ABSTRACT

GHOSH, ANGSHUMAN. Temperature Prediction in Ultrasonic Microextrusion. (Under the direction of Dr. Gracious Ngaile.)

Micro-extrusion is an important option for mass production of micro parts such as micro-pins, micro-gears, micro-shafts, etc. for various medical, electrical and electro-mechanical applications. Although there are a number of technical challenges to produce micro-parts due to size effects, material handling, anisotropy, severe tribological conditions, etc.; the application of ultrasonic energy could alleviate some of these challenges. There are experimental results showing that in microextrusion process, the application of ultrasonic energy results in significant temperature rise of the billet and the finished product, mostly due to the friction heating from material-die relative velocity and also due to the plastic deformation heating. This temperature rise has significant effects on the load reduction, surface finish, material property, lubrication, etc. Therefore, there arises the need for a model to predict this temperature rise during ultrasonic microextrusion, which can yield better control of the process variables to fully utilize the potential of ultrasonic energy. Some important applications of the controlled elevated temperature are fabrication of micro parts from difficult to form material, in-process heating for superplastic material flow, etc.

In this research, a model for temperature prediction in ultrasonic microextrusion is developed, where the temperature rise is mainly caused by the plastic deformation and the frictional energies. In order to develop this model a number of variables/phenomena affecting the ultrasonic microextrusion process are addressed. They are ultrasonic wave propagation and transmissibility, surface roughness of the billet, trapped lubricants, friction conditions at the die-workpiece interface, plastic deformation characteristics of the billet, and heat transfer.
In order to determine the plastic deformation energy, an elastic-plastic finite element based deformation model is developed considering the governing factors such as the material property, the role of the trapped lubricants in the surface roughness pits of the billet, etc. This role of trapped lubricants is treated with micro plasto lubrication theory along with a modeling of the actual surface roughness by a regular trapezoidal asperity surface which retains the basic features of the surface roughness such as pits to hold the lubricants. This deformation model also provides the information about the die-workpiece contact area required for the friction analysis. This friction analysis also requires the information about the die-workpiece relative movement and engagement-disengagement which are governed by the ultrasonic wave propagation and transmission through the ultrasonic microforming set-up. Numerical structural analyses along with experimental data are used to quantify the wave phenomena. Then the heat energy is estimated as a fraction (90%) of plastic deformation and frictional energies. Finally the temperature distribution in the die-workpiece interface is obtained by the numerical transient thermal analysis.

From the model it is observed that for ultrasonic microextrusion process, the friction energy is the dominating factor for the temperature rise. A maximum temperature rise of 98°C is predicted for ultrasonic assisted (20 kHz, 67% amplitude at ultrasonic generator) forward extrusion of a Brass sample. The comparison with the experimental data shows the model overestimates the temperature rise by 9% at the 83% completion of the process. This can be principally explained by the fact that there is an overall improvement in the friction condition during the process, whereas a constant friction condition is considered in the model.
Temperature Prediction in Ultrasonic Microextrusion

by
Angshuman Ghosh

A dissertation submitted to the Graduate Faculty of North Carolina State University in partial fulfillment of the requirements for the Degree of Doctor of Philosophy

Mechanical Engineering

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2011

APPROVED BY:

__________________________________________  ______________________________
Dr. Gracious Ngaile                              Dr. Kara Peters
Chair of Advisory Committee

__________________________________________  ______________________________
Dr. Tiegang Fang                                Dr. Yong Zhu
DEDICATION

In loving memory of my great-grandmother Smt. Gyanadasundari Saha.
BIOGRAPHY

Angshuman Ghosh was born on 1981 in Haluaghat, Bangladesh. He received BSc degree in Mechanical Engineering from Bangladesh University of Engineering & Technology (BUET). He was a research scholar at National University of Singapore (NUS), from where he received MEng degree in Mechanical Engineering. He enrolled into the Department of Mechanical & Aerospace Engineering at North Carolina State University (NCSU) in the Fall of 2007. After completion of PhD program, his objective is a career in the research & development sector.
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CHAPTER ONE: INTRODUCTION

1.1 ULTRASONIC MICROEXTRUSION AND RESEARCH MOTIVATION

The ever-increasing need of micro-part for various medical, electrical, electro-mechanical applications has initiated the search for economic and efficient metal forming process. Micro-extrusion can be a very important option for mass production of micro-pins, micro-gears, micro-shafts, etc. [1]. However, there are a number of technical challenges to produce micro-parts due to size effects, material handling, anisotropy, severe tribological conditions, etc. There is a potential to alleviate some of these challenges by using ultrasonic vibrations. In ultrasonic assisted microextrusion process, the microforming load can be reduced up to 23% with better part surface quality [2].

Although there are reported applications of ultrasonic energy in micro forming process, the full potential of this energy is yet to be explained. There are experimental results showing that in microextrusion process, the application of ultrasonic energy results in significant temperature rise of the billet and the finished product, mostly due to the friction heating from material-die relative velocity and also due to the plastic deformation heating. This temperature rise has significant effects on the load reduction, surface finish, material property, lubrication, etc. Moreover it can make it possible to fabricate micro-parts from difficult to form material which has better flow characteristics at elevated temperature. However, to fully utilize the potential of ultrasonic energy in microextrusion process and to ensure better control over the process variables, modeling of the process for temperature prediction is imperative. The controlled temperature also can provide in-process heating for superplastic material flow which requires a heating of the superplastic material up to 40% of its melting temperature. This research could also provide useful information on the selection of the lubricants for the ultrasonic microextrusion process considering their properties may change due to temperature rise.
1.2 RESEARCH PROBLEM STATEMENT

In the ultrasonic microextrusion process, the ultrasonic energy is imposed by an ultrasonic transducer. Figure-1.1 shows a schematic of an ultrasonic microextrusion set-up where the punch forces the workpiece to move in a constant speed opposite the imposed ultrasonic wave propagation direction. In solids, sound waves can propagate in four principle modes that are based on the way the particles oscillate such as longitudinal waves, shear waves, surface waves, and in thin materials as plate waves. Although the ultrasonic transducer is transmitting wave energy in the longitudinal direction of the system, it can be transformed into another form. For example, when a longitudinal wave hits an interface at an angle, some of the energy can cause particle movement in the transverse direction to start a shear (transverse) wave. However, for this problem statement, the two most widely observed propagation modes namely longitudinal and shear modes are discussed. Another important factor to be considered is the possibility of encountering wave propagation in the punch assembly. This propagation needs to be minimized while designing the system in order to ensure that the punch and the workpiece do not oscillate with the die.

Figure-1.1: Schematic of Ultrasonic Microextrusion Process
At a given frequency, the particle motion (figure-1.2, enlarged view of section-A from figure-1.1) at any point in an ultrasonic wave is sinusoidal if the stresses developed in the waves remain in the linear elastic range of the medium. Depending on the solid structure, this particle motion can be in either of longitudinal or shear direction. Figure-1.2 also shows two different zones namely inlet (section-B) and work (section-C) zones which are enlarged (figures 1.3 and 1.4) to show the microscopic phenomena occurring at the die-workpiece interface.

In the inlet zone, the case of smooth die surface and longitudinal wave propagation results in the surface roughness trapping the liquid lubricant as shown in figure-1.3a. However, for the same condition, shear wave propagation yields repeated closing (figure-1.3b) and opening (figure-1.3c) of the pits depending on the imposed ultrasonic energy and hence the direction of the die velocity $V_D$. 

Figure-1.2: Enlarged View of Workpiece and Die in Microextrusion Process

Figure-1.3: Enlarged View of Inlet Zone Die-Workpiece Interface
As the workpiece enters to the die work zone, the surface roughness pits will start to contract. Unlike the inlet zone, for both the longitudinal and the shear wave propagation the pits will open and close repeatedly. Although for shear wave propagation the opening-closing of pits are similar to that of the inlet zone, for the longitudinal wave propagation it depends on both the properties of imposed ultrasonic energy and the extrusion speed. Consider three cases shown in figure-1.4, where \( V_E \) and \( V_D \) denote the extrusion and die velocities respectively and extrusion direction is the positive direction. In the first case (figure-1.4a) where \( V_E < V_D \), the pits are open, the asperity top surfaces are not in contact with the die surface and the liquid lubricants can escape from the pits. In the second and third cases (figures 1.4b and 1.4c) where \( V_E > V_D \), the pits are closed, the asperity top surfaces are in contact with the die surface and the liquid lubricants are trapped inside the pits. The trapped lubricants will exert pressure as shown in figure-1.5 (enlarged view of section-E from figure-1.4b), depending on their properties, process parameters and also the change of roughness pit size. This will cause micro plasto hydro lubrication (static and/or dynamic) which has significant influence on the deformation characteristics of the surface roughness asperities. From the above discussion, for both the inlet and work zone, the contact between the asperity top surface and the die surface are intermittent which depend primarily on the ultrasonic energy. Although the die surface is assumed smooth compared to the workpiece surface, the actual contact area between the asperities and the die is influenced by the actual surface roughness of the die.
Moreover the onset of micro plasto hydro lubrication tends to reduce the actual asperity-die contact. All of these phenomena affect the friction conditions and hence the frictional energy.

The ultrasonic energy can change/domi...
cavitation bubbles oscillate and grow due to the ultrasonic energy and eventually collapse creating localized hot-spots where the temperature and the pressure can reach in excess of 5000 K and 500 atm respectively [3]. Another important phenomenon is the circulation of fluid occurring in the vicinity of bubbles set into motion by oscillating sound pressure. The repeated increase-decrease in bubble size causes rapid fluctuations in the magnitude and the direction of fluid movement, and hence results in a significant shear force which eventually erodes the surface by removing the particle from it (figure-1.6, enlarged view of section-D from figure-1.4a). Due to rapid movement of liquid lubricant at the interface, the lubricant transport is improved. All these phenomena affect the surface roughness of the workpiece and also the die, and hence change the frictional behavior.

During the ultrasonic microextrusion, both the plastic deformation and the frictional energies are converted to heat energy and transferred to the workpiece, lubricant, die assembly and adjacent ambient. This heat energy changes the deformation characteristics of the workpiece, lubricant characteristics and hence friction condition of the process.

From the above discussion a number of variables/phenomena pertaining to ultrasonic microextrusion process are identified and they are presented with possible solution methodologies in the table-1.1.
Table-1.1: Variables/Phenomena with Possible Solution Methodologies

<table>
<thead>
<tr>
<th>Number</th>
<th>Variables/Phenomena</th>
<th>Solution Methodology</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Ultrasonic Wave Propagation and Transmissibility</td>
<td>Numerical Method with Experiments</td>
</tr>
<tr>
<td>2</td>
<td>Surface Roughness of the Billet</td>
<td>Analytical Model based on Experimental Investigation</td>
</tr>
<tr>
<td>3</td>
<td>Trapped Lubricants</td>
<td>Micro Plasto Hydro Lubrication</td>
</tr>
<tr>
<td>4</td>
<td>Lubricant Transportation</td>
<td>Micro Fluidics</td>
</tr>
<tr>
<td>5</td>
<td>Sonochemical Effects on the Liquid Lubricant</td>
<td>Sonochemistry and Cavitation Model</td>
</tr>
<tr>
<td>6</td>
<td>Particle Erosion from Surface</td>
<td>Microstreaming Theory from Ultrasonic Cleaning</td>
</tr>
<tr>
<td>7</td>
<td>Friction Condition at Die-Workpiece Interface</td>
<td>Analytical Model</td>
</tr>
<tr>
<td>8</td>
<td>Plastic Deformation Characteristics of the Billet</td>
<td>Finite Element Deformation Model</td>
</tr>
<tr>
<td>9</td>
<td>Heat Transfer</td>
<td>Numerical Transient Thermal Analysis</td>
</tr>
</tbody>
</table>

### 1.3 RESEARCH OBJECTIVE

The main objective of the current research is to develop a model to predict the temperature generation during the ultrasonic microextrusion process. In order to achieve this objective, the following issues from the above stated research problem would be addressed.

- Ultrasonic Wave Propagation and Transmissibility
- Surface Roughness of the Billet
- Trapped Lubricants
- Friction Condition at Die-Workpiece Interface
- Plastic Deformation Characteristics of the Billet
- Heat Transfer
1.4 DISSERTATION ORGANIZATION

The main objective of this research is to develop a model for the temperature prediction in the ultrasonic microextrusion process. Therefore a literature review is conducted in the second chapter to establish the necessary background. Then in the third chapter the approach taken for this research is discussed, which is followed by three major chapters describing the research works. Chapter four, starting with the design of an ultrasonic microforming test set-up, discusses the structural analysis of that set-up using both numerical and experimental ways to provide the necessary information for the temperature prediction model. In order to establish the basis for the temperature prediction model, an experimental study of the ultrasonic microextrusion process is conducted in chapter five. The data from this chapter is also used in later chapter for comparison with the predicted temperature. Chapter six discusses the development of the temperature prediction model. Conclusions from this research are drawn along with the future works in chapter seven. Finally the dissertation ends with publication list, references used for this research and an appendix containing the derivation of the elastic-plastic finite element model.
CHAPTER TWO: LITERATURE REVIEW

2.1 INTRODUCTION

The intent of this chapter is to review the literatures in order to establish the necessary theoretical background to address the research problems under the scope of the research objective of temperature prediction in ultrasonic microextrusion. Since the effective use of ultrasonic energy requires the knowledge of wave propagation and associated phenomena, the first section of this chapter focuses on that, followed by the use of ultrasonic energy in the metal forming with special interest in the extrusion process. As discussed earlier, in the microforming processes severe tribological conditions are observed mainly due to the size effects. Therefore tribology in microforming is also reviewed. Then literatures on heat generation and temperature prediction in metal forming especially in extrusion process are reviewed. Finally a review on the sonochemical and ultrasonic cleaning effects from other fields of applications is conducted. Although sonochemistry and ultrasonic cleaning phenomena are not under the current research scope, this review is valuable for shaping the future research direction in ultrasonic microforming.

2.2 WAVE PROPAGATION

An ultrasonic wave can be explained as an infinite number of oscillating masses or elements connected by the elastic springs having each element influenced by the motion of its nearest neighbor. At a given frequency, the particle motion at any point in an ultrasonic wave is sinusoidal if the stresses developed in the waves remain in the linear, elastic range of the medium. This ultrasonic wave propagates through the medium at the velocity of the sound which is governed by the type of the oscillatory motion (longitudinal, shear, etc.), the elastic properties and the density of the medium, and the mode of the vibration. Since this research is about application of ultrasonic energy in metal forming process, the discussions are
concentrated on wave propagation in the solids, where longitudinal and shear wave propagations are defined by the particle motions in the parallel and normal to the propagation directions respectively [4]. Apart from these two types of wave propagation, surface waves such as Love wave and Rayleigh wave, and Lamb wave in thin plates are of interest in the ultrasonic applications [5, 6]. In the Love waves, the particle motion is normal to the propagation direction which is polarized in the plane of the surface. The Rayleigh waves have both longitudinal and shear components where the shear component is normal to the surface and the particle motion is elliptical.

A set of equations of motion (rectangular co-ordinates) for an isotropic elastic medium is derived (equation-2.1) [7]. In ideal situations, the solution of these equations with appropriate boundary conditions is sufficient to treat any vibration or stress propagation problem. Although the exact solution for an infinite cylinder was obtained by Pochhammer (1876) and independently by Chree (1889), the numerical solutions became possible only in the 1940s. Pochhammer solution gives answers close to the truth for a real finite rod as long as the rod length is large enough compared to the rod diameter, since the exact solution of the finite rod is not possible. This is a classic example of complexity of vibration analysis where a simple case of finite rod can not be treated fully analytically.

\[
\rho \frac{\partial^2 u}{\partial t^2} = (\lambda + \mu) \frac{\partial \Delta}{\partial x} + \mu \nabla^2 u
\]

\[
\rho \frac{\partial^2 v}{\partial t^2} = (\lambda + \mu) \frac{\partial \Delta}{\partial y} + \mu \nabla^2 v
\]

\[
\rho \frac{\partial^2 w}{\partial t^2} = (\lambda + \mu) \frac{\partial \Delta}{\partial z} + \mu \nabla^2 w
\]

(2.1)
Where, \(u\) is the displacement in the \(x\)-direction, \(\rho\) is the density of the material, \(\lambda\) and \(\mu\) are two elastic constants known as Lame’s constants, \(\Delta\) is called dilatation of a unit cube and defined as \(\left(\frac{\partial u}{\partial x} + \frac{\partial v}{\partial y} + \frac{\partial w}{\partial z}\right)\), the operator \(\nabla^2\) is defined as \(\left(\frac{\partial^2}{\partial x^2} + \frac{\partial^2}{\partial y^2} + \frac{\partial^2}{\partial z^2}\right)\).

After Pochhammer, a number of ideal geometries are investigated for both the free and forced vibration conditions. Gazis (1958) provided the exact solution of the vibration equations for the infinite hollow cylinder using the Bessel’s functions which showed similar results as Pochhammer infinite solid rod if the inner radius tends to zero. More recently the classic zero body force elastic bar is treated for a non-local material property (body force exists) using the infinite Fourier series [11]. The wave propagation through an interface between two media is more complicated, since the wave energy is divided into both of the media. How the wave approaches to the interface and the acoustic properties of the propagating media is dependent on the type of the incident wave. Tso and Hansen (1995) derived a generalized method for the analysis of the wave propagation in cylinder coupled to different types of plate configurations. Although the exact solutions of the idealized vibration problems remain as the benchmark for any numerical approach, the advent of computerized numerical analysis eases the burden of extensive calculations providing reliable solutions for the real problems.

### 2.3 ULTRASONIC METAL FORMING

Ultrasonic energy has long been introduced in metal forming [13]. Pohlman and Lehfeldt (1966) reported that when a sound impulse is applied, the drawing force immediately drops to a lower level, at which it remains for the duration of the impulse (figure-2.1). Moreover, they explained the reduction of friction in terms of breaking of microscopic welds by inducing high alternating stress amplitudes which cause fatigue fractures and eventually all the unevenness are worn away. Siegert and Ulmer (2001) also reported reduction of drawing
force as mainly a function of the ultrasonic amplitude and also smoother surface of the part. They explained that the vibration leads to a separation between die and workpiece and improves the properties of the lubricant and hence leads to better surface properties in ultrasound assisted tube drawing.

Mousavi et al. (2007) claimed that if the extrusion speed is below a certain critical speed, the extrusion force and the material flow stress would be reduced by applying the ultrasonic vibrations. According to them, if \( a \) and \( f \) are the amplitude and frequency of the applied sinusoidal vibration, that critical speed is equal to the maximum vibration speed \( V_{\text{ul}}(t_{\text{max}}) = 2\pi af \). When the extrusion speed is less than the maximum vibration speed (figure-2.2b), the die could be separated from the material (figure-2.2a). However, they would resume contact after a certain period of time. This contact and separation repeat during the process and hence results in decreasing the extrusion force due to reduction of friction forces.
In the case of ultrasonic assisted microextrusion processes, Ngaile and Bunget (2008) reported reduction in the forming load (figure-2.3) as 12 – 18% for forward extrusion (FE), 7 – 23% for double cup extrusion (DCE) and 3 – 13% for forward-backward cup extrusion (FBCE). They have also reported better lubrication conditions and better surface finish for such processes.
There are also a number of works showing the reduction of force and improvements of friction conditions due to the imposed ultrasonic energy in different metal forming processes such as upsetting, press forming, etc. [17-23].

2.4 MICROFORMING

Although macro forming has established itself as a vastly accepted industrial process due to its good surface quality, high accuracy and high manufacturing efficiency; micro forming has so far only established limited applications. This is mainly due to so called size effects which prevent the application of known technology from the macro world into the micro world [1]. For example, in the microforming processes the grain size becomes comparable to the part size [24]. There are also issues such as anisotropy, material handling, etc. More importantly microforming processes generally have severe tribological conditions. A comprehensive review of the state-of-the-art of microforming technologies and its challenges can be found in [25] and [26]. There are applications where lithographic technologies such as LIGA-processes can successfully produce microparts; however the relatively high cost and limited pool of applicable material restricts its application [26]. Therefore significant research efforts have been taken to develop suitable microforming technology. Saotome and Iwazaki (2000) developed a superplastic forward extrusion machine and formed microgear shafts of Al-78Zn super-plastic alloy with 10 µm module and 100 µm pitch circle diameter.
Cao et al. (2004) studied the grain size effects on microforming by extrusion of cartridge brass (Cu/Zn: 70/30). They found that the flow stress increases with decreasing grain size. However they noticed that the flow stress is independent of the part size, only if the part size is more than twice of the grain size of the workpiece material.

Since the ratio between the grain size and the workpiece dimension controls the size effect on the flow stress, conventional FEA simulation based on continuum mechanics cannot accurately predict material deformation. One way to deal with this is to divide the geometry
of a 2-dimensional workpiece into two areas, namely surface area and inner area. Then the flow stress for the surface area grains is assigned based on the flow stress property of the individual grain. Using this approach, good agreement between the results from simulation and experiment for the cylindrical upsetting process was observed. [29]

The process parameters in micro level tend to scatter due to the fact that a small number of grains are involved in the process which makes the process highly dependent on the individual behavior and orientation of the grains. Eichenhueller et al. (2007) experimentally found that scattering effects are reduced if microforming is carried out at elevated temperature. They used CuZn15 and stainless steel X4CrNi18-10 for upsetting, lateral extrusion and can backward extrusion process where the temperatures were elevated up to 400°C. The flow stress value as well as the scatter decreases with increasing process temperature. Similarly, the variation in the micro hardness properties can be reduced significantly if microparts are formed at elevated temperatures. Hence a homogeneous material property obtained at elevated temperature operation enhances the process stability and reliability in microforming process. Finally the researchers [30] reported a substantial improvement in material formability if microforming is carried out at elevated temperature.

Some of the major challenging issues in microforming include preparation of a well defined length of a billet/blank, gripping, transferring, and positioning of the billet in the forming set-up. [25]

2.4.1 Tribology in Microforming

The severe tribological conditions observed in the microforming process are basis for many important researches. The friction increases with decreasing part size and this is explained by the model of open and closed lubricant pockets [25, 31, 32]. As shown in figure-2.6 for the open lubricant pockets the forming load acts directly on the surface roughness peaks and thus
increases the friction. However, for the closed lubricant pockets, the trapped lubricant helps to transmit the forming load and thus decreases the friction. When the ratio of the open and closed lubricant pockets decrease with decreasing part size, friction increases severely.

Figure-2.6: Open and Closed Lubricants Pockets [25]

Figure-2.7: Size Effects on Areas with Open and Closed Lubricant Pockets [25]

The conventional FEA simulation does not consider the surface roughness features which are acceptable for macro forming processes. However in microforming processes such as micro-deep drawing the surface roughness of the blank is comparable to the thickness of the blank and therefore it can no longer be ignored in the FE model. Manabe et al. (2008) proposed a cyclic concavo-convex configuration for the surface elements of the blank and the tools,
where the variable values of $R_z$ for the simulation was the measured average surface roughness. Using a commercial software LS-DYNA, they observed a good agreement between the simulation and experiment surface roughness values for a two stage micro deep drawing process to form 500 µm diameter cups. They also reported that the resultant surface roughness decreases for tension deformation, whereas it increases for compression deformation.

![Figure-2.8](image)

Figure-2.8: Surface Roughness Model of Blank Undersurface in Micro-Deep Drawing Process [33]

The role of the lubricant trapped in the surface roughness pits for metal forming process has drawn attention to many researchers [34-38]. It plays an important role on the resultant surface roughness of the finished product. Based on different experimentation procedure, a number of researchers developed theories to explain the trapped lubricants behavior. Bech et al. (1999) performed the experiment procedure developed by Azushima (1995) in order to study the behavior of the trapped lubricant. With artificial pyramidal and flat-bottomed pyramidal shape macro-size lubricant pockets on aluminum strips, they observed the behavior of the trapped lubricants in plane strip drawing (figure-2.7). Calculating the volume change of the pockets by measuring the base area change of the pockets, they explained two phenomena, namely Micro Plasto Hydrodynamic and Hydrostatic Lubrication (MPHDL and MPHSL).
As the pockets containing lubricants contract during the plastic deformation of the strip, the hydrostatic pressure builds up in the lubricants. This hydrostatic pressure could be estimated by combining the reductions in pocket volume with the pressure-density data [40] for the particular lubricant. Figure-2.8 shows the hydrostatic pressure build-up with the axial position of the sealed pocket.

![Figure-2.9: Schematic of Trapped Lubricant [39]](image)

From the experimental observations, hydrodynamic pressure increase (equation-2.2) can be attributed to the escape of the lubricant from the pockets (figure-2.9). By solving the Reynolds equation (equation-2.3) for a plane-strain stationary condition the pressure rise can be calculated.

![Figure-2.10: Pressure Distribution [39]](image)
\[ q_{\text{dyn}} = p_r - q_0 \]  
\[ \frac{dq}{dx} = 6\eta u \frac{h - h_m}{h^3} \]

where, \( q(x) \) is the local pressure in the lubricant film and \( h(x) \) is the local film thickness. \( h_m \) is the film thickness outside the pocket, \( x = 0 \), where the plateau is parallel with the tool surface, and \( dq/dx = 0 \). \( \eta = \eta_0 \exp(\alpha q) \) is the pressure-dependent dynamic viscosity and \( u \) is the sliding speed against the tool surface.

Micro Plasto Hydrostatic Lubrication (MPHSL) occurs when the hydrostatic pressure in the trapped lubricant exceeds the tool/workpiece interface pressure causing forward escape of the trapped lubricant (figure-2.10a). Micro Plasto Hydrodynamic (MPHDL) occurs when the hydrodynamic pressure increase (using Reynolds equation) exceeds the required pressure increase to overcome rear pressure causing the backward escape of the trapped lubricant (figure-2.10b). Since the increased hydrostatic and/or hydrodynamic pressure works on the roughness pits wall, they significantly influence the deformation characteristics.
Another important tribological factor is the real area of contact. Both the die and the billet inherit roughness which affects the real area of contact during the sliding movement of die and the billet. The real area of contact is much lower than the apparent area of contact and it depends on the surface roughness of the matting surfaces and the normal pressure acting on the interfaces [34].

As discussed earlier, in case of ultrasonic microextrusion process, repeated opening and closing of the workpiece surface roughness pits due to the wave propagation into the system will occur. These pits will act as lubricant traps where micro plasto hydrostatic and/or hydrodynamic lubrication is likely to occur. The hydrostatic and/or hydrodynamic pressure influences the plastic deformation characteristics of the workpiece. Furthermore, along with the micro plasto lubrication, the actual contact area between the workpiece and the die has significant influences on the friction condition at the die-workpiece interface. The discussions in this section will be revisited in later chapters where plastic deformation and friction analyses in ultrasonic microextrusion process are discussed.

2.5 HEAT GENERATION IN METAL FORMING

In 1925, Farren and Taylor published the first experimental results on the conversion of heat from the work done on a metal forming operation. They used straight metal bars of steel, copper and aluminum for longitudinal pulling and then calculated the total mechanical work from the stress-strain curves. They accounted the fraction of mechanical work converted to
heat is in a range of 0.84 to 0.965, where temperature rises were measured using
thermocouple and corrections for the adiabatic heating were made. They also reported that
the conversion rate does not have a direct relationship with the hardening of the material as it
is almost constant for different stages of test for a particular sample. Then Taylor and
Quinney (1934) obtained the heat conversion fraction (0.88 – 0.97) using calorimetric
methods to measure the heat produced during quasi-static deformation by severe twisting for
annealed and decarburized mild steel, and also for annealed pure copper. However in most
metal forming applications, a typical value of 0.95 is used as the fraction of plastic
deformation energy transformed into heat [43].

Moreover, there are a number of works where the influences of strain and strain rate on heat
conversion from the plastic deformation energy were studied. Zehnder (1991) proposed a
model to calculate the fraction of plastic deformation energy which dissipates as heat.
According to the model, that fraction increases with increasing strain and decreases with
higher hardening (lower $n$). Hence that fraction for low hardening material is close to unity.
Although this model correlates with previous experiment results only qualitatively, the
correlation can be useful for estimating temperature rise during dynamic deformation.

Mason et al. (1994) investigated the strain and strain rate dependency of the fraction of
plastic work converted to heat using infra-red detectors and Kolsky bar for 4320 steel, 2024
aluminum and Ti-6Al-4V titanium alloys. As expected, that fraction for 4320 steel and 2024
aluminum did not show any strain rate dependency over a large range, since those materials
are little or no strain rate dependent. However, the value of fraction quickly rose from a
lower value (0.5 for aluminum, 0.4 for steel) at lower strain level and then steadied to the
final value of 0.85 at higher strains. However for a strain rate sensitive material Ti-6Al-4V,
that fraction is significantly dependent on strain and at higher strain it dropped from 1 to 0.5
(5% to 20% strain). Since twining is the dominant deformation mechanism at higher strain
rate, more plastic work might be stored in the material rather than converting to heat.
Kapoor and Nemat-Nasser (1998) tested high strain rate deformation of Ta-2.5% W, commercially pure Ti, 1018 steel, 6061 Al and OFHC Cu using split Hopkinson pressure bar (SHPB) and infra-red temperature measurement. They loaded a sample with half the strain of a previous test and then cooled that second sample to the room temperature. Then again that sample was heated up to an estimated temperature considering 100% conversion of heat from the deformation work to resume the first experiment condition. Only this particular way they could trace back the original stress-strain curve from the first experiment with the second set of results. Although the infra-red detection underestimated the sample temperature, comparing the studies of past researchers, they concluded that up to 4% deformation energy could be stored as internal energy such as dislocations, elastic energy of the defects, etc.

2.5.1 Temperature Prediction in Extrusion Process

There are a number of researches that have been carried out to predict the temperature generation in the extrusion processes. In 1960, Johnson and Kudo proposed an upper bound analysis that was used to determine the temperature rise in extrusion process. In this approximate analysis they calculated the temperature rise at each point in the deforming region by using the local rate of work input. This rate of work input was guessed based on various boundary requirements and also considering the principle of minimum total work input. However, as it was an approximate approach it did not consider the various real features such as variations of yield stress, surface roughness, interface friction etc. Altan and Kobayashi (1967) developed a numerical method to estimate the non-steady-state temperature distribution in extrusion through conical dies considering the temperature dependencies of the flow stress and of the thermal constants of the billet and the tool material. They considered cooling of the billet in the air and heat conduction between the billet and the container prior to the start of the extrusion process. In 1992, Udagawa et al., calculated the heat generated due to deformation in the extrusion of Ti-6Al-4V tubes applying an automated remeshing procedure in FEM model, neglecting the frictional effects
on the heat generation. However, in case of extrusion process, heat is generated by both the frictional work and deformation work. This heat is transported with the extruded material and conduction takes place simultaneously. Some of the generated heat remains in the extruded metal, some is transmitted to the container and die and some even increases the temperature of the part of the billet that is not yet extruded. Saha (1998) obtained approximate information regarding the temperature distribution in the billet by writing the differential equations in finite difference form and then calculating the temperature at any point of the billet numerically with suitable boundary conditions. Then Altan (1983) introduced the equation to estimate the temperature rise due to plastic deformation. Recently Ajiboye and Adeyemi (2008) proposed a numerical method to estimate the non-steady-state temperature distributions during forward extrusion process. This method considers the temperature rise due to the plastic deformation, shearing at the deformation-zone boundaries and the friction at tool-material interfaces. However, this method does not consider the effects of the surface roughness of the die and the billet and also the effects of lubricant.

2.6 SONOCHEMISTRY

Since liquid lubricant is typically used in ultrasonic metal forming, the possibility of sonochemical effects in the forming process needs to be investigated. However due to the lack of prior research on sonochemistry in ultrasonic metal forming, it is reasonable to review the established theories of sonochemistry from other fields of applications. Hence the understanding of the sonochemistry can shed light upon the pertinent problems in the ultrasonic metal forming with liquid lubricant.

The term ‘sonochemistry’ describes the subject which uses ultrasound to affect the chemical processes. The application of sonochemistry has gone beyond the laboratory research to industrial usage. Ultrasonics may influence the chemical activity in several ways, which are any or all of the following: production of heat, promotion of mixing, promotion of intimate
contact between materials, dispersion of contaminated layers of chemicals, and production of free chemical radicals. There are a number of applications which involves the sonochemistry; some of these are acceleration of etching, treatment of beverages, juices and essential oils, treatment of sewage, extraction processes, demulsification of crude petroleum, desalination of seawater, dissolution of steel in hydrochloric acid and in phosphoric acid, intensification of nearly all chemical-heat treatment processes in metals, electrolysis and electroplating [4]. This section focuses on mechanism of sonochemistry which is mainly dependent of cavitation phenomenon.

2.6.1 Cavitation

The chemical effects of ultrasound do not directly come from interaction with molecular species; instead it derives principally from acoustic cavitation which is generated during the rarefaction or negative pressure period of sound waves. There are two different theories about cavitation; the hot-spot and the electrical theory. However each theory proves that the origin of sonochemical effects is cavitation. According to the hot-spot theory when the bubbles collapse the localized hot-spots are formed which reach temperatures and pressures in excess of 5000 K and 500 atm. According to the electrical theory, an electrical charge is created on the surface of a cavitation bubble. That charge forms massive electrical field gradients across the bubble and upon collapse which can break the bond [3]. Among these two theories, the hot-spot theory is widely used to explain the cavitation effects.

In order to explain the hot-spot theory (thermal theory) reference may be made to Mason and Lorimer (2002) and Margulis (1995). The critical pressure ($P_k$) and bubble radius relationship, time of bubble collapse, motion of cavity wall, maximum bubble temperature ($T_{max}$) and pressure ($P_{max}$) can be obtained using this model.
There is a minimum critical hydrostatic pressure, a small reduction of which causes a dramatic increase in bubble radius $R$, i.e. the bubble becomes unstable and grows explosively. The radius and pressure at which this occurs are termed as $R_k$ and $P_k$ respectively and they are related as follows.

$$
P_k = P_v - \frac{2}{3} \left\{ \frac{(2\sigma/R_e)^3}{3(P_h + 2\sigma/R_e)} \right\}^{1/2}
$$

(2.4)

where, $R_e$ = radius of the bubble at its equilibrium, $P_v$ = vapor pressure of the liquid, $P_h$ = hydrostatic pressure, $\sigma$ = surface tension of the liquid. If the pressure of the vapor in the bubble is neglected ($P_v \approx 0$) and $P_k = P_h - P_B$, where $P_B$ is the Blake threshold pressure which is negative (or rarefaction) pressure to must be applied in excess of the hydrostatic pressure ($P_h$) to create a bubble of radius $R_e$; then equation 2.4 can be rewritten for large and small bubbles.

for large bubbles (i.e. $2\sigma/R_e << P_h$),

$$
P_B \approx P_h + \frac{8\sigma}{9} \left\{ \frac{3\sigma}{2P_hR_e^3} \right\}^{1/2}
$$

(2.5)

for small bubbles (i.e. $2\sigma/R_e >> P_h$),

$$
P_B \approx P_h + 0.77\sigma/R_e
$$

(2.6)

The time ($\tau$) to collapse a bubble in an acoustic field is given by Khoroshev’s equation.

$$
\tau = 0.915R_m \left( \frac{P}{P_m} \right)^{1/2} \left( 1 + \frac{P_v}{P_m} \right)
$$

(2.7)

where, $R_m$ = maximum bubble radius just before collapse, $P_m$= the liquid pressure at transient collapse.
The motion of the cavity wall can be given by following Rayleigh-Plesset equation.

\[
R\ddot{R} + (3/2)\dot{R}^2 = \frac{1}{\rho} \left[ \left( \frac{P_h}{R_e} - \frac{2\sigma}{R_e^2} \right) \left( \frac{R_e}{R} \right)^{3k} - \frac{2\sigma}{R} - 4\eta \frac{\dot{R}}{R} - \left( P_h - P_a \right) \right]
\]  

(2.8)

where, \( \dot{R} \) = velocity of the cavity wall, \( \ddot{R} \) = acceleration of the cavity wall, \( \eta \) = viscosity of the liquid, \( P_a \) = applied acoustic pressure, \( k \) = polytropic index of the gas (\( k \) varies between \( \gamma \), the specific heat ratio, and unity, the limit for adiabatic and isothermal conditions).

The wall motion of a transient, gas-filled cavity (assuming adiabatic compression, neglecting surface tension) is described by following equation.

\[
R\dddot{R} + \frac{3}{2} \dot{R}^2 = \frac{1}{\rho} \left[ \frac{P_g}{\gamma} \left( \frac{R_m}{R} \right)^{3\gamma} - P_m \right]
\]  

(2.9)

where, \( P_g \) = gas pressure in a bubble of initial radius \( R_e \).

The maximum temperature (\( T_{\text{max}} \)) and the maximum pressure (\( P_{\text{max}} \)) obtained within the transient collapsing bubble (assuming adiabatic collapse) are given by,

\[
T_{\text{max}} = T_0 \left\{ \frac{P_{\text{m}}(\gamma - 1)}{P} \right\}^{\frac{\gamma}{\gamma - 1}}
\]  

(2.10)

\[
P_{\text{max}} = P \left\{ \frac{P_{\text{m}}(\gamma - 1)}{P} \right\}^{\frac{\gamma}{\gamma - 1}}
\]  

(2.11)

where, \( T_0 \) = ambient (experimental) temperature, \( P \) = pressure in the bubble at its maximum size (usually assumed to be equal to the vapor pressure \( P_v \) of the liquid).
Stable cavities are bubbles which form and oscillate around a mean radius in a sound field and exist for many acoustic cycles. However a transient cavity is one which exists for only a few acoustic cycles. [3, 52]

In the structured hot-spot model three regions for the occurrence of chemical reactions are postulated: (1) a hot gaseous nucleus, (2) an interfacial region with radial gradient in temperature and local radical density, and (3) the bulk solution at ambient temperature. Reactions involving free radicals can occur within the collapsing bubble, at the interface of the bubble, and in the surrounding liquid. Within the center of the bubble, harsh conditions generated on bubble collapse cause bond breakage and/or dissociation of the water and other vapors and gases, leading to the formation of free radicals or the formation of excited states. The high temperatures and pressures created during the cavitation provide the activation energy required for the bond cleavage. The radicals generated either react with each other to
form new molecules and radicals or diffuse into the bulk liquid to serve as oxidants. The second reaction site is cavity, which has been estimated to heat up to approximately 2000 K during cavity implosion. In this solvent layer surrounding the hot bubble, both combustion and free-radical reactions occur. Reactions here are comparable to pyrolysis reactions. At this interface between the bubble and bulk liquid, surface-active reagents also accumulate and species produced in the bubble first react with chemicals in the bulk liquid. It has been shown that the majority of degradation takes place in the bubble-bulk interface region. In the bulk liquid, no primary sonochemical activity takes place although subsequent reactions with ultrasonically generated intermediates may occur. [54]

Reference may be made to Margulis (1995) to explain the electrical theory. This theory considers (i) the main tenets of the dynamic theory of the cavitation, (ii) pulsation and deformation of cavitation bubbles, (iii) non-uniformity of charge distribution on their surface, (iv) the distribution of radicals and solute in space and time, (v) the effect of acoustic flows near bubbles. The main tenets and differential equations of the electrical theory are simple enough to be solved analytically; however the differential equations of thermal theory can only be solved by numerically.

The intensity of cavitation is greatly affected by the ambient conditions of the reaction and as a result it affects the reaction rate and/or yield. [3, 52, 55]

**Frequency:** The production and intensity of cavitation in liquids decreases with increasing ultrasonic frequency.

**Solvent:** Cavitation is easier in solvents with low viscosity and low surface tension. Moreover the higher the vapor pressure the less violent the collapse of bubbles. So that in order to get maximum sonochemical benefit any experiment should be performed with a solvent of low vapor pressure.
Temperature: Temperature rise increases vapor pressure and this leads to easier bubble formation containing more vapors which reduces the ultrasonic energy produced upon cavitation because it cushions the implosion. In general, the largest sonochemical effects are observed at lower temperatures when a majority of the bubble contents is gas.

Gas Type and content: Employing gases with large $\gamma (=C_p/C_v)$ give greater sonochemical effects since collapse temperature is proportional to ($\gamma$-1). The local heating during the collapse increases with the smaller thermal conductivity of the gas. The dissolved gas acts as cavitation nuclei and leads to more facile cavitation. However the intensity of the shock wave reduces with increasing amount of dissolved gas.

External Applied Pressure: Increasing external pressure ($P_h$) decreases the vapor pressure and hence leads to an increase in both cavitation threshold and the intensity of bubble collapse.

Intensity: In general an increase in intensity ($I$) provides an increase in sonochemical effects. However the increase in intensity is limited by the material stability of transducer, decoupling with the medium and a large number of bubbles (transmission barrier).

2.7 ULTRASONIC CLEANING

In ultrasonic metal forming, the improvement of surface finish indicates towards the similar effects obtained from the ultrasonic cleaning. Therefore it is important to study the ultrasonic cleaning for understanding of the effects of ultrasonics in metal forming.

Ultrasonic has long been used for cleaning purposes. It has been found to be very effective in precision cleaning of complex parts. The parts with small crevices, blind holes, or deep recesses can be readily cleaned by the ultrasonic cleaning. Some typical applications for
ultrasonic cleaning are turbine engine blades, bearings, aircraft fuel nozzles, stamped parts (pieces or strips), pistons, piston rings, valve lifters, battery cans, semiconductor wafers, etc [56]. The intents of this section are to discuss the mechanisms of ultrasonic cleaning and the factors affecting it along with the effects of cleaning on the surface of the material.

2.7.1 Mechanisms of Ultrasonic Cleaning

The mechanism of ultrasonic cleaning is attributed to the cavitation. The cleaning mechanism comprises the phenomena associated with cavitation are (1) development of stresses between the cleaning fluid and the contaminated surface, (2) agitation and dispersion of contaminant throughout the cleaning fluid, (3) increase of the attractive forces between contaminant and cleaning fluid, (4) promotion of chemical reactions at the contaminated surfaces in some cases, and (5) effective penetration of pores and crevices [4]. The brief descriptions of these phenomena are presented below.

Ultrasound generates cavitation bubbles at a liquid/solid interface and collapse of these bubbles imposes severe stresses on the solid interface which cause the surface severely eroded. The intensity of the stress depends on the factors which primarily affect the cavitation such as the vapor pressure of the liquid, the gas content of the liquid, and the adhesive force between the liquid and the surface.

The agitation provides a scrubbing action which promotes the removal of contaminants which may be loose, solid particles or it may be materials which will dissolve or emulsify in the cleaning fluid.

Upon application of ultrasonics ions are produced in and near the walls of cavitation bubbles. When ions from the cleaning fluid show a preferential attraction for ions from the
contaminant, the net attractive forces are greater than those that would exist between the liquid and the contaminant in the absence of ultrasonic agitation resulting in the contaminant to be more readily removed by the cleaning fluid.

2.7.2 Particle Removal using Ultrasonic Cleaning

The particle removal from the solid surface can be explained through several different mechanisms (figure-2.14) such as acoustic streaming, microstreaming, microstreamers and micro-jets [57, 58].

Figure-2.14: Mechanisms for Particle Removal/Detachment observed with Ultrasonic Cleaning [57]
Acoustic streaming which is defined as the absorption of acoustic energy resulting in fluid flow does not require the collapse of cavitation bubbles. Acoustic streaming occurs on the order of the centimeters to tens of centimeters with velocities in the range of 10 cm/s. Such type of ultrasonic cleaning mechanism is important for the nearby surfaces with loosely attached particles or with readily dissolved surfaces. Acoustic streaming increases with increase of ultrasonic frequency and power intensity.

Microstreaming is the time-independent circulation of fluid occurring in the vicinity of bubbles set into motion by oscillating sound pressure. Oscillations in bubble size cause rapid fluctuations in the magnitude and direction of fluid movement, and hence result in a significance shear force. The effective range of this mechanism is on the range of a bubble diameter (~1-100 µm).

Cavitation bubbles forming at nucleation sites within the liquid are subsequently translated to a mutual location (antinodes) are called microstreamers. These bubbles travel in ribbon like structures along twisting paths at velocities approximately at an order of magnitude faster than the average velocity of the fluid, coalescing as they collide with other bubbles. They scour away particles while translating to the antinodes and have an effective range on the order of millimeters.

When a cavitation bubble collapse in the presence of an asymmetry (i.e. a surface or another bubble), micro-jets are formed. During collapse, the bubble wall accelerates more on the side opposite to a solid surface, resulting in the formation of a strong jet of liquid (e.g. water) with an estimated velocity of 100-200 m/s. The effective range of micro-jets is on the order of the bubble diameter.
2.7.3 Factors Affecting Ultrasonic Cleaning

The effectiveness of an ultrasonic cleaner depends upon the choice of cleaning fluid and the ability to provide sufficient energy at the contaminated surface to promote the desired cleansing.

Cleaning Fluids: Stresses in the walls of cavitation bubbles are controlled partly by the vapor pressure of the fluid. As vapor pressure increases and surface tension decreases, the maximum stress associated with cavitation decreases and hence it is a major consideration in the choice of solvent. Moreover the chemical classification of the solvents and contaminants is a major consideration. In general polar solvents (such as alcohol, acetone, acids, aldehydes, water, etc.; high dielectric constant and chemically active) are chosen to remove polar contaminants and nonpolar solvents (such as hydrocarbons and their derivatives, etc.; low dielectric constant and comparatively chemically inert) to remove nonpolar contaminants. Acidic solvents may cause hydrogen embrittlement and the effect is accelerated under ultrasonic excitation. Moreover solvents may cause dissolving of base material, swelling of materials such as elastomeric coatings, cracking and distortion of certain plastics, and coagulation of certain materials (such as proteinaceous materials at high temperature) that inhibits their removal and dispersion by ultrasonic energy. These are some considerations for choice of solvents. Some commonly used ultrasonic cleaning fluids include aqueous materials (including household detergents), acidic chemicals, alkaline chemicals, and hydrocarbons. [4]

Energy Density: The amount of power required to remove contaminants from a surface varies with the contaminant and the solvent used. Since cavitation is associated with the cleaning rates for which ultrasonics is noted, the intensity at the surface must exceed the threshold of cavitation. Pohlman et al. (1972) showed that energy density is a direct measure of degree of cleaning produced by the ultrasonic bath. Because the ratio \( \frac{E}{E_0} \) of the mean
energy density $E$ to the reference energy density $E_0$ increases linearly in a semi-logarithmic plot with increase of electric power, $N$ and also degree of cleaning, $R$ increases similar way with increase of electric power, $N$. However, the conversion efficiency of the electronic generator and of the transducer determines the amount of power available to the cleaning solution.

Other Factors: The degree of cleaning increases faster initially and then becomes gradually less effective with increasing duration of the cleaning. Similar phenomena have been observed for the cases of bath temperature increase. [59, 60]

Three types of modulations namely continuous ultrasound, half-wave ultrasound and double half-wave ultrasound are commonly used in commercial ultrasonic generators. The degree of cleaning is greater when single half-wave ultrasound is used than when continuous ultrasound is used, however its value in double half-wave falls between these two. Therefore a given power is the most economical when used in the half-wave modulation mode, since the amplitude magnification occurred then is more effective than the intervening pauses in the ultrasonic excitation. [59]

2.7.4 Effects of Ultrasonic Cleaning on Surface

In many occasions ultrasonic cleaning results in surface erosion. Maisonhaute et al. (2002) reported on surface erosion brought about by ultrasound. They sonicated a 60 µm diameter gold electrode in pure water at 20 kHz and 8.9 Wcm$^{-2}$, and the horn to electrode distance was 2 mm. The electrode surface became rough and some cracks in the glass surrounding the electrode were observed (figure-2.15). After 15 minutes of sonication some pitting in the glass was observed and the depth of damage was then ~200 µm. It is important to note that no pitting was observed for the horn to electrode distance of 5 mm or, for sonication of glass rod. The glass/gold sealing is susceptible to ultrasonic erosion than glass alone. At short
electrode distances, the shear stress produced by the acoustic bubble is strong enough to break the electrode around the sealing. Once a first pit is made, bubbles are preferentially nucleated around this pit and the collapse may also be even stronger when it occurs in a crack. These two features enhance the ultrasonic effects. However the AFM pictures (figure-2.16) revealed that for a smaller zone (5 µm by 5 µm), no difference was observed for sonicated sample of gold electrode polished with 0.3 µm alumina, for example the polishing lines still can be observed. The shear stress does not seem to affect the electrode at this scale, i.e. a smooth gold surface at this scale is hard enough to resist to power ultrasound. This phenomenon is related to the size of cavitation bubble that induces these effects. Since a wide range of bubble diameters could appear on the surface (up to 0.8 mm diameter bubbles could be detected) and it is likely that the mechanical effects differ with the bubble size.

Figure-2.15: Optical Pictures of a 60 µm diameter Gold Electrode sonicated in Water with a power of 8.9 Wcm⁻² and a distance of 2 mm after (a) 0 min; (b) 2 min and (c) 15 min [61]

Figure-2.16: AFM Picture of a 3 mm diameter Gold Electrode sonicated in Water with a power of 8.9 Wcm⁻² and a distance of 2 mm after (a) 0 min and (b) 2 min [72]
Lamminen et al. (2004) presented the SEM images of sonicated particle-fouled ceramic membranes. These images along with power intensity and frequency experiments allow relating the ultrasonic cleaned surface to the mechanism of cleansing. Figure-2.17 shows the indentation marks caused by the micro-jet impact on the surface cake layer of the membranes. The average diameter of these cavities is $18\pm6$ µm and found to be independent of frequency and power intensity. However the numbers of indentations vary from sample to sample and no trend could be established among different frequencies and power intensities. Moreover indentations with similar shape and size were found on metal surface caused by micro-jets.

![Figure-2.17: SEM Images showing Evidence of Micro-Jet Impacts on the surface of the Cake Layer. Left: 1062 kHz for 5 s, 0.21 Wcm$^{-2}$. Right: 620 kHz for 5 s, 0.12 Wcm$^{-2}$ [57]](image)

Figure-2.18 shows another type of surface formation regarding microstreamers or microstreaming. A circular patch of removal with an average diameter of $2.30\pm0.7$ mm at 620 kHz (panels A and C in figure-2.18) is observed. On the edges of these circular patches of removed particles, channels as long as 1 mm in length but only several micrometer wide are observed (panels B and D in figure-2.18). Moreover channels were found in isolated regions formed due to microstreaming caused by localized high fluid velocities near the fouled surface-cavitation bubble interface as the bubbles oscillate. For cleaning of particle-fouled ceramic membranes, microstreamers are found to be the major mechanism and
microstreaming plays a role in the cleaning in conjunction with microstreamers. However micro-jets are present but do not greatly enhance the cleaning and acoustic streaming is not capable of cleaning without other cavitation-based mechanism. In addition, there was no visible damage due to ultrasonic cleaning on membrane surface even for prolonged periods of ultrasound at high power intensity and low frequency (20 Wcm$^{-2}$, 20 kHz).

Figure-2.18: SEM Images of Circular Patches of Cake Layer Removal and Channel-like Formations along the Edges of the Circular Patches that is attributed to Microstreaming/Microstreamers: (A) 620 kHz for 5 s, 0.21 Wcm$^{-2}$; (B) 620 kHz for 5 s, 0.12 Wcm$^{-2}$; (C) 620 kHz for 5 s, 0.42 Wcm$^{-2}$; (D) 205 kHz for 5 s, 0.21 Wcm$^{-2}$ [57])
2.8 SUMMARY OF LITERATURE REVIEW

A brief summary of the literature review useful for the current study is presented.

Ultrasonic wave propagation in a real structure cannot be treated with the exact solution approach; rather numerical methods with appropriate boundary conditions are required for analyzing a structure experiencing ultrasonic vibration.

For an ultrasonic assisted forming process, the separation of the die and the workpiece is controlled by the relative velocity between the die and the workpiece which depends on the extrusion speed and the characteristics of imposed ultrasonic energy.

For microforming, the flow properties of the material deviate from the bulk material properties if the part size is comparable to the grain size of that particular material.

The shape and size of the surface roughness pits changes as deformation progresses. Therefore hydrostatic pressure is generated in the lubricants trapped in those pits. This hydrostatic pressure rise in the trapped lubricant could be estimated by calculating lubricant density. Moreover, hydrodynamic pressure rise in the trapped lubricant occurs based on the forming process parameters and the lubricants properties. Actual contact area between the die and the workpiece depends on the surface roughness of the matting surfaces and the normal pressure acting on the interfaces.

In case of the strain rate insensitive materials, the fraction of plastic deformation energy converting to heat is 0.90 for moderate to higher strain values.

Ultrasound can initiate the chemical reaction, accelerate the rate of the reaction and change the reaction pathway, where cavitation is the origin of all of these sonochemical effects.
However the ambient conditions greatly affect the intensity of cavitation. Since in case of micro-forming, the enclosures containing the lubricant between two metal surfaces are very small (such as 20 to 30 µm diameter and 5 to 10 µm depth), the emphasis needs to be put on the cavitation phenomena occurring in such small zone.

The mechanism of ultrasonic cleaning is also attributed to the cavitation. The particle removal from the solid surface can be explained through several different mechanisms such as acoustic streaming, microstreaming, microstreamers and micro-jets. However acoustic streaming is not capable of cleaning without other cavitation-based mechanism. The cleaning effect on the surface largely depends on the bubble size, because mechanical effects differ with the bubble size and it is more prominent for micro-scale sample.
CHAPTER THREE: RESEARCH APPROACH

In the ultrasonic microextrusion process, the temperature is generated from the heat energy converted from both the plastic deformation and frictional energies. In order to model the plastic deformation of the billet, governing factors such as the material property, the role of the trapped lubricants in the surface roughness pits of the billet, etc. need to be considered. Therefore a number of experiments will be conducted to analyze the ultrasonic extrusion process for modeling purposes. The elastic-plastic finite element method will be used to model this plastic deformation where the lubricant effect on the deformation characteristics will be treated by the micro plasto hydro lubrication theory. However, for such method to be applied, the surface roughness profile of the billet needs to be represented as a specific initial geometry capable of containing lubricants on the surface. Therefore the actual surface roughness of the billet will be modeled to produce that geometry. As the extrusion process progresses, that surface roughness profile changes along with the actual contact between the surface roughness asperity and the die surface. This actual contact area depends on the ultrasonic energy, surface roughness of the matting surfaces, normal pressure and the lubrication condition. All these variables dictate the friction condition at the die-workpiece interface. Hence for the friction analysis, information about the relative movement of the die-workpiece, the changing workpiece surface roughness and actual contact area are required. However, in order to obtain the relative movement of the die-workpiece, the ultrasonic wave propagation mode and wave transmissibility to the punch and workpiece need to be quantified. This will be performed with numerical harmonic response analysis of the test set-up which is designed to have minimum wave transmissibility to the punch and the workpiece. Moreover experiments will be required to obtain the amplitude of the imposed ultrasonic vibration at the set-up. From the structural analysis of the test set-up, the necessary information to calculate the relative movement of the die and the workpiece will be obtained. Hence the frictional energy will be calculated using the friction condition at the die-workpiece interface and the relative velocity of die-workpiece. Once the plastic deformation
and frictional energies are known, a percentage of the energy that is converted to heat energy will be calculated. This energy will then be applied on the die and the workpiece. Since this energy is generated over the period of extrusion time, numerical transient thermal analysis will be used to predict the temperature distribution on the die-workpiece interface and the die assembly over that time. Finally the predicted temperature will be compared with the previously recorded experimental temperature. The research approach discussed above is schematically presented in figure-3.1.

Figure-3.1: Schematic of Research Approach
CHAPTER FOUR: RESPONSE OF ULTRASONIC MICROFORMING TEST SET-UP

4.1 INTRODUCTION

The temperature in ultrasonic microextrusion process is generated due to plastic deformation and frictional energies. In order to determine that frictional energy, the friction analysis needs to be performed where the information about the relative movement of die-workpiece is necessary. This relative movement information is obtained by quantifying the wave propagation mode and also wave transmissibility to both the die assembly and the workpiece. Since the experimental analysis alone can not perform this task, numerical methods such as harmonic response analysis will be applied along with available experimental data such as the amplitude of the imposed ultrasonic vibration at different locations of the ultrasonic microforming test set-up. At first an ultrasonic microforming test set-up is designed and then the experiments and numerical analysis provide the response of that set-up.

4.2 DESIGN OF ULTRASONIC MICROFORMING TEST SET-UP

A 2nd generation ultrasonic microextrusion test set-up (figure-4.1) is designed where a 10 ton hydraulic cylinder is used to provide the forming load. The design includes a support frame for this hydraulic cylinder and the punch and the die assemblies. Figure-4.2 shows the die assembly. ANSYS is used for all the structural analyses (static and dynamic). The punch assembly, which is attached to the hydraulic cylinder, is not included in the analysis. The set-up is meshed using eight nodes tetrahedral element (SOLID45) with 48604 nodes and 201619 elements. The properties for orthotropic steel material used here are as follows: density = 7820 kg/m³, \( EX = 206.8 \times 10^9 \) Pa, \( GXY = 80.155 \times 10^9 \) Pa, \( NUXY = 0.29 \). From the static analysis it is observed that only the support frame deflects up to a reasonable small value (maximum 0.15 mm) under 10 ton loading (figure-4.3) and therefore the design is
accepted. Since the stroke of the microextrusion process can be as small as 2 mm, the deflection of the structure is considered as one of the design criteria.

Figure-4.1: Design of 2nd Generation Ultrasonic Microforming Test Set-up
Figure-4.2: Microextrusion Die Assembly

Figure-4.3: Displacement Profile of Ultrasonic Microforming Test Set-up (SI Units)
In the microforming test set-up, different sensors are installed for measurement and operation. They are namely displacement transducer, pressure gauge, thermocouple, load cell and strain gauge. Figure-4.4 shows the 2nd generation ultrasonic microforming test set-up. Ultrasonic energy from the generator is transferred to the test set-up by the transducer.

Sonic Digital LC Premium Ultrasonic Generator (Weber Ultrasonics)
- Frequency: 20 kHz
- Effective power: 2000 W
- Nominal Peak-to-Peak Amplitude: 16-18 µm at the face of the transducer connector

Ultrasonic Transducer (Weber Ultrasonics)
- Piezoelectric transducer
- 3000W/20kHz rating

Figure-4.4: 2nd Generation Ultrasonic Microforming Test set-up
4.3 STRUCTURAL ANALYSIS OF ULTRASONIC MICROFORMING TEST SET-UP

4.3.1 Experimental Investigation of Test Set-up Response

The vibration response of the test set-up is investigated using a Laser Vibrometer and strain gauge. These results are then compared with the numerical analysis results. To ensure that the simulation results match with experiments, variables such as damping ratio can be changed. Two different settings of ultrasonic generator are used for the test set-up response experiments as shown in table-4.1. The details of ultrasonic generator settings will be discussed in chapter five.

Table-4.1: Ultrasonic Generator Settings for Test Set-up Response Experiments

<table>
<thead>
<tr>
<th>Settings</th>
<th>Percentage of Amplitude (A), %</th>
<th>Resonant (Working) Frequency, kHz</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>100</td>
<td>20</td>
</tr>
<tr>
<td>2</td>
<td>50</td>
<td>20</td>
</tr>
</tbody>
</table>

4.3.1.1 Laser Vibrometer Measurement

A Polytec Laser Vibrometer is used for this measurement. Figure-4.5 shows the experimental set-up for the Laser Vibrometer measurement. The Laser light is focused on a special reflector tape attached to the desired location on the plate holding the die assembly. An oscilloscope is used for the data acquisition from the vibrometer controller. Finally a LabVIEW code with 10 kHz high pass filter and 10⁶ sampling rate is used for the data processing. The specifications of the vibrometer and the oscilloscope are given below.
Polytec Vibrometer Controller OFV-5000
- Displacement Decoder DD-900
- Fiber-coupled Sensor Head OFV-534
- Specifications
  - Measurement Range 10 µm/V
  - Full Scale (Peak-to-Peak): 200 µm
  - Resolution (rounded): 3 nm
  - Frequency Range: 0 – 2.5 MHz

Agilent Oscilloscope DSO7104B
- 100 MHz Bandwidth
- 2 GSa/s per Scope Channel
- 4 Scope Channels

Figure-4.5: Vibration Amplitude Measurement using LASER Vibrometer
There are two points (point-A and point-B) on the die assembly holding plate, where the vertical displacements are measured for different settings of the ultrasonic generator. For point-A and point-B, the radial distances from the center of the set-up are 95 mm and 110 mm respectively. The measured vertical displacements data for point-A and point-B are presented in figures 4.6 and 4.7 respectively. Point-A which is nearer to the center exhibits more vertical displacement than point-B. Furthermore, both points show about twice the peak-to-peak displacement when the amplitude percentage is increased from 50% to 100%.

![Experiment Data using Vibrometer](image)

Figure-4.6: Measurement of Vertical Displacements of Point-A on Plate Surface for 50% and 100% Amplitudes at 20 kHz Frequency using LASER Vibrometer
4.3.1.2 Strain Gauge Measurement

Since the Laser Vibrometer cannot measure the response at the die-container, a strain gauge is used, where it is attached on the die-container outer surface (figure-4.8). Then the absolute strain values are converted into corresponding local vertical displacement (equal to strain multiplied by the gauge length) amplitudes shown in figure-4.9.
Figure-4.8: Vibration Amplitude Measurement using Strain Gauge

![Image of vibration measurement setup]

Figure-4.9: Local Vertical Displacement of Die-Container Outer Surface for 100% Amplitude at 20 kHz Frequency

![Graph showing vertical displacement over time]

**Experiment Data using Strain Gauge**

- Vertical Displacement (micron)
- Time (sec)

![Experiment data graph]

Exp
4.3.2 Numerical Structural Analysis of Test Set-up

In order to calculate the frictional energy, the relative movement of the die and the workpiece is required, for which the response at the die inner surface is necessary. Since it is not possible to measure the response at the die inner surface, numerical analysis is used to estimate that. However the input dynamic loading for the numerical analysis is not readily obtainable from the system. A set of harmonic analyses with different inputs are performed and then the comparison with the experimental data yields the actual input. With the actual input, the response of the test set-up is analyzed. Finally the variation of the responses due to the thickness variation of the die-assembly holding plate is discussed. It should be noted that the original plate thickness is 12.7 mm and two more thickness variation such as 5 mm and 8 mm are analyzed. Hence unless otherwise stated, all the data analyses are for the 12.7 mm plate.

4.3.2.1 Pre-Processing

ANSYS is used for the finite element structural analysis of the set-up, where it is modeled as a single part. This part is meshed (figure-4.10) using eight nodes tetrahedral element (SOLID45) which has three degrees of freedom in each node, i.e. translations in the nodal x, y and z directions. Here the number of nodes and elements used to mesh this set-up are 48604 and 201619 respectively. Orthotropic material properties are defined for this element type. The properties for orthotropic steel material are as follows: density = 7820 kg/m$^3$, $EX = 206.8 \times 10^9$ Pa, $GXY = 80.155 \times 10^9$ Pa, $NUXY = 0.29$. 
4.3.2.2 Harmonic Response Analysis

Harmonic response is required to quantify the wave propagation to the die. During the ultrasonic microextrusion process, a forced vibration loading i.e. a load that varies sinusoidally (equation 4.1) is applied by the ultrasonic transducer on the microextrusion test set-up. Since there is damping in the structure, the harmonic response will be out of phase with the loads and hence all the solutions will be complex in nature. The complex solutions
define the displacements of the structure according to equation 4.2, where the maximum displacement represents the amplitude of the complex solution (equation 4.3).

\[
Variable \ Load = Load \ Amplitude \times \cos(\omega t) \tag{4.1}
\]

\[
Displacement = Real \ Solution \times \cos(\omega t) - Imaginary \ Solution \times \sin(\omega t) \tag{4.2}
\]

\[
Amplitude = \sqrt{(Real^2 + Imaginary^2)} \tag{4.3}
\]

The ultrasonic transducer to be used has a rating of 3000W/20kHz, so that the operating range is considered as 19.50 – 20.50 kHz with 10 equal steps. It should be noted that the maximum ultrasonic power available is 2000 W which is restricted by the capacity of the ultrasonic generator (Sonic Digital LC Premium). The bottom of the set-up is constrained in the vertical direction and a displacement is imposed on the transducer connector which acts as the forced vibration loading (figure-4.11). Four different loadings are used here namely 0.65 µm, 1.3 µm, 5 µm and 10 µm displacements. A constant damping ratio of 1% is used for this steel structure.

![Figure-4.11: Boundary Conditions for Harmonic Response Analysis](image)
4.3.2.2.1 Comparison with Experimental Measurement

In order to obtain the actual input condition for the numerical analysis, the results from the four different loading conditions are compared with the experimental data from the Laser Vibrometer measurement. To present the vertical displacements in a full cycle (360°), deformed shapes of the die-assembly zone for 1.3 µm input are shown in figure-4.12 at different angular positions. Since these are the measurable values during the real operation with the proper instruments (vibrometer, strain gauge), they are compared with the previously obtained experimental data if the data acquisition system possesses sufficient sampling rate. The harmonic response analysis provides insights on how the vibration loading affects the die movement and hence affects the microextrusion process.

Figure-4. 12: Deformed shapes of the set-up at Frequency of 20 kHz for 1.3 µm Loading, Vertical Displacements Plots (SI Units)

Since there are two points (A and B) on the plate where the Laser measurements are taken, from the harmonic response analysis results the responses at those radial distances on the
plate are presented in table-4.2. Then the peak-to-peak values for those four input conditions are plotted together with the experimental data in the figure-4.13. From the figure it is observed that the experimental data for 50% and 100% amplitudes agree with the 0.65 µm and 1.3 µm input condition results respectively.

Table-4.2: Maximum Vertical Displacement Data from Harmonic Response Analysis at 20kHz Frequency

<table>
<thead>
<tr>
<th>Input (µm)</th>
<th>Point-A</th>
<th></th>
<th>Point-B</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Maximum Vertical Displacement (µm)</td>
<td>Standard Deviation (µm)</td>
<td>Peak-to-Peak Value (µm)</td>
<td>Maximum Vertical Displacement (µm)</td>
</tr>
<tr>
<td>0.65</td>
<td>0.532</td>
<td>0.042</td>
<td>1.064</td>
<td>0.481</td>
</tr>
<tr>
<td>1.3</td>
<td>1.064</td>
<td>0.083</td>
<td>2.128</td>
<td>0.961</td>
</tr>
<tr>
<td>5.0</td>
<td>2.465</td>
<td>0.192</td>
<td>4.930</td>
<td>2.228</td>
</tr>
<tr>
<td>10.0</td>
<td>4.358</td>
<td>0.340</td>
<td>8.716</td>
<td>3.939</td>
</tr>
</tbody>
</table>

Figure-4.13: Plate Surface Response (Peak-to-Peak value) Analysis for Different Inputs (Simulation and Vibrometer Data) at 20 kHz Frequency
Figures 4.14 show the plate surface response (1.3 µm input) comparison with the Laser Vibrometer data (100% amplitude), where the response at point-A shows better agreement than that of point-B.

Figure-4.14(a): Plate Surface Response at Point-A for 1.3 µm Loading and 100% Amplitude (Simulation & LASER Vibrometer Data) at 20 kHz Frequency

Figure-4.14(b): Plate Surface Response at Point-B for 1.3 µm Loading and 100% Amplitude (Simulation & LASER Vibrometer Data) at 20 kHz Frequency
The plate surface response (0.65 µm input) comparison for 50% amplitude at Point-A and Point-B are shown in figures 4.15(a) and 4.15(b) respectively.

![Figure-4.15(a): Plate Surface Response at Point-A for 0.65 µm Loading and 50% Amplitude (Simulation & LASER Vibrometer Data) at 20 kHz Frequency](image)

![Figure-4.15(b): Plate Surface Response at Point-B for 0.65 µm Loading and 50% Amplitude (Simulation & LASER Vibrometer Data) at 20 kHz Frequency](image)
Since the strain gauge provides local vertical displacements, corresponding local displacements of the position where the strain gauge is mounted need to be calculated from the simulation results. Figure-4.16 shows the schematic of the die container and the strain gauge, where points A and B are the two end points of the strain gauge position on the container outer surface. From the harmonic response analysis the real and imaginary components of the vertical displacements (microns) are extracted as A(7.210, 5.035) and B(7.039, 4.901) respectively for 1.3 µm input at 20 kHz frequency. Using equation-4.2, the vertical displacements for these two points are calculated for one complete cycle and they are shown in the table-4.3 along with the local vertical displacement at the length AB. However the maximum vertical displacement (amplitude) of the points A and B are calculated as 8.789 µm and 8.577 µm respectively using equation-4.3.

![Figure-4.16: Schematic of the Die-Container and the Strain Gauge](image)

*Figure-4.16: Schematic of the Die-Container and the Strain Gauge*
Thus, the obtained local vertical displacements data from the simulation are plotted with the experimental data from the strain gauge measurement (Figure-4.17) and it is observed that the peak-to-peak values of the simulation data are in agreement with the average values of the experimental peak-to-peak data. From this data analysis, it can be concluded that a good agreement between the simulation and experimental data is observed for the test set-up.

Table-4.3: Vertical Displacements of End Points of the Strain Gauge Position from Simulation for 1.3 µm Input at 20 kHz Frequency

<table>
<thead>
<tr>
<th>Time (sec)</th>
<th>Angle (degree)</th>
<th>Point-A Displacement, µm</th>
<th>Point-B Displacement, µm</th>
<th>Local Displacement (difference), µm</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.000000</td>
<td>0</td>
<td>7.210</td>
<td>7.039</td>
<td>0.171</td>
</tr>
<tr>
<td>0.000004</td>
<td>30</td>
<td>3.727</td>
<td>3.645</td>
<td>0.081</td>
</tr>
<tr>
<td>0.000008</td>
<td>60</td>
<td>-0.755</td>
<td>-0.725</td>
<td>-0.031</td>
</tr>
<tr>
<td>0.000012</td>
<td>90</td>
<td>-5.035</td>
<td>-4.901</td>
<td>-0.134</td>
</tr>
<tr>
<td>0.000017</td>
<td>120</td>
<td>-7.965</td>
<td>-7.764</td>
<td>-0.202</td>
</tr>
<tr>
<td>0.000021</td>
<td>150</td>
<td>-8.762</td>
<td>-8.546</td>
<td>-0.215</td>
</tr>
<tr>
<td>0.000025</td>
<td>180</td>
<td>-7.210</td>
<td>-7.039</td>
<td>-0.171</td>
</tr>
<tr>
<td>0.000029</td>
<td>210</td>
<td>-3.727</td>
<td>-3.645</td>
<td>-0.081</td>
</tr>
<tr>
<td>0.000033</td>
<td>240</td>
<td>0.755</td>
<td>0.725</td>
<td>0.031</td>
</tr>
<tr>
<td>0.000037</td>
<td>270</td>
<td>5.035</td>
<td>4.901</td>
<td>0.134</td>
</tr>
<tr>
<td>0.000042</td>
<td>300</td>
<td>7.965</td>
<td>7.764</td>
<td>0.202</td>
</tr>
<tr>
<td>0.000046</td>
<td>330</td>
<td>8.762</td>
<td>8.546</td>
<td>0.215</td>
</tr>
</tbody>
</table>
4.3.2.2.2 *Response of Test Set-up*

The deformed shapes of the set-up at different instances of a 360° cycle (20 kHz, 1.3 µm loading) are shown in the figure-4.18. Here the die assembly stretches and contracts vertically with little or no lateral movement during the oscillations, implying that there will be negligible transmission of shear wave to the punch assembly while they will be in contact during the operation. Moreover the top structure does not show any shear wave transmission. This implies that the shear wave in the punch and die assembly can be neglected for this set-up for operation at 20 kHz frequency.
Since the die assembly is the zone of interest, the maximum vertical displacement data are analyzed for that zone. Figure-4.19 shows the responses of the container outer surface at different frequencies for 1.3 µm loading. It is observed from the figure that for a particular frequency the imposed amplitude is amplified and increases along the length towards the top.

Then the responses for different loading conditions at 20000 Hz are shown in figure-4.20. From this figure it is apparent that the displacement profile shape is similar for different loading conditions, hence the response for loading condition in between them could be estimated by interpolating the available data.
Figure-4.19: Harmonic Responses (Maximum Vertical Displacement) of Container Outer Surface at Different Frequencies for 1.3 µm Loading

Figure-4.20: Harmonic Responses (Maximum Vertical Displacement) of Container Outer Surface at Frequency 20 kHz for Different Loading Conditions
The maximum vertical displacements of the die-container top surface are plotted in the figure figure-4.21 for 1.3 µm loading, which are found to be near constant for the respective frequency level. Hence for any cross sectional area of the die-container, the vertical displacements can be estimated as constant for the particular frequency. Figure-4.22 shows the responses for different loading conditions at 20 kHz.

Figure-4.21: Harmonic Responses (Maximum Vertical Displacement) of Die-Container Top Surface at Different Frequencies for 1.3 µm Loading
Figure-4.22: Harmonic Responses (Maximum Vertical Displacement) of Die-Container Top Surface at Frequency 20 kHz for Different Loading Conditions

Figure-4.23 shows the maximum vertical displacements of the die inner surface (inlet zone) for 1.3 µm loading and figure-4.24 shows the displacements for different loading conditions at 20 kHz. Hence the maximum vertical displacements at the die inner surface at 20 kHz frequency are obtained as 8.46 µm and 4.23 µm for 1.3 µm (100% amplitude) and 0.65 µm (50% amplitude) loading condition respectively. Since displacement profile shape is similar for different loading conditions, the response for loading condition in between them can be estimated by interpolating the available data. For example, the maximum vertical displacement of die inner surface will be 5.67 µm for 67% amplitude loading at 20 kHz frequency.
Figure-4.23: Harmonic Responses (Maximum Vertical Displacement) of Die Inner Surface at Different Frequencies for 1.3 µm Loading

Figure-4.24: Harmonic Responses (Maximum Vertical Displacement) of Die Inner Surface at Frequency 20 kHz for Different Loading Conditions
4.3.2.2.3  Response Variations for Different Plate Thickness

The response variations for the variation of the plate thickness which holds the die-assembly are investigated. Apart from the original plate (thickness 12.7 mm), two other plates (thickness 8 mm and 5 mm) are used for that investigation. The finite element model for the later two structures are created the same as the original one, however the nodes and elements number for them are different. The number of nodes and elements used to mesh the structure with 8 mm plate are 39386 and 153837 respectively; where for the structure with 8 mm plate they are 41414 and 159192 respectively. For this investigation 1.3 μm loading condition is used. Figures 4.25, 4.26 and 4.27 show the response variations for the different plates. From the figures it is evident that the response increases with decreasing plate thickness.

Figure-4.25: Harmonic Responses (Maximum Vertical Displacement) of Container Outer Surface at Frequency 20 kHz for Different Plate Thickness
Figure-4.26: Harmonic Responses (Maximum Vertical Displacement) of Die-Container Top Surface at Frequency 20 kHz for Different Plate Thickness

Figure-4.27: Harmonic Responses (Maximum Vertical Displacement) of Die Inner Surface at Frequency 20 kHz for Different Plate Thickness
4.4 CONCLUDING REMARKS

From the response analysis of the test set-up, it can be summarized that the imposed ultrasonic wave propagates in the longitudinal direction of the test set-up and also the transmission of the wave into the punch assembly is negligible. At 20 kHz frequency and for 67% amplitude of the ultrasonic generator, the longitudinal amplitude of the die is estimated as 5.67 µm which will be used for the relative movement calculation of the die-workpiece in the friction analysis to determine the frictional energy.
CHAPTER FIVE: EXPERIMENTAL STUDY OF ULTRASONIC MICROEXTRUSION

5.1 INTRODUCTION

A number of experiments are conducted to analyze the various aspects of the ultrasonic microextrusion process. This study will serve as the basis for the modeling of the temperature prediction. The phenomena such as effects of ultrasonic energy and tribological condition, the surface roughness of the billet and the formed part, and microstructure of the billet material are studied. Later the experimental temperature data from this section will be used for comparison with the predicted ones.

5.2 EXPERIMENTAL PROCEDURES

Forward extrusion experiments are conducted with and without ultrasonic energy. Two different lubricants are used with varied quantities. As received Brass (Cu/Zn: 70/30) rods of 2 mm diameter are cut to 5 mm length (140±2 mg) to be used as the billet. Care is taken to ensure that the surface of the billet does not get any scratch during the preparation. Split dies with 2 mm entry diameter and 1.2 mm exit diameter are used. Before the experiment, the billet and the split dies are washed using acetone. After air drying, the dies are placed into the container to ensure that an initial die temperature of 24°C is maintained for all the experiments. The lubricated billets are weighted to obtain the quantity of the lubricant for each condition. However, once a particular method of applying lubricant is established, the lubricated billets are not weighted during the experiment stages, since the lubricant tends to stick on the weighing scale. After placing the billet into the die using tweezers, a button is activated to start the extrusion process. The forming stroke and forming load data are recorded into the PC. The pressure gauges (electronic pressure sensor PA9020) are used to measure the forming load. The temperature data is recorded into a data-logger thermometer.
by inserting a 0.25 mm diameter thermocouple probe into the die-container hole. For ultrasonic assisted extrusion process, an ultrasonic generator is started to run for the desired time (3 seconds) coordinated with the forming stroke. A complete experiment from opening the die assembly to assembling back takes about 5 minutes, where the extrusion (3 mm stroke) itself takes only 3-6 seconds. Figure-5.1 shows the forward extrusion dies, punch and parts.

![Forward Extrusion Dies, Punch and Parts](image)

**Figure-5.1: Forward Extrusion Dies, Punch and Parts**

### 5.2.1 Ultrasonic Equipment Settings

As discussed earlier, an ultrasonic generator (20 kHz, 2000 W) and an ultrasonic transducer (3000 W, 20 kHz) are used for the experiments. There are three parameters which are set for the desired operation of the ultrasonic generator. They are the starting frequency ($f_{\text{start}}$), percentage of the amplitude ($A$) and the application time ($t$). Since the rated frequency of the transducer is 20 kHz and the generator has an automatic frequency tuning in a 1 kHz capture range, the $f_{\text{start}}$ is typically set to 0.50 kHz above the desired resonant frequency. The amplitude is initially set to 50% and then it is gradually increased to obtain the desired power output. The application time can be set up to 10 seconds, however if the generator can not obtain the resonant frequency for a particular starting frequency and amplitude percentage, it
will be automatically stopped even before the desired end time. After each successful operation, the resonant (working) frequency, maximum power output (W), and the energy (Ws) are known from the ultrasonic generator.

The following settings of ultrasonic generator are applied for the ultrasonic microextrusion experiments. These settings were also used for the test set-up response experiments described in chapter four. However for all the numerical and analytical modeling purposes, a working frequency of 20 kHz is used, which is very close to the experimental resonant frequency. It should be noted that varying these settings and also the transducer connector design, a power output up to 2000 W can be obtained.

Ultrasonic Generator Settings
- Starting Frequency ($f_{\text{start}}$): 20.50 kHz
- Percentage of Amplitude ($A$): 67%
- Application Time ($t$): 3 seconds

Ultrasonic Generator Output
- Resonant (Working) Frequency: 19.91 kHz
- Maximum Power Output: 700 W
- Energy Output: 1665 Ws

5.3 FORMING LOAD

5.3.1 Effects of Ultrasonic Energy

In order to study the effects of ultrasonic energy on the forming load, forward extrusion with and without ultrasonic energy are conducted for two different lubricant conditions. A 3 mm stroke is used for all the experiments. Figure-5.2 shows the forming load comparison for
case-1 condition, where 9.75% load reduction for ultrasonic assisted extrusion process is observed at maximum loading. Similarly, 7.30% load reduction for case-2 condition is observed at figure-5.3.

- **Case-1**: Lubsol W-72SK, billet is 2/3 vertically dipped into the lubricant and then rolling on paper, 1 mg per billet
- **Case-2**: Dailube DR-38, billet is 1/3 vertically dipped into the lubricant, 3 mg per billet

![Figure-5.2: Effects of Ultrasonic Energy on Forming Load, Case-1](image-url)
5.3.2 Effects of Tribological Condition

The presence of micro-plasto lubrication in the forward extrusion process is investigated using the artificially created macro-indents on the billet surface. If these macro-indents can trap the liquid lubricant during the extrusion process, the onset of micro-plasto lubrication affects the forming load. Therefore the forming load data from experiments with un-indent ed and indented billets are compared.

A Zwick-3212 Vickers hardness testing indenter is used for creating the inverted pyramid shape indents. Four small indents are created by applying 9.7 kgf load and 1 big indent by applying 19.7 kgf load. The small indents are on a line approximately 1 mm apart from center to center (figure-5.4) and the diagonal length of the small indent is 340 µm (figure-5.5a). The big indent is 180° away from the small indent center line and at the mid-length of the billet with 490 µm diagonal length (figure-5.5b). A Keyence VHX-600ESO digital microscope is used for capturing the 3-D images of the billets and the formed parts.
Forward extrusion experiments are conducted with un-indented and indented billets for three different lubricant conditions. Figures 5.6, 5.7 and 5.8 show the forming load comparison for un-indented and indented billets for case-1, case-2 and case-3 conditions respectively. For all three conditions, the forming loads are decreased for indented billets indicating the presence of the micro-plasto lubrication.

- **Case-1**: Lubsol W-72SK, billet is 2/3 vertically dipped into the lubricant, 4 mg per billet; 7.88% forming load reduction at maximum loading
Case-2: Lubsol W-72SK, billet is 1/3 vertically dipped into the lubricant, 3 mg per billet; 14.59% forming load reduction at maximum loading
Case-3: Lubsol W-72SK, billet is horizontally half dipped into the lubricant and then rolling on paper, 2 mg per billet; 7.85% forming load reduction at maximum loading

Figure-5.6: Effects of Tribological Condition on Forming Load, Case-1

Figure-5.7: Effects of Tribological Condition on Forming Load, Case-2
During the extrusion process, the macro-indent volume starts to decrease causing increase of pressure on the trapped lubricant (figure-5.9, big diagonal reduced from 490 µm to 412.49 µm). When the trapped lubricant pressure exceeds the interface pressure, the lubricant escapes the indentation pit supplying lubricant film between the matting surfaces of die and workpiece. Hence due to better lubrication conditions during the process, forming load decreases. Based on this observation it can be argued that the surface roughness pits may act in a similar fashion as these macro-indents. It is expected that for a perfectly smooth surface, devoid of any pits, will not promote any micro-plasto lubrication and therefore will require more forming load even though the initial lubrication conditions are the same.
5.4 TEMPERATURE MEASUREMENT

Since the extrusion die is fully covered by the punch assembly, no optical temperature measurement is possible. Therefore a K-type thermocouple probe with 0.25 mm diameter is used along with Omega HH127 data-logger thermometer. The probe is bent along the die-container outer surface and then inserted into the die-container hole to measure the temperature. This probe measures the temperature inside the die (figure-5.10), although the temperature of interest is at the die-workpiece interface. The sampling rate of the thermometer used is 4/sec and then the thermometer averages those 4 samples to record the temperature at 1 sec interval. Therefore the measured temperature is the average temperature rise in each second of the ultrasonic microextrusion process. It should be noted that the point beyond the forming duration cannot show the exact trend, since it is dominated by the temperature drops after the process duration. The measured temperature rise will later be compared with the temperature predicted by the model. In this experiment, Dailube DR-38 lubricant is used as a quantity of 3 mg per billet. The ultrasonic equipment settings are discussed in section-5.2.1.

![Temperature Measurement at Die](image)

Figure-5.10: Measurement of Temperature Rise at Die during Ultrasonic Microextrusion
5.5 SURFACE ROUGHNESS ANALYSIS

As observed from the tribology study, the surface roughness features are very important for microextrusion process, since they can trap liquid lubricant during the process and promote micro-plasto lubrication. Therefore attempts are made to analyze the surface roughness of the billet and the extruded part. A stylus profilometer is used to plot the surface roughness in the radial direction of the Brass billet and the extruded part.

- Form Talysurf Series Profilometer
  - Contact 2D profilometry
  - 10 mg stylus load, 2 µm stylus tip diameter
  - Vertical range 6 mm with 5 nm resolution

Figures 5.11 and 5.12 show the surface roughness profiles of a brass billet and an extruded part respectively. From these profiles, the presence of random pits capable of lubricant entrapment is evident. These pits are the basic features of the surface roughness which need to be further quantified as a regular geometry to be incorporated in the deformation model discussed in chapter six.
Figure-5.11: Surface Roughness Profile of the Brass Billet

Figure-5.12: Surface Roughness Profile of the Extruded Part
5.6 MICROSTRUCTURE ANALYSIS

The flow stress in microforming can vary depending on the relation between the material grain size and the desired part size. Therefore the grain sizes of the Brass sample are measured. The sample is embedded onto epoxy block followed by a polishing. Then the embedded sample is etched using a mixture of HNO₃ (5 ml), HCl (200 ml) and FeCl₃ (65 gm) for 10 seconds. After air drying, the sample is observed using microscope (figure-5.13a). Then the image is analyzed to obtain the following grain size measurements (figure-5.13b). In this study, the desired part size is more than twice of the maximum grain size. Therefore the part size will not affect the flow stress during the forming process.

Grain Size of Brass Sample

- Average Grain Size = 29.14 µm
- Maximum Grain Size = 59.83 µm
- Minimum Grain Size = 13.02 µm
- Standard Deviation = 10.42 µm

Figure-5.13: Microstructure of Brass Sample; (a) Raw Image, (b) Grain Size Measurement
5.7 CONCLUDING REMARKS

From the experimental study of ultrasonic microextrusion process the following conclusions can be drawn.

➢ The application of ultrasonic energy on the forward microextrusion process lead upto 9.75% load reduction.
➢ A load reduction upto 14.6% was observed when artificially indented billets were used. This indicates the presence of micro-plasto lubrication regime.
➢ A significant temperature rise inside the die was detected during the ultrasonic assisted microextrusion process.
➢ From the surface roughness analysis, the random pits capable of lubricant entrapment are observed for Brass samples.
➢ Since the desired part size in the current research is more than twice that of the Brass grain size, it will not affect the flow stress during the forming process.
6.1 INTRODUCTION

The intent of this chapter is to develop a model which can predict the temperature generated due to the plastic deformation and frictional energies during the ultrasonic microextrusion process. In order to determine this plastic deformation energy, a deformation model will be developed. This model requires that the original surface roughness be modeled as a regular geometry which will retain the basic features such as the roughness pits to hold the liquid lubricants. This model, along with the previous structural analysis results, will also provide the information required to determine the frictional energy. Finally the numerical transient thermal analysis is carried out to determine die temperature distribution over the forming time.

6.2 SURFACE ROUGHNESS MODELING

The surface roughness profile obtained in the previous chapter is analyzed to model the surface roughness as a regular geometry. In order to identify the pattern of the surface roughness profile, a number of asperities are superimposed along the profile (figure-6.1). Then the maximum height, the bottom width and the angles represented by left and right sides of each asperity are calculated. It should be noted that the side angles shown in figure-6.1 are not in scale, since the X and Y axes are in different units (mm and µm respectively). Since the sizes of asperities randomly vary along the length, they should be analyzed statistically. Although there are total 15 asperities identified in the profile, only 9 sets are chosen in a way such that a low standard deviation should prevail. The average values and the standard deviations for asperity are shown in table-1. Thus obtained average values of the asperity dimensions parameters are used for the surface roughness modeling.
Figure-6.1: Surface Roughness Profile of the Billet with the Superimposed Path

Table-6.1: Brass Billet Surface Roughness Asperity Dimensions

<table>
<thead>
<tr>
<th></th>
<th>Maximum Asperity Height (µm)</th>
<th>Bottom Width (µm)</th>
<th>Left Side Angle (degree)</th>
<th>Right Side Angle (degree)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Average</td>
<td>3.64</td>
<td>60.36</td>
<td>63.37</td>
<td>50.96</td>
</tr>
<tr>
<td>Standard Deviation</td>
<td>0.89</td>
<td>10.05</td>
<td>8.12</td>
<td>10.32</td>
</tr>
</tbody>
</table>

From this surface roughness analysis of the Brass billet, an asperity with 5µm height, 60 µm square bottom length and an angle of 60° for both left and right side is selected to represent the original surface roughness in the deformation model. Since the stylus tip is of 2 µm diameter, 1 µm height is added to the average height of the asperity to accommodate the probable depth not reachable by that stylus.

Figure-6.2 shows the proposed 3-D model of the workpiece where the actual surface roughness profile is modeled as surface asperities on the cylindrical billet where the number
of asperities on a circular section and the asperity height are defined based on the surface roughness analysis of the billet. The asperities are assumed to be of trapezoidal shape with a square bottom and top. Alternative sections of solid ring and ring with asperity are assumed, where the solid ring is also of the similar shape. Therefore the lubricant pits are of triangular shape in the initial condition.

![Figure-6.2: Proposed 3-D Model of the Workpiece showing the Surface Roughness Asperities](image)

### 6.3 ASPERITY PLASTIC DEFORMATION MODEL

In order to calculate the plastic deformation energy, a finite element method based model is proposed to explain the plastic deformation of cylindrical workpiece in forward micro-extrusion process. This model also provides the information necessary for calculating frictional energy.
In this deformation model, both micro and macro analyses are used to explain the plastic deformation (figure-6.3). Macro-analysis is used to estimate the deformation of the inner cylinder and it provides die interface pressure and the boundary conditions for the micro-analysis to estimate the deformation of the asperities on the cylinder. In order to analyze the deformation of the micro-asperities, finite element method is adopted and FORTRAN is used to formulate the analysis.

![Diagram of Coupling of the Macro and Micro Analyses](image)

**Figure-6.3: Coupling of the Macro and Micro Analyses**

6.3.1 Assumptions

Forward micro-extrusion process for cylindrical workpiece is chosen for this modeling (figure-6.4). The process has three zones, namely inlet zone (zone-I), work zone (zone-II) and outlet zone (zone-III).
Assumptions

- Mixed-film lubrication is assumed, so that friction will be present.
- Plastic deformation occurs only in the work zone (zone-II).
- Average interface pressure in the work zone is estimated and it is assumed that this pressure is applied as constant value throughout the work zone.
- Trapped lubricant exerts pressure on the four sides of the asperity. However, for the solid ring it exerts pressure on both sides.
- Since the lubricant is trapped and its mass is very small compared to die assembly, heat transfer to lubricant is neglected.
- Work for asperity deformation and friction work are converted to heat energy and this heat energy is transferred to the die assembly.
- Temperature at the die inner surface is the temperature at the workpiece-die interface.
6.3.2 Macro Analysis

As discussed above, macro analysis is used to establish two major variables, namely interface pressure acting at the billet-die interface and displacement boundary conditions. These variables are later used in the micro analysis.

6.3.2.1 Interface Pressure

As shown in figure-6.5, the interface pressure is determined with the aid of slab analysis. In this analysis Von-Mises yield criterion is applied and smaller die semi-angle \((\alpha)\) is assumed. The extrusion pressure \(\sigma_{\text{ext}}\) is determined using equation-6.1 where \(Y\) is the yield stress, \(\mu\) represents the friction coefficient, \(L\) is the die length and \(P\) is the interface pressure.

\[
\sigma_{\text{ext}} = Y\left(1 + \frac{\tan \alpha}{\mu}\right)\left[1 - \left(\frac{d_0 - 2x \tan \alpha}{d_e} \right)^{2\mu \cot \alpha}\right] 
\]

\[ (6.1) \]

Figure-6.5: Slab Analysis in Forward Extrusion Process
In the inlet zone (zone-I, figure-6.6), there is no deformation, therefore the interface pressure in zone-I will be equal to the extrusion pressure.

\[ P_I = \sigma_{ext} \quad (6.2) \]

In the work zone (zone-II, figure-6.6), the interface pressure is determined by manipulating equations 6.3 and 6.4 [62].

\[ \sigma_{ext} + P_{II} = Y \quad (6.3) \]
\[ P_{II} = Y - Y \left( 1 + \frac{\tan \alpha}{\mu} \right) \left[ 1 - \left( \frac{d_0 - 2x \tan \alpha}{d_e} \right)^{2 \mu \cot \alpha} \right] \quad (6.4) \]
\[ P_{av} = \frac{1}{L} \int P_{II} dx \quad (6.5) \]
6.3.2.2 Displacement Boundary Conditions

Figure-6.6 shows the separation of the proposed 3D model into two parts, namely inner cylinder and outer asperity surface. The analysis of the inner cylinder provides the information about the displacement boundary conditions which are used in the modeling of the asperity deformation.

![Forward Extrusion Process](image1)

(a) Inner Cylinder, (b) Outer Asperity Surface

Figure-6.7 shows 2D views of the various stages of forward extrusion for a billet with three circular sections, where \( r \) denotes the radius of the cylindrical workpiece, \( l \) denotes the bottom width of the asperity, \( \alpha \) is the die semi-angle and \( h \) is the height of the asperity. Figure-6.7(a) shows the initial billet which is just about to enter the die. Figure-6.7(b) shows the deformed first section inside the die and subsequently figure-6.7(c) shows the first two deformed sections inside the die. It is assumed that the deformed inner cylinder (bulk material) follows the die profile. A schematic of the cross-section of the workpiece is shown in figure-6.8. Using the geometrical relations and applying the volume constancy, the dimensions of the deformed billet are calculated, where the number of sections (\( N \)) in the billet is constant.
Figure-6.7: Various Stages of Forward Extrusion Process

Figure-6.8: Cross Section of Workpiece
Using the geometrical relations, equation-6.6 can be derived from figure-6.7(b). The constancy of the undeformed and deformed section volumes (figure-6.7b) is shown in the equation-6.7. Then solving these equations, both the deformed radius of inner cylinder \((r_{of1a})\) and width of bottom asperity \((l_{of1a})\) can be obtained, if the dimensions of the preceding section \((r_{oi1a}, r_{oi2}, l_{oi2})\) are known.

\[
l_{oi1a} = \frac{r_{oi1a} - r_{of1a}}{\tan \alpha} \quad (6.6)
\]

\[
\pi (r_{oi2})^2 l_{oi2} = \pi \left( \frac{r_{oi1a} + r_{of1a}}{2} \right)^2 l_{of1a} \quad (6.7)
\]

6.3.3 Micro Analysis

Micro analysis is used to analyze the plastic deformation of the asperities and hence to calculate the deformation energy. This also provides the necessary information for calculating the frictional energy.

6.3.3.1 Elastic-Plastic FEM Model

Finite element approach is used to analyze deformation of the micro-asperities. The initial trapezoidal shape of the asperity is set in the global co-ordinate system \((xyz)\). This geometry is transformed into a local co-ordinate system \((rst)\) in order to handle it conveniently. Figure-6.9 shows the same asperity in global and local co-ordinate systems. Hence it is analyzed as a 3-dimensional 8-node isoparametric element. The references for this FEM model are made from Bathe (1996) and Cook et al. (2002). Details on the derivations are presented in the appendix-A.
In this analysis, virtual displacement theory is applied to define the stiffness matrix $K$. According to the virtual displacement theory, the equilibrium of a body requires that for any compatible, small virtual displacements that satisfy the essential (geometric) boundary conditions imposed onto the body, the total internal virtual work is equal to the external virtual work (equation-6.8). The internal virtual work given on the left side of equation-6.8 is equal to the actual stresses $\tau$ going through the virtual strains $\varepsilon$ corresponding to the imposed virtual displacements $\mathbf{U}$. The external work given on the right side of the same equation is equal to the actual forces $f^B$ (body force), $f^S$ (surface force) and $R_C$ (concentrated force) going through the virtual displacements $\mathbf{U}$. The superscript $S$ denotes that surface displacements are considered and the superscript $i$ denotes the displacements at the point where the force $R_c$ is applied. The stress $\tau$ is defined in equation-6.9, where $\tau'$ is the given element stress and $C$ is the material matrix. The strain $\varepsilon$ corresponding to the displacement $U$ is defined by the equation-6.10 where $B$ is strain-displacement matrix.
\[
\int_{v} \bar{e}^T \delta v = \int_{v} \bar{U}^T f^B dv + \int_{f} \bar{U}^s \bar{f}^{s} ds + \sum_{i} \bar{U}^i R^i \quad \text{(6.8)}
\]

\[
\tau^{(m)} = C^{(m)} \varepsilon^{(m)} + \tau^{f(\infty)} \quad \text{(6.9)}
\]

\[
\varepsilon = BU \quad \text{(6.10)}
\]

If unit virtual displacement is imposed on equation-6.8, using equations 6.9 and 6.10, the equation-6.8 can be rewritten in form of equation-6.11, where the terms \( K \) and \( R \) (total load) are defined by the equations 6.12 and 6.13 respectively. Since the body load \( (R_B) \) and initial load \( (R_I) \) are assumed to be zero, only the surface load \( R_S \) (equation-6.14) and the reaction load imposed by the displacements are considered. The surface pressure \( f^S \) is obtained from the hydrodynamic and/or hydrostatic pressure and the die-billet interface pressure using slab analysis for the respective surfaces. \( S_I \) to \( S_q \) denotes the surface where the surface forces are working and \( H^S \) denotes the respective surface interpolation matrix.

\[
K U = R \quad \text{(6.11)}
\]

\[
K = \sum_{m} \int_{(m)} B^{(m)T} C^{(m)} B^{(m)} dv^{(m)} \quad \text{(6.12)}
\]

\[
R = R_B + R_s - R_I + R_c \quad \text{(6.13)}
\]

\[
R_s = \sum_{m} \int_{S_I}^{S_q} H^{s(m)T} f^{s(m)} ds^{(m)} \quad \text{(6.14)}
\]

In order to calculate the stiffness matrix \( K \), the material matrix \( C \) needs to be calculated. Here elastic-plastic material model is assumed and the material matrix \( C \) is defined by \( E_{ep} \) (equation-6.15). In the derivation of \( E_{ep} \), isotropic hardening \( (W_p) \), associative flow rule and Von-Mises theory are assumed. In equation-6.15, \( E \) refers to the Young’s modulus of the elasticity, whereas \( \frac{\partial F}{\partial \sigma} \) and \( P_\lambda \) are defined by the equations 6.16 and 6.17 respectively. \( F \) is the Von-Mises yield function and \( H_P \) is the strain hardening parameter (plastic modulus).
\[
[E_{ep}] = [E] \left( [I] - \frac{\partial F}{\partial \sigma} [P_{\lambda}] \right) \tag{6.15}
\]
\[
\left\{ \frac{\partial F}{\partial \sigma} \right\} = \frac{3}{2\sigma_0} \begin{bmatrix} S_x & S_y & S_z & 2S_{xy} & 2S_{xz} & 2S_{yz} \end{bmatrix}^T \tag{6.16}
\]
\[
[P_{\lambda}] = \frac{\left\{ \frac{\partial F}{\partial \sigma} \right\}^T [E] \left[ \frac{\partial F}{\partial \sigma} \right] - \frac{\partial F}{\partial W_p} \left[ \sigma \right]^T \left\{ \frac{\partial F}{\partial \sigma} \right\} - \left\{ \frac{\partial F}{\partial \sigma} \right\}^T \left[ E \right] \left\{ \frac{\partial F}{\partial \sigma} \right\} + H_p \tag{6.17}
\]

Once the material matrix \( E_{ep} \) is known, the stiffness matrix \( K \) could be calculated from equation-6.12. Using an initial guess which is lower than initial yield condition of the material, the material matrix and the stiffness matrix can be calculated. The load needs to be applied on the system. Newton-Raphson iteration is used here to solve the nonlinear equation involving displacement dependent stiffness and load (equation-6.18). The total load is comprised of known surface load and unknown reaction loads due to the applied displacement boundary conditions. The surface load is applied incrementally and also the displacement boundary conditions are applied in incremental values. At first iteration, the stiffness matrix calculated for the initial guess is used and then using the equation-6.18 the displacement increment is calculated. From this displacement, the strains and stresses are calculated. With these new values of \( B \)-matrix and material matrix, the stiffness matrix is updated. For this new condition, the nodal forces are calculated (equation-6.19). The imbalance is calculated by subtracting the nodal force from the applied load. Then this imbalance is used as applied load and displacement is calculated using similar way. The convergence criterion for this iteration is controlled by the equation-6.20.

\[
[K][\Delta U] = \{ \Delta R \} \tag{6.18}
\]
\[
\{ \Delta R \}_{nf} = \int [B]^T \{ \sigma \} dV \tag{6.19}
\]
6.3.3.2 Hydrostatic and Hydrodynamic Pressure

In the proposed model the geometry of the surface asperities are assumed to be trapezoidal. Moreover the billet is assumed to be composed of alternative sections of solid ring and ring with asperity. With this configuration, the liquid lubricant will be trapped in the gaps enclosed by the solid rings and between two adjacent asperities. Once the ring with asperity enclosed by two solid rings enters the inlet zone (zone-I), then the surrounding gaps are sealed and hydrostatic pressure starts to build up. The gap volume surrounding a single asperity moving toward extrusion direction (along y-axis) can be calculated easily from the dimensions of the asperity. If the gap volume decreases, the density of the liquid lubricant increases and hence pressure builds-up. From the density-pressure data [40] for a particular lubricant, the hydrostatic pressure for the trapped lubricant is estimated (figure-6.10).

![Diagram](image)

Figure-6.10: Calculation of Hydrostatic Pressure Build-Up

Due to the speed and viscosity of the lubricant, there may be hydrodynamic pressure rise which can be calculated by solving the Reynolds equation, \( \frac{dq}{dx} = 6\mu \frac{h - h_m}{h^3} \). However, for
this model, hydrodynamic pressure rise is not considered. Hence the contribution of the trapped lubricant to the pressure on the side walls of the asperity comes only from the hydrostatic pressure rise.

6.3.4 Summary of the Asperity Deformation Model

In this model, the initial dimensions of the asperity are estimated from the surface roughness of the billet material. Then the die interface pressure is calculated, which is converted to surface load using the surface interpolation functions. Macro analysis also gives the dimensions for the bottom four points of the asperity which are used as the boundary conditions for FEA based micro analysis. With the initial guess, the simulation starts, however there is no hydrostatic pressure build-up in the first step. After the first section (on a ring) is deformed, the displacements and positions of the deformed asperity are updated. Here, only the actual displacements are considered and all other translational displacements are excluded. Using the dimensions of the deformed asperity, the hydrostatic pressure build-up is calculated if there is any. After the deformation, the stress-strain conditions for the 8 sampling points will be known and from that information the stress-strain at any point inside the asperity will be known. The stress-strain values and the dimensions of the deformed asperity will then be used to calculate the deformation and frictional energy. Since alternative sections of solid ring and ring with asperity are assumed for this model, the effective stress and strain calculated for an asperity is assumed to be equal to the effective stress and strain for a section (on solid ring) with same bottom dimensions of an asperity. Moreover, the volume and top surface area for that solid section is very close to the asperity with similar bottom dimension.
6.3.5 Plastic Deformation Energy

The incremental plastic deformation work per volume is defined by equation-6.21 [65] and the amount of plastic work converted into heating is defined by equation-6.22 [43]. The information necessary to calculate plastic deformation energy that is converted to heat energy is obtained from the previously developed asperity deformation model.

\[ dw = \bar{\sigma} d\bar{\varepsilon} \]  
\[ q_D = \beta dw \]

where, \( dw \) - plastic deformation work per volume, \( \bar{\sigma} \) - the effective stress, \( \bar{\varepsilon} \) - the effective strain, \( q_D \) – heat energy converted from plastic work, \( \beta \) - the fraction of plastic work converted into heating.

6.4 FRICTION ANALYSIS

The frictional energy component of the total heat energy generated during the ultrasonic microextrusion is calculated using the friction analysis of the process.

6.4.1 Relative Movement of Die and Billet due to Ultrasonic Vibration

Due to imposed ultrasonic energy on the tooling, the die and container move forward and backward causing a sliding velocity between the die-container and the billet. Since the velocity of the die-container is much larger than the velocity of the billet, continuous engagement and disengagement of die and the billet will occur throughout the process. Figure-6.11 shows the schematic of relative movement of the die-container and the billet.
The motion of the die assembly is controlled by the transducer which transfers the vibrational motion to the die assembly and the motion of the billet is controlled by the punch. Since the velocity before and after the engagement of die and the billet follows the respective velocity profiles controlled by the transducer and the punch, any effect of collision or impact during the engagement is neglected. Otherwise the resultant velocity of collision will deviate from the imposed velocity profile. Moreover it should be noted that a wave transmissibility coefficient, $\alpha_{\text{eff}} = 0$ is considered for the die-billet system as no shear wave propagation is observed in the numerical structural analysis of the test set-up at the operating vibration frequency. Bunget and Ngaile (2008) have defined wave transmissibility coefficient as a measure of how much the translational motion of the die is transmitted to the billet. They stated that this coefficient can vary from 0 to 1. Where $\alpha_{\text{eff}} = 0$ means the die oscillations are not transmitted to the work-piece, and $\alpha_{\text{eff}} = 1$ means that die oscillations are completely transmitted to the work-piece, implying that the relative velocity between the die and the billet is zero.
The disengagement and engagement time can be calculated from the displacement profiles of the die and the billet (figure-6.12). The billet is moving to the extrusion direction at velocities $v_0$ and $v_1$ in zone-I and zone-II respectively. The die is assumed to follow a simple harmonic motion with angular velocity $\omega$, velocity $v_d$, frequency $f$ and amplitude $A$. This amplitude $A$ is obtained from structural analysis of the microforming test set-up for the given conditions as discussed in chapter four. The relative velocities between the die and the billet in the inlet zone and in the work zone are calculated by equations 6.26 and 6.27 respectively. However, it should be noted that the die and the billet remain engaged at the inlet zone for the entire cycle, whereas at the work zone they remain engaged from time $t_1$ until the end of the cycle at time $t_2$. Hence, the sliding distances at inlet and work zones for a full cycle are calculated using equations 6.28 and 6.29.

$$v_1 = \frac{v_0}{\cos \alpha} \quad (6.23)$$

$$\omega = 2\pi f \quad (6.24)$$
\[ v_d = \omega A \cos \omega t \quad (6.25) \]
\[ v_{\text{relative-I}} = v_0 - \omega A \cos \omega t \quad (6.26) \]
\[ v_{\text{relative-II}} = v_1 - \omega A \cos \omega t \quad (6.27) \]
\[ Sliding \ Distance (\text{Zone} - I) = \int_{t_0}^{t_2} v_{\text{relative-I}} \, dt \quad (6.28) \]
\[ Sliding \ Distance (\text{Zone} - II) = \int_{t_1}^{t_3} v_{\text{relative-II}} \, dt \quad (6.29) \]

6.4.2 Real Area of Contact

The real area of contact is much lower than the apparent area of contact and it depends on the surface roughness of the matting surfaces and the normal pressure acting on the interfaces [34]. In this study, the surface roughness of the die is almost smooth compared to that of the billet. Therefore only the real contact area of the billet surface will be estimated. As observed in the surface roughness profile of the billet, there are a number of pits which need to be quantified in order to measure the real area of contact. A statistical method proposed by Ahmed and Sutcliffe (2000) is used to identify the pits. In this method, the height \( h(i) \) of each point in the surface roughness profile is compared with the heights of certain neighboring points to obtain the pit depth \( d(i) \) relative to the neighboring area. The location of neighboring points to be used depends on the spacing of the data points (\( \Delta \)), the spanning length (\( L \)) and the number of points (\( n \)). The data spacing of the stylus profilometer used is 0.28 \( \mu m \). The spanning length should be greater than the typical pit size such that \( L/\Delta \) is an integer. In this analysis, a spanning size of 5.5 \( \mu m \) is used. The value of \( n \) is taken as 4, which provides total of 8 neighboring data points to calculate the mean height. Equation-6.30 shows the modified equation to calculate the relative pit depth \( d(i) \). Then this relative pit depth needs to be normalized by the average surface roughness \( R_d \) (0.72 \( \mu m \)) and compared with a pit depth criterion \( \delta_p \). Hence any point satisfying the equation-6.31 belongs to a surface
roughness pit. The value of pit depth criterion needs to be selected considering the objective of the pit identification. A value such as 0.2 can be used to identify the even smaller pits, whereas a higher value such as 0.7 can be used to identify the larger pits only. In order to visually identify the pits on the surface roughness profile, the dimensionless values from the equation-6.32 can be plotted together with roughness profile. Then any positive value indicates the pit. The choice of the parameters $L, n$ and $\delta_p$ should be verified by checking whether the equation-6.32 is able to resolve the desired pits on the roughness profile.

$$
d(i) = \frac{1}{2n} \left( \sum_{j=i-L/n+1}^{j=i-L/n} h(j) + \sum_{j=i+L/n}^{j=i+L/n+1} h(j) \right) - h(i)
$$

$$
\frac{d(i)}{R_a} > \delta_p \quad (6.31)
$$

$$
Y_p = \frac{d(i)}{R_a} - \delta_p \quad (6.32)
$$

Figure-6.13(a) shows the surface roughness profile of the Brass billet along with the pit identifier (equation-6.32) and figure-6.13(b) shows an enlarged part of previous figure. From these figures, it is clearly observed that this statistical method can identify the desired surface roughness pits successfully. Although there are 4000 data points in the surface roughness profile, the first 20 and the last 20 data points cannot be checked due to the lack of neighboring points. Among the 3960 data points, 1001 data points yield positive values (equation-6.32) and hence identified as the part of the pit regions. Therefore the remaining points can be assumed as the points which can be in contact with die. However due to the presence of lubricant film and the acting normal pressure, all of those points can not make contact with the die. Assuming about half of those points make contact with the die surface, a ratio ($\zeta$) of real and apparent area of contact can be obtained as 0.35. This ratio will be later required for the frictional energy calculation.
6.4.3 Frictional Energy

The friction work per cycle (equation-6.33) is estimated using the friction force (equation-6.34) and the sliding distance per cycle at respective zones (inlet and work). Then the fraction of this friction work is converted into heating which is defined by equation-6.35. The
pressure \((P)\) and the asperity top surface area are obtained from the asperity deformation model.

\[
Friction \text{ work (per cycle)}, W_f = Friction \text{ Force} \times Sliding \text{ Distance} \tag{6.33}
\]

\[
Friction \text{ Force}, F_f = (P \times \text{Asperity Top Surface Area} \times \zeta) \mu \tag{6.34}
\]

\[
q_f = \alpha W_f \tag{6.35}
\]

where, \(\mu\) - friction coefficient, \(\zeta\) - ratio of the real and apparent area of contact, \(q_f\) - heat energy converted from friction work, \(\alpha\) - the fraction of friction work converted into heating.

### 6.5 THERMAL ANALYSIS FOR TEMPERATURE PREDICTION

In the ultrasonic microextrusion process, the temperature is generated due to the effect of both plastic deformation and frictional energies. As discussed earlier, the energy for plastic deformation of the asperity and the fictional energy generated in each cycle of the ultrasonic vibration are calculated. Then a fraction of these energies that is converted into heat energy is applied to the system to predict the temperature distribution over the forming time.

Finite element software package ANSYS is used for this numerical analysis. In order to perform the transient thermal analysis, several assumptions are made. Since the mass of the workpiece is very small compared to that of die assembly, it is not considered in the thermal analysis. The frictional energy generated in the interface is assumed to be consumed only by the die assembly. Moreover only the asperity deformation energy is transferred to the die assembly. Hence it is assumed that the die inner surface temperature due to this energy is the workpiece surface temperature.

In this study, a number of asperity rings are proposed to define the billet (figure-6.14). The total energy from plastic deformation of one ring is known from the deformation model.
Moreover, knowing the number of cycles a ring experiences in a certain location, the total frictional energy for the ring in that location can be obtained. Finally the fraction of this combined plastic deformation and frictional energy as heat energy is applied on the die inner surface over the time it takes to pass that section. Hence, the table containing the input heat energy for the die inner surface divided into small sections is obtained. A detailed pre-processing of this thermal analysis will be presented in the results chapter.

From this transient thermal analysis, the temperature mapping on the die assembly is established over the entire forming time. The die-workpiece interface temperature will also be established from this analysis. The predicted temperature is verified experimentally by measuring the temperature on die-workpiece interface and inside the die assembly during the ultrasonic microextrusion process.

Figure-6.14: Intermediate Stage of Forward Extrusion Process
6.5.1 Process Conditions and Material Properties

Die-Workpiece Interface Friction Coefficient, $\mu = 0.035$ (This typical value for mixed-film lubrication is selected)

Punch Speed = 1 mm/sec
Forming Time = 3 sec
Initial Temperature of the Test Set-up = 297.15 K (24°C)
Air Convection Film Coefficient (Typical Indoor) = 40 W/m²K
Bulk Temperature of Air (Room Temperature) = 297.15 K (24°C)
$\alpha = \beta = 0.90$

Ultrasonic Energy: 5.67 µm Longitudinal Amplitude at Die, for 20 kHz vibration and 67% Amplitude at Ultrasonic Generator

Workpiece

Material: Brass-CDA-110
Young’s Modulus, $E = 115$ GPa
Poisson’s Ratio, $\nu = 0.33$
Initial Yield Stress = 200 MPa
Constant Plastic Modulus, $H_p = 427$ MPa
Density, $\rho = 8500$ kg/m³
Specific Heat Capacity, $C = 0.377 \times 10^3$ J/kgK
Billet: 2 mm Diameter, 5 mm Long
Asperity: 0.005 mm Height, 0.06×0.06 mm Bottom, 60° Side Angle, $1.63495 \times 10^{-5}$ mm³
Volume
Number of Asperities on a Ring = 104
Number of Rings = 83
Die Assembly

Material: A2 Tool Steel (Harden and Tempered to 62 HRC)
Die: 2 mm Entry Dia, 1.20 mm Exit Dia, 1.20 mm Long

Table-6.2: Physical Properties of A2 Tool Steel (Harden and Tempered to 62 HRC)

<table>
<thead>
<tr>
<th></th>
<th>293.15 K (20°C)</th>
<th>473.15 K (200°C)</th>
<th>673.15 K (400°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thermal Conductivity (W/m²K)</td>
<td>26.0</td>
<td>27.0</td>
<td>28.5</td>
</tr>
<tr>
<td>Density (kg/m³)</td>
<td>7750</td>
<td>7700</td>
<td>7650</td>
</tr>
<tr>
<td>Heat Capacity (J/kgK)</td>
<td>460</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

6.5.2 Heat Energy from Plastic Deformation and Friction Analysis

Pressure Calculations

\[ P_I = \sigma_{ext} = 254 \text{ MPa} \quad \text{(taking the absolute value)} \]

\[ P_{av} = 334.5 \text{ MPa} \]

Hydrostatic Pressure Rise: Synthetic Lubricant (Diester, D1 - (2-Ethyl-Hexyl) Sebacate) is used for calculation. Data from ASME Pressure-Viscosity Report (1953).
Table-6.3: Heat Energy from Plastic Deformation and Frictional Energy

<table>
<thead>
<tr>
<th>Zone</th>
<th>Location of Ring Section</th>
<th>Asperity Top Surface Area (mm²)</th>
<th>Sliding Distance (mm)</th>
<th>Heat Energy per Asperity and per Cycle from Friction Work (J)</th>
<th>Heat Energy per Asperity from Plastic Deformation Work (J)</th>
</tr>
</thead>
<tbody>
<tr>
<td>I</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>2.94E-03</td>
<td>22.66E-03</td>
<td>1.87E-07</td>
<td>5.85E-07</td>
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<tr>
<td></td>
<td></td>
<td>3.09E-03</td>
<td></td>
<td>1.29E-07</td>
<td>4.27E-07</td>
</tr>
<tr>
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<td></td>
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<td></td>
<td>1.32E-07</td>
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</tr>
<tr>
<td></td>
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<td>3.23E-03</td>
<td></td>
<td>1.35E-07</td>
<td>5.78E-07</td>
</tr>
<tr>
<td></td>
<td></td>
<td>3.37E-03</td>
<td></td>
<td>1.41E-07</td>
<td>7.02E-07</td>
</tr>
<tr>
<td></td>
<td></td>
<td>3.64E-03</td>
<td></td>
<td>1.52E-07</td>
<td>8.21E-07</td>
</tr>
<tr>
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<td></td>
<td>4.16E-03</td>
<td></td>
<td>1.74E-07</td>
<td>9.31E-07</td>
</tr>
<tr>
<td>II</td>
<td></td>
<td>5.06E-03</td>
<td>11.33E-03</td>
<td>2.12E-07</td>
<td>10.89E-07</td>
</tr>
<tr>
<td></td>
<td></td>
<td>6.52E-03</td>
<td></td>
<td>2.72E-07</td>
<td>12.87E-07</td>
</tr>
<tr>
<td></td>
<td></td>
<td>8.72E-03</td>
<td></td>
<td>3.64E-07</td>
<td>15.73E-07</td>
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<td>11.94E-03</td>
<td></td>
<td>4.99E-07</td>
<td>19.26E-07</td>
</tr>
<tr>
<td></td>
<td></td>
<td>16.60E-03</td>
<td></td>
<td>6.93E-07</td>
<td>24.22E-07</td>
</tr>
<tr>
<td></td>
<td></td>
<td>23.44E-03</td>
<td></td>
<td>9.80E-07</td>
<td>32.06E-07</td>
</tr>
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<td></td>
<td></td>
<td>33.96E-03</td>
<td></td>
<td>14.19E-07</td>
<td></td>
</tr>
</tbody>
</table>

Note: The location of ring section in zone-II is illustrated in the figure-6.14.

6.5.3 Pre-Processing of Numerical Transient Thermal Analysis

ANSYS is used for the numerical transient thermal analysis of the die assembly which is isolated from the microforming set-up with appropriate boundary conditions. Furthermore this die assembly (figure-6.15a) is assumed to be a single part which is modeled as a 2-D axisymmetric geometry with Plane55 2-D thermal solid element consisting of four nodes with a single degree of freedom (temperature) at each node. The number of nodes and elements used are 1385 and 1192 respectively. Figure-6.15(b) shows the finite element model.
of the assembly with the applied boundary conditions. Since this assembly is attached to the microforming test set-up which is very heavy compared to the die assembly and also the bottom of the die assembly is relatively far from the die zone, the bottom of the die assembly is constrained at constant room temperature of 24°C. All other open surfaces except the die inlet and work zones are considered to be in contact with typical indoor air (convective boundary condition).

Figure-6.15: (a) CAD Model of the Die Assembly, (b) Finite Element Model of the Die Assembly showing the Boundary Conditions

6.5.4 Inlet Zone Heat Energy

Since in the inlet zone (zone-I), the asperities are not deformed, the frictional energy generated in each cycle is equal. That energy is applied on a 0.5 mm line (surface) on the axisymmetric 2-D model and this section is passed by the material in 0.5 sec, since the extrusion speed is 1 mm/sec. It is assumed that the passing of 0.5 mm section is subjected to
a linearly decreasing energy, whereas the other contact sections are subjected to the constant heat energy. From the analytical model the heat energy (J) developed by a single asperity is calculated (refer to table-6.2) which is multiplied by the number of asperities on a ring section (104), number of the possible rings (8.3) of the billet on a 0.5 mm width section and the number of cycles (10000) they experience to pass this 0.5 mm width. Then, the heat flux (w/m²) is calculated based on the heat energy (J) generated by 0.5 mm width billet section, the surface area represented by the line (0.5 mm) section of that 2-D model and the time (0.5 sec). This heat flux calculation steps are shown below.

- Heat Energy per Asperity and per Cycle, \( H_1 = 1.87 \times 10^{-7} \) J
- Heat Energy per Ring and per Cycle, \( H_2 = 104 \times H_1 = 1.94 \times 10^{-5} \) J
- Heat Energy per 0.5 mm section width and per Cycle, \( H_3 = 8.3 \times H_2 = 1.61 \times 10^{-4} \) J
- Heat Energy per 0.5 mm section width and for 10000 Cycles, \( H_4 = 10000 \times H_3 = 1.61 \) J

Since the heat energy \( H_4 \) is generated in 0.5 sec, the heat rate, \( Q = H_4 / 0.5 \) J/sec = 3.22 w

Heat Flux = \( Q / (\text{Area defined by 0.5 mm line section in 2-D model}) \) w/m²  
= \( Q / (3.14 \times 10^{-6}) \) w/m²  = 1.03 \times 10^6 w/m²

Finally, this heat flux as a surface load is applied on the line section. Hence for a 5 mm long billet, 10 sections (0.5 mm width) are taken for the inlet zone along the extrusion direction. Table-6.3 shows the applied heat flux on the sections and figure-6.16 shows the section locations. Total heat energy as a fraction (90%) of the frictional energy applied on the inlet zone is 78.90 J over a period of 3 sec. This is calculated by adding all the heat energies generated by the billet sections that are in contact with the die in the 3 sec duration and also considering the passing of sections where the energy generation linearly decreases to zero.
Table-6.4: Heat Flux ($\times 10^6$ w/m$^2$) Applied on the Inlet Zone of the 2-D Model

<table>
<thead>
<tr>
<th>Time (sec)</th>
<th>Sec-1</th>
<th>Sec-2</th>
<th>Sec-3</th>
<th>Sec-4</th>
<th>Sec-5</th>
<th>Sec-6</th>
<th>Sec-7</th>
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<td>1.03</td>
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<tr>
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<tr>
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<td>1.03</td>
<td>1.03</td>
<td>1.03</td>
</tr>
</tbody>
</table>

Figure-6.16: Line Sections on the Die Zone to apply the Heat Energy
6.5.5 Work Zone Energy

Both the deformation and frictional energies are established in the work zone (zone-II). Since the asperities are deformed, the surface area which is in contact with the die is also changed. Unlike the inlet zone, the frictional energy is not constant in the work zone; rather it is varying with the deformation characteristics of the asperity. Hence, the frictional energy for the deformed ring sections belong to a certain line section on the 2-D model which is added together and then multiplied by the number of cycles (eg.: 10000 cycles for a 0.5 mm width section) it experiences there. The deformation energy that is generated for each ring section is also added to the respective frictional energy. However, it should be noted that the asperity deformation energy only contributes about 0.03% to the total heat energy generated in the work zone, which is rather insignificant for this thermal analysis. For this work zone, both the heat flux and the total heat energy are calculated in a similar way as the inlet zone calculation except that the deformation energy is also added to the total heat energy.

Table-6.4 shows the corresponding heat flux values applied on the line section in the 2-D model. Total heat energy as a fraction (90%) of the frictional and deformation energy applied on the work zone is 27.22 J over a period of 3 sec. Hence the total heat energy applied on the die assembly is 106.12 J over a period of 3 sec.

Table-6.5: Heat Flux ($\times 10^6$ w/m$^2$) Applied on the Work Zone of the 2-D Model

<table>
<thead>
<tr>
<th>Time (sec)</th>
<th>Sec-1</th>
<th>Sec-2</th>
<th>Sec-3</th>
</tr>
</thead>
<tbody>
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<td>0.00</td>
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<td>0</td>
<td>0</td>
</tr>
<tr>
<td>0.50</td>
<td>0.74</td>
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<tr>
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<td>0.74</td>
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</tr>
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<td>1.50</td>
<td>0.74</td>
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<td>3.00</td>
<td>0.74</td>
<td>2.36</td>
<td>3.52</td>
</tr>
</tbody>
</table>
6.5.6 Numerical Transient Thermal Analysis

Full transient thermal analysis with 0.01 sec step size is performed using ANSYS. Figure-6.17 shows the final state (at the end of 3 sec) of the die assembly. Figure 6.18 shows the progression of temperature generation over the forming time.

Figure-6.17: Temperature (K) Distribution on the Die Assembly at the End of 3 sec

Figure-6.18: Progression of Temperature (K) Generation
Figure 6.19 shows the temperature distributions on the die inner surface along the extrusion direction at different time steps, where the maximum temperatures are observed at the work zone. The work zone surface temperature distributions along the extrusion direction at different time steps are presented in figure 6.20. It is observed that after the material reaches the exit (after 1.2 sec), the temperature profiles follow a certain pattern and the highest temperature (98°C) is found at 0.3 mm distance from the work zone entry (Li).

Figure-6.19: Temperature Distribution on the Die Inner Surface
Figure-6.20: Temperature Distribution at the Die Work Zone Surface

The radial direction temperature distributions at a distance of 0.3 mm from the die work zone entry are shown in figure-6.21, where the temperature drops very fast as it approaches to the outer surface. Figure-6.22 shows the temperature distribution on the container outer surface. This surface temperature can easily be measured during the ultrasonic microextrusion process. By comparing the measured temperature values with the predicted ones from the numerical analysis, the actual die inner surface temperature can be estimated.
Figure-6.21: Radial Direction Temperature Distribution on Die Assembly Cross-Section

Figure-6.22: Temperature Rises on the Container Outer Surface
6.5.7 Comparison with Experimental Results

In order to compare the predicted temperature rise with the experimental one, the data from chapter five is used. Figure-6.23 shows the measured temperature data with the predicted data, where the probe is located at the end of the bigger side hole. The model can predict the temperature data at 0.01 sec interval which are presented by the set ‘Sim’. From the figure, it can be observed that the model overestimates the temperature by 8.66% at the end of 2.5 seconds, which is 83% completion of the process. The next available measured data is beyond the forming duration and is dominated by the sudden drops in temperature. However from the overall trend, it can be argued that the actual temperature at the end of the process (3.0 seconds) is closer to the predicted one.

![Temperature Rise at Die](image)

Figure-6.23: Comparison between Measured and Predicted Temperature

The discrepancy between the measurement and the prediction can be explained by two groups of factors, one that arises from the experiment technique and the other from the model itself. During the temperature measurement, since there is a smaller hole ahead of the probe, the heat transfer to the probe is not solely from the conduction. A convective heat transfer
may occur. Therefore the measured temperature by the probe is smaller than the actual temperature at the die location of the end of the bigger side hole.

Although the model considers a number of phenomena/variables pertaining to ultrasonic microextrusion process, there are still some phenomena left which can be attributed to the overestimation. Those phenomena can be identified mainly as two groups, one that is directly responsible for the change in the friction condition and the other is the sonochemical effects, which also contribute to the friction condition.

In the model, a constant friction condition is assumed throughout the extrusion process. However in reality, the friction condition changes with the progress of the extrusion process. As discussed in chapter six, the frictional energy is the dominating factor for the overall temperature generation in ultrasonic microextrusion process. Therefore, a reduction in frictional energy will generate a lesser temperature than the temperature generated for a constant friction condition. The following are the factors which can affect the assumed constant friction condition during the extrusion process.

- As the extrusion process progresses, the geometries of the surface roughness asperities change and the micro-plasto hydro lubrication may occur. Therefore the friction condition improves causing the reduction of frictional energy.
- Due to application of the ultrasonic energy, there is an improvement in the lubricant transportation. This will also improve the friction condition during the ultrasonic microextrusion process.
- The ratio of real and apparent area of contact between the die and workpiece surfaces is assumed constant. However this ratio may decrease based on the interface pressure and better lubrication, thus reducing frictional energy.
However, there may be the presence of particle erosion phenomenon due to the imposed ultrasonic energy. This phenomenon can have detrimental effects on the friction condition by increasing the frictional energy.

The application of ultrasonic energy can introduce sonochemical effects into the liquid lubricant. As discussed in chapter one, there may be cavitation micro hot-spots in the lubricant emitting very high temperature and pressure. This phenomenon can change the lubricant behavior during the ultrasonic microextrusion process, which is not considered in the model. The characteristics of lubricant can also be affected by the sonochemical reactions. Consequently, the friction condition can be affected causing change in the frictional energy.

6.5.8 Sensitivity Analysis

It is observed in the work zone energy calculation that the asperity deformation energy contributes less than 0.5% to the total heat energy. Therefore parameters contributing to the friction energy are very important for the temperature generation in ultrasonic microextrusion process. Two such parameters are friction coefficient ($\mu$), and ratio of real and apparent area of contact ($\zeta$). Sensitivity analyses are performed to investigate the dependency of the temperature rise on these parameters. For each case the particular parameter is varied keeping all other parameters constant. Figure-6.24(a) shows the final temperature variation at the die inner surface with respect to friction coefficient ($\mu$). Figure-6.24(b) shows that the maximum temperature linearly increases with increasing value of $\mu$. 
Figure-6.24(a): Temperature Distribution at Die Inner Surface at the end of the Forming Time for Variation of Friction Coefficient in Ultrasonic Microextrusion

Figure-6.24(b): Maximum Temperature Rise in Ultrasonic Microextrusion for Variation of Friction Coefficient
Figure-6.25(a) shows the final temperature variation at the die inner surface with respect to ratio of real and apparent area of contact ($\zeta$). Figure-6.25(b) shows that the maximum temperature linearly increases with increasing value of $\zeta$. Since the both $\mu$ and $\zeta$ are varied in same percentage ($\mu$: 0.02, 0.03, 0.035, 0.04, 0.05; $\zeta$: 0.2, 0.3, 0.35, 0.4, 0.5), the frictional energy variations are same for both cases causing same final temperature generation. However it should be noted that the $\mu$ and $\zeta$ are mutually dependent, therefore change in one parameter will cause change in other parameter.

![Temperature Distribution on Die Inner Surface](image)

Figure-6.25(a): Temperature Distribution at Die Inner Surface at the end of the Forming Time for Variation of Ratio of Real and Apparent Area of Contact in Ultrasonic Microextrusion
6.6 CONCLUDING REMARKS

A model for predicting the temperature rise during ultrasonic microextrusion process is developed. This model predicts a maximum temperature of 98°C for forward extrusion of a Brass billet upon application of ultrasonic energy at 20 kHz vibration and 67% amplitude of ultrasonic generator for 3 seconds. The comparisons with the experimental results show that the model overestimates the temperature rise by 9% at the 83% completion of the process. This can be explained by the fact that there is an overall improvement in the friction condition during the process, whereas a constant friction condition is considered in the model. Moreover there are some experimental errors associated with the temperature measurement which contribute to the discrepancy between the predicted and the measured temperatures during ultrasonic microextrusion process.
CHAPTER SEVEN: CONCLUSIONS AND FUTURE WORKS

Ultrasonic microextrusion is a very promising technology for mass production of micro parts for various electro-mechanical, medical, electrical, etc. applications. The current research aims to investigate the mechanism of this process leading to prediction of the temperature generation. This research addresses a number of variables/phenomena affecting the ultrasonic microextrusion process. They are ultrasonic wave propagation and transmissibility, surface roughness of the billet, trapped lubricants, friction conditions at the die-workpiece interface, plastic deformation characteristics of the billet, and heat transfer. Thus a model is developed for predicting the temperature generation in the ultrasonic forward microextrusion process by calculating the plastic deformation and frictional energies. The conclusions drawn from this study are as follows.

- Structural analysis of the present set-up shows that the ultrasonic wave propagates in the longitudinal direction of the ultrasonic microforming set-up with negligible wave transmission to the punch assembly.
- It is observed that for the ultrasonic microextrusion process, the asperity deformation energy only contributes about 0.03% to the total heat energy generated in the work zone which is quite insignificant for the thermal analysis.
- This model predicts a maximum temperature of 98°C on the die-workpiece interface at the end of 3 sec forming time for ultrasonic vibration (20 kHz, 67% amplitude at ultrasonic generator) assisted forward extrusion of brass billet (5 mm long, 2 mm entry dia and 1.2 mm exit dia).
- The maximum predicted temperature at the die-container outer surface for the above condition is 34°C which is observed at the top edge of that container.
- Temperature on a cross-section of the die assembly drops very fast as it approaches to the outer surface.
Comparison with the experimental data shows that the model overestimates the temperature rise by 9% at the 83% completion of the process, which can be attributed to an overall improvement in the friction condition and some experimental errors.

Although the developed temperature generation model addresses a number of variables/phenomena relevant to ultrasonic microextrusion process, the following limitations can be identified.

- While the effects of micro-plasto hydrostatic lubrication are considered, the effects of micro-plasto hydrodynamic lubrication are neglected.
- The enhancement of the lubricant transportation due to ultrasonic energy needs to be accounted for.
- The change in friction condition needs to be considered. The major factors are onset of micro-plasto lubrication, enhancement of lubrication transportation, and the change in contact area between die-workpiece interfaces.
- The sonochemical effects and also the particle erosion phenomena need to be addressed.
- The variation of lubricant quantity, which can affect the temperature generation, is not considered.

In this research, a favorable ultrasonic equipment setting is used for all the experiments. However a variation of equipment settings will result in different ultrasonic power output. Consequently, the response of the test set-up will be different, which can be measured by the Laser vibrometer and strain gauge. As discussed in chapter five, the parameters that can be set in the ultrasonic generator are starting frequency ($f_{start}$), percentage of the amplitude ($A$) and the application time ($t$). After each successful operation, the resonant (working) frequency, maximum power output (W) and the energy (Ws) are known from the ultrasonic generator. In order to use as a future reference, a number of settings are tested and the
corresponding parameters are recorded as presented in table-7.1. However the power output can be changed by changing the design of ultrasonic transducer connector.

Table-7.1: Variation of Ultrasonic Generator Settings

<table>
<thead>
<tr>
<th>Number</th>
<th>Parameters to be set</th>
<th>Resulting Parameters</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Starting Frequency ($f_{\text{start}}$), kHz</td>
<td>Percentage of Amplitude ($A$), %</td>
</tr>
<tr>
<td></td>
<td>Application Time ($t$), sec</td>
<td>Resonant (Working) Frequency, kHz</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Maximum Power Output, W</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Energy Output ($E$), Ws</td>
</tr>
<tr>
<td>1</td>
<td>20.50</td>
<td>67</td>
</tr>
<tr>
<td>2</td>
<td>20.50</td>
<td>67</td>
</tr>
<tr>
<td>3</td>
<td>20.50</td>
<td>70</td>
</tr>
<tr>
<td>4</td>
<td>20.40</td>
<td>63</td>
</tr>
<tr>
<td>5</td>
<td>20.40</td>
<td>65</td>
</tr>
<tr>
<td>6</td>
<td>20.40</td>
<td>80</td>
</tr>
<tr>
<td>7</td>
<td>20.20</td>
<td>60</td>
</tr>
<tr>
<td>8</td>
<td>21.00</td>
<td>70</td>
</tr>
<tr>
<td>9</td>
<td>21.00</td>
<td>90</td>
</tr>
<tr>
<td>10</td>
<td>21.00</td>
<td>100</td>
</tr>
</tbody>
</table>

For future research, ultrasonic microextrusion experiments with variation of the $f_{\text{start}}$, $A$ and $t$ are suggested, where the temperature rise will be measured. It will be ensured that the thermocouple probe has full contact with the die at the end of the smaller side hole. As discussed earlier, the temperature rise will be predicted using the model for the variation of the ultrasonic energy. Then the experimental and the predicted temperature data will be plotted together to identify the percentage of discrepancy arising from the model as described in chapter six for different ultrasonic energy levels. Hence for a particular extrusion process, a 3-dimensional graph can be plotted where that percentage of discrepancy will depend on two variables namely the time and the ultrasonic energy. Then using the developed temperature prediction model and the proposed graph, appropriate ultrasonic settings can be selected for desired temperature rise for a particular extrusion process.
Using this model, temperature rises for different materials can be predicted for different ultrasonic energy levels. Thus the parameters required to achieve the appropriate temperature for warm forming process can be set. In microforming, it is difficult to form material like steel, since the punch cannot withstand the relatively higher stress. Warm forming can solve this problem. Ultrasonic microextrusion experiments using the desired parameters obtained form the model are suggested.

During ultrasonic microextrusion, variation of the friction condition needs to be identified. A number of experiments can be performed to observe the changes in the surface roughness asperities at different ultrasonic energy levels.

The temperature prediction from this model will also help to select the proper lubricant that can withstand the higher temperature generated during the ultrasonic microextrusion. Future research can focus on this aspect and also should consider the sonochemical effects on the liquid lubricants.
RESEARCH PUBLICATION

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APPENDIX-A: DERIVATIONS OF ELASTIC-PLASTICFINITE ELEMENT
MODEL

In this FEM modeling, virtual displacement theory is used (Bathe, 1996; Cook et al., 2002). From the discussions at section-6.3.3.1, the following equations are found.

\[ \int _{V} \varepsilon ^{T} \sigma dV = \int _{V} \mathbf{U} ^{T} \mathbf{f} ^{B} dV + \int _{\Gamma _{f}} \mathbf{U} ^{T} \mathbf{f} ^{I} dS + \sum _{i} \mathbf{U} ^{T} R_{i} \]  
(A.1)

\[ \tau ^{(m)} = C^{(m)} \varepsilon ^{(m)} + \tau ^{(m)} \]  
(A.2)

\[ \varepsilon = BU \]  
(A.3)

\[ KU = R \]  
(A.4)

\[ K = \sum _{m} \int _{\Gamma ^{(m)}} B^{(m)} C^{(m)} B^{(m)} d\psi ^{(m)} \]  
(A.5)

\[ R = R_{B} + R_{y} - R_{I} + R_{c} \]  
(A.6)

\[ R_{I} = \sum _{m} \int _{\Gamma ^{(m)}} H^{s(m)} f^{s(m)} dS^{(m)} \]  
(A.7)

The necessary steps to derive this model are discussed onwards. The strain-displacement matrix \( B \) is defined by the equation-A.8. The global coordinates \( x, y \) and \( z \) are functions of the local coordinates \( r, s, t \) and vice-versa. Therefore the partial differentiations of interpolation function \( N \) with respect to \( x, y \) and \( z \) are calculated using the Jacobian matrix (equation-A.9). Then using the equation-A.10, the elements of \( B \)-matrix can be found.
\[ [B_i] = \begin{bmatrix} \frac{\partial N_i}{\partial x} & 0 & 0 \\ 0 & \frac{\partial N_i}{\partial y} & 0 \\ 0 & 0 & \frac{\partial N_i}{\partial z} \end{bmatrix} \] ; \quad i = 1, 2, \ldots, 8 \] (A.8)

\[ J = \left[ \sum \left( \frac{\partial N_i}{\partial r} x_i \right) \right] \left[ \sum \left( \frac{\partial N_i}{\partial s} y_i \right) \right] \left[ \sum \left( \frac{\partial N_i}{\partial t} z_i \right) \right] \] (A.9)

\[ \frac{\partial N_i}{\partial x} = J^{-1} \frac{\partial N_i}{\partial r} \] (A.10)

Since the integrations in equations A.5 and A.7 require only numeric value, it is convenient to do these in local co-ordinate. Therefore the incremental volume \( dV \) and incremental surface \( dS \) are defined by the following equations. Gauss quadrature is used for the numerical integration. For this 8-node isoparametric modeling, two dimensional Gauss quadrature is applied. Hence there are 8 sampling points (Gauss points), where all the calculations are performed.

\[ dV = \det J \, dr \, ds \, dt \] (A.11)

\[ \begin{cases} ds = \det J^S \, dr \, ds \\ ds = \det J^S \, dr \, dt \end{cases} \] (A.12)
The total incremental strain $d\varepsilon$ has two portions, namely the elastic and plastic portion. The incremental stress $d\sigma$ is related to elastic portion of the strain by the equation-A.14, where $E$ is the elastic matrix of the material. Since isotropic hardening ($W_p$) is assumed which is the plastic work per unit volume; the yield function $F$ is the function of stress and that hardening. The associative flow rule is assumed (commonly used for ductile materials), i.e. $Q = F$, where $d\lambda$ is the plastic multiplier.

$$\{d\varepsilon\} = \{d\varepsilon^e\} + \{d\varepsilon^p\}$$  \hspace{1cm} (A.13)

$$\{d\sigma\} = [E]\{d\varepsilon^e\}$$  \hspace{1cm} (A.14)

$$F = F(\{\sigma\}, W_p)$$  \hspace{1cm} (A.15)

$$W_p = \int \{\sigma\}^T \{d\varepsilon^p\}$$  \hspace{1cm} (A.16)

$$\{d\varepsilon^p\} = \left\{\frac{\partial Q}{\partial \sigma}\right\} d\lambda = \left\{\frac{\partial F}{\partial \sigma}\right\} d\lambda$$  \hspace{1cm} (A.17)

Since during the increment of plastic straining the increment of yield function is zero (i.e. $dF = 0$), equation-A.18 can be found and using the above relations equation-A.19 can be derived. According to Von Mises theory, the plastic multiplier $d\lambda$ is equal to the increment of effective plastic strain that corresponds to $\sigma_e$.

$$\left\{\frac{\partial F}{\partial \sigma}\right\}^T \{d\sigma\} + \frac{\partial F}{\partial W_p} dW_p = 0$$  \hspace{1cm} (A.18)

$$d\lambda = \frac{\left\{\frac{\partial F}{\partial \sigma}\right\}^T \{d\varepsilon\} + \frac{\partial F}{\partial W_p} dW_p}{\left\{\frac{\partial F}{\partial \sigma}\right\}^T \{d\sigma\} - \frac{\partial F}{\partial W_p} \{\sigma\}^T \left\{\frac{\partial F}{\partial \sigma}\right\}}$$  \hspace{1cm} (A.19)

$$d\lambda = \left[P_{\lambda}\right] \{d\varepsilon\}$$  \hspace{1cm} (A.20)

$$d\lambda = d\varepsilon^p$$  \hspace{1cm} (A.21)
Using the above equations the following stress-strain relations can be derived. From these stress-strain relations (equations A.22-A.24), the elastic-plastic material matrix $E_{ep}$ (i.e. material matrix $C$) can be calculated.

\[
\{d\sigma\} = [E]\left\{d\varepsilon\right\} - \left\{\frac{\partial F}{\partial \sigma}\right\}[P_{\lambda}][d\varepsilon] \tag{A.22}
\]

\[
\{d\sigma\} = [E]\left\{d\varepsilon\right\} - \left\{\frac{\partial F}{\partial \sigma}\right\}[P_{\lambda}] \tag{A.23}
\]

\[
\{d\sigma\} = [E_{ep}]\{d\varepsilon\} = [C^{(m)}][d\varepsilon] \tag{A.24}
\]

\[
[E_{ep}] = [E]\left\{d\varepsilon\right\} - \left\{\frac{\partial F}{\partial \sigma}\right\}[P_{\lambda}] \tag{A.25}
\]

In this analysis, Von Mises definition of effective stress ($\sigma_e$), effective plastic strain ($d\varepsilon_{ep}$) and yield function ($F$) are used. $\sigma_o$ is the largest value of $\sigma_e$ reached in previous plastic straining. Deviatoric stresses ($S$) are also defined.

\[
\sigma_e = \frac{1}{\sqrt{2}}\left[\left(\sigma_x - \sigma_y\right)^2 + \left(\sigma_y - \sigma_z\right)^2 + \left(\sigma_z - \sigma_x\right)^2 + 6\left(\tau_{xy}^2 + \tau_{yz}^2 + \tau_{zx}^2\right)\right]^{\frac{1}{2}} \tag{A.26}
\]

\[
\sigma_e = \sqrt{\frac{2}{3}\left[S_x^2 + S_y^2 + S_z^2 + 2\left(S_{xy}^2 + S_{yz}^2 + S_{zx}^2\right)\right]} \tag{A.27}
\]

\[
d\lambda = d\varepsilon_{ep} = \sqrt{\frac{2}{3}\left[\left(d\varepsilon_{ep}\right)^2 + \left(d\varepsilon_{ep}\right)^2 + \left(d\varepsilon_{ep}\right)^2 + \frac{1}{2}\left(\left(d\gamma_{xy}\right)^2 + \left(d\gamma_{yz}\right)^2 + \left(d\gamma_{zx}\right)^2\right)\right]} \tag{A.28}
\]

\[
F = \sigma_e - \sigma_0 \tag{A.29}
\]

\[
F = \left[\frac{3}{2}\left(S_x^2 + S_y^2 + S_z^2\right) + 3\left(S_{xy}^2 + S_{yz}^2 + S_{zx}^2\right)\right]^{\frac{1}{2}} - \sigma_0 \tag{A.30}
\]
In order to calculate the elastic-plastic material matrix, \( E_{ep} \) (equation-A.25), \( \frac{\partial F}{\partial \sigma} \) and \( P_\lambda \) need to be calculated. The partial differentiations of yield function \( F \) are calculated in terms of deviatoric stresses. \( H_P \) is the strain-hardening parameter (plastic modulus) which is the experimentally obtained stored value for the each material for different effective stresses.

\[
\frac{\partial F}{\partial W_p}dW_p = \frac{\partial F}{\partial \sigma_0}d\sigma_0 \quad (A.33)
\]

\[
\frac{\partial F}{\partial \sigma_0} = -1 \quad (A.34)
\]

\[
d\sigma = H_Pd\varepsilon^P \quad (A.35)
\]

\[
d\sigma_0 = H_Pd\lambda \quad (A.36)
\]

\[
\begin{bmatrix}
\frac{\partial F}{\partial \sigma} \\
\frac{\partial F}{\partial \sigma_0}
\end{bmatrix} = \frac{3}{2\sigma_0} \begin{bmatrix} S_x & S_y & S_z & 2S_{xy} & 2S_{yz} & 2S_{zx} \end{bmatrix}^T \quad (A.37)
\]

\[
[P_\lambda] = \frac{\begin{bmatrix} \frac{\partial F}{\partial \sigma}^T & [E] \end{bmatrix}^T}{\begin{bmatrix} \frac{\partial F}{\partial W_p}^T & [\frac{\partial F}{\partial \sigma}]^T \end{bmatrix}} - \frac{\begin{bmatrix} \frac{\partial F}{\partial \sigma}^T \end{bmatrix}^T}{\begin{bmatrix} \frac{\partial F}{\partial \sigma_0}^T \end{bmatrix}} = \frac{\begin{bmatrix} \frac{\partial F}{\partial \sigma}^T \end{bmatrix}^T}{\begin{bmatrix} \frac{\partial F}{\partial \sigma}^T \end{bmatrix}} + H_P \quad (A.38)
\]

Finally using the equations A.37 and A.38 the material matrix \( E_{ep} \) can be calculated and hence the stiffness matrix \( K \).