A Simulation Perspective on Dimensional Control and Formability in Impact Forming

THESIS

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By

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Abstract

Traditional stamping technologies that sandwich sheet metal between a die and punch have several inherent limitations such as the use of heavy tools, localized deformation that damage the parts and inhibit consistency. High speed forming is a lightweight tooling assembly that forms the part without using any punch. Electromagnetic forming (EMF) is one among the gamut of high speed forming technologies that is used for embossing fine surface features onto sheet metals. This work investigates the dynamics of sheet metal impact through a simulation study. The primary objective of this work is to develop a modelling facility that guides experimental design of flat ridged parts. Critical factors that influence the product quality are investigated. The high impact energy translates into an appreciable rebound that affects the product shape. Interface conditions play a critical role in influencing the shape of the final part. Contrary to intuition, friction is beneficial in high speed forming unlike traditional stamping where friction leads to tearing of sheet metal. Shape fidelity is investigated through a prototypical study of the expansion of a round tube into a square hole.

Traditional modelling techniques solve a coupled system of equations with spatially varying electromagnetic fluxes controlling the dynamics of the plastic deformation. Because the magnetic pressure is spatially uniform, the flux equations are obviated from the coupled system rendering them computationally efficient. The calibration of contact
mechanics that influence the rebound behaviour of the sheet metal remains as a difficult issue. The interfaces between various sheet metals and the metal die play a critical role in controlling the shape of the final product. The characterization of such an interface using appropriate calibrated friction coefficients is assessed. The role of magnetic pressure in reducing the sheet metal rebound is demonstrated via a comparison between results from mechanical and electromagnetic simulations. The influence of the channel geometry on final shape is illustrated through simulations and experiments.
Dedication

To my parents; Mr. Raman & Mrs. Vijayalakshmi Srinivasan

and my sister, Sunita Srinivasan.
Dr. Glenn Daehn has been the backbone of this research work. I am grateful for his advice and encouragement. Dr. Suresh Babu has been very helpful with his comments on editing the document. The electromagnetic forming research group members including Geoff Taber, Huimin Wang, Jason Johnson, Yuan Zhang, Anupam Vivek and Bradley Kabert have been very kind and supportive. Dr. Suleman Dregia, Dr. John Morral, Dr. Sheikh Akbar and Dr. James Williams have been very encouraging with timely advice on technical concepts. Peers, Ryan Paul, Michael Susner, Haris Ansari, Vikas Dixit, Santosh Koduri, Benjamin Dinan and Santino Carnevale have been very helpful in exemplifying few topics. The funding for this work was provided by General Motors. Dr. John Bradley from General Motors is acknowledged for his involvement.
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Field of Study

Major Field: Materials Science and Engineering
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Electromagnetic forming uses the magnetic repulsion between two very large opposing currents to form sheet metal\(^1\). The punch is eliminated from the assembly and the sheet metal is driven into the die by its own inertia. Typical primary currents on the order of 100 kA with rise times of about 10-30 µs are common and velocities within the range of 100-300 m/s are often achieved. The current investigation concerns high speed forming for the manufacture of grooved nominally flat plates. Understanding both the correspondence between the die shape and final part shape and ability to make the part without sheet rupture are paramount in a successful forming operation. This study considers two kinds of problems, first a more complex one dealing with the manufacture of a nominally flat grooved plates (Chapter 3). This motivates the need for simpler, more tractable problems such as the study of expansion of the proverbial round tube into a square hole that is considered in subsequent sections.

In traditional stamping, the sheet metal is sandwiched between a die and a punch that have complementary shapes as shown in Figure 1 (a). The punch is generally driven by a press at small velocities of the order of few mm/s at the bottom of the press stroke. The large contact forces between the tools drive the sheet into the die. It is hard to manufacture parts with fine surface features (Figure 1 (b)) using traditional
forming. Full surface conformation into the fine features requires pressures that exceed typically 3 times the material flow stress (Taber’s rule on hardness\(^2\)). This necessitates very large pressures. Designing a punch and die with the fine surface features that fit together is not so easy. Since the punch and die are in intimate contact with the sheet during each forming operation, the friction between contact interfaces blunts the fine features over a period of time. Also, the precise alignment of the die and punch is critical.

![Figure 1. Schematics of (a) Traditional sheet metal forming technologies (b) High speed forming (Banik, 2008)\(^3\)](image)

High-speed forming technologies such as EMF do not involve any punch to make contact with the sheet during the impact process\(^4\). The sheet metal is driven by a magnetic pressure wave that is a result of an electromagnetic repulsion between two large opposing currents\(^5\) (100-200 kA). The theory behind this repulsion is explained by Lenz’s law. When two currents opposite in direction are in close vicinity, the magnetic field lines around them interact and repel each other very strongly. The velocities generated through this process are of the order of 100-300 m/s. The manufacturing equipment in EMF is lightweight as there is no punch or hydraulic press. Due to few contact interfaces, tool wear and tear is minimal. There are fewer issues concerned with tool alignment.
The goals of this research are to understand issues pertinent with dynamics of the impact forming process, contact friction, rebound effects and shape fidelity. In traditional stamping operations, the presence of friction negatively affects strain distributions making it difficult to form a given shape. This document will show that in high speed forming, friction is often beneficial to the formability of the sheet metal workpiece. If a lubricant is used on the interface between sheet metal and the die the propensity for tearing increases thus reducing the resulting formability. The rebound of the sheet metal from the die upon impact influences the shape of the workpiece. The numerical calibration of this rebound is very critical in understanding aspect pertinent to shape fidelity. There is a characteristic difference observed in the rebound characteristics in a purely mechanical simulation compared with the coupled electro-mechanical simulation. Through experiments of a ring expanding inside a square cavity, the relative effect of tearing versus bending of the ring specimen is assessed.
Chapter 2: Literature Review

In the early stages of investigation concerned with formability, the focus was on forming large-scale automotive components such as closure panels and hoods. More recently, efforts have turned towards use of the uniform pressure actuator\textsuperscript{6,7,8,9} for the fabrication of fuel cell bipolar plates, and investigations of fundamental mechanisms of extended ductility have continued\textsuperscript{10,11}. In this time, there has been some informal questioning of the results and mechanisms of ‘hyperplasticity’. This work seeks to put some of these questions to rest by first documenting in the literature areas where there is now broad agreement. Also it makes progress in terms of using these ideas to move towards a new class of manufacturing methods that make use of this phenomenology.

2.1 Background

With work performed in the early 1990’s Balanethiram and Daehn\textsuperscript{12,13} created a bit of a controversial stir by demonstrating that in electrohydraulic forming experiments extraordinary ductility could be obtained by forming flat sheets into conical sections. With uncharacteristic bravado, these researchers used the term ‘hyperplasticity’ to describe this class of results. The results have been influential both in bringing attention
back to industrial high strain rate metal forming as well as in attracting skeptical attention to the claims that high strain rates and sample velocities can produce extended ductility.

These results on electrohydraulic sheet metal forming were followed by a series of experiments that considered expanding slender rings and later tubes and thicker rings. Experiments by Altynova\textsuperscript{14} which were later analyzed using simple numerical models by Hu and Daehn\textsuperscript{15,16} showed that ring expansion and uniaxial tension were fundamentally different in terms of how the stress wave in loading affects ductility and that increased uniform and total elongation could be expected at axisymmetric high launch velocities. Because high velocity expansion caused significant ring fragmentation following deformation, high elongations were only possible at energies that would cause significant fragmentation, unless a die was used to arrest the sample from high velocity. Later experiments by Tahamne\textsuperscript{17} in 1995 demonstrated that if the ratio of ring height to wall thickness was increased, providing tubular instead of ring geometries, ductility could be further increased with or without fragmentation. Seth\textsuperscript{18,19} also examined the effect of ring geometry. She found that both uniform and total elongation of rings increases significantly if the cross section of the ring increases. If rings are very slender there was virtually no increase in ductility with expansion velocity. Figure 2 shows key aspects of these results demonstrating how size and ring velocity affect total circumferential elongation.

There is also a clear and fundamental difference between free ring, tube, or sheet expansion and that includes die strike. It was observed that ring and tube expansion can increase total elongation by a factor of about 2 to 3, but in the original work by Balanethi-
ram, an approximately 4 fold increase in ductility versus the forming limit diagram was measured. This further ductility increment was attributed to an ‘inertial ironing’ effect seen in die strike. Seth\textsuperscript{20} carefully studied the inertial ironing effect by forming using impact over gently curved punches and she found the effect to be quite powerful. It was shown that in impact forming the shape of the contact punch was more important in developing extended ductility than materials parameters and that plane-strain extensions on the order of 100\% were possible in impact forming.

\begin{figure}[h]
\centering
\includegraphics[width=\textwidth]{graph.png}
\caption{Total ring elongation as a function of cross section and peak velocity in machined 5754 rings (Seth and Daehn, 2005).}
\end{figure}

While this body of work is compelling to some, it has not gone without some skeptical criticism. Golovaschenko was initially critical of the ‘hyperplasticity’ concept in public forums, but in more recent work\textsuperscript{21} he reports that in free sheet expansion experiments, without die strike there are only modest (at best) increases in formability if a
metal sheet is formed into an open ‘window’, but by forming into a die shape there can be very significant increases in formability. Figure 3 shows an example of his work in this area. It is very likely that when forming sheets into open windows, the sheet does not have the necessary velocity to develop greatly extended ductility. Again, even with ring expansion, it is often difficult to find a set of conditions where ductility is significantly increased without rupture, or multiple ruptures.

Figure 3. Formability measured in 6111 T-4 in a variety of free expansion and fixed die forming experiments from Golovaschenko, 2007
The first paper in a series by Zhang and Ravi-Chandar\textsuperscript{22} provided a truly beautiful experimental study of high speed slender ring expansion complemented with high speed photography. This work also appeared to be quite critical of the idea of extended ductility based on high velocity ring expansion. Their first paper concluded that there is no gain in uniform elongation due to expansion velocity and necking would commence at the usual Considéré limit. They suggest that the additional apparent ductility seen in the experiments by Altynova and others were due to the necking strain provided by in multiple necks, rather than real ‘formability’. These experiments used rings cut from 0.5 mm thick sheets with a ring-width of 1.0 mm. This is in accord with the 2005 study by Seth who also found that for such slender rings there is no increase in formability.

Figure 4. Effect of ring cross sectional dimensions on uniform elongation as a function of peak velocity from Zhang and Ravi-Chandar (2009).
More recent work by Zhang and Ravi-Chandar\textsuperscript{23,24} clearly demonstrates both experimentally and from an analytical point of view that in ring expansion both uniform and total elongation will increase with strain rate as the sample cross section, and height to thickness ratio increases, in accord with the results of Tahmane and Seth. An example of this recent work is shown in Figure 4.

It seems now there are many aspects of “hyperplasticity” that have been replicated by several labs and broad agreement now exists. In particular:

- Velocity boundary conditions are very important. Ring expansion can give exceptional extended ductility whereas in tension or upon sheet expansion into an open window, extended ductility is difficult to obtain.

- For materials that fail by plastic instability, absolute sample dimensions (thickness, ring heights, etc.) are very important and uniform and total elongation in free forming can increase dramatically with increases in these dimensions.

- Die strike and ‘inertial ironing’ can have a powerful effect in producing very high levels of ductility. It is presently unclear how absolute sample dimensions affect ductility increases related to die strike.

- A mechanical effect, inertial stabilization of necking, is often the leading factor in providing extended ductility. This is the main tenet of the work of Hu and Daehn, and Zhang and Ravi-Chandar have reinforced this point. With appropriate provisions for axial and radial inertial effects, solid mechanics codes based on neck growth can predict ductility. Changes in material constitutive behavior with increasing strain rate, such as increased strain and strain rate hardening, can also
stabilize neck formation and growth. High strain rates can increase both rate sensitivity and rates of hardening.

- Significant velocities (well over 50 m/s) are often needed to produce significant increases in ductility. These velocities may cause multiple significant fragmentation of samples if dies are not used to constrain them.

2.2 Constitutive Behavior

One of the popular constitutive relationships that simulate the material response under high strain rate deformation was proposed by Johnson & Cook (1983)\textsuperscript{25}. The constants $A$ and $B$ and ‘$n$’ are calibrated using quasi-static tensile tests\textsuperscript{26}. The parameters that require high strain rate experimental calibration are $C$ and $m$.

$$
\sigma = (A + B\varepsilon^n) \left(1 + C \ln \varepsilon^* \right) \left(1 - (T^*)^m \right)
$$

$\sigma =$ Flow stress, MPa $A =$ Yield strength, MPa $B, n =$ Hardening constants $C =$ Strain rate sensitivity, $\varepsilon^* =$ Dimensionless strain rate $\frac{d\varepsilon^*}{dt}$ w.r.t quasi-static strain rate, $\varepsilon_o$ $T^* =$ Dimensionless temperature w.r.t reference temperature, $T_R$, $m =$ Temperature sensitivity

The above relation has a simple mathematical form. The first term describes rate independent material behavior. The second term in the first parenthesis represents strain hardening\textsuperscript{27}. The strain and the strain rate terms enhance the strength of the material as plastic deformation proceeds. Contrarily, temperature has the competing effect of reducing the strength. The relative magnitudes of $n, C & m$ determine which effect has dominance over the material behavior. As the material changes shape there is a limit strain
where the material can no longer sustain the applied stress and fractures. This sets an additional requirement for modeling material behavior. A description of the fracture criterion was proposed by Johnson & Cook (1985)\textsuperscript{28}.

\[
\varepsilon_f = \left( D_1 + D_2 \exp \left( \frac{D_3 \sigma_m}{\sigma} \right) \right) \left( 1 + D_4 \ln \varepsilon^* \right) \left( 1 - D_5 T^* \right)
\]

\(\varepsilon_f\) = Effective von-Mises Strain at Fracture, \(D_1, D_2, D_3, D_4, D_5\) = Calibration parameters
\(\sigma_m\) = Mean hydrostatic stress, equal in all 3 directions
\(T^*\) = Dimensionless temperature w.r.t reference temperature, \(T_R\)

A simplified version of the Johnson-Cook failure law is also used in LS-DYNA’s simulation algorithm. This is shown in equation (3). In the subsequent sections, the simulation results are based on this condition.

\(\text{If } \varepsilon \geq \varepsilon_f \text{ then element is deleted from simulation} \)

\(\varepsilon\) = Instantaneous value of true strain
\(\varepsilon_f\) = Effective von-Mises Strain at Fracture,

The important take-away from the above expressions is that replicating the material response including fracture warrants the calibration of the model with respect to at least 7 constants. 5 of these are used for modeling fracture alone and the remaining 2 (\(m\) and \(C\)) are used for depicting high strain rate behavior.

2.3 Ghost Lines

Oliveira et al (2005) have observed some linear features in the formed part as shown in Figure 5 (a). They have attributed the occurrence of this feature to arcing\textsuperscript{29}. Kamal et al (2007) have expressed their uncertainty on the presence of “ghost lines”\textsuperscript{30} in
their EM formed cell phone cases. The intensity of these lines increases as the discharge energy is increased (Figure 5(b) & (c)). Fracture and welding to opposing materials are often associated with these features. These lines are found to recur in some of the simulations discussed in Chapters 3, 4 and 5 and the supporting experiments thereof. The true reason behind their occurrence still remains unresolved.

Figure 5. (a) Ghost lines (Oliveira et al, 2005) (b) & (c) Ghost lines (Kamal, 2007) (d) Rebound effects (Oliveira et al, 2005)
Chapter 3: Impact Forming of Grooved Plates

3.1 Introduction

Banik\textsuperscript{31} studied the forming of relatively thin sheets into shapes appropriate for fuel cell bipolar plates using the uniform pressure actuator (UPA). She used just one die geometry, but varied impact speed through the capacitor bank level of charge and impact velocities were measured using Photon Doppler Velocimetry\textsuperscript{32}. A charged capacitor bank discharges a high electric current into a primary coil embedded inside a box. The primary current induces a secondary current in the metal blank. The two currents being opposite in nature generate repulsion high enough to drive the ring into the die. A photon Doppler velocimeter system is used to measure velocities of the accelerating specimen. The overall utility, design and behavior of this UPA forming system have been described by Kamal\textsuperscript{33} previously.

3.2 Experimental Setup (Kamal & Daehn, 2007)

The experiment system consists of a 16 kJ capacitor bank, uniform pressure coil (Figure 7) and a flat box casing. The primary copper coil is embedded inside the casing by cast tooling grade urethane. The sheet specimen sits on the casing to complete the secondary circuit. The separation distance between the specimen and the die is 3 mm.
Figure 6 shows that a high electrical discharge from the capacitor bank charges the primary circuit embedded in the coil box. The coil used in the experiment is shown in Figure 7. The coil inductance was 0.5 $\mu$H. The total bank capacitance was 425 $\mu$F and inductance was 0.25 $\mu$H. Titanium sheets of thickness 0.003” were formed into a steel die with fine semicircular channels with depths of the order of 0.015 inches. A copper driver sheet of thickness 0.006” was used to complete the secondary circuit owing to its high electrical conductivity. The copper driver provides pressure to the titanium sheet during the forming process, accelerating it and co-deforming with the titanium. The forming is accomplished under vacuum to eliminate the resistance to forming due to compressed air.
3.3 Simulation Methods

The Copper sheet was simulated with 1000 x 1 x 9 brick elements and the Titanium sheet with 1000 x 1 x 5 solid brick elements (L x W x t) shown in Figure 8. The thickness of the copper driver was 0.15 mm and that of Titanium was 0.076 mm. The simulated length of both sheets was 20 mm. LS-DYNA has a 2D plane strain simulation module but the contact cards are configured to the 3D simulation. 2D simulations will depict very high rebound of sheet metal upon impact. Therefore, plane strain simulation was conducted with at least one element in the width direction. The nodes were constrained to allow for movement only in the X-Y plane. The mesh density in the die should be high enough to reproduce the fine surface contours. The shape contours of the metal die should be used as a benchmark to decide the mesh density in the x-direction. In this case the die was divided into an upper and lower part to make computation efficient. The
lower part had a coarse mesh with density 25 x 1 x 15 brick elements. The metal die shouldn’t be simulated as a rigid body because it results in excessive rebound of the sheet metal specimen. It is conducive to ascribe a Holoman type or Johnson-Cook model similar to the sheet metal. The die material should be made strong enough not to undergo any deformation. The contact cards are critical in controlling the rebound of the specimen. The contact keyword used in the simulation was CONTACT_AUTOMATIC_SURFACE_TO_SURFACE shown below.

![Figure 8. Mesh density for the bipolar plate forming simulation (LSTC)](image)

```
*CONTACT_AUTOMATIC_SURFACE_TO_SURFACE

CID  IF. NAME

1

SSID    MSID    SSTYP    MSTYP    SBOXID    MBOXID    SPR
5       7       3        3        0         0         0
0

FS      FD      DC      V        VDC       PENCHK     BT
0.3     0.1     0.1     0.25     0.5       0         0.0
1.000E+20

SFS     SFM     SST     MST      SFST      SFMT      FSF
```

16
Table 1: Contact cards for bipolar plate simulation

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Definition</th>
</tr>
</thead>
<tbody>
<tr>
<td>CID</td>
<td>Contact definition unique id</td>
</tr>
<tr>
<td>SSID</td>
<td>Slave part id; Can be part, segment or node id</td>
</tr>
<tr>
<td>MSID</td>
<td>Master part id; Can be part, segment or node id</td>
</tr>
<tr>
<td>SSTYP</td>
<td>Slave segment type, use 3 for part id</td>
</tr>
<tr>
<td>MSTYP</td>
<td>Slave segment type, use 3 for part id</td>
</tr>
<tr>
<td>SBOXID</td>
<td>Not relevant for simulation</td>
</tr>
<tr>
<td>MBOXID</td>
<td>Not relevant for simulation</td>
</tr>
<tr>
<td>SPR</td>
<td>Not relevant for simulation</td>
</tr>
<tr>
<td>MPR</td>
<td>Not relevant for simulation</td>
</tr>
<tr>
<td>FS</td>
<td>Coefficient of static friction</td>
</tr>
<tr>
<td>FD</td>
<td>Coefficient of dynamic friction</td>
</tr>
<tr>
<td>DC</td>
<td>Decay coefficient</td>
</tr>
<tr>
<td>V</td>
<td>Coefficient of viscous friction</td>
</tr>
<tr>
<td>VDC</td>
<td>Viscous damping coefficient</td>
</tr>
<tr>
<td>PENCHK</td>
<td>Checks penetration of nodes at the contact interface</td>
</tr>
<tr>
<td>BT</td>
<td>Birth time to activate contact card</td>
</tr>
<tr>
<td>Parameter</td>
<td>Description</td>
</tr>
<tr>
<td>-------------</td>
<td>-----------------------------------------------------------------------------</td>
</tr>
<tr>
<td>DT</td>
<td>Death time to deactivate contact card</td>
</tr>
<tr>
<td>LINE 3</td>
<td>All values set to 1. Not important for simulation</td>
</tr>
<tr>
<td>SOFT</td>
<td>Important for forming operations (useful for contact scaling)</td>
</tr>
<tr>
<td>SOFSCL</td>
<td>Scale factor for constraint forces in Figure 9</td>
</tr>
<tr>
<td>LCIDAB</td>
<td>Curve id for airbag thickness (Not relevant)</td>
</tr>
<tr>
<td>MAXPAR</td>
<td>Default 1.025 (Not relevant)</td>
</tr>
<tr>
<td>SBOPT</td>
<td>Segment based contact options (Not relevant)</td>
</tr>
<tr>
<td>DEPTH</td>
<td>Search depth in contact, default 2</td>
</tr>
<tr>
<td>BSORT</td>
<td>Bucket sort algorithm for contact search, default 0</td>
</tr>
<tr>
<td>FRCFRQ</td>
<td>Number of cycles before contact force updates for penalty contact formulations. Recommended value 0.</td>
</tr>
<tr>
<td>PENMAX</td>
<td>Maximum allowable penetration distance between interface nodes</td>
</tr>
<tr>
<td>THKOPT</td>
<td>Thickness offsets included in case penetration occurs</td>
</tr>
<tr>
<td>SHLTHK</td>
<td>Use 1 to activate thickness offsets</td>
</tr>
<tr>
<td>SNLOG</td>
<td>Recommended value of 1 for metal forming</td>
</tr>
<tr>
<td>ISYM</td>
<td>Use 1 (Not relevant)</td>
</tr>
<tr>
<td>I2D3D</td>
<td>Use 1 (3D simulation), 0 (2D)</td>
</tr>
<tr>
<td>SLDTHK</td>
<td>Optional solid element thickness</td>
</tr>
<tr>
<td>SLDSTF</td>
<td>Optional solid element thickness, ( K_n ) in Figure 9</td>
</tr>
</tbody>
</table>

Table 2: Parameter definitions for contact cards
An analogous prototypical model was simulated using a metal ball and solid die to configure parameters specific to the contact cards. This prototypical model is discussed in detail the subsequent section. The parameters FS and FD are the co-efficients of static and dynamic friction. The parameter SOFSC (refer DYNA manual for details of other parameters) is a scaling co-efficient that is used to control the rebound elastic energy retained in the metal ball after impact. An et al., (2007) elaborately discusses the means to control the contact stiffness through a scaling coefficient in his paper on discrete element method for simulating inelastic contacts as shown in Figure 9. As the ball impacts the
metal die, there is an increase in contact force up to point A, remains constant until point B and then undergoes a decline. The x-axis or contact overlap represents the common interface between the two impacting bodies as it changes with time. The nature of the curve can be conveniently expressed using the power law equation shown in Figure 9. The constant slope ‘Kn’ in Figure 9 is modified using the ‘SLDSTF’ parameter in Table 1. In this simulation it has a value of 0.5. The area under this curve represents the retained kinetic energy of the flying specimen (metal ball in this case). The power parameter ‘b’ is the scaling coefficient ‘SOFSCL’ shown in Table 1. LS-DYNA automatically computes the parameter ‘a’ based upon the user supplied value of ‘b’ or the SOFSCL. Based on the two parameters SOFSCL and SLDSTF the contact curve shown in Figure 9 can be reproduced. The parameter PENMAX and SLDTHK in Table 1 are used to ensure that there is no penetration between nodes of the two contacting bodies. These parameters are used to control numerical issues pertinent with nodal penetrations and have less to do with the physics of the process.

The simplified Johnson and Cook (J-C) model\(^35\) as shown in equation (4) was selected to characterize the material behavior. This form was chosen for its relative simplicity\(^36\). For simplicity, the influence of temperature on the flow stress was neglected\(^37\). The J-C form is represented as:

\[
\sigma_y = (A + B \varepsilon^n) \left(1 + c \ln \dot{\varepsilon}\right)
\]

(4)

Where \(\sigma_y\) represents the flow stress (applied force per unit area), \(A\) is the yield stress (transition from elastic to plastic regime), \(B\) and \(n\) are parameters corresponding to the
strain hardening law, \( c \) is the strain-rate parameter, \( \varepsilon \) is the effective plastic strain, and 
\[ \dot{\varepsilon} \] is the strain-rate. The simplified failure law of Johnson-Cook involves a characteristic 
parameter i.e. the effective (von-Mises criterion) plastic strain at fracture (\( \varepsilon_f \)) shown in 
(2). Numerically, this implies that if the instantaneous true strain (von-Mises criterion) in 
any grid element is greater than fracture strain then the element is deleted from the simu-
lation. This plastic strain at fracture is calibrated through experimental measurements.

\[ \varepsilon \geq \varepsilon_f \quad \text{at failure} \quad (5) \]

Since the equation (4) has the dynamic strain rate effects decoupled from the static 
effects, a decoupled fitting algorithm as suggested by Meyer\(^{38}\) (2001) was used. High 
strain-rate test data is essential to calibrate the strain rate sensitivity. Since, such data was 
unavailable, the first estimate for the strain rate sensitivity ‘c’ was used from the paper by 
Khan\(^{39}\) (2004). The strain rate was then optimized against measured formed depths for 
titanium and copper sheet specimens. The values are tabulated in Table 3. The quasi-
static tensile parameters were fixed by experimental data and the strain-rate parameter for 
each entity was independently optimized to tailor to experimental strain distributions. 
Simulations are carried out in the fully explicit simulator LS-DYNA. The electromagneti-
cal calculations are decoupled from the mechanical deformations to reduce computational 
time. Since the coil is a uniform pressure actuator, the magnetic pressure is spatially con-
stant. Thus, the boundary condition for the simulation is set using the magnetic pressure 
on the copper driver plate. The transient magnetic pressure curve can be estimated using 
the equation by Kamal\(^{40}\) and fed to the DYNA simulator using the 
LOAD_SEGMENT_SET and DEFINE_CURVE keywords.
\[ P(t) = \frac{\mu_0 f_s n^2}{2 \cdot l_s^2} \cdot I^2(t) \]

\( P(t) \) – Magnetic pressure, \( \mu_0 = \) permeability of free space \( 4\pi \times 10^{-7} \)

\( f_s \) – Coupling factor between primary current \( I(t) \) and secondary current

\( n \) – Number of turns in the primary coil, \( l_s \) – Length of the solenoid

\( \text{(6)} \)

---

**Figure 10.** Schematic of the pressure boundary condition to LS-DYNA

<table>
<thead>
<tr>
<th>Specimen</th>
<th>A (MPa)</th>
<th>B (MPa)</th>
<th>n</th>
<th>c</th>
</tr>
</thead>
<tbody>
<tr>
<td>Copper driver</td>
<td>100</td>
<td>150</td>
<td>0.3</td>
<td>0.06</td>
</tr>
<tr>
<td>Titanium</td>
<td>200</td>
<td>700</td>
<td>0.22</td>
<td>0.06</td>
</tr>
<tr>
<td>Steel (Die)</td>
<td>20</td>
<td>550</td>
<td>0.05</td>
<td>0.05</td>
</tr>
</tbody>
</table>

**Table 3:** Johnson-Cook parameters for titanium specimen and copper driver

The schematic of the pressure boundary condition to the simulator is shown in Figure 10. The primary current is measured using a Rogowski probe. For simplicity the
coupling factor is assumed to be constant value between 0 and 1. For this case it is assumed to be 0.85. It is a function of the conductivity and the thickness of the driver. This magnetic pressure used in the simulation must be calibrated using the velocity of the flyer measured from experiments using the PDV system.

3.4 Coefficient of Restitution (COR)

The rebound characteristics of sheet metal upon impact critically influence the shape conformity of the formed product. A suite of test runs were executed in LS-DYNA to investigate an analogous impact scenario between a metal sphere and a metal wall/die. The objective of these test runs was to study energy loss and calibrate the contact cards for depicting rebound behavior. The quasi-static constitutive relations for copper (sphere) and steel (wall) were inferred from literature\textsuperscript{41, 42}. The ball response served a good purpose in calibration of the strain-rate sensitivity. The energy loss\textsuperscript{43} on impact is a critical factor that governs the rebound mechanics of the metal ball. At low impact velocities under quasi-static conditions, the kinetic energy imparted to the ball is within the elastic regime and is transmitted through the die off to the other end from where it reflects back to ball. As the impact velocities increase to the orders in electromagnetic forming, the deformation mode progresses from elastic to elastic-inelastic and fully plastic regimes\textsuperscript{44}. A part of the ball’s kinetic energy is dissipated in plastic deformation and elastic stress wave propagation through the die; and the rebound is a manifestation of the remnant energy of the ball. A coefficient of restitution defined as the ratio of reflected velocity to incident velocity, characterizes the rebound behavior of the ball subsequent to impact.
The sensitivity of coefficient of restitution to strain-rate sensitivity and impact velocity is depicted in Figure 11.

Figure 11. (a) COR vs. Impact velocity (b) COR vs. Strain rate parameter (‘c’ parameter of Johnson-Cook model)

Figure 12. COR vs. contact scaling coefficient
The coefficient of restitution was found to decrease with an increase in impact velocity. This decrease is attributed to an increase in the fractional plastic deformation in the ball evident from Figure 11. As the impact velocity was increased, the fractional loss in kinetic energy increased and consequently the coefficient of restitution decreased. This test was compared against data cited in literature to validate the legitimacy of the mechanics of rebound behavior. A critical control parameter offering leverage over the contact between the metal ball and the wall is a scaling coefficient discussed by An Baoquan46 (2007).

This treats the contact between two surfaces as a spring whose stiffness undergoes dynamic changes during the impact process. The magnitude of this change has direct bearing on the rebound behavior apparent in the coefficient of restitution as illustrated in Figure 12. Figure 13 shows that the retained kinetic energy of the ball decreases with in-
creasing impact velocity. Larger proportion of the kinetic energy of the ball is converted to plastic deformation evident from the plastic strains.

The parameters for Johnson-Cook model for the metal ball and die are specified below:

$\textbf{For the metal ball}$

*MAT_SIMPLIFIED_JOHNSON_COOK

$\begin{array}{cccccc}
\text{mid} & \text{ro} & \text{e} & \text{pr} & \text{vp} \\
3 & 9.0E-6 & 125 & 0.35 & 1 \\
\end{array}$

$\begin{array}{cccccccc}
\text{density} & \text{Elastic modulus} & \text{Poisson ratio} \\
a & b & n & c & psfail & \text{sigmax} & \text{sigsat} & \text{epso} \\
0.10 & 0.40 & 0.20 & 0.12 & 1.0 & 20 & 0.001 \\
\end{array}$

$\textbf{For the Metal Wall Die}$

*MAT_SIMPLIFIED_JOHNSON_COOK

$\begin{array}{cccccc}
\text{mid} & \text{ro} & \text{e} & \text{pr} & \text{vp} \\
4 & 9.0E-6 & 300 & 0.35 & 1 \\
\end{array}$

$\begin{array}{cccccccc}
\text{density} & \text{Elastic modulus} & \text{Poisson ratio} \\
a & b & n & c & psfail & \text{sigmax} & \text{sigsat} & \text{epso} \\
0.05 & 2.00 & 0.05 & 0.12 & 1.0 & 10.5 & 0.001 \\
\end{array}$

Table 4: Johnson-Cook parameters for metal ball and die

The parameter definitions are in Table 5. The constants a, b, c and n in Table 4 are the parameters from equation (4). The initial value of contact scaling-coefficient in the simulation was 0.1. It was optimized to obtain a realistic rebound behavior and the calibrated contact cards were implemented in the bipolar plate simulation model. A scaling coefficient of 0.7 was used to simulate the interface between the blank and the die. The effective von-Mises plastic strain at failure (‘psfail’ above) was set at 1. The estimation of the plastic strain at failure is based on the ratio of initial and final cross-sectional areas of the specimens elaborately discussed in Chapter 5. The Johnson-Cook parameters for steel die were inferred from literature$^{47,48}$. 

26
Parameter | Definition
---|---
mid | Material unique id
ro | Density of material
e | Elastic modulus
pr | Poisson’s ratio
vp | Formulation for rate effects
   | $\text{vp} = 0$, Scale Yield stress; $\text{vp} = 1$ viscoplastic formulation
a | Yield stress
B $\varepsilon^n$ | Strain hardening term
c | Strain rate parameter
psfail | Effective plastic strain at failure; if zero rate effects are ignored
sigmax | Maximum stress obtainable from work hardening before rate effects added.
   | Ignored if $\text{vp} = 1$
sigsat | Saturation stress that limits the maximum value of effective stress which can develop after rate effects are added (optional).
epso | Effective plastic strain rate

Table 5: Johnson-Cook parameter definitions

<table>
<thead>
<tr>
<th>Specimen</th>
<th>A (MPa)</th>
<th>B (MPa)</th>
<th>n</th>
<th>c</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ball</td>
<td>100</td>
<td>400</td>
<td>0.2</td>
<td>0.12</td>
</tr>
<tr>
<td>Die</td>
<td>200</td>
<td>550</td>
<td>0.05</td>
<td>0.12</td>
</tr>
</tbody>
</table>

Table 6: Johnson-Cook parameters for metal ball and die
3.5 Results and Conclusions

The measured input currents and the output velocities are shown in Figure 14 and Figure 15. The strain distributions from the simulation model have been superimposed on the experimental measurements in Figure 16. Although there are minor systematic differences, the simulation model predicts the necked regions in reasonable agreement with the experimental specimens. The region between the grooves (at both energies) shows rebound of the specimen upon striking the flat region of the die. This is more pronounced in the experiment indicating that the rebound is physical. The titanium comparisons show
that the necking is predicted very well. The numerical model does not have robust fracture criterion to make predictions about fracture zones. It only has a simple condition that deletes the numerical element if the instantaneous effective strain exceeds a critical effective strain at failure such as described by equation (5).

Figure 16. Deformed specimens of titanium sheet and copper driver at two energies (Experimental work of Huimin Wang, Ohio State University)

3.6 Discussion

The presented coefficients for Johnson-Cook model are a result of a one dimensional optimization for strain-rate parameter. The effective plastic strain at failure is estimated from experiments. This procedure is elaborately discussed in Chapter 5. Typically
the values did not exceed 1 within the velocity limits of 100-300 m/s. In the above simulations, if the effective von-Mises strain in any element exceeded the failure strain of 1 it was deleted from the simulation. This value is not constant and increases with increasing impact velocity. In fact, this is another degree of freedom. The uncertainty in this failure strain can be minimized by procuring dynamic tensile test data using ring expansion experiments or split Hopkinson bar measurements. A FIRE test (Johnson et al., 2009) is under development at the OSU renders constitutive parameters at high strain rates. The constitutive behavior of materials at high strain rates is available in literature. They can narrow the uncertainty in the numerical model. Although the Johnson-Cook model can predict strain distributions reasonably well its failure criterion are not robust enough to capture the failure dynamics. Ascribing failure laws for materials still remains a difficult challenge.
Chapter 4: Effect of Friction on Formability of the Workpiece

4.1 Introduction

In quasi-static conditions, the material may be drawn over the edge to form into it. As speed increases, the metal will tend to strike a die edge and shear off at this location. In this case, inertia acts to reduce the effective formability of the material. This punchless shearing operation has been shown to provide essentially no burr and can produce a very high quality edge. There has been little systematic study of this interaction of sheet and dies in impact forming, however it is important to understand for both shearing and forming as two limiting cases of how a sheet may behave upon impacting a die edge.

One of Banik’s workpiece materials was type 304 stainless steel. Because austenitic stainless steels have relatively high resistivity, a copper driver is used between the actuator and sheet. At impact velocities above about 200 m/s the forming experiments were complicated by the stainless steel impact welding to the copper driver and the die. Thin layers of grease and other interface materials were studied as means to reduce the propensity for impact welding. Each interfacial material that was used to mitigate the welding problems dramatically encouraged tearing of the workpiece. Further work examined the effect of friction both on the die-workpiece interface as well as at the driver-workpiece interface. In both cases, higher coefficients of friction provide better
forming behavior. Both Banik’s and this work support the idea that whether layers are added between the driver and workpiece, or lubricants are provided on the die, both encourage lateral flow of the material as it moves down into the channel and this encourages localization and shearing. This is consistent with the study by Taleff\textsuperscript{50} that demonstrates how a lubricated entry radius can encourage thinning and failure.

As a result of these and subsequent experiments, and simulations, it is believed that shallow channel forming is best accomplished if the workpiece can travel along its initial launch direction and upon impacting the die the material basically stops and will not slide laterally. This is accomplished with high coefficients of friction, particularly at the die-workpiece interface and high friction at the driver interface can be important if a driver is used. This is in contrast with most quasi-static metal forming where low coefficients of friction encourage the most uniform straining and this maximizes the ability to create component shape.

4.2 Experimental Setup

The experiment system is identical to the one described in the previous chapter. Experiments were conducted with austenitic stainless steels SS201 (instead of Titanium) and copper was used as a driver. The thickness of the SS201 sheets was 0.12 mm. The density of sheet metal was 7500 kg/m\textsuperscript{3}. Figure 18 shows comparison between model and experiments for the SS201 sheets. The comparison for copper sheets is not shown. The same metal die described in section 2.1 was used. A lubricant such as boron nitride was used to study frictional effects on interfaces\textsuperscript{51}. 
4.3 Simulation Methods

The schematic of the boundary conditions used in this model were identical to those in Chapter 3. The pressure condition is calibrated against experimentally evaluated velocities (PDV system). The mesh sizes and elements are shown in Figure 17. A section of the plates of length 20 mm is simulated to save computational resources.

![Figure 17. Mesh densities for the bipolar plate forming simulation for friction studies](image)

<table>
<thead>
<tr>
<th>Specimen</th>
<th>A (MPa)</th>
<th>B (MPa)</th>
<th>n</th>
<th>c</th>
</tr>
</thead>
<tbody>
<tr>
<td>Copper driver</td>
<td>100</td>
<td>150</td>
<td>0.3</td>
<td>0.06</td>
</tr>
<tr>
<td>SS201</td>
<td>250</td>
<td>800</td>
<td>0.2</td>
<td>0.01</td>
</tr>
<tr>
<td>Steel (Die)</td>
<td>200</td>
<td>550</td>
<td>0.05</td>
<td>0.05</td>
</tr>
</tbody>
</table>

Table 7: Johnson-Cook parameters for SS201 specimen and copper driver

The Johnson-Cook parameters for SS201 were inferred from the data sheets of AK Steel available on the web. The quasi-static tensile parameters for copper are taken from Padmanabhan’s thesis (1997).
The friction conditions in LS-DYNA factor in the contact cards that are used to describe the interface conditions. The materials were characterized using the Johnson and Cook model depicted in equation (4) in the previous chapter. The Johnson and Cook parameters are specified in Table 7. The effective von-Mises strain at fracture was fixed at 1. The CONTACT_AUTOMATIC_SURFACE_TO_SURFACE>Title keyword was used to simulate all the interfaces (Sample data file for the frictional case study available in Appendix A. Details about the supporting theory available in the LS-DYNA manual 2006, http://lstc.com/manuals.htm). The Coulombic friction co-efficient was used in the simulation as shown in equation (3) below. The static and dynamic friction coefficients under normal conditions were 0.5 and that with lubrication were 0.05. The decay co-efficient was set to 0.1 for all cases.

\[
\mu_{\text{eff}} = \left( \mu_{\text{dynamic}} + (\mu_{\text{static}} - \mu_{\text{dynamic}}) \cdot (e^{-\alpha \cdot v_{\text{rel}}}) \right)
\]

\(\mu_{\text{eff}}\) = Effective frictional coefficient; \(\alpha\) = decay coefficient
\(v_{\text{rel}}\) = relative velocity between contacting bodies

The relative velocity between the contacting bodies varies dynamically as the specimen moves towards and away from the metal die. Consequently the effective coefficient of friction also undergoes a dynamic change.
4.4 Results

Figure 18 only shows the comparison between experimental and model for austenitic steels SS201. The copper driver is not shown since SS201 is the product of concern. It is clear that the second case where lubrication is applied only between copper and SS201 has the maximum damage induced on the specimen. This is counterintuitive. The traditional expectation is that adding lubricants between interfaces prevents failure or damage and friction is often considered detrimental to the final product. However, under high speed conditions, it seems like the absence of friction is detrimental to the product shape and quality.

Figure 18. Deformed specimens of SS201 sheet from simulation and experiment (Energy 4.8 kJ) (a) No lubrication (b) Lubrication between SS201 and Die (c) Lubrication on all interfaces

4.5 Discussion

The contact interface between the two metal sheets and the die influences the impact dynamics and strongly affects local strain accumulation. It governs the lateral motion between components involved in the impact. Under normal conditions the frictional
coefficient is high and ensures the lateral motion is minimal. This gives relatively uniform strain distributions except at the channel corners. With the addition of lubricant, the friction coefficient is reduced and the propensity of relative lateral motion between the components is enhanced. This tends to cause enhanced strain localization at the die corners. Figure 18(b) shows that when lubrication is applied only between SS201 and the die this effect is very pronounced. Figure 18(c) shows that when lubrication is applied on all surfaces the failure is not as pronounced as in Figure 18(b). All of this is again attributed to the lateral motion at the interface.

The effect of wall angle reveals interesting insights in shape fidelity. The mesh densities for the simulation are shown in Figure 19. A section of the plates of length 20 mm is simulated to save computational resources. The mesh density of the die is not crti-
cal because it doesn’t undergo any deformation. However, the die shouldn’t be simulated as a rigid body for reasons of the unrealistic rebound of specimen stated earlier in Chapter 3. The results are shown in Figure 20.

Figure 20. Effect of entry wall angle and frictional coefficient (µ) on formed workpiece

The quality and shape of the formed sheet metal is highly dependent upon the entry wall angle of the die channels. A steeper angle increases the propensity for shear under high speed impact. A shallower angle is beneficial for good shape conformity as shown in Figure 20(e) and (f). The effect of friction has been established from section 2.4. From Figure 20 (c) and (d), it is evident that there is higher kinetic energy in the
plate that causes material to flow into the cavity and consequently shear at the edges. For these cases, a secondary forming operation is essential to ensure better shape conformity.
5.1 Introduction

With general agreement secure on these topics it seems we can confidently move to the simpler issue of forming useful shapes by inertia-driven sample impact into dies. A simple example of this is shown in Figure 27. Here a simple circular ring is given an axisymmetric launch into a square cavity. Rebound is maximum for normal impact and causes a complicated impact dynamic that produces a formed part that is neither square or round and it necks and ruptures in reproducible locations between the corners and mid-points of the sides of the square. This kind of rupture can be dominant in forming shallow pan-like structures. Experimental work in forming shallow pans with the UPA and similar driver systems has demonstrated that dimensional irregularities, failure and ‘ghost lines’ can often form away from the corners. A good example of this is cited by Kamal and Shang\textsuperscript{52}. Here a simple prototypical problem is considered and the numerical finite element model is validated against the experimental strain distributions and velocity measurements.
5.2 Experimental Setup

A circular well annealed aluminum ring was expanded into a steel square die. The experiment uses a 16 kJ capacitor bank (at maximum charging voltage of 8.66 kV), 5 turn helical copper coil, a cylindrical casing of outer radius 24 mm, a square metal die cavity (Figure 21), aluminum rings of inner radius 24 mm, width 10 mm and thickness 1.3 mm. The primary copper coil is embedded inside the cylindrical casing by means of tooling grade urethane. The coil inductance is 0.4 $\mu$H. The ring fits onto the casing to complete the secondary circuit. The standoff distance between the ring and the die cavity is 3 mm. The same capacitor bank is used in the experiments. Discharge from the capacitor bank charges the primary circuit embedded in the coil box. The primary current induces a secondary current in the metal blank. The two currents being opposite in nature generate repulsion high enough to drive the ring into the die.
5.3 Simulation Methods

The traditional modeling algorithm\textsuperscript{53} is divided into two coupled parts; electromagnetic flux equations and the mechanics governing plastic flow. In this case, the elec-
The magnetic component of the system of equations is decoupled from the mechanical simulation. A magnetic pressure boundary condition is applied to the ring similar to the boundary condition described in equation (6) in section 3.3. The coupling factor between copper coil and aluminum rings is assumed to be 0.7. The keywords used are LOAD_SEGMENT_SET and DEFINE_CURVE. The pressure boundary condition is released at the onset of impact to let the ring flow plastically on account of its own inertia. The schematic of the boundary condition is shown in Figure 24. Two velocities were measured in the directions indicated by yellow and orange arrows in Figure 22 using the Photon Doppler Velocimeter system.

![Figure 24. (a) Magnetic pressure boundary condition (b) Experimental and simulated velocity measured at the central location (yellow arrow location) for energy of 5.6 kJ](image)

The red curve in Figure 24 shows that upon impact rebounds with a finite velocity (visible through second and third peaks). The black curve (simulated velocity profile) changes its sign at 40 µs indicating rebound. The small peak in the black curve at
40 $\mu$s is higher than the peak from the experimental velocity profile indicating exaggerated rebound. The second pressure peak is absent in the magnetic pressure boundary condition. Only the first part of the current/magnetic pressure is fed as input to the simulation. Thus, the ring in the simulation continues its rebound trajectory for a larger time than what is actually evident from experiments. It is the magnetic pressure that stabilizes the ring’s rebound motion. A one-quarter symmetry model was used to conserve computational time. The mesh densities are shown in Figure 25.

Figure 25. Mesh densities and dimensions for the one-quarter symmetry model

Also the black curve retains a finite velocity for a longer time while the red curve dies down after a few peaks with subsequently smaller amplitudes. This implies that it is the second peak of the magnetic pressure that stabilizes the ring after the impact. This second peak is absent in the simulated profile and hence the simulated ring specimen shows higher rebound. This higher rebound is reinforced by the comparison between experimen-
tal ring and the simulated ring specimens shown in Figure 26 (b) and Figure 27 (b). The plastic strain at fracture as described in equation (2) is inferred empirically from the relative change in cross sectional areas of the sheet metal specimen before and after impact. Two velocities were measured in the directions shown in Figure 26(b). The Johnson-Cook parameters for the ring (taken from Padmanabhan’s thesis\(^\text{54}\) (1997)) and the die are shown in

<table>
<thead>
<tr>
<th>Specimen</th>
<th>A (MPa)</th>
<th>B (MPa)</th>
<th>n</th>
<th>c</th>
</tr>
</thead>
<tbody>
<tr>
<td>Aluminum ring</td>
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<td>50</td>
<td>0.35</td>
<td>0.015</td>
</tr>
<tr>
<td>Steel (Die)</td>
<td>200</td>
<td>550</td>
<td>0.05</td>
<td>0.05</td>
</tr>
</tbody>
</table>

Table 8: Johnson-Cook parameters for aluminum ring specimen and metal die

Figure 26. (a) Input current and output velocity (b) Model vs. ring expansion experiments @ 5.6 kJ

Figure 26 & Figure 27 show the input current, the experimental and simulated impact velocities (along the diagonal direction only) and the comparison between the ring configurations after impact at two energies. The impact velocities measured at two locations on
the ring circumference in both the indicated directions help were used to calibrate the model. Both the simulations and experiment show reduced propensity for failure at the higher impact energy. This is presumably because the sample strikes the die more uniformly\textsuperscript{55}.

![Figure 27](image)

Figure 27. (a) Input current and output velocity (b) Model vs. ring expansion experiments @ 7.2 kJ

Both the simulations and model show that there is a rebound phenomenon that produces a component that is far from square in the end and these rebounds drive the failure process. The failure strains for the simulation model were calibrated through experiments. The change in cross sectional areas\textsuperscript{56} of the specimens were measured to infer the strain at fracture using (8). Figure 28 shows that sharp directional velocity gradients cause complex range of stress states to cause the ring to fail at the indicated regions in red circles.

\[
\varepsilon_f = \ln \left( \frac{A_i}{A_f} \right); A_i = \text{Initial area, } A_f = \text{Final area at failure} \tag{8}
\]
The strain at failure increases with an increase in the impact velocity. This dependence on strain rate and temperature has been explicitly covered in the traditional form of Johnson and Cook model in equation (2) in Chapter 2.

<table>
<thead>
<tr>
<th>Energy (kJ)</th>
<th>$A_i$ (mm$^2$)</th>
<th>$A_f$ (mm$^2$)</th>
<th>$\varepsilon_f$</th>
</tr>
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<tbody>
<tr>
<td>5.6</td>
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<tr>
<td>7.2</td>
<td>13</td>
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</tr>
</tbody>
</table>

Table 9: Inferred plastic strains at fracture through experimental ring specimens

In the above simulation model this dependence is implicitly accounted through an empirically measured strain. Although this reduces tedious optimization of the 5 calibration constants in (2), it has an inherent limitation of dependence on experiments. The values of fracture strains are tabulated below.

Figure 28. (a) Failed ring at 6.6 kJ (b) Model schematic with velocity vectors
5.4 Results and Conclusions

In part (1) of Figure 29, as the ring strikes the square die, it rebounds and undergoes a rapid change in stress states (tensile to compressive in the circumferential direction). Meanwhile, the magnetic pressure causes the ring to advance in the diagonal direction. The intermediate region between the central and diagonal direction acts like a tensile specimen. It necks and consequently fails under influence of a multiaxial stress states. There is a complicated interaction between stress waves travelling towards each other as shown in Figure 29. In this process there is a strong interaction between the wave fronts subsequently followed by their energy dissipation (Part (2)). Part (4) shows the stress release upon fracture. The final shape of the product is not exactly the same as the square die. This is largely attributed to rebound of the ring from the edge of the die. There is a distinct similarity observed in the fracture paths from the simulation model and the experiments shown in Figure 30. Part of the kinetic energy of the ring has to be plastically dissipated in order to minimize the bounce-off.
5.5 Discussion: Rebound Effects

The qualitative comparison between the numerical model and formed specimens shown in Figure 29 reveal insights into the coupling between the electromagnetic and the mechanical process. The numerical model in general has higher rebound than the experimental product. This is because the pressure boundary condition is released upon contact to let the ring specimen fly on account of its own inertia. In reality, the specimen upon impact is stabilized against the square die by the magnetic pressure that prevents it from bouncing back. The measured velocity profiles in Figure 26 and Figure 27 show a trough.
followed by another peak in sync with the current profiles. The second peak is due to the magnetic pressure. The simulation model does not adequately portray this peak and shows a steady decline and manifests as a rebound. At low energy (5.6 kJ) the magnetic pressure begins its decline prior to the strike of the ring specimen (compare second peak in Figure 26 (a)). At 7.2 kJ, the second peak in velocity precedes that in current thus stabilizing the rebound effect and producing flatter parts. The magnetic pressure thus holds the part firmly against the die dissipating its kinetic energy into stress waves that traverse the circumference of the specimen.

Figure 30. X-shaped fracture patterns (a) Model (Effective von-Mises strain contours) (b) Experimental ring specimen

A comparative study was conducted between simulations with the magnetic pressure boundary condition (also called mechanical simulation here) and the fully coupled electromagnetic simulation to gain insights into the rebound process. The coupled electromagnetic simulator consumes enormous computational resources. Thus, a coarse scale mesh with densities are shown in Figure 31. Figure 32 shows the velocity profiles from
both the simulations overlaid on the experimental current and velocity profiles. Two re-
gions marked A and B are of critical interest. The green curve curve clearly shows two
small peaks following the first big peak at 40 \( \mu \)s.

Figure 31. Mesh densities for the electromagnetic simulation
The first small peak at 50 µs is because of the rebound of the ring specimen after impact against the square die. The second small peak coincident with the current peak at about 75 µs manifests hits the ring again to stabilize it against the die. The Photon Doppler Velocimeter does not measure the direction of motion and hence all peaks are in the same direction. The electromagnetic simulation shown in red clearly replicates the rebound behavior through one change in direction (sign) near 50 µs and another change in direction at about 65 µs and yet another change of direction at 90 µs. These peaks are similar to the two small peaks discussed in the empirical velocity profiles. The electromagnetic simulation is carried in pure vacuum with no energy losses other than plastic
dissipation. The experiment is not carried out in vacuum and has more energy losses during the motion of the ring. Hence the green curve dies down at about 85 µs.

![Figure 33](image)

**Figure 33.** (a) Velocity vectors from mechanical simulation at 50 µs (b) Velocity vectors from electromagnetic simulation at 50 µs (c) Velocity vectors from mechanical simulation at 80 µs (d) Velocity vectors from electromagnetic simulation at 80 µs

In Figure 33 (a) the velocity vectors (orange arrow) indicate the regions moving in the diagonal direction of the square (top right) and the blue vectors (top left and bottom right) indicate the regions that have rebounded after impacting the square die. This cor-
responds to region A (first small peak in green velocity curve in Figure 32) at 50 µs. In Figure 33(b), the velocity vectors replicate similar rebound behavior at 50 µs. In Figure 33(c) the green arrow in the top right corner indicates vectors that are moving towards diagonal of the square. At 80 µs an increasing number of vectors originally moving towards the top right corner now begin to traverse their rebound path upon striking the die. (orange and blue arrows).

Figure 34. Correlation between ghost lines in the cell-phone die case (Kamal & Daehn, 2007) and failure zones in the ring expansion in square die

The blue vectors at the tips of the quarter ring in contrast with orange vectors represent sharp velocity gradients. These result from stress waves that continuously trace the circumference of the ring until all the energy in the system is dissipated. They are responsible for subjecting the ring to a complex stress state and result in consequent failure.
In electromagnetic simulation, although the velocity gradients exist, they are not as sharp due to a stabilization effect by the magnetic pressure. In Figure 33 (d), it can be clearly seen from the direction of the vectors that the center of the ring is stabilized by the second current peak at 75 $\mu$s corresponding to region B in Figure 32. This magnetic pressure restores the shape of the ring to conform better to the square die. Figure 34 shows a nice comparison between the ghost lines in the specimens formed into cell-phone dies and the ring expanded into a square die. The shape conformity studies seem to offer some explanation for the formation of ghost lines. It could be a result of sharp velocity gradients that are a consequence of complex stress states or stress wave interactions discussed above.
Chapter 6: Conclusions

1. **Rebound**: In impact forming the shape of the component can vary significantly from the shape of the die upon which the sheet strikes. This is influenced by rebound of the specimen upon impact, friction between interfaces, geometry of the die (including the stiffness of the component produced) and the persistence of magnetic pressure after die strike. The persistent magnetic pressure after impact stabilizes the ring specimen. This is revealed through a comparison between fully coupled electromagnetic and mechanical simulation with a magnetic pressure boundary condition.

2. **Friction**: Friction is very critical to form failure free parts. The ability to make a given shape with a specific material is closely related to frictional conditions in impact forming, as it is in traditional quasi-static forming. However, in impact forming high coefficients of friction are generally favored because draw-in is difficult in impact forming. The design strategy is to have each material segment maintain a path along its launch vector.

3. **Channel Entry Angle**: The entry wall angle plays a critical role in the shape conformity of sheet metal. For vertical shear walls, the formed shape is very different from the metal die shape. For steep angles a secondary forming operation is beneficial in improving the conformity.
4. **Ghost lines:** Failure mechanisms are attributed to interaction between stress and/or displacement waves, rapid change in stress states that lead into necking and consequent failure. These could possibly explain the origins of the ghost lines observed in Kamal’s experiments.
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Appendix A: LS-DYNA Keywords for sheet metal forming

The DYNA data file is arranged as a sequence of sections, whose chronology is unimportant from the point of view of code execution but if organized can be helpful. The following is a brief synopsis of the important keywords relevant for controlling boundary conditions for sheet metal forming problems. The detailed description should be referred in the DYNA manuals available at www.lstc.com:

*KEYWORD memory = 1550000000

This is used to set or increase the memory limits of the CPU depending upon the computational complexity of the mesh.

*LOAD_SEGMENT_SET : This keyword is used to specify the mechanical loading conditions (pressure) on the specimen. SEGMENT refers to a set of element, nodes or parts defined separately in the respective sections SET_SEGMENT, SET_NODE_LIST, SET_NODE_GENERAL.

*DEFINE_CURVE: This keyword supplements the boundary conditions specified by the LOAD_SEGMENT keyword. It entails the time gradients or the nature of the load curve.

*INITIAL_VELOCITY: This keyword is used to specify the initial velocity or the dynamic acceleration curve for the flyer. This is typically a nodal boundary condition.

*BOUNDARY_SPC_NET : This keyword is used to specify the constraints on motion of the workpiece or flyer. This is a nodal boundary condition.
*CONTROL_KEYWORDS: These keywords control the simulation run parameters like the time steps, termination times and the total energy content of the system.

*CONTACT_KEYWORDS: These keywords are cardinal to controlling boundary conditions after impact. They are very sensitive to boundary conditions. It is recommended that the user run test cases with prototypes like the tennis ball problem to calibrate contact cards prior to using them in their actual simulation scenario. Typically the CONTACT_AUTOMATIC_SURFACE_TO_SURFACE keyword is very handy and offers good leverage over the impact dynamics. The scaling coefficient that controls the specimen rebound characteristics discussed in section 5.5 is specified under this keyword.

*DATABASE_KEYWORDS: These keywords are used to control the output data and the rate at which output is generated depending upon the time period of interest.

*MAT_KEYWORDS: These keywords set the material type and constitutive parameters that are available from experiments. A good starting point is the MAT_JOHNSON_COOK or MAT_SIMPLIFIED_JOHNSON_COOK keyword.

*SECTION_KEYWORDS: These keywords set the element type and control the numerical complexity and convergence characteristics of the problem.

Mesh file: *NODE and *ELEMENT_SOLID: Following these keywords is the mesh file that is fed from a mesh preprocessor such as LS-Prepost or Hypermesh. It is a good practice to include the mesh file separately in the main data file instead of pasting data from the mesh file into the main file. This offers freedom to change the mesh file independently.
Common sources of error

Aspect ratio of the elements: Typical sheet metals have very high aspect ratios i.e. the thickness dimension is very small compared to the length and the breadth. To save computational time, if an attempt is made to discretize the plane of the sheet metal into coarse elements, their aspect ratio turns very high. High aspect ratio elements typically greater than 10 are extremely detrimental for numerical convergence. This error shows up in DYNA in the form of a parameter ‘ah’ as follows:

Aspect ratio = 10; EM-BEM implicit iteration # 30, ah = 0.1564631E+30,
Aspect Ratio = 4; EM-BEM implicit iteration # 30, ah = 0.1564631E+4

The parameter ‘ah’ is a measure of the numerical divergence between the assumed solution and the functional value at a given time step. Notice the increase in the exponent with an increase in the aspect ratio. This increase is a clear sign of numerical instability. Under such circumstances it is a good practice to use the CONTROL_TIMESTEP keyword to specify smaller time steps. Thus, as the aspect ratio is increased the time for computation increases. The numerical stability is a function of the ratio of time-steps to the smallest dimension of the element. Ideally regular cubic elements work best.

In the case of electromagnetic simulations, beware of using hexahedral elements and preferably regular cubic. They are conducive for simulation of electromagnetic fields. If any of the parts in the model have non-hexahedral elements, the program terminates giving an error message with a library function ‘lib’. This is also true if the segments (cross-sections of the solenoid that mark input and output direction) that define the current path
are opposite in direction. The direction of the current path is specified in the data file using EM_CIRCUIT keyword. The numbers signify 1- Input, 2-Output, 3-intermediate and are preceded by negative signs if the direction of 2 is opposite to 1. The current versions of DYNA at OSU are not equipped to handle prismatic, tetrahedral or trapezoidal elements. The EM-DYNA module is being modified to incorporate the variable nature of elements at LSTC. A good starting material model recommended for use with EM-DYNA is MAT_POWER_LAW_PLASTICITY. The MAT_SIMPLIFIED_JOHNSON_COOK model does not work well with the EM-module. The code is prone to numerical instabilities.

Sample Data File (Frictional Case study)

$$$$$$$$$$$$$Contact cards from plastic sphere impact $$$$$$$$$$$$$$
#
*KEYWORD
*TITLE
$# title
LS-DYNA keyword deck by LS-Prepost
*parameter
R p1 -0.055
R p2 -0.005
R p3 -0.01
R t1 0.06
R t2 0.0275
R t3 0.08

*LOAD_SEGMENT_SET
$# esid LCID SF
1 3 -1 0
*DEFINE_CURVE
$# LCID
3
$# abscissa ordinate
0.00 0.00
&t1 &p1
&t2 &p2
*CONTROL_TERMINATION
$ endtim endcyc dtmin endeng endmas
&t3
*control_contact
$  SLSFAC RWPNAL ISLCHK SHLTHK PENOPT THKCHG ORIEN
   0.1  0.0   2   1   4   0   0   1
$  USRSTR USRFAC NSBCS INTERM XPENE SSTHK ECDT
TIEDPRJ
   0   0   10   0   5.0   0
*CONTROL_OUTPUT
$  npopt neecho nrefup iaccop opifs ipnint ikedit
   1   3
*CONTROL_TIMESTEP
$  dtinit tssfac isdo tslimt dt2ms lctm erode
msist
   2E-6   2E-7   1

*CONTROL_ENERGY
$  hgen rwen slnten rylen
   2   2   2   2
*DATABASE_ABSTAT
$#  dt binary lcur ioopt
  0.00200   1
*DATABASE_ELOUT
$#  dt binary lcur ioopt
  0.00200   1
*DATABASE_GLSTAT
$#  dt binary lcur ioopt
  0.00200   1
*DATABASE_MATSUM
$#  dt binary lcur ioopt
  0.00200   1
*DATABASE_NODOUT
$#  dt binary lcur ioopt dthf binhf
  0.00200   1
*DATABASE_RCFORC
$#  dt binary lcur ioopt
  0.00200   1
*DATABASE_BINARY_D3PLOT
$  dt lcdt
  0.002   7
$#  ioopt
   2

*DEFINE_CURVE
$#  LCID
   7
$#  abscissa ordinate
   0.015  0.015
   0.016  0.001
*DATABASE_EXTENT_BINARY
$  NEIPH NEIPS MAXINT STRFLG SIGFLG EPSFLG RLTFLG ENGFLG
   5   1
$  CMPFLG IEVERP BEAMIP DCOMP SHGE STSSZ
   1   2
$$$$$$$$$$$$$$$$$$$$$Contact cards from plastic sphere impact $$$$$$$$$$$$$$$$$$
*CONTACT_AUTOMATIC_SURFACE_TO_SURFACE_TITLE
$ CID IF. NAME
 1
$ SSID MSID SSTYP MSTYP SBOXID MBOXID SPR MPR
 5  7  3  3  0  0  0
 0
$ FS FD DC V VDC PENCHK BT DT
 0.5
1.000E+20
$ SFS SFM SST MST SFST SFMT FSF VSF
 .100E+01 .100E+01
 .100E+01 .100E+01 .100E+01 .100E+01
 $ SOFT SOFSCL LCIDAB MAXPAR SBOPT DEPTH BSORT FRCFRQ
 1  0.70  1.025  5  2.0  0
 1
$ PENMAX THKOPT SHLTHK SNLOG ISYM I2D3D SLDTHK SLDSTF $ Checks if node penetrates the master surface
 0.01  1  1  1  1  1  0.003
0.5
*CONTACT_AUTOMATIC_SURFACE_TO_SURFACE_TITLE
$ CID IF. NAME
 1
$ SSID MSID SSTYP MSTYP SBOXID MBOXID SPR MPR
 7  8  3  3  0  0  0
 0
$ FS FD DC V VDC PENCHK BT DT
 0.5
1.000E+20
$ SFS SFM SST MST SFST SFMT FSF VSF
 .100E+01 .100E+01
 .100E+01 .100E+01 .100E+01 .100E+01
 $ SOFT SOFSCL LCIDAB MAXPAR SBOPT DEPTH BSORT FRCFRQ
 1  0.70  1.025  5  2.0  0
 1
$ PENMAX THKOPT SHLTHK SNLOG ISYM I2D3D SLDTHK SLDSTF $ Checks if node penetrates the master surface
 *CONTACT_AUTOMATIC_SURFACE_TO_SURFACE_TITLE
$ CID IF. NAME
 1
$ SSID MSID SSTYP MSTYP SBOXID MBOXID SPR MPR
 6  5  3  3  0  0  0
 0
$ FS FD DC V VDC PENCHK BT DT

66
0.5  0.5  0.1  0.25  0.5  0  0.0
1.000E+20
$  SFS  SFM  SST  MST  SFST  SFMT  FSF
VSF
.100E+01  .100E+01  .100E+01  .100E+01  .100E+01
.100E+01
$  SOFT  SOFSCL  LCIDAB  MAXPAR  SBOPT  DEPTH  BSORT
FRCFRQ
  2  0.10  1.025  5  2.0  0  1
$  PENMAX  THKOPT  SHTLTK  SNLOG  ISYM  I2D3D  SLDTHK
SLDSTF $ Checks if node penetrates the master surface
  0.01  1  1  1  1  1  0.005
0.1
*PART
$  pid  sid  mid  eosid  hgid  grav  adpopt
  Titanium
  5  5  5
  Metal die
  7  7  7
  Copper
  6  6  6
  Die
  8  8  8
$$$$ Materials
*MAT_SIMPLIFIED_JOHNSON_COOK
$  mid  ro  e  pr  vp
  5  5.0E-6  115  0.35  0
$  a  b  n  c  psfail  sigmax  sigsat  epso
  0.20  0.700  0.2  0.06  1  20
0.001
*MAT_SIMPLIFIED_JOHNSON_COOK
$  mid  ro  e  pr  vp
  6  8.0E-6  115  0.35  0
$  a  b  n  c  psfail  sigmax  sigsat  epso
  0.10  0.15  0.30  0.06  1  20
0.001
*MAT_SIMPLIFIED_JOHNSON_COOK
$  mid  ro  e  pr  vp
  7  9.0E-6  250  0.35  0
$  a  b  n  c  psfail  sigmax  sigsat  epso
  0.2  0.55  0.05  0.05  1.0  1.0
0.001
*MAT_SIMPLIFIED_JOHNSON_COOK
$  mid  ro  e  pr  vp
  8  9.0E-6  250  0.35  0
$  a  b  n  c  psfail  sigmax  sigsat  epso
0.20  0.55  0.05  0.05  1.0  1.0

0.001

*BOUNDARY_SPC_SET

$#     nsid       cid      dofx      dofy      dofz     dorfx     dorfy

dorfxz
7      0      0      1      1      1      1

1

*SET_NODE_GENERAL

$#     sid

7

$#     opti      nid2      nid3      nid4      nid5      nid6      nid7

nid8

PART   7      5      6      8

$** Sections

*SECTION_SOLID

$      sid    elform      shrf       nip     propt   qr/irid     icomp

setyp
5      2

*SECTION_SOLID

$      sid    elform      shrf       nip     propt   qr/irid     icomp

setyp
7      2

*SECTION_SOLID

$      sid    elform      shrf       nip     propt   qr/irid     icomp

setyp
6      2

*SECTION_SOLID

$      sid    elform      shrf       nip     propt   qr/irid     icomp

setyp
8      2

*BOUNDARY_SPC_SET

$#     nsid       cid      dofx      dofy      dofz     dorfx     dorfy

dorfxz
2      1      0      1      1      1      1

1

*BOUNDARY_SPC_SET

$#     nsid       cid      dofx      dofy      dofz     dorfx     dorfy

dorfxz
1      1      1      1      1      1      1

1

*BOUNDARY_SPC_SET

$#     nsid       cid      dofx      dofy      dofz     dorfx     dorfy

dorfxz
3      1      1      1      1      1      1

1

Copy the Mesh data at this point or include the mesh file

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