled except for design point 5 from Table A.1, the hole diameter measured exceed the drill diameter.
Crystallization

X-ray diffraction techniques reveal information about the crystalline structure of a material, including average atomic spacing, crystal structure/orientation, presence of crystalline defects, etc. This information is obtained through the location and size of intensity peaks during a two-theta scan. The absence of sharp intensity peaks implies an amorphous atomic structure [151]. As described in the introduction, the Péclet number for micro-scale drilling of BMG is on the order of approximately 0.5-1.5, indicating a possibility of crystallizing the material surrounding the hole.

Holes in experiment 1 are drilled in a line such that all inner surfaces of the drilled holes are exposed when the BMG sample is cross-sectioned. The XRD test targets all of the inner drilled surfaces of experiment 1 in one scan. The XRD results are shown in Fig. A.11.

![XRD 2-theta scan](a) as-received (b) after drilling

It is seen that both the as-received BMG material and the inner surfaces of the holes exhibit broad peaks that are typically associated with amorphous materials. The slight differences between the scans shown in Fig. A.11a and Fig. A.11b are primarily attributed to the sample orientation inside the XRD instrument. The sharp intensity peaks, specifically ZrO₂, observed by Bakkal et al. [151] in crystallized BMG samples are not present in the x-ray diffraction scans taken.
here. This indicates that no crystallization occurs in the BMG sample for any of the tests performed during experiment 1.

Amorphous BMG exists at room temperature as a metastable material. The transformation into a lower energy crystalline state is possible by increasing the atomic mobility within a BMG sample. It has been shown in the past that BMG exhibits a time-temperature dependent relationship to crystallization [46]. Annealing the BMG at high temperatures above its glass transition temperature drastically increases the atomic mobility within the BMG sample and accelerates the rate at which it reaches a thermodynamically stable equilibrium (i.e. a crystallized structure). According to the time-temperature-transformation (TTT) diagram for BMG [46], the shortest time to re-crystallization occurs over a period of 50 seconds at a temperature of 850 K. Drilling processes are typically on the order of seconds and possess relatively short processing times. Although the temperature of the workpiece may have exceeded the glass transition temperature of BMG, there is not enough time for the structural relaxation of the BMG workpiece to occur.

Alternatively, the precipitation of nano-crystals has been observed in the presence of large strains within the super-cooled liquid regime of BMG. Material deformation leads to a stress-driven enhancement of atomic mobility. The combination of both deformation heating and strain effects increases the rate of strain-induced nano-crystallization [64]. Due to the relatively short time span of a drilling process, any crystallization is assumed to be entirely strain-induced. Since large strains primarily occur within the primary and secondary shear zones, the formation of BMG nano-crystals is restricted to within the BMG chip. Crystallization of the BMG workpiece is therefore unlikely to occur unless an exothermic oxidation reaction occurs.

**Chip Morphology**

Chips created during the micro-drilling process are collected and subsequently inspected on the SEM. Figure A.12 provides an overview of the most common chips for each of the tests performed at different cutting conditions. It is seen from these images that discontinuous chips are more prevalent for higher chip loads while ribbon/spiral continuous chips are prevalent for lower chip load cases. As the chip travels up the rake face of the tool, the chip begins to exhibit bending stress. Therefore, according to beam bending theory, as the chip thickness becomes
greater, bending stresses at the surface increase accordingly. These higher stresses induce material failure and create a higher likelihood for discontinuous chips.

There is one exception to this case at design point 5 from Table A.1, the lowest chip load tested. In this case, the chip load had a nominal value of 0.318 μm. This indicates that the MCT effect shifts the material removal mechanism from a shearing-dominated to a ploughing-dominated regime. Within the ploughing-dominated regime, the chips become discontinuous as well as begin to appear more impacted.

Chip serrations are also seen at the combination of high cutting speeds and high chip loads. The cases where chip serrations are observed correspond to the test conditions that exhibit the highest drilling temperatures. This type of chip formation is comparable to the chip morphology of other materials with similar low thermal conductivity, such as titanium and nickel-based alloys. As a result of the high heat generation and low thermal conductivity, thermal instabilities during machining cause chip serrations, which represent a cyclical pattern of high shear strain and low shear strain areas [155]. In addition, the presence of strain-induced nano-crystals helps facilitate the formation of chip serrations. These nano-crystals inhibit atomic mobility thereby causing stress localization and inhomogeneous deformation [62]. The low failure strain of these brittle crystalline regions provides a point of initiation of shear bands during the formation of serrated edges seen in the BMG chips.
Figure A.12: Chip morphology with varying cutting conditions: cutting speed (m/s)/chip load (µm) (a) 17.56/5.75 (b) 28.73/6.68 (c) 39.90/5.75 (d) 12.77/3.5 (e) 28.73/3.5 (f) 44.69/3.5 (g) 17.56/1.25 (h) 28.73/0.318 (i) 39.90/1.25

A.3 Experiment 2 Results

Tool wear is highly dependent on cutting conditions. When performing a tool wear study, it is critical to specify a set of cutting conditions that takes into account both machinability responses as well as productivity. Machining productivity is represented by material removal rate (MRR), given by Eq. A.1,
where FR is federate, A is the drill cross-sectional area, $\phi$ is drill diameter, T is number of flutes, $x_1$ is cutting speed, and $x_2$ is chip load. When considering both machinability and productivity, there are often several conflicting criteria. To find appropriate test conditions, multi-criteria decision analysis (MCDA) is developed here following the approach described by Derringer et al. [173].

Individual desirability functions are first constructed for each machining response. The three most common desirability functions are “minimize”, “maximize”, and “target”. These transformations accomplish two-fold; first to normalize the data such that all responses are scaled between 0 and 1, thereby eliminating units. Second, this transformation allows a manufacturer to set bounds on a response. Equations A.2, A.3, and A.4 define “minimize”, “maximize”, and “target” type desirability functions, respectively,

\[
MRR = (FR)(A) = \left[ \left( \frac{x_1}{\pi \phi} \right) (T)(x_2) \right]^\frac{\pi}{4} \phi^2 = \frac{(x_1(x_2)\phi)}{2}, \tag{A.1}
\]

\[
d_i = \begin{cases} 
0 & y_i < p \\
\frac{y_i - p}{q - p} & p \leq y_i \leq q, \\
1 & y_i > q 
\end{cases} \tag{A.2}
\]

\[
d_i = \begin{cases} 
1 & y_i < p \\
\frac{q - y_i}{q - p} & p \leq y_i \leq q, \\
0 & y_i > q 
\end{cases} \tag{A.3}
\]

\[
d_i = \begin{cases} 
0 & y_i < p \\
\frac{2(y_i - p)}{q - p} & p \leq y_i \leq \frac{p + q}{2}, \\
\frac{2(q - y_i)}{q - p} & \frac{p + q}{2} < y_i \leq q \\
1 & y_i > q 
\end{cases} \tag{A.4}
\]

where $d_i$ represents the individual desirability function, p and q represent the upper and lower limits of the desirability function, and $y_i$ represents the individual machining response.
An overall desirability response is computed through a weighted geometric mean, defined as,

$$\text{Desirability} = \left( \prod_{i=1}^{n} d_i^{w_i} \right)^{\frac{1}{\sum_{i=1}^{n} w_i}},$$

(A.5)

where $d_i$ are the individual desirability function and $w_i$ are the weighting factors.

The weighting on each term represents the relative importance of each response. This provides a convenient method for a manufacturer to specify the most important factors when combining all of the individual responses. The overall weighted response is then optimized to find desired cutting conditions. Table A.2 provides example desirability parameters and weighting factors for one possible desirability function. In this case, temperature is weighted heavily to minimize high temperature effects. Values for $p$ and $q$ are chosen to span the overall range of machining responses measured in experiment 1.

**Table A.2: Desirability transformation parameters**

<table>
<thead>
<tr>
<th>Response</th>
<th>Desirability Function</th>
<th>p</th>
<th>q</th>
<th>w</th>
</tr>
</thead>
<tbody>
<tr>
<td>T</td>
<td>Minimize</td>
<td>40</td>
<td>200</td>
<td>3</td>
</tr>
<tr>
<td>$F_z$</td>
<td>Minimize</td>
<td>10</td>
<td>24</td>
<td>1</td>
</tr>
<tr>
<td>SL</td>
<td>Minimize</td>
<td>0</td>
<td>0.5</td>
<td>1</td>
</tr>
<tr>
<td>AERMS</td>
<td>Minimize</td>
<td>1.5</td>
<td>2.5</td>
<td>1</td>
</tr>
<tr>
<td>$\phi$</td>
<td>Target</td>
<td>488</td>
<td>528</td>
<td>1</td>
</tr>
<tr>
<td>$h_{Burr}$</td>
<td>Minimize</td>
<td>0</td>
<td>1.3</td>
<td>1</td>
</tr>
<tr>
<td>MRR</td>
<td>Maximize</td>
<td>0</td>
<td>80</td>
<td>1</td>
</tr>
</tbody>
</table>

Using the desirability parameters tabulated in Table A.2, a desirability contour plot is created in Fig. A.13. The desirability plot shows a local optimum at a cutting speed of 20 m/min and a chip load of 1.25 μm.
A validation experiment is performed by drilling five additional holes using these optimized cutting parameters and comparing measured results with the regression model predictions. Table A.3 provides data from this experiment. These results show all experimental results match within 10% of model predictions.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Mean Value</th>
<th>Model Prediction</th>
<th>Percent Error (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>T (°C)</td>
<td>61.61</td>
<td>58.7</td>
<td>4.96</td>
</tr>
<tr>
<td>Fz (N)</td>
<td>14.01</td>
<td>14.55</td>
<td>3.76</td>
</tr>
<tr>
<td>SL (V)</td>
<td>0.14</td>
<td>0.131</td>
<td>7.041</td>
</tr>
<tr>
<td>AE_{RMS} (V)</td>
<td>2.035</td>
<td>1.919</td>
<td>6.048</td>
</tr>
<tr>
<td>φ (μm)</td>
<td>512.8</td>
<td>513.7</td>
<td>0.2</td>
</tr>
<tr>
<td>h_{burr} (μm)</td>
<td>0.481</td>
<td>0.5</td>
<td>3.819</td>
</tr>
</tbody>
</table>
Tool wear tests are performed using the cutting condition of 20 m/min cutting speed and 1.25 μm chip load. For this experiment, 100 holes are drilled in BMG using 508 μm tungsten carbide drills. Tool wear is analyzed as a function of the number of holes drilled in BMG. 3D stereoscopic images of the deterioration of the tool tip are shown in Fig. A.14. Figure A.15 provides a plot of the corresponding values of flank wear and rake wear as a function of number of holes drilled.

From Fig. A.15, it is seen that both rake wear and flank wear are most significant at the drill tip and gradually becomes smaller moving towards the center of the drill. This is expected because cutting speed varies with the radius of the drill. The highest cutting speed, and also
temperature, occur at the outer edges of the drill, or the margin of the drill, and therefore wear the fastest.

![Figure A.15: Tool wear vs. # holes drilled (a) flank wear (b) rake wear](image)

When comparing the rake wear and flank wear rates, they are approximately equal for the first 20 holes. This represents a wear-in period where the new sharp edge of the drill is rapidly worn down. After this point, it is seen that rate of flank wear levels off and begins to wear at a slower pace. Rake wear, on the other hand, increases drastically at an even higher rate. The acceleration in rake wear is attributed primarily to cratering. Evidence of attrition wear is also observed from rough worn surfaces. It is hypothesized that due to the low thermal conductivity of the BMG workpiece, the majority of heat generated from drilling is transferred through the drill. This results in high tool temperature, particularly along the rake face. Cobalt, the binder phase in tungsten carbide tools, has been cited to possess a relatively high diffusion coefficient in Zr-based BMG at high temperatures near the glass transition temperature [174]. Combined with the large pressures on the rake face experienced during drilling, a high rate of dissolution/diffusion of the tungsten carbide tool into the BMG chip is expected to occur [175,176]. On the other hand, flank wear most commonly results from abrasion of the cutting edge. Due to the thermal-softening induced viscous flow effect of BMG, abrasion will have a diminished effect. Similar situations where tool wear is dominated by rake face wear have been observed by Dearnley in the machining of steels using uncoated tungsten carbide tools [177].
A.4 Conclusions

The following specific conclusions can be drawn from this work:

1. Drilling temperature has a major role in dictating machining responses due to the BMG transition from an amorphous micro-structure to a super-cooled liquid state. As drilling temperatures rise above the glass transition temperature, the BMG starts to soften and exhibit viscous characteristics.

2. MCT is observed to shift cutting mechanics from a shear-dominated to a ploughing-dominated regime. Experimental data has shown significant changes in machining responses as chip load decreases. Evidence of drill instability is found in the spindle load response due to this effect.

3. The effective rake angle has a significant effect on entry burr height at small chip loads. As chip load approaches the MCT, the effective rake angle becomes highly negative. With a decreased effective rake angle, the degree of ploughing increases, thereby increasing the entry burr formed.

4. X-ray diffraction techniques reveal that no crystallization occurred in the BMG workpiece after any of the micro-drilling tests performed. High drilling temperatures increase the atomic mobility in the BMG, however, crystallization through this mechanism alone is unlikely due to the relatively short time frame of a drilling operation. Alternatively, strain-induced nano-crystallization is found to primarily affect BMG chips due to large strains experienced during drilling. Crystallization of the BMG workpiece is therefore unlikely to occur unless an exothermic oxidation reaction occurs.

5. It is found that rake wear dominates when performing micro-drilling on bulk metallic glass. This is attributed to two reasons: (a) high temperatures/pressures at the tool-chip interface during machining increases diffusion of cobalt into the BMG chip, accelerating crater wear (b) thermal softening-induced viscous flow lowers the hardness of BMG and reduces the effects of abrasion wear on the flank.
Appendix B

Testbed Automation Implementation

The automation platform is implemented on a Labview compactRIO (cRIO) platform. As outlined in Section 3.4.1, the automation software can be broken into three layers, namely, the host computer, the real-time (RT) processor, and the field programmable gate array (FPGA). This section will provide a detailed explanation of each layer followed by a description of the electrical components included in the system.

B.1 Host Computer Program

The host computer is the human-machine-interface (HMI) where the user may input commands to control the hybrid manufacturing testbed. In the following section, the host computer code for the event-driven graphical user interface (GUI), the queued state machine, and the data acquisition and display is described.

B.1.1 Event-Driven GUI

Commands found on the GUI in Fig. 3.23 are primarily set-up to transmit instruction packets to the RT processor. Each instruction packet is encoded in the form of a structure containing an enumerated constant representing a command and a variable of data type variant. Variant data types are a specialty data type found in Labview that can be converted to an arbitrary data type depending on the code (i.e. double floating point, integer, boolean, etc.). The variant data is a piece of data associated with the command. An example of a full instruction would be an enumerated command of “dwell” with the variant converted to a double floating point data type with value “2.000” that represents the amount of time to pause.
Transmitting the code is performed using Network Streams, which are Labview feature that allows communication between separate devices through a network. The code implementing network streams is coded using an event-driven programming structure as shown in Fig. B.1. This structure is programmed to capture any event, such as a button press or variable change, that the user inputs into the GUI interface. Once the event is captured, an appropriate instruction is generated and transmitted to the RT processor.

![Figure B.1: Host computer to RT processor network stream (write) code](image)

**B.1.2 Queued State Machine**

The primary process that the host computer software runs a queued state machine, which is shown in Fig. B.2. As stated in Section 3.4.1, the host computer possesses a GUI that contains a set of macros. These macros automate the set of instructions required to produce blades on the testbed. Whenever any macro in Fig. 3.24 is activated, an associated set of instructions are loaded into a first-in-first-out (FIFO) queue that drives the state machine. The queued state machine has been programmed such that instructions generated by the macros trigger the network streaming code in Fig. B.1 to transmit instructions down to the RT processor.

![Figure B.2: Host computer state machine code](image)
B.1.3 Data Acquisition and Display

The GUI also contains many indicators that display data gathered from the testbed. Data is gathered from the RT processor/FPGA layer and received through a network stream to be read by host computer. The data reading network stream code is given in Fig. B.3. The network stream simply receives a large array containing all of the data captured during the process, including temperature, position, velocity, force, etc. In addition, while the drawing process occurs, all of the data is logged into a *.csv file for further data processing.

Figure B.3: Host computer to RT processor network stream (read) code

The data read by the network stream in Fig. B.3 is then displayed on the GUI using the code provided in Fig. B.4.
B.2 Real-Time Processor Program

The RT processor is deterministic and therefore handles several processes that require precise timing as well as manages several miscellaneous tasks. In the following section, the RT processor code for data transmission, signal conditioning, temperature control, and motor control is described.

B.2.1 Data Transmission

Data transmission on the RT processor again uses the network streaming feature of Labview. The RT processor both receives instruction packets from the host computer while at the same time transmits data back. Each instruction packet received by the RT processor is deciphered and the commands and data are stored in local variables for further processing. The receiving node of the network stream is shown in Fig. B.5 and the transmitting node is shown in Fig. B.6. It should be noted that the transmitting code in Fig B.6 communicates data directly from the FPGA to the host computer.
In addition, data generated on the RT processor is transmitted to the FPGA, which is shown in Fig. B.7.
B.2.2 Signal Conditioning

RTD temperature data and LVDT position data are acquired from ADC channels on the cRIO modules. The data is collected in a raw integer format and therefore must be tuned according to voltage calibrations for the ADC channels as well as scaled to their appropriate units. Finally, since both these signals are originally analog signals, there is inherent noise in the signal, which is filtered out using an exponential moving average (EMA) filter. This filter output is defined as

\[ S_k = \alpha Y_k + (1 - \alpha)S_{k-1}, \]  

(B.1)

where \( S_k \) is the filter output at time \( k \), \( \alpha \) is the EMA filter coefficient, \( Y_k \) is the input measurement at time \( k \). Signal conditioning for the LVDT is given in Fig. B.8a and signal conditioning for the RTD temperature measurements is given in Fig. B.8b.
The supervisory controller developed for the drawing operation contains a force PID controller. For the manufacture of multi-facet/curvilinear blades, it is necessary to apply time-varying force profiles. This is implemented by generating a force setpoint trajectory based on user-selectable functions. Currently, the trajectory generator is capable of generating polynomial, power, exponential, logarithmic, sinusoidal, and hyperbolic functions. The code for implementing the trajectory generator is given in Fig. B.9.
B.2.3 Temperature Control

Temperature control is performed using a PID algorithm with a feedforward term that is proportional to the input setpoint temperature and is shown in Fig. B.10.

![Figure B.10: RT processor temperature controller code](image)

The PID algorithm is updated with a cycle time of 5 seconds. Since temperature can vary significantly over a period of 5 seconds, temperature integration must be performed over much shorter time increments to retain accuracy in the integral term. The temperature integral is calculated separately, as shown in Fig. B.11, and is updated every 25 ms. In addition, an integral reset function is implemented when the temperature signal crosses the setpoint. This minimizes integral wind-up when the controller is initially turned on.

![Figure B.11: RT processor temperature integrator code](image)

The output of the PID temperature controller is a duty cycle for a PWM generator. The PWM generator also possesses a cycle time of 5 seconds to minimize wear on the electromechanical relay driving the heaters. One example of a PWM generator is provided in Fig. B.12.
B.2.4 Motor Control

Control of the brushless DC molding actuator motor is performed on the RT processor. For the brushless DC motor module, Labview has several built-in functions to simplify the control of the actuator. The control can be split into synchronous and asynchronous motor commands, as shown in Fig. B.13a and B.13b, respectively.
Figure B.13: RT processor motor control code (a) synchronous (b) asynchronous
B.3 FPGA Program

As described in Section 3.4.1, the FPGA is a reconfigurable chip capable of implementing complex digital logic. Since the FPGA architecture is based inherently on hardware, the chip offers both parallel processing and high performance. This is particularly beneficial for implementing the customized supervisory control algorithm described in Section 3.4.2. FPGAs, however, possess significant limitations in both the available on-chip memory as well as data type availability. In particular, floating point data is not supported and therefore any numerical calculations are implemented in fixed-point variables. In the following section, the FPGA code for data transmission, signal conditioning, timing, supervisory control is described.

B.3.1 Data Transmission

The RT processor and the FPGA are connected within the same chassis and therefore network streaming is not required. Variables defined in the FPGA can be accessed directly in the RT processor code. In addition, the physical I/O channels may be accessed directly from the FPGA. Figure B.14 shows the code for reading RTD temperature and LVDT measurements and writing Boolean commands to switch the relays controlling mold heaters.
Figure B.14: FPGA I/O code
B.3.2 Signal Conditioning

Since both force and position data are required by the supervisory controller, signal conditioning for these two signals must be performed directly on the FPGA. First, the quadrature encoder signal from the drawing actuator is decoded into an integer count variable in Fig. B.15.

![FPGA quadrature encoder code](image)

**Figure B.15:** FPGA quadrature encoder code

Once the position count is determined, the position is scaled into mm units. In addition, force sensors are calibrated from the raw integer data. This signal conditioning code is provided in Fig. B.16.
B.3.3 Timing

The force setpoint trajectory generator described in Section B.2.2 requires an accurate measurement of time during the drawing operation. The timer code is implemented manually to ensure synchronization between the different processors and is shown in Fig. B.17.

Figure B.16: FPGA signal conditioning code
B.3.4 Supervisory Controller

The supervisory controller described in Algorithm 3.1 is implemented directly on the FPGA, as shown in Fig. B.18. On the FPGA, it is possible to set the control loop frequency to 2 kHz. The FPGA possesses only a set number of multiply blocks to perform all numerical multiplication. Therefore, in addition to limitations in memory, there are also limitations on the number of multiply blocks available. In this code, there are several multiply blocks that are multiplexed to conserve both memory space and multiply blocks.

In manual mode, the drawing actuators are capable of position, velocity, force, and open loop control, which are user-selectable. Initially, the integrals and derivatives of position, velocity, and force are calculated. It should be noted that velocity, acceleration (derivative of velocity), and force signals are filtered using EMA filters to reduce noise. Depending on which controller type is selected, the appropriate gains are then multiplied to produce a control effort that is transmitted to the DAC to drive the voice coil drawing actuator. The control effort has a user-defined saturation limit as well as an actuator enable signal to turn the voice coil amplifier on/off.

In the case of supervisory control, both velocity and force PID contributions must be calculated simultaneously. Therefore, two multiply blocks are present to calculate both contributions. Following Algorithm 3.1, these two PID contributions can be linearly interpolated during the drawing operation.

Figure B.17: FPGA timing code
Figure B.18: FPGA supervisory controller code
B.4 Electrical Components

While much of the computational processing is performed in the cRIO system, there are several electronic components required for the hybrid manufacturing testbed. In particular, there is a need for high power amplifiers, power distribution, and transducer signal conditioning. The electronics board for the testbed is shown in Fig. B.19. In the following section, the electronics for the hybrid manufacturing testbed are described.

![Figure B.19: Electronics board for the hybrid manufacturing testbed](image)

B.4.1 CompactRIO

The compactRIO is a modular automation platform that includes up to eight I/O modules that can be reconfigured based on application needs. For the hybrid manufacturing testbed, the following modules are installed.
• 1X NI 9514 – This is a brushless DC servo drive interface used to generate control signals for the molding actuator.

• 2X NI 9505 – These are H-bridge DC brushed servo drive interfaces used for the voice coil actuators in the drawing stages. The specifications on the H-bridge are not sufficient for the voice coil so these modules are primarily used to collect encoder signals for position/velocity feedback control.

• 1X NI 9263 – This is a ±10V analog output module. This module outputs the control signals for the voice coil actuators as well as the enable signals for the amplifiers.

• 2X NI 9217 – These are PT100 RTD analog input modules used to acquire temperature measurements on the testbed.

• 1X NI 9215 – This is a ±10V analog input module. This module acquires the force sensor signals as well as the LVDT signal.

• 1X NI 9472 – This is a 24V digital I/O module that is used to drive the electromechanical relays for the heaters and pneumatic solenoid for the two clamps on the drawing actuators.

B.4.2 Molding Components

The control signal generated by the NI 9514 module is connected to a Parker Aries amplifier that generates the 3-phase motor power signals. In addition, the LVDT is connected to a Macro Sensors DMI-A1 signal conditioner that converts the LVDT signal into a ±10V signal corresponding to linear position.

B.4.3 Drawing Components

The drawing actuators use BA Series PWM Amplifiers from Aerotech. These PWM power signals are switched with a carrier frequency of 20 kHz. Since the L/R time constant of the voice coil motor is so small, the PWM signal actually causes ringing in the motor. To dampen these vibrations, several fixed core inductors are connected in series with the voice coils to
increase the inductance and thereby increase the L/R time constants. This slows down the
transients of the electrical circuit and reduces the high frequency vibrations caused by the
PWM switching signal. The Stellar Technology VLC856 button load cell sensor is amplified
by a Sensotech DV-10 bridge amplifier and then fed to the analog input module in the
compactRIO.

B.4.4 Temperature Components

Temperature control is accomplished using PWM control of a series of cartridge heaters
located throughout the testbed. These cartridge heaters operate from 120V AC power.
Electromechanical relays switch power on/off in a PWM configuration with carrier frequency
of 0.2 Hz. RTD temperature sensors are connected directly to the compactRIO in a 3-wire
configuration.