Effect of polyurea on dynamic response and fracture resistance of steel plates under impulsive loads
Effect of Polyurea on Dynamic Response and Fracture Resistance of Steel Plates under Impulsive Loads

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Chair

University of California, San Diego

2007
DEDICATION

To my family.

To my friends.

To my teachers.

To all who supported me in my life and made me believe in myself.
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ABSTRACT OF THE DISSERTATION

Effect of Polyurea on Dynamic Response and Fracture Resistance of Steel Plates under Impulsive Loads

by

Mahmoud Reza Amini

Doctor of Philosophy in Engineering Sciences (Mechanical Engineering)

University of California, San Diego, 2007

Professor Sia Nemat-Nasser, Chair

Enhancing the dynamic performance and fracture resistance of steel plates under impulsive loads has always been of great interest to the researchers and scientists. A convenient technique to enhance the energy absorption capability of steel plates is to spray-cast a layer of polyurea onto the
plates. Since polyurea readily adheres to metallic surfaces and has a short curing time, the technique may be used to retrofit existing metallic structures to improve their blast resistance. We have examined the effectiveness of this approach, focusing on the question of the significance of the relative position of the polyurea layer with respect to the loading direction; i.e., we have explored whether the polyurea layer cast on the front face (the impulse-receiving face) or on the back face of the steel plate would provide a more effective blast mitigating composite. In addition we have studied the effects of the thickness of the polyurea layer and the steel-polyurea interface bonding strength.

The experimental results suggest that the polyurea layer can have a significant effect on the response of the steel plate to dynamic impulsive loads, both in terms of failure mitigation and energy absorption, if it is deposited on the back face of the plate. And, remarkably, when polyurea is placed on the front face of the plate, it may actually enhance the destructive effect of the blast, promoting the failure of the steel plate, depending on the interface bonding strength between the polyurea and steel layers and the polyurea layer thickness. These experimental results are supported by our computational simulations of the entire experiments. In addition, SEM and optical microscopy is performed to examine the microstructure of the failed samples, and also understand the fracture and necking patterns, and the underpinning mechanisms of failure. Based on the micrographs, finite-element models are developed that are capable of predicting the fracture process of the steel plates.
An independent chapter completes this dissertation. In Chapter 7, we report the results of an experimental and numerical investigation of dynamic and quasi-static compressive response of single and hex-arrayed thick aluminum tubes.
Chapter One

Introduction

1.1. A review of dynamic deformation and failure of steel plates

The dynamic response of thin steel plates is studied for several decades. Several reviews on the dynamic performance of steel plates is presented by Jones [1,2,3,4] and Nurick and Martin [5,6]. There are many theoretical and numerical studies on the deformation of the plates under dynamic loads. Some of these works are supported by experiments. Various experimental techniques are used to study the dynamic response and fracturing of plates. Among the various techniques used for this purpose are: Air pressure waves created by explosive devices, underwater explosion forming, direct impulsive loading using plastic sheet explosives, spring loaded arms, and direct impact using metal-foam projectiles. These investigations were primary focused on the final deformed shape of the plate since it is difficult to capture the transient response of the structures. However, several methods are used to gain insight into the dynamic response of the tested structures, including using strain gauges, high speed photography, stereophotogrammometric, condenser microphones, light
interference techniques, pressure sensors and load cells. Nurick and Martin [6] used a dimensionless number introduced by Johnson [7] to compare the experimental results presented by different researchers using different experimental techniques, different plate materials and plate dimensions.

The existing experimental investigations [8, 9, 10, 11] have led the researchers to define three failure modes (referred to as Modes I, II, and III), for beams and plates subjected to impulsive loads; see Figure 1.1. These are:

Mode I- large inelastic deformation
Mode II- shear failure at the edges.
Mode III- tensile tearing at the edges

Figure 1.1. Schematic view of the three modes of failure of thin plates under impulsive loads.
The first theoretical studies performed by Taylor [12], Hudson [13], and Richardson and Kirkwood [14], on the dynamic response of steel plates only considered bending deformation. This assumption resulted in small deformations predictions. This was extended by considering the membrane effects in addition to the bending effects. The analytical methods based on bending and membrane effects could predict the deformations up to the plate thickness but failed to predict the greater deformations. This necessitates the introduction of more complicated numerical methods to analyze this set of problems. A momentum conservation approach, eigenvalue expansion methods, wave form approaches and are among the various analytical-numerical methods used to investigate the response of plates under dynamic loads [5, 15, 16, 17]. The introduction of these numerical-analytical which rely on mode concept and transverse displacements, enabled predictions of up to 9 times the plate thickness. The prediction capability of these methods was further increased by considering lateral displacement in addition to the transverse displacements. However, these analytical-numerical methods were restricted to simple constitutive models, geometries and boundary conditions. With the introduction of commercially available finite-element codes many researchers tried to use this powerful tool to predict the evermore complex dynamic problems. In a recent research conducted by Balden and Nurick [18], full scale numerical simulations of the post-failure motion of steel plates subjected to blast loads have been conducted using the finite-element code ABAQUS. Another investigation of this kind has been conducted by Lee and
Wierzbicki [16, 17] in which they examine the fracture of thin plates under localized impulsive loads and the possible dishing, disking, and petaling of the plates. Zhu [19] performed a numerical investigation to understand the transient behavior of thin plates under explosive loads and compared his predictions with the experimental results. Yuen and Nurick [20] used ABAQUS to predict the response of quadrangular stiffened plates subjected to uniform blast loads and compared their predictions with experimental results. Bahei-El-Din et al. [21] studied the effect of interlayer elastomeric polyurea on the dynamic response of sandwich plate under dynamic loads. Finite-element calculations of Xue and Hutchinson [22] seek to explore the ability of the bilayer plates to sustain intense impulses and assess the results by comparing to the estimated performance of monolithic steel plates of the same total mass, using the finite strain version of the ABAQUS Explicit code. The significance of these full-scale numerical simulation codes is that they allow first, to study the transient response of the plate, second, to incorporate complex temperature-, pressure-, and rate- dependent constitutive models in the finite-element code, and finally, to conduct parametric studies to gain insight into the dynamic deformation mechanisms.

1.2. Enhancing the fracture resistance of steel plates using polyurea

Spray-casting a layer of polymer material onto steel plates to enhance their dynamic performance and fracture resistance has recently received
considerable interest. Among the polymers, polyurea seems to be a great candidate with unique mechanical, physical and chemical properties to enhance the fracture resistance of metal plates. Polyurea was introduced in 1989 by Texaco Chemical Company derived from the reaction product of an isocyanate component and a synthetic resin blend component. In this study we mainly focus on the mechanical and physical properties of polyurea. Polyurea can exhibit a wide range of mechanical properties, from soft rubber to hard plastic depending on its chemical characteristics. Polyurea at room temperature is highly elastic, flexible, and resistant to abrasion, impact, and weather. The glass transition temperature, $T_g$, of polyurea is below $-50^\circ C$ [23, 24] which is well below the typical range of the service temperature. Above the glass transition temperature, polyurea has a nearly-elastic volumetric response and a viscoelastic shear response at moderate pressures and strain rates.

This elastomer has been extensively used in the coating industry in solid form (e.g. tunnels, bridges, roofs, parking decks, storage tanks, freight ships, truck beds). In addition to the coating industry, this material has recently been used to increase the impact resistance of buildings, vehicles and structures. Polyurea coatings are also applied to military armor to increase its resistance to ballistic penetration [25]. Mock and Blaizer [26] experimentally observed that the polyurea coating can change the response of the steel plate from full penetration of a projectile to complete mitigation of its fracture. Xue and Hutchinson [22] studied the neck retardation under biaxial stretching of
bilayer elastomer-metal plates. They show that substantial increase in necking limits and consequent energy absorption can be achieved in metal-elastomer bilayers. To understand the effect of polyurea on the dynamic response of structures and the underpinning mechanisms of failure, it is essential to characterize its properties at a wide range of temperatures, strain-rates and pressures. The viscoelastic properties of this polyurea have been systematically studied by Amirkhizi et al. [27] over a broad range of strain rates and temperatures, including the high-pressure effects. Based on this, a pressure, temperature, and strain-rate sensitive model has been developed by these authors and implemented into LS-DYNA. The model involves only a finite number of internal variables and is specifically well-suited for use in an explicit finite-element code. Roland et al. [28] studied the uniaxial tensile behavior of polyurea over a strain rates from 0.06 to 573s$^{-1}$. Sarva et al. [29] investigated the large deformation stress-strain behavior of elastomeric-thermoset polyurea over strain rates from 0.001 s$^{-1}$ to 10,000 s$^{-1}$ (in tension). They reported that polyurea undergoes transition from a rubbery-regime behavior at low rates to a leathery-regime behavior at the highest rates. Jiao et al. [30] studied the nonlinear dynamic response of polyurea at shearing rates from 10$^5$ to 10$^6$ s$^{-1}$ under high pressures using pressure-shear plate impact experiments. Roland and Casalini [31] studied the viscoelastic response of polyurea under hydrostatic pressure by measuring the relaxation times for the soft segments of polyurea using dielectric spectroscopy.
The aim of this study is to understand the effect of polyurea on the dynamic response and fracture resistance of steel plates, focusing on understanding the underpinning mechanisms of deformation and effect of relative position of polyurea with respect to the loading direction (i.e. polyurea on the front face or back face).

![Figure 1.2. Schematic view of the bilayer plate with polyurea on the impact receiving side (front face) on the right and bilayer plate with polyurea on the opposite to the impact side (back face) on the left.](image)

This study shows that the presence of polyurea layer has three main effects on the dynamic response of steel plates:

- The polyurea alters the initial shock wave
• Polyuera dissipates the energy based on two mechanisms: first, viscoelasticity and second, pressure and strain-induced transition from the rubbery to the glassy state

• Retarding the onset of necking of steel by increasing the overall tangent modulus of the plate.

1.3. Outline

Most of the chapters in this dissertation are prepared to be stand-alone publications. Therefore, some of the fundamentals are repeated for the sake of independence. Chapter 2 presents the results of a series of reverse ballistic experiments performed to assess the dynamic response of circular monolithic DH-36 steel and steel-polyurea bilayer plates to impulsive loads. The initial kinetic energy per unit thickness of the steel layer is used to assess the performance of the monolithic and bilayer plates. In Chapter 3 we present and discuss the results of our numerical simulation of the dynamic response and failure modes of circular DH-36 steel plates and DH-36 steel-polyurea bilayers, subjected to impulsive loads in reverse ballistic experiments reported in chapter 2. For the numerical simulations, we have used physics-based and experimentally-supported temperature- and rate-sensitive constitutive models for steel and polyurea, including in the latter case the pressure effects. Comparing the simulation and the experimental results, we focus on identifying the potential underpinning mechanisms that control the
deformation and failure modes of both monolithic steel and steel-polyurea bilayer plates. In Chapter 4, we summarize the results of the response of monolithic steel plates and steel-polyurea bilayer plates to impulsive blast loads produced in direct ballistic experiments, focusing on the deformation and failure modes of the plates. Using high-speed photography, the deformation and fracturing of some of the plates are also captured. In addition, the total force acting on the steel plate is measured as a function of time in a few cases. Chapter 5 presents the results of the finite-element modeling of the response of monolithic DH-36 steel plates and bilayer steel-polyurea plates to impulsive loads (pressure) in direct ballistic experiments presented in Chapter 4. The entire experimental setup is modeled using the finite-element code, LS-DYNA, in which a physics-based temperature- and strain rate-sensitive constitutive model for DH-36 steel, developed by Nemat-Nasser and Guo [32], and an experimentally-supported temperature-, rate-, and pressure-sensitive constitutive model for polyurea, developed and implemented by Amirkhizi et al. [27], have been implemented. The transient response of the plates under impulsive pressure loads are studied focusing on the effects of the relative position of polyurea with respect to the loading direction, the thickness of the polyurea layer, and the polyurea-steel interface bonding strength. The numerical simulations of the entire experiment support the experimentally observed results provided one employs realistic physics-based constitutive models for all constituents. In Chapter 6, the ductile fracture of monolithic DH-36 steel and bilayer steel-polyurea plates under impulsive loads is studied.
experimentally and numerically. The plates are loaded in the direct ballistic experimental setup presented and discussed in Chapter 4. Selected tested samples are analyzed using optical microscope and scanning electron microscope (SEM) to examine the microstructure of the failed samples, and also understand the fracture and necking patterns, and the underpinning mechanisms of failure. The ductile fracture observed herein involves void nucleation, growth and coalescence, causing a dimpled fracture surface. This failure mechanism is observed both by optical microscope and SEM. The examination of the microstructure of the deformed steel samples also reveals that the microstructure does not change significantly and no shear banding is observed at the fracture zone. Finite-element models are developed based on the micro-scale examinations to further study the fracturing process of the steel plates. For the numerical simulations, we have used physics-based and experimentally-supported temperature- and rate-sensitive constitutive model for steel. The finite-element models are capable of predicting the fracture process of the steel plates. Additional finite-element simulations are performed to investigate the effect of polyurea coating on the fracture resistance of the steel plates.

A stand-alone chapter completes this dissertation. This is reproduction of an archival publication [33] which has been developed while I worked on a project involving the buckling aluminum tubes under compressive loads. In this chapter we report the results of an experimental and numerical
investigation of dynamic and quasi-static compressive response of single and hex-arrayed thick aluminum tubes. The numerical simulations produce results that show excellent correlation with the results of the corresponding experiments. The investigation is aimed at characterizing the energy absorbing capability of tube-based sandwich structures.

1.4. Reference


[12] G.I. Taylor, The distortion under pressure of a diaphragm which is clamped along its edge and stressed beyond its elastic limit, Underwater Explosion Research, 3 (1950) 107-121.


Chapter Two

Experimental investigation of response of monolithic and bilayer plates to impulsive loads

Here we present the results of a series of experiments performed to assess the dynamic response of circular monolithic steel and steel-polyurea bilayer plates to impulsive loads. A convenient technique to enhance the energy absorption capability of steel plates and to improve their resistance to fracturing in dynamic events, is to spray-cast a layer of polyurea onto the plates. Since polyurea readily adheres to metallic surfaces and has a short curing time, the technique may be used to retrofit existing metallic structures to improve their blast resistance. We have examined the effectiveness of this approach, focusing on the question of the significance of the relative position of the polyurea layer with respect to the loading direction; i.e., we have explored whether the polyurea layer cast on the front face (the impulse-receiving face) or on the back face of the steel plate would provide a more effective blast mitigating composite.

The experimental results suggest that the polyurea layer can have a significant effect on the response of the steel plate to dynamic impulsive loads,
both in terms of failure mitigation and energy absorption, if it is deposited on
the back face of the plate. And, remarkably, when polyurea is placed on the
front face of the plate, it may actually enhance the destructive effect of the
blast, promoting (rather than mitigating) the failure of the steel plate,
depending on the interface bonding strength between the polyurea and steel
layers. These experimental results are supported by our computational
simulations of the entire experiment, employing realistic physics-based
constitutive models for the steel (DH-36, in the present work) and polyurea [1].

2.1. Introduction

In the past several decades, many researchers have investigated the
dynamic deformation and failure modes of plates of various geometries and
boundary conditions, under dynamic loads. Most of these studies have focused
mainly on theoretical predictions, since the corresponding experiments are
difficult to conduct. Also, for the experimental investigations, different
experimental techniques and test conditions have been used that make the
comparison of the results difficult. These experiments include: pressure waves
created by explosive devices; under-water explosion; direct impulse using
plastic sheet explosives; and direct impact by metal-foam projectiles [2, 3].

The existing experimental investigations [4, 5, 6, 7] have led the
researchers to define three failure modes (referred to as Modes I, II, and III),
for beams and plates subjected to impulsive loads. These are:
Mode I- large inelastic deformation
Mode II- tensile tearing at the edges
Mode III- shear failure at the edges.

Figure 2.1. Schematic view of the reverse ballistic experimental setup.

Recently, a new reverse ballistic technique has been developed and successfully implemented at UCSD’s Center of Excellence for Advanced Materials (CEAM). Using this method, one can apply a controlled dynamic pressure load on a plate, record its deformation and possible fracturing by high-speed photography, and hence be able to compare, both quantitatively and qualitatively, the resulting sample response under various reproducible conditions. The reverse ballistic tests on plates were performed at CEAM/UCSD’s gas gun facilities laboratory. In this study, a projectile, carrying a plate, is propelled by a gas gun at a controlled velocity toward a soft polyurethane target that rests against a 3-inch Hopkinson bar within a confining steel cylinder. Soft polyurethane with a durometer hardness of 40A
is used to load the plate. Two types of plates are studied: monolithic DH-36 steel plates and steel-polyurea bilayer plates. Each bilayer plate consists of a polyurea layer spray-cast onto a DH-36 steel plate. Figure 2.1 is a schematic view of the reverse ballistic experimental setup. The main goals of this study are:

(i) To investigate the dynamic response and fracture of monolithic steel plates under impulsive loads, with the aim of exploring the underpinning mechanisms of the deformation and the failure modes.

(ii) To demonstrate the effect of the polyurea coating on the dynamic response and fracture of steel plates under impulsive loads, focusing on the significance of the relative position of the polyurea layer with respect to the loading direction.

(iii) To use the experimental results to investigate the accuracy of the finite-element modeling and the prediction of the dynamic response and fracture of steel plates with and without polyurea coating; see [1] for numerical simulations, analysis, and the discussion of the results.

The reverse ballistic experiment is designed such that the failure of the plates occurs near their center, involving deformation localization and necking
at the central region of the plate accompanied by radial and circumferential crack propagation and possible petaling and disking.

Polyurea at room temperature is highly elastic, flexible, and resistant to abrasion, impact, and weather. This elastomer has been extensively used in the coating industry in solid form. Its growing acceptance derives in part from the ease with which this material can be spray-cast onto retrofit steel plates, and in part because of its excellent performance in otherwise normally corrosive environments. Recent studies show that applying a layer of polyurea to steel plates can vastly improve their ballistic efficiency. Previous experimental studies have shown that when polyurea is sprayed on a three-sixteenth inch steel armor, the resulting bilayer offers the same protection as a three-eighth inch armor. Mock and Blaizer [8] experimentally observed that the polyurea coating can change the response of the steel plate from full penetration of a projectile to complete mitigation of its fracture.

In this study, a reverse ballistic technique is employed to explore the effect of the polyurea coating on the dynamic response and failure of simply supported circular steel plates, focusing on the question of the significance of the relative position of the polyurea layer with respect to the loading direction, \textit{i.e.}, whether the polyurea cast onto the \textit{front face} or onto the \textit{back face} of the steel plates would provide better blast mitigating composites. In addition, the experimental results revealed that the initial kinetic energy per unit thickness
of the steel layer is a reasonable parameter to quantify the experimental results. First the experimental procedure and the material used to fabricate the samples are discussed. Then the experimental observations and results are reported and discussed.

2.2. Materials, samples, and experiments

Monolithic plates are made from DH-36 steel, a high strength structural steel used in naval applications. This steel has high toughness and high strength under various load and temperature conditions. Its mechanical properties have been systematically studied over a broad range of strain rates (from 0.001/s to about 8,000/s) and temperatures (from 77 to 1000K) by Nemat-Nasser and Guo [9] who also developed a physics-based (PB) constitutive model for the material. Since the mechanical characteristics of the DH-36 steel plates may vary depending on the rolling and other factors, we have checked the high strain rate stress-strain relations for the present case and compared the results with those reported in [9], observing that the plate used in the current study has about 10% higher flow stress compared with the one considered by Nemat-Nasser and Guo [9]. The adjustment to the model parameters was however quite minor, as is explained in [1]. This model has thus been incorporated into the finite-element code, LS-DYNA, and used to predict the experimental results.
The composite plate consists of a polyurea layer directly cast onto the steel plate. The physical properties of polyurea vary with its composition. The specific polyurea used in the present work is based on Isonate2143L [10] and Versalink P1000 [11]. A five percent excess of Isonate 2143L is used to produce a lightly cross-links polymer [12]. The glass transition temperature, \( T_g \), is below -50°C [12, 13] which is well below the typical range of the service temperature. Above the glass transition temperature, polyurea has a nearly-elastic volumetric response and a viscoelastic shear response at moderate pressures and strain rates. The viscoelastic properties of this polyurea have been systematically studied by Amirkhizi et al. [14] over a broad range of strain rates and temperatures, including the high-pressure effects. Based on this, a pressure, temperature, and strain-rate sensitive model has been developed by these authors and implemented into LS-DYNA. The model involves only a finite number of internal variables and is specifically well-suited for use in an explicit finite-element code.

Figure 2.2. Schematic view of monolithic steel plate: geometry and dimensions.
The monolithic plates are hand-machined from a 4.77mm thick DH-36 steel sheet. Each steel sample is circular with a diameter of 76mm, having a 4.77mm thick rim of 57mm inner diameter. The inner portion of the sample is about 1mm thick; see Figure 2.2. The dimensions of each plate are measured at various locations and averaged to arrive at a nominal value. The thickness of the plates varies from 0.95mm to 1.07mm. In addition, the plates are weighed before each experiment.

The polyurea layer of the bilayer plates is cast onto the inner portion of the sample and hence is about 3.77mm thick. In a typical test, the plate (monolithic or bilayer) impacts a 25.4mm thick soft polyurethane layer of 40A durometer hardness that rests against a 3-inch Hopkinson bar inside a confining steel cylinder, as shown in Figure 2.1. For the bilayer plates, the average thickness of each is also measured before and after the polyurea coating. The thickness of the polyurea layer is about 3.77mm. Figure 2.3 shows a typical un-deformed monolithic and a bilayer plate.
Two sets (denoted as set-\(I\) and set-\(II\)) of experiments are performed. In set-\(I\), the projectile that carries the sample which impacts the polyurethane target is a 7075 aluminum cylindrical tube of 76.2mm outer and 63.5mm inner diameter, and 114.5mm length. Since aluminum is a relatively soft metal, remachining is required after each experiment. To avoid this, in the second set of tests (set-\(II\)), a 6.3mm thickness, 76.2mm outer and 63.5mm inner diameter steel ring was threaded onto the projectile to protect the aluminum tube from damage; see Figure 2.4. In this case, the sample sits on the steel ring, as it is carried by the projectile at a controlled velocity towards the polyurethane target.
Figure 2.4. Projectile used in set-II experiments, comprise of a stepped 7075 aluminum tube and a threaded steel ring.

Table 2.1. Material properties of the reverse ballistic experimental components.

<table>
<thead>
<tr>
<th>Material/part</th>
<th>Density (Kg/m³)</th>
<th>Young's modulus (GPa)</th>
<th>Poisson ratio</th>
</tr>
</thead>
<tbody>
<tr>
<td>7075 aluminum/projectile</td>
<td>2770</td>
<td>70</td>
<td>0.345</td>
</tr>
<tr>
<td>Steel/output bar</td>
<td>7830</td>
<td>210</td>
<td>0.29</td>
</tr>
<tr>
<td>Steel/confinement</td>
<td>7830</td>
<td>210</td>
<td>0.29</td>
</tr>
</tbody>
</table>

Set-II experiments are subdivided into two subgroups, *set-IIA* and *set-IIIB*, the first set being loaded on their flat side whereas the second being loaded on their dish side (with or without polyurea). The projectile, carrying the steel plate, is propelled by a gas gun at a controlled velocity, which is
measured using velocity sensors placed at the end of the gas gun muzzle. Upon impact, the confined soft polyurethane loads the plate within a short interval (about several hundred microseconds). The steel plate deforms by stretching and bending that may result in localized deformation, necking, and fracture of the plate. The experiment is designed such as to simplify the modeling of its support against the cylindrical projectile as well as to ensure that the fracture and failure initiate within the central region of the steel plate. The material properties of the components used in the reverse ballistic experiments are tabulated in Table 2.1. The experimental conditions are summarized in Tables 2.2 and 2.3. The projectile velocities range from 64m/sec to 77m/sec, providing projectile kinetic energies of 1,598J to 2,231J. Figure 2.5 shows the experimental setup and its various components.

Figure 2.5. Experimental setup. The projectile carrying the steel plate is propelled by a gas gun at a controlled velocity toward the confined polyurethane target that rests against a 3-inch steel bar.
Table 2.2. Experimental condition of first set of reverse ballistic tests: set-I. (all the samples are monolithic plates)

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Test Loading direction</th>
<th>Thickness (mm)</th>
<th>Mass (g)</th>
<th>Projectile Mass (g)</th>
<th>Initial conditions Velocity (m/s)</th>
<th>Kinetic energy (J)</th>
</tr>
</thead>
<tbody>
<tr>
<td>S-8</td>
<td>Flat side</td>
<td>1.041</td>
<td>94.53</td>
<td>632.90</td>
<td>68.9</td>
<td>1727</td>
</tr>
<tr>
<td>S-9</td>
<td>Flat side</td>
<td>1.006</td>
<td>93.42</td>
<td>626.70</td>
<td>73.2</td>
<td>1929</td>
</tr>
<tr>
<td>S-10</td>
<td>Flat side</td>
<td>0.978</td>
<td>93.14</td>
<td>620.40</td>
<td>71.8</td>
<td>1841</td>
</tr>
<tr>
<td>S-11</td>
<td>Flat side</td>
<td>0.940</td>
<td>92.52</td>
<td>613.70</td>
<td>70.1</td>
<td>1733</td>
</tr>
<tr>
<td>S-12</td>
<td>Flat side</td>
<td>1.021</td>
<td>94.10</td>
<td>606.20</td>
<td>72.3</td>
<td>1830</td>
</tr>
<tr>
<td>S-13</td>
<td>Flat side</td>
<td>1.011</td>
<td>94.24</td>
<td>598.90</td>
<td>72.4</td>
<td>1817</td>
</tr>
</tbody>
</table>

Table 2.3. Experimental conditions of second set of reverse ballistic tests: set-II. (SP-41, SP-42, SP-44 and SP-36 are bilayer and the rest are monolithic plates)

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Test Loading direction</th>
<th>Thickness (mm)</th>
<th>Mass (g)</th>
<th>Projectile Mass (g)</th>
<th>Initial conditions Velocity (m/s)</th>
<th>Kinetic energy (J)</th>
</tr>
</thead>
<tbody>
<tr>
<td>S-14</td>
<td>Flat side</td>
<td>0.998</td>
<td>89.53</td>
<td>657.74</td>
<td>72.0</td>
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</tr>
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<td>90.29</td>
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</tr>
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<td>72.2</td>
<td>1990</td>
</tr>
<tr>
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<td>103.48</td>
<td>656.10</td>
<td>76.7</td>
<td>2231</td>
</tr>
<tr>
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<td>656.10</td>
<td>76.4</td>
<td>2185</td>
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<td>656.10</td>
<td>70.5</td>
<td>1849</td>
</tr>
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<td>91.57</td>
<td>656.10</td>
<td>72.3</td>
<td>1951</td>
</tr>
<tr>
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<td>656.10</td>
<td>64.9</td>
<td>1598</td>
</tr>
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<td>656.10</td>
<td>67.0</td>
<td>1687</td>
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<td>94.27</td>
<td>656.10</td>
<td>67.7</td>
<td>1719</td>
</tr>
<tr>
<td>S-51</td>
<td>Dish side</td>
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<td>95.20</td>
<td>656.10</td>
<td>67.3</td>
<td>1703</td>
</tr>
</tbody>
</table>
2.3. Results and analysis

A total of 30 tests are performed. Selected deformed plates are sectioned after the test and their final thickness and other dimensions are measured, from which the principal stretches can be calculated [1]. In addition, all the deformed plates are carefully examined for any visible signs of failure. Based on their response, the deformed plates are subdivided into three categories: No Failure, Moderate Failure, and Severe Failure. The plates in the first category did not have any cracks but multiple parallel necks were visible in the central region of some of them. The second category samples developed severe necking with crack initiation and minor petaling. And, finally, the last category samples had radial and circumferential cracks with petaling and possibly disking or edge tearing. A typical sample of each failure category is shown in Figure 2.6.

![Figure 2.6. Side view (right column) and top view (left column) of selected sample for each categories of deformed steel plates: severely failed, moderately failed, and not failed.](image)

2.3.1. Effect of plate thickness

The strength of the specimen increases if its thickness is increased. The membrane stiffness varies linearly with the plate thickness while the bending
stiffness relates linearly to the square of the thickness. At low velocities, the deflection is small and hence the bending is dominant, while the stretching effect becomes significant at high impact velocities which produce large tensile deformation. In large deformations, the plastically dissipated work in the membrane deformation is much larger than that in the bending deformation. Therefore, the energy absorbed by the specimen’s plastic deformation has a linear relation with the thickness at large deformations. This means that for the same impact velocity, as the thickness of the specimen increases, the kinetic energy that is converted into the plastic work increases linearly with the thickness. Therefore, kinetic energy per unit thickness of the steel layer may be used as a parameter to quantify the dynamic performance efficiency of this structure. It is experimentally verified that this parameter can be used to predict the failure onset of the plates. The experimental results suggest that when the impact kinetic energy per unit thickness is greater than an experimentally-obtained critical value, the sample fails. This critical value depends on the experimental conditions.

2.3.2. Deformation and failure mechanisms

In the monolithic steel-plate experiments, fracture seems to begin with the onset of necking. Deformation then localizes, giving rise to large local plastic strains within the neck region where cracks initiate and grow circumferentially near the center, and/or radially towards the edge of the plate,
the actual crack path being affected by the imperfections and other statistical parameters. Multiple parallel necking patterns can be visually detected in the central region of some of a number of the deformed samples.

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Test condition</th>
<th>Result</th>
<th>Failure</th>
</tr>
</thead>
<tbody>
<tr>
<td>S-8</td>
<td>Flat side</td>
<td>16586</td>
<td>35.0</td>
</tr>
<tr>
<td>S-9</td>
<td>Flat side</td>
<td>19178</td>
<td>36.8</td>
</tr>
<tr>
<td>S-10</td>
<td>Flat side</td>
<td>18822</td>
<td>36.5</td>
</tr>
<tr>
<td>S-11</td>
<td>Flat side</td>
<td>18439</td>
<td>32.8</td>
</tr>
<tr>
<td>S-12</td>
<td>Flat side</td>
<td>17927</td>
<td>36.0</td>
</tr>
<tr>
<td>S-13</td>
<td>Flat side</td>
<td>17969</td>
<td>35.5</td>
</tr>
</tbody>
</table>

2.3.3. Experimental results

The results of the two sets of experiments are summarized in Tables 2.4 and 2.5 and further discussed below. The main difference between the two test sets is the amount of energy that is dissipated through the rotation of the steel rim.

Set-I

Only monolithic plates are used in this set of experiments. The kinetic energy per unit thickness varies from 16,586J/cm to 20,423J/cm. The average rotation of the edge for this set of experiments is about 35 degrees. The two samples with kinetic energy per unit thickness of less than 17,950J/cm did not fail. On the other hand, the plate with kinetic energy per unit thickness of 20,423J/cm (S-10) experienced severe failure.

Set-II
This set includes 24 tests that are divided into two groups: those loaded on the flat side (referred to as set-IIA) and those loaded on the dish side (referred to as set-IIB) of the steel plate, as indicated in the third column of Table 2.5. Four samples are with polyurea coating, three of which are loaded on the flat side and one on the dish side.

Table 2.5. Summary of second set of reverse ballistic (set-II) experiments.

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Test condition</th>
<th>Result</th>
<th>Failure</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Loading direction</td>
<td>Kinetic energy per unit thickness (J/cm)</td>
<td>edge rotation (Deg.)</td>
</tr>
<tr>
<td>S-14</td>
<td>Flat side</td>
<td>19408</td>
<td>47.8</td>
</tr>
<tr>
<td>S-15</td>
<td>Flat side</td>
<td>19137</td>
<td>51.8</td>
</tr>
<tr>
<td>S-16</td>
<td>Flat side</td>
<td>19096</td>
<td>48.3</td>
</tr>
<tr>
<td>S-17</td>
<td>Flat side</td>
<td>19838</td>
<td>50.3</td>
</tr>
<tr>
<td>S-18</td>
<td>Flat side</td>
<td>19180</td>
<td>56.0</td>
</tr>
<tr>
<td>S-19</td>
<td>Flat side</td>
<td>19302</td>
<td>55.8</td>
</tr>
<tr>
<td>S-20</td>
<td>Flat side</td>
<td>20885</td>
<td>55.8</td>
</tr>
<tr>
<td>S-21</td>
<td>Flat side</td>
<td>20701</td>
<td>53.5</td>
</tr>
<tr>
<td>S-22</td>
<td>Flat side</td>
<td>20606</td>
<td>45.9</td>
</tr>
<tr>
<td>S-25</td>
<td>Flat side</td>
<td>20357</td>
<td>54.9</td>
</tr>
<tr>
<td>S-26</td>
<td>Flat side</td>
<td>19253</td>
<td>57.9</td>
</tr>
<tr>
<td>S-27</td>
<td>Flat side</td>
<td>19856</td>
<td>58.8</td>
</tr>
<tr>
<td>S-29</td>
<td>Flat side</td>
<td>21501</td>
<td>60.3</td>
</tr>
<tr>
<td>S-32</td>
<td>Flat side</td>
<td>21014</td>
<td>59.3</td>
</tr>
<tr>
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<td>Flat side</td>
<td>20873</td>
<td>57.3</td>
</tr>
<tr>
<td>SP-42</td>
<td>Flat side</td>
<td>18705</td>
<td>57.3</td>
</tr>
<tr>
<td>SP-44</td>
<td>Flat side</td>
<td>21031</td>
<td>66.3</td>
</tr>
<tr>
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<td>Flat side</td>
<td>20441</td>
<td>70.8</td>
</tr>
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<td>S-28</td>
<td>Dish side</td>
<td>19542</td>
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</tr>
<tr>
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<td>Dish side</td>
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<td>28.0</td>
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<td>Dish side</td>
<td>16701</td>
<td>18.3</td>
</tr>
<tr>
<td>S-51</td>
<td>Dish side</td>
<td>15995</td>
<td>28.0</td>
</tr>
</tbody>
</table>

The kinetic energy per unit thickness of the set-IIA tests varies from 18,704J/cm to 21,501J/cm. All monolithic samples failed when the kinetic energy per unit thickness exceeded 19,400J/cm whereas two bilayer samples
did not fail for kinetic energy per unit thickness of 18,704J/cm (SP-42) and 21,031J/cm (SP-44) respectively, when the polyurea coating was on the face opposite to the impact surface, indicating the effective role of polyurea backing on enhancing the energy absorption of the steel plate. However, the third bilayer plate (SP-41) severely necked without any petaling at a kinetic energy per unit thickness of 20,873J/cm, showing the onset of failure. The overall higher critical kinetic energy per unit thickness of the steel plate of this set compared to the first set of experiments is due to the extra energy dissipated in the plastic deformation of the rim. The average rim rotation of the samples in this set is about 56 degrees, 75% higher than the corresponding average value in set-I. On the other hand, the kinetic energy per unit thickness of set-IIB test samples that are loaded on the dish side varies from 15,995J/cm to 19,922J/cm, significantly lower than the typical corresponding values of set-IIA. This difference in the critical kinetic energy per unit thickness is considered to be due to the rim rotation of these samples, which is remarkably lower (approximately 3 times) than that of the set-IIA samples, being caused by the difference in the applied torque. The results of the set-IIB experiments reveal that the presence of polyurea coating in front of the steel plate cannot mitigate the failure. Sample SP-36 with polyurea coating on the front side failed severely at a kinetic energy per unit thickness of 16,122J/cm, although its rim rotated significantly more than the monolithic samples in set-IIB.
Figure 2.7. Deformed steel plate samples loaded on their flat side at approximately the same kinetic energy per unit thickness: (a) SP-44: bilayer plate loaded on steel side; and (b) S-51: monolithic steel plate.

Figure 2.8. Deformed steel plate sample loaded on the dish side at approximately the same kinetic energy per unit thickness: (a) SP-36: bilayer plate loaded on the polyurea side and; (b) S-51: monolithic steel plate.
2.3.4. Discussion

The experimental results show the significance of the relative position of the polyurea layer with respect to the loading direction. This may be explained by considering the initial shock effect. When polyurea is cast on the impact side, it is loaded in compression. Experimental study at CEAM/UCSD [14] has shown that polyurea is a highly pressure sensitive elastomer, with its stiffness increasing remarkably with increasing pressure. When the confined polyurea is loaded in compression, its stiffness can increase 10 to 20 fold, thereby attaining a better impedance match with the steel plate. Consequently, more energy is transferred to the plate. On the other hand, when polyurea is cast onto the opposite side of the impact (back side), the soft polyurethane loads the steel plate first and then a part of this energy is transferred to the polyurea, compressing it and thereby increasing its stiffness, and hence the amount of the energy that it captures and damps because of its viscoelasticity. Figure 2.7 compares the response of two plates that are deformed under essentially the same kinetic energy per unit thickness (of approximately 21KJ/cm) loaded on the flat side (set-IIA), one without polyurea and the other with polyurea on its back side. As the figure suggests, the monolithic steel plate failed severely whereas no visible sign of failure and necking were detected in the polyurea-backed steel plate. Figure 2.8 compares two selected plates with the same kinetic energy per unit thickness (approximately 16KJ/cm) loaded on the dish side (set-IIB), one without polyurea coating and the other with polyurea coating on the front side. The steel plate with polyurea
failed severely despite having 27.3% more rim rotation compared to the monolithic steel plate.

In addition to the significant shock effect, the polyurea layer has a secondary effect. Provided that the plate does not fail during the initial shock loading, the presence of a polyurea layer, either on the front or on the back face of the steel plate, increases the effective tangent modulus of the plate. Therefore, if the steel plate in a bilayer system does not fail and the polyurea does not detach during the initial shock loading, then the presence of polyurea tends to delay the onset of the necking instability, as has been pointed out by Hutchinson [15]. However, Hutchinson's analysis is mainly under quasi-static conditions, ignoring the influence of the initial shock effect and using a simple constitutive model and geometry.

2.4. Summary and conclusions

In this chapter, the dynamic behavior of circular, monolithic steel and bilayer steel-polyurea plates is investigated experimentally using a reverse ballistic experimental setup. This setup allows the application of an impulsive pressure onto a sample, producing severe plastic deformation of the steel plate by extensive stretching and possible fracturing and petaling near the central region of the plate.
Since the membrane stiffness of a thin plate varies linearly with its thickness while its bending stiffness relates linearly to the square of its thickness, the energy absorbed by the specimen’s plastic deformation has an essentially linear relation with its thickness under high-velocity impact that produces large stretching deformations. The experiments show that the samples fail when the kinetic energy per unit thickness of the steel plate exceeds an experimentally established critical value which may vary as the experimental conditions are changed.

In the monolithic steel-plate experiments, fracture seems to begin with the onset of necking. Deformation then localizes, giving rise to large local plastic strains within the neck region where cracks initiate and grow circumferentially near the center, and/or radially towards the edge of the plate, the actual crack path being affected by the imperfections and other parameters. Multiple parallel necking patterns can be visually detected in the central region of some of the deformed samples.

When a polyurea layer is cast onto the impact side, its presence may promote failure during the initial shock effect. Under pressure, the stiffness of the polyurea layer increases substantially, attaining a better impedance match with the steel and thereby increasing the energy that is transferred to the plate. On the other hand, when polyurea is cast onto the opposite to the impact side (back face), the initial shock loads the steel plate first and then a part of the
shock is captured and dissipated by the polyurea layer. Polyurea can also increase the effective tangent modulus of the bilayer and thus delay the onset of the necking instability. Our experimental results are supported by systematic computational simulations of the entire experiment, employing realistic physics-based constitutive models for the steel (DH-36, in the present work) and polyurea, the results of which are reported in a separate article [1].

2.5. Acknowledgments

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2.6. References


Chapter Three

Numerical modeling of response of monolithic and bilayer plates to impulsive loads

In this chapter, we present and discuss the results of our numerical simulation of the dynamic response and failure modes of circular DH-36 steel plates and DH-36 steel-polyurea bilayers, subjected to impulsive loads in reverse ballistic experiments. In our previous article, we reported the procedure and results of these experiments [1]. For the numerical simulations, we have used physics-based and experimentally-supported temperature- and rate-sensitive constitutive models for steel and polyurea, including in the latter case the pressure effects. Comparing the simulation and the experimental results, we focus on identifying the potential underpinning mechanisms that control the deformation and failure modes of both monolithic steel and steel-polyurea bilayer plates.

The numerical simulations reveal that the bilayer plate has a superior performance over the monolithic plate if the polyurea layer is cast on its back.
The presence of the polyurea layer onto the front face (blast-receiving side) amplifies the initial shock loading and thereby enhances the destructive effect of the blast, promoting (rather than mitigating) the failure of the steel plate. In addition, the interface bonding strength between polyurea and steel is examined numerically and it is observed that the interface bonding strength has a significant effect on the performance of the steel-polyurea bilayer plates. The numerical simulations support the experimentally observed facts provided the entire experiment is simulated, employing realistic physics-based constitutive models for all constituents.

### 3.1. Introduction

The response of plates under distributed dynamic impulsive loads has been numerically and analytically studied by a number of researchers. A review of the relevant literature is given by Nurick and Martin [2]. A momentum conservation approach, eigenvalue expansion methods, and wave form approaches are among the various analytical-numerical methods previously used to investigate the response of plates under dynamic loads [3,4,5]. In a recent research conducted by Balden and Nurick [6], full scale numerical simulations of the post-failure motion of steel plates subjected to blast loads have been conducted using the finite-element code ABAQUS. Another investigation of this kind has been conducted by Lee and Wierzbicki [4, 5] in which they examine the fracture of thin plates under localized
impulsive loads and the possible dishing, diskong, and petaling of the plates. Finite-element calculations of Xue and Hutchinson [7] seek to explore the ability of the bilayer plates to sustain intense impulses and assess the results by comparing to the estimated performance of monolithic steel plates of the same total mass, using the finite strain version of the ABAQUS Explicit code. The significance of these full-scale numerical simulation codes is that they allow first, to study the transient response of the plate, second, to incorporate complex temperature-, pressure-, and rate- dependent constitutive models in the finite-element code, and finally, to conduct parametric studies to gain insight into the dynamic deformation mechanisms.

The objective of this paper is to numerically simulate the results of our experiments, presented in a recent paper [1]. To this end, we use the explicit version of the commercially available finite-element code, LS-DYNA, within which physics-based constitutive models of the involved materials, DH-36 steel and polyurea are incorporated. A comprehensive mechanical testing program is used to characterize and model these materials. A temperature and strain-rate sensitive constitutive model, developed by Nemat-Nasser and Guo [7] on the basis of the kinetics and kinematics of dislocation motion, is employed and implemented into LS-DYNA through a user-defined FORTRAN subroutine for the steel plates. Moreover, an experimentally-based viscoelastic, rate-, pressure-, and temperature-sensitive constitutive model, developed by Amirkhizi et al. [8] and implemented into LS-DYNA by Amirkhizi, is used for
modeling the polyurea. After validating the numerical model, the model is employed to investigate various aspects of the effects of polyurea coating on the response of the steel plates. The main focus of the calculations is to understand the effects of (1) the relative position of the polyurea layer with respect to the loading direction (i.e., either on the back or the front face), (2) the polyurea-steel interface bonding strength, and (3) the polyurea layer thickness relative to that of the steel layer.

### 3.2. Numerical simulation procedures

In this section, the reverse ballistic experimental setup is briefly explained and the finite-element model is detailed. The material constitutive models used for steel, polyurea, and polyurethane are discussed, and finally, the details of the deformed plate’s measurement method are presented.

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Plate type</th>
<th>Thickness (mm)</th>
<th>Mass (gr)</th>
<th>Projectile Mass (gr)</th>
<th>Initial conditions Velocity (m/s)</th>
<th>Kinetic energy (J)</th>
</tr>
</thead>
<tbody>
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</tr>
<tr>
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<td>107.1</td>
<td>656</td>
<td>76.83</td>
<td>2252</td>
</tr>
</tbody>
</table>

### 3.2.1. Experimental setup

In a reverse ballistic experimental setup, developed at CEAM/UCSD’s gas gun facilities, a projectile carrying a plate is propelled by a gas gun at a
controlled velocity toward a soft polyurethane target resting against a 3-inch Hopkinson bar within a confining thick-walled steel cylinder. Upon impact, the soft polyurethane loads the steel plate impulsively, resulting in large deformations of the plate [1].

Figure 3.1. Finite-element model of reverse ballistic experiment, showing: the aluminum projectile that carries the steel plate at velocity $V$, impacting the polyurethane target that rests on a Hopkinson bar (top: side view; bottom: sectioned angled view).

3.2.2 Numerical model

Initial and boundary conditions

Two views of the model used in the numerical simulation are shown in Figure 3.1. A 3-dimensional simulation is initiated when an aluminum
projectile carrying the steel plate sample at a uniform initial velocity, \( V \), is brought into frictionless contact with the cylindrical polyurethane target resting against a 3-inch Hopkinson bar. The projectile mass and the mass and the thickness of the steel plates are given in Table 3.1. All steel plates have a diameter \( D_{st} = 76.00 \text{mm} \), rim thickness of \( t_{rim} = 4.79 \text{mm} \), and rim width of \( w_{rim} = 9.52 \text{mm} \); see Figure 3.2.

![Figure 3.2. Angled view of the specially discretized monolithic plate, with outer diameter \( D_{st} = 76.00 \text{mm} \), rim thickness of \( t_{rim} = 4.79 \text{mm} \), and rim width of \( w_{rim} = 9.52 \text{mm} \).](image)

The simulation uses the contact algorithm option of LS-DYNA, entitled CONTACT_AUTOMATIC_SURFACE_TO_SURFACE to model the steel plate-polyurethane interaction. The same contact algorithm is employed to model the aluminum projectile-steel plate interaction. To model the confinement of the polyurethane target (with a Poisson ratio, \( \nu \), of about 0.495) under pressure, the side nodes of the polyurethane that are in contact with the confining cylinder are restricted to allow sliding only in the longitudinal direction. In addition, the polyurethane nodes in contact with the
supporting Hopkinson bar are fixed by requiring that the radial, tangential, and longitudinal displacement components be zero, i.e., \( u_r = u_t = u_z = 0 \). There are no other initial or boundary conditions imposed in the model.

Figure 3.3. Side and angled view of spatially discretized plates: (a) monolithic steel plate (left), (b) bilayer plate with polyurea on the dish side (middle), and (c) bilayer plate with polyurea on the flat side.

**Spatial discretization and element formulation**

The plate is modeled using eight-node brick elements with one integration point whereas the rim (outer part of the plate) is modeled using the fully integrated quadratic eight-node brick elements with nodal rotation to avoid the undesired hourglass energy. There are 56 elements along the diameter and three elements per millimeter through the thickness of the plate. Figure 3.3 presents the spatial discretization of the three plate configurations: the monolithic steel plate (left), the bilayer plate with polyurea on the dish side (middle), and the bilayer plate with polyurea on the flat side (right). The 7075 aluminum projectile that carries the plate is modeled by fully integrated selectively-reduced eight-node brick elements. The Flanagan-Belytschko
integration hourglass-control algorithm is used to damp out the overall zero-energy modes.

![Figure 3.4. Typical polyurethane edge-element distortion produced by a Lagrangian simulation. Half of the model is masked to obtain a better view of the element distortions.](image)

**Lagrangian and Arbitrary Lagrangian-Eulerian (ALE) simulations**

Both a Lagrangian and an Arbitrary Lagrangian-Eulerian (ALE) model are used. An eight-node brick element with one integration point is employed in the Lagrangian formulation of the polyurethane, whereas a one-point ALE multi-material brick element is used in the ALE formulation. The ALE formulation has proven to be more stable numerically and to predict the experimental results more accurately. It does not introduce edge-element distortion in the highly deformed polyurethane, as occurs when using the Lagrangian formulation. Figure 3.4 shows a typical polyurethane element
distortion obtained by the Lagrangian calculations. The detailed comparison of
the results of these two numerical formulations is discussed in the next section.

\textit{Material constitutive models}

The physics-based model, proposed by Nemat-Nasser and Guo \cite{8} for
DH-36 steel, expresses the flow stress (effective stress), $\tau$, as a function of
temperature, $T$, effective plastic strain, $\gamma$, and effective plastic strain rate, $\dot{\gamma}$,
as

$$
\tau = c_0 + c_1 \gamma^n + \tau_0 \left\{ 1 - \left[ -\frac{kT}{G_0} \left( \ln \frac{\dot{\gamma}}{\dot{\gamma}_0} + \ln f(\gamma, T) \right) \right] \right\}^{1\over p} f(\gamma, T), \text{ for } T \leq T_c,
$$

$$
f(\gamma, T) = 1 + a \left[ 1 - \left( \frac{T}{T_m} \right)^2 \right] \gamma^m, \quad T_c = \frac{-G_0}{k} \left( \ln \frac{\dot{\gamma}}{\dot{\gamma}_0} + \ln f(\gamma, T) \right)^{-1},
$$

and

$$
\tau = c_0 + c_1 \gamma^n, \text{ for } T \geq T_c,
$$

where, $G_0$ is the total energy of the short-range barrier to the motion of
dislocations, measured per atom, $k$ is the Boltzmann constant, $\dot{\gamma}_0$ is a
reference strain rate, $T_m$ is the melting temperature, and $c_0$, $c_1$, $\tau_0$, $n$, $m$, $p$, $q$, and $a$ are material parameters. The details of the parameters used in this model
are reported by Nemat-Nasser and Guo \cite{8}. Here, the parameter $c_0$ is modified
to account for the 10\% higher yield stress of our DH-36 steel as compared to
the one used in [8]. The final constitutive relation (denoted as the PB-model, for physics-based) for our material is: for $T \leq T_c$,

$$\tau = 70 + 750\gamma^{0.25} + 1500 \left[ 1 - \left(-6.6 \times 10^{-5} T \left( \ln \frac{\dot{\gamma}}{2 \times 10^{10}} \right) \right)^{\frac{1}{3}} \right],$$

(3)

where the stress is in MPa and $T$ is in degrees Kelvin. On the other hand, for $T \geq T_c$, we use

$$\tau = 70 + 750\gamma^{0.25},$$

(4)

where $T_c = \left[-6.6 \times 10^{-5} \left( \ln \frac{\dot{\gamma}}{2 \times 10^{10}} \right)\right]^{-1}$.

Figure 3.5 compares three experimentally obtained stress-strain curves with their corresponding PB-model predictions at the indicated strain rates. As is seen, excellent correlation between the experimental data and the model predictions is obtained.
Figure 3.5. Comparison of the PB-model predictions with the experimental results at indicated strain rates and initial room temperature.

An experimentally-based viscoelastic constitutive model, including pressure and temperature sensitivity, introduced and implemented into LS-DYNA by Amirkhizi et al. [9], has been employed for modeling the polyurea. For bulk deformations, this model assumes the trace of the Cauchy stress tensor, $\sigma$, which is given by

$$tr(\sigma) = 3k \frac{\ln J}{J},$$

(5)

where $J = \det F$ is the Jacobian of the deformation, and $k$ is a temperature-dependent bulk modulus, assumed to be given by,

$$k(T) = k(T_{ref}) + m(T - T_{ref}),$$

(6)
with \( T_{\text{ref}} \) being the reference (room) temperature. A hereditary integral is employed to express the deviatoric part of the stress,

\[
\sigma'(t) = \int_0^t \frac{T(\tau)}{T_{\text{ref}}} 2G_{\text{ref}}(\xi(t) - \xi(\tau))D'(\tau) \, d\tau ,
\]

where \( D' \) is the deviatoric part of the deformation rate tensor, and \( \xi \) is a pressure sensitive reduced time, related to the actual time, \( t \), through the following equation:

\[
\xi(t) = \int_0^t \frac{d\tau}{a_\tau(\tau)} ,
\]

\[
a_\tau = 10^{\frac{A(\tau-C_{tp}P-T_{\text{ref}})}{B+C_{tp}P-T_{\text{ref}}}} .
\]

Here, \( P \) is the pressure and \( C_{tp} \) is a time-pressure coefficient.

In this study the four-term \((n = 4)\) Prony series option of this code is used to define the relaxation function,

\[
G_{\text{ref}}(t) = G_\infty \left( 1 + \sum_{i=1}^{n} p_i e^{-t/q_i} \right) .
\]

The local temperature is calculated using

\[
\frac{\partial T}{\partial t} = \frac{1}{C_v} \frac{\partial W_d}{\partial t} ,
\]

where \( C_v \) is the heat capacity at constant volume, and \( W_d \) is the dissipated work, both measured per unit original volume. The right-hand side of this equation is expressed as,
\[
\frac{\partial W_d}{\partial t} = G_{\infty} \frac{T(t)}{T_{ref}} \sum_{i=0}^{n} \frac{p_i}{q_i} \varepsilon_d^i(t) : \varepsilon_d^i(t),
\]

where

\[
\varepsilon_d^i(t) = \int_{0}^{t} e^{-(\xi(t') - \xi(t))/q_i} D'(\tau) d\tau.
\]

The numerical values of the parameters used in this model are listed in Table 3.2.

<table>
<thead>
<tr>
<th>$T_{ref}$(K)</th>
<th>A</th>
<th>B(K)</th>
<th>$C_0$(K/GPa)</th>
<th>$C_d$(J/mm²/K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>273</td>
<td>-10</td>
<td>107.54</td>
<td>7.2</td>
<td>1.977×10⁻³</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>$\text{CTE}(/K)$</th>
<th>$m$(GPa/K)</th>
<th>n</th>
<th>$k_{ref}$(GPa)</th>
<th>$G_-(GPa)$</th>
</tr>
</thead>
<tbody>
<tr>
<td>2×10⁻⁴</td>
<td>-0.015</td>
<td>4</td>
<td>4.948</td>
<td>0.0224</td>
</tr>
</tbody>
</table>

$p_1$  $p_2$  $p_3$  $p_4$
0.8458  1.686  3.594  4.342
$q_1$(ms)  $q_2$(ms)  $q_3$(ms)  $q_4$(ms)
463.4  0.06407  1.163×10⁻⁴  7.321×10⁻⁷

The polyurethane is modeled using a Mooney-Rivlin rubber constitutive model with shear modulus $G = 16$ MPa and Poisson's ratio $\nu = 0.495$, the density being $\rho = 1.19$ g/cc; for more details, see LS-DYNA theoretical manual [10].
3.2.3. Measurement of the deformed plates

The variations along the radial line of the three principal stretches are measured for selected plates, and are used to compare with the corresponding model results. In order to obtain these quantities, the deformed plates are sectioned along their diametral plane using an Accutom cutting machine. The sectioned faces of the plates are ground and polished first, and then finish-ground by a 1,200 grit paper. The plates are then scanned (left part of Figure 3.6). The Auto color and gradient filter options of Photoshop are then invoked to make a clear distinction between the plate and the background (right part of Figure 3.6).

![Figure 3.6. Scanned view of a typical plate, sectioned along the diametral plane before (left) and after (right) applying the gradient filtering.](image)

The upper and lower faces of the deformed cross section are identified and marked at a finite number of points (~50 points on each face), and the coordinates of these points are imported into MATLAB. The profile
measurements are restricted to the gauge section of the steel plates, excluding the rim. These points are mapped into their corresponding position in the undeformed plate, using the following procedure. The points selected on the two faces of the deformed plate are used to divide the cross section into a number of trapezoids. Each trapezoid is the projection, on the cutting plane, of a ring, the collection of which constitutes the entire flat part of the steel plate. The volume of each ring is calculated. Since the plastic deformation of steel can be considered nearly isochoric, the volume of the ring is the same in the deformed and undeformed states. Using this fact and the known constant thickness of the steel in the undeformed state, one can calculate the length of the central ring in the radial direction in the undeformed state. By repeating this calculation and marching towards the rim of the plate and given the volume of each ring, its constant thickness, and its distance from the center, the radial length of each element and the location of the next further element can be obtained; see Figure 3.7.

Figure 3.7. (Left) A ring segment of the plate along with the corresponding projected trapezoidal elements on the cutting plane. The dots indicate the inner and outer faces of the plate digitized from the scanned image of the cross section and imported into MATLAB. (Right) A trapezoidal element in the deformed configuration (top) along with its mapped un-deformed rectangular configuration (bottom) (Courtesy of Jeff Simon).
This mapping enables us to obtain the deformation gradient tensor, $\mathbf{F}$, for each element using,

$$
\mathbf{F} = \begin{bmatrix}
\frac{\partial r}{\partial R} & 0 & \frac{\partial r}{\partial Z} \\
0 & \frac{r}{R} & 0 \\
\frac{\partial z}{\partial R} & 0 & \frac{\partial z}{\partial Z}
\end{bmatrix},
$$

(14)

where $r$ and $z$ are radial and axial coordinates of a point in a deformed state, in cylindrical coordinates presented in Fig 1, and $R$ and $Z$ are the radial and axial coordinates of the same point in the un-deformed state. Next, the Biot tensor, $\mathbf{B}$, is calculated,

$$
\mathbf{B}^{-1} = (\mathbf{F}^{-1})^T \cdot \mathbf{F}^{-1}
$$

(15)

and then the left-stretch tensor, $\mathbf{V}$, is obtained,

$$
\mathbf{V} = (\mathbf{B})^{1/2}.
$$

(16)

![Figure 3.8. Three principal stretches and thickness profiles of sample S-18: experimental results.](image)
The principal stretches of each element can now be calculated by obtaining the eigenvalues of the corresponding left-stretch tensor. The principal stretches and thickness profiles of these analyzed plates are then plotted as functions of the distance from the center of the plate in the undeformed state. Figure 3.8 shows the variation along the radial line of the three principal stretches and thickness for specimen S-18. The same computational procedure is applied to the numerical model of the deformed plates and the final principal stretches and thickness profiles are calculated.

<table>
<thead>
<tr>
<th>Test</th>
<th>Polyurea</th>
<th>Shear failure strength of interface bond (MPa)</th>
<th>Steel thickness (mm)</th>
<th>Polyurea thickness (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>M1</td>
<td>N/A</td>
<td>N/A</td>
<td>1.02</td>
<td>N/A</td>
</tr>
<tr>
<td>B1</td>
<td>back face</td>
<td>$\infty$</td>
<td>1.02</td>
<td>3.78</td>
</tr>
<tr>
<td>B2</td>
<td>front face</td>
<td>$\infty$</td>
<td>1.02</td>
<td>3.78</td>
</tr>
<tr>
<td>B3</td>
<td>back face</td>
<td>140</td>
<td>1.02</td>
<td>3.78</td>
</tr>
<tr>
<td>B4</td>
<td>back face</td>
<td>100</td>
<td>1.02</td>
<td>3.78</td>
</tr>
<tr>
<td>B5</td>
<td>front face</td>
<td>140</td>
<td>1.02</td>
<td>3.78</td>
</tr>
<tr>
<td>B6</td>
<td>front face</td>
<td>100</td>
<td>1.02</td>
<td>3.78</td>
</tr>
</tbody>
</table>

### 3.3. Numerical simulation results

In this section, we compare the three measured principal stretches and thickness profiles of a selected set of tested monolayer and bilayer samples with their corresponding finite-element model predictions. We also discuss the effect of the polyurea-steel interface bonding strength on the dynamic response.
of the bilayer plates. The details of the finite-element model parameters are tabulated in Tables 3.1 and 3.3.

![Figure 3.9. Comparison of the experimental and numerically-predicted results (ALE formulation) of monolithic plate S-18: (a) principal stretches (left), and (b) thickness (right).](image)

![Figure 3.10. Comparison of the experimental and numerically-predicted results (Lagrangian formulation) of monolithic plate S-18: (a) principal stretches (left), and (b) thickness (right) as functions of the initial distance from the center of the plates.](image)

### 3.3.1. Monolithic steel plates

The parameters used for the finite-element simulation of experiment S-18 are given in Table 3.1. Figures 3.9 and 3.10 show the finite-element predictions of the three principal stretches and thickness profiles obtained using the ALE and Lagrangian calculations, and compare these with the
corresponding experimental results. As shown, the Lagrangian calculation over-estimates the thickness profile, due to the excess hourglass energy caused by edge-element distortion in the highly deformed polyurethane; see Figure 3.4. A sequence of thickness profiles shown in Figure 3.11 reveals that most of the thinning occurs over the time interval from 75 to 150µs.

![Graph showing numerical prediction of thickness variations of sample S-18](image)

**Figure 3.11.** Numerical prediction of the thickness variations of sample S-18 as functions of the initial distance from the origin at different indicated instances.

Comparison of the steel plate's mid-span deflection history, obtained by Lagrangian and ALE models, is presented in Figure 3.12. It is clear that some elastic vibrations occur after the peak deflection has been attained, and that this
peak deflection is approximately equal to the final permanent mid-span deflection. The ALE formulation predicts larger mid-span deflection as compared to that of the Lagrangian formulation. This observation is consistent with the stretching profiles of the two formulations presented in Figures 3.9 and 3.10. Figure 3.13 shows a sequence of deformed configurations of the S-18 sample at 40µs time intervals, obtained using the ALE formulation. The numerical model reveals that the deformation initiates at the rim while the center part is still un-deformed, and the deformation then proceeds toward the center until the maximum mid-span deflection is attained and the plate is unloaded which then is followed by the elastic vibrations.

Figure 3.12. Monolithic plate S-18: Comparison of Lagrangian and ALE predictions of the mid-span deflection history.
Figure 3.13. Sequence of photos of the side view of the deflection of S-18 monolithic steel plate at 40ms time intervals; note that a quarter of the model is masked to provide a better view of the thickness profile.

### 3.3.2. Steel-polyurea bilayer plates

Unless otherwise stated, all calculations reported hereafter are performed using the ALE formulation due to its superior stability and accuracy. The experimentally-observed partial de-bonding of the polyurea layer is simulated through incorporating a shear failure-strength for the polyurea-steel interface bonding. Since no experimental data is available for the interface bonding strength, a trial and error method is employed to reproduce the experimentally observed partial de-bonding by adjusting the value of the interface bonding strength. For this, it is assumed that interface debonding is governed by the following simple shear failure model:

\[
\frac{\tau}{S_{FLS}} \leq 1.0,
\]

where \( \tau \) is the shear stress at the interface and \( S_{FLS} \) is the interface shearing strength. In the simulations, the \( S_{FLS} \) is assumed to be in the range of 100 to
140MPa. In addition, an upper limiting bound is also obtained by assuming a perfect interface bonding \( (SFLS = \infty) \). Two bilayer experiments are selected and modeled: SP-42 and SP-44. The data for these cases are given in Table 3.1. The comparison of the experimentally and numerically obtained principal stretches and thickness profiles of SP-42 for several assumed bonding strengths are presented in Figures 3.14, 3.15, 3.16 and 3.17.

![Figure 3.14. Comparison of the experimental and numerically-predicted results (SP-42): (a) principal stretches (left), and (b) thickness (right) as functions of the initial distance from the center of the plate for SFLS= 100MPa.](image1)

![Figure 3.15. Comparison of the experimental and numerically-predicted results (SP-42): (a) principal stretches (left), and (b) thickness (right) as functions of the initial distance from the center of the plate for SFLS= 120MPa.](image2)
Figure 3.16. Comparison of the experimental and numerically-predicted results (SP-42): (a) principal stretches (left), and (b) thickness (right) as functions of the initial distance from the center of the plate for SFLS = 140MPa.

Figure 3.17. Comparison of the experimental and numerically-predicted results (SP-42): (a) principal stretches (left), and (b) thickness (right) as functions of the initial distance from the center of the plate for SFLS = \infty.

The finite-element model using a SFLS of 140MPa for the polyurea-steel bonding strength, predicts a partial de-bonding whereas the strength of 100 and 120MPa result in a complete de-bonding. All the simulations suggest that the de-bonding initiates from the edge and develops circumferentially toward the center of the plate, as is also experimentally observed. Figures 3.18 and 3.19 show the predicted deformation sequence of SP-42 for the shear-failure
strengths of 100 and 140MPa which suggest a complete and a partial de-bonding, respectively.

Figure 3.18. Sequence of photos of the side view of the deflection of SP-42 bilayer plate at 40ms time intervals with SFLS= 100MPa; note that a quarter of the model is masked to obtain a better view of the profile.
Figure 3.19. Sequence of photos of the side view of the deflection of SP-42 bilayer plate at 40ms time intervals with SFLS= 140MPa; note that a quarter of the model is masked to obtain a better view of the profile.

The same trend is observed in the finite-element model predictions for the SP-44 sample, as shown in Figures 3.20, 3.21 and 3.22. These results clearly indicate that the stretches and thickness profiles are significantly affected by the steel-polyurea bonding strength. Among all models with different interface
shear failure strengths, the ones with strength of 100MPa show the best correlation with the experimental measurements. The same applies to both SP-42 and SP-44 bilayer plates.

Figure 3.20. Comparison of the experimental and numerically-predicted results (SP-44): (a) principal stretches (left) and (b) thickness (right) as functions of the distance from the center of the plate for SFLS= 140MPa.

Figure 3.21. Comparison of the experimental and numerically-predicted results (SP-44): (a) principal stretches (left) and (b) thickness (right) as functions of the distance from the center of the plate for SFLS= 120MPa.
3.3.3. Effect of relative position of polyurea layer

To study the effect of the relative position of the polyurea, a set of simulations is performed for both monolithic and bilayer plates, and, in the latter case, several different polyurea-steel interface bonding strengths are used. In all cases, it is assumed that the flat face of the steel plate impacts the confined polyurethane target at an initial velocity of 71.66m/s. The values of the parameters used for these calculations are summarized in Table 3.3. In all of these simulations the projectile has a mass of 658gr.

The accumulated effective plastic strains of the elements within a 4mm (original) distance from the center of the steel plates are averaged and used to assess the response of the plates, since it has been observed experimentally that failure initiates somewhere within the central region of the steel plates [1]. These computed effective plastic strain histories are presented in Figure 3.23.

Figure 3.22. Comparison of the experimental and numerically-predicted results (SP-44): (a) principal stretches (left) and (b) thickness (right) as functions of the distance from the center of the plates for SFLS= 100MPa.
Comparing the effective plastic strain history of monolithic plate, M1, with those of bilayer plates, one with a perfectly-bonded polyurea on its back face, B1, and the other on its front face, B2, it is seen that the bilayer plate with polyurea cast onto its back face has a superior performance relative to the other two cases. The presence of polyurea on the back face, with an assumed perfect bonding, reduces the maximum effective plastic strain by more than 53%, whereas its presence on the front face only slightly improves the overall performance of the steel plate. This clearly indicates the importance of the relative position of the polyurea layer with respect to the loading direction.

This remarkable observation can be explained by considering the initial shock effect. As has been shown by Nemat-Nasser et al. [11], the stiffness of polyurea is highly pressure-dependent and can increase by orders of magnitude with compression. Thus, when polyurea is cast onto the front face of a bilayer plate, it would transmit a greater amount of the impact energy to the steel plate (due to better impedance matching), in comparison to when it is cast onto the back face of the plate. However, if the bilayer plate does not fail during the initial shock loading, then the presence of the polyurea on either face would increase the corresponding overall tangent modulus, thereby retarding the necking of the plate which has been observed to precede failure in thin DH-36 steel plates. It is experimentally observed that the polyurea layer may detach from the steel plates, either partially or completely depending on the impact conditions. When this occurs where the necking of the plate is proceeding, the
neck-retardation effect of the polyurea would be lost while its presence on the front face would have magnified the initial energy transfer to the plate due to the above-mentioned shock loading effect.

Figure 3.23. Average effective plastic strain history of a monolithic plate and bilayer plates of the indicated polyurea-steel interface bonding strengths.

Simulations B3 through B6 are aimed at evaluating the effect of the polyurea-steel interface bonding strength on the performance of the bilayer plates. The values of the parameters used for these simulations are given in Table 3.3. Figure 3.22 shows the results of these simulations in terms of the effective plastic strain history. The calculations predict that the performance of the bilayer plate with polyurea on the back face does not change noticeably
when the $SFLS$ is 140MPa, B3, compared to that of the perfectly-bonded case, B1. On the other hand, when the $SFLS$ is reduced to 100MPa, B4, the significance of the presence of polyurea on the back face is reduced. However, even in this case, B4, the performance of the bilayer plate is improved by more than 27% compared to the monolithic plate, M1. In addition, the finite-element models predict a complete de-bonding when the $SFLS$ is set to 100MPa, B4, and partial de-bonding when it is set to 140MPa, B3. On the contrary, the performance of the bilayer plate that has polyurea on the front face declines for both B5 and B6 ($SFLS = 140$ and $100MPa$) compared to the monolithic plate due to the initial shock effect. It should be noted that the delamination criterion used in this study needs to be investigated systematically for more accurate predictions.

### 3.4. Summary and conclusions

A finite-element method is employed to study the impulsive response of monolithic steel and bilayer steel-polyurea plates. The entire experimental set up explained in a previous article [1] is modeled using the commercially available finite-element code, LS-DYNA, integrating into this code physics-based and experimentally-supported temperature- and rate-sensitive constitutive models for steel and polyurea, including in the latter case the pressure effects. Comparing the simulation and the experimental results, we focus on identifying the underpinning mechanisms that control the deformation
and failure modes of both monolithic steel and steel-polyurea bilayer plates. The variations along the radial line of the three principal stretches are measured for selected plates, and used to compare with the corresponding model results. The predictions obtained using a Lagrangian and an ALE formulation are compared with the corresponding experimental results. It is found that the ALE calculation is superior to the Lagrangian calculation in terms of its stability and accuracy. Therefore, the ALE formulation is employed to predict the response of both the monolithic and bilayer plates, including a de-bonding algorithm in the latter case.

After validating the finite-element model, the effects of the relative position of polyurea with respect to the loading direction and the polyurea-steel interface bonding strength are studied numerically. The study shows that, when the polyurea-steel interface bonding is perfect, the presence of polyurea on the back face reduces the maximum average critical effective plastic strain of the plate by more than 53%, whereas when the polyurea is on the front face then the average critical effective plastic strain of the plate is reduced by less than 9%. Moreover, if the de-bonding is allowed, then the presence of the polyurea on the front face is predicted to actually increase the maximum critical effective plastic strain of the plate, as compared to that of the monolithic plate. This numerical prediction is in good agreement with our experimental observations reported in [1] and can be explained by the initial shock effect. In the case that the polyurea layer is cast onto the front face of the steel plate, its
stiffness increases substantially as it is compressed, attaining a better impedance match with the steel and thereby increasing the energy that is transferred to the plate. On the other hand, when polyurea is cast onto the opposite to the impact side (back face), the initial shock loads the steel plate first and then a part of the shock is captured and dissipated by the polyurea layer. Polyurea can also increase the effective tangent modulus of the bilayer plate and thus delay the onset of the necking instability. However this secondary mechanism is only important if the plate has not failed during the initial shock loading.

The numerical investigations also reveal that the strength of the polyurea-steel interface bond plays a significant role in the performance of the bilayer plates. If the polyurea-steel interface bond is perfect, then the deformation localization is postponed, whereas if the shear failure strength of the polyurea-steel interface bond is about 100MPa or less, a complete debonding is predicted for both configurations (polyurea on front face or back face) resulting in greater critical effective plastic strains.

3.5. Acknowledgment

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G. Barsoum's research program. The authors wish to acknowledge Mr. Jeff Simon, a former undergraduate student at UCSD in the Department of Mechanical and Aerospace Engineering, for his help to develop and code the program which has been used to digitally map the scanned images of the cut steel samples and estimate their 3D deformation gradient. The dissertation author was the primary investigator of this paper.

3.6. Reference


Chapter Four

Investigation of effect of polyurea on response of steel plates by direct ballistic experiments

We summarize the results of the response of monolithic steel plates and steel-polyurea bilayer plates to impulsive blast loads produced in direct ballistic experiments, focusing on the deformation and failure modes of the plates. Using high-speed photography, the deformation and fracturing of some of the plates are also captured. In addition, the total force acting on the steel plate is measured as a function of time in a few cases.

The experimental results suggest that the presence of polyurea on the back face (opposite to the load-receiving side) of the steel plates can enhance the energy absorption of the plates and help to mitigate their failure. On the other hand, when polyurea is placed on the front face (load-receiving side), it will magnify the initial shock effect and promote failure. These experimental results are paralleled by our numerical simulations of the entire experiment, employing physics-based models for the DH-36 steel and polyurea [1].
4.1. Introduction

Enhancing the energy absorption and improving the dynamic fracture resistance of steel-plate structures have been the focus of many studies for decades. Coating the steel plates with a polyurea layer has proven to significantly affect the survivability of such structures under high intensity impulsive loads, including those encountered in blast and ballistic events. Polyurea is an inexpensive, fire-resistant, light-weight, soft, abrasion-resistant retrofit for steel plates and it can be easily applied through spray-cast techniques to large steel panels (e.g., ship hulls).

There are only a few published experimental studies on the effect of polyurea on the ballistic performance of steel plates, as controlled laboratory-scale ballistic experiments are difficult to perform. In a recent study, Amini et al. [2] employed a reverse ballistic experimental technique to investigate the effect of polyurea on the impulsive response of steel plates. Using direct penetration tests, Mock and Balizer [3] report that a polyurea layer cast onto the back face of a steel plate can change the response of the plate from full penetration of the impacting projectile to its full arrest.

In the present experimental study, we have examined the deformation and failure modes of steel (DH-36) and steel-polyurea bilayer plates to
impulsive pressure loads, using a direct ballistic technique. In these experiments, an impulsive pressure pulse is applied to a steel plate through water or soft polyurethane that simulates shock loading with a peak pressure of about 80MPa and duration of ~50µs, followed by a cavitation period and a post-cavitation peak pressure of about 40MPa and 400µs duration. This experimental method allows us to assess the shock resistance, dynamic response, and fracturing of monolithic steel and steel-polyurea bilayer plates. High-speed photography is also employed to investigate the time variation of the deformation and the subsequent failure of some of the plates.

4.2. Materials and samples

4.2.1 Materials

DH-36 Steel

The plates are made out of DH-36 steel sheets. DH-36 steel is a high-strength steel used in naval applications. The stress-strain behavior of this material has been systematically studied by Nemat-Nasser and Guo [4] over a wide range of strain rates and temperatures. Nemat-Nasser and Guo also developed a physics-based (PB) constitutive model for this material. It is known that the mechanical properties of steel may vary depending on the rolling direction and other production details. In order to compare the high strain rate properties of the present DH-36 with those reported in [4], a set of Hopkinson bar experiments are performed at various strain rates. It is observed
that the plate used in the current study has approximately 10% higher flow stress compared with the one reported in [4]. Considering this difference, minor changes are applied to the PB model presented by Nemat-Nasser and Guo. This modified PB model is incorporated into the commercially available finite-element code, LS-DYNA, and used to predict the experimental results [1].

Figure 4.1. Nominal dimensions of the monolithic steel plate (quarter of plate)

Polyurea

The bilayer plates are comprised of polyurea and DH-36 steel. Polyurea’s physical and mechanical properties vary with its composition. The polyurea used in this study is prepared at CEAM/UCSD, using Isonate 2143 [5] and Versalink P1000 [6]. The viscoelastic properties of this polyurea have been experimentally studied by Amirkhizi et al. [7] over a wide range of temperatures and strain rates, including its pressure sensitivity. Based on this study [7], a pressure, temperature, and strain-rate dependent model has been developed by these authors and implemented into LS-DYNA.
4.2.2 Monolithic DH-36 steel plate samples

The monolithic steel circular plates used in these experiments are EDM machined from a 4.77mm thick steel sheet. The plates have a nominal thickness of 1mm at the inner portion and a nominal outer diameter of 76mm. All of the plates have a rim on the outside with the nominal inner diameter of 57mm, nominal outer diameter of 76mm, and nominal thickness of 4.75mm. The plates have a radius of curvature on the edge of the inner section of 1.6mm. Figure 4.1 shows the dimensions of the monolithic steel plate. The actual thickness of the steel plate at the central portion varies from 0.97mm to 1.05mm. The details of the monolithic steel plates are presented in Tables 4.1 and 4.2.
Table 4.1. Conditions of the direct ballistic experiments: DB-I. (samples denoted by SP are bilayer and samples denoted by S are monolithic)

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Test</th>
<th>Polyurea cast</th>
<th>Thickness (mm)</th>
<th>Mass (g)</th>
<th>Projectile</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>Steel</td>
<td>Polyurea</td>
<td>Mass (g)</td>
</tr>
<tr>
<td>S-52</td>
<td>N/A</td>
<td>1.02</td>
<td>N/A</td>
<td>90.4</td>
<td>832.6</td>
</tr>
<tr>
<td>S-53</td>
<td>N/A</td>
<td>1.05</td>
<td>N/A</td>
<td>91.3</td>
<td>832.6</td>
</tr>
<tr>
<td>S-55</td>
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<td>N/A</td>
<td>90.6</td>
<td>832.6</td>
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<tr>
<td>S-57</td>
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<td>N/A</td>
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<td>832.6</td>
</tr>
<tr>
<td>S-58</td>
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<td>N/A</td>
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</tr>
<tr>
<td>S-59</td>
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<tr>
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<td>N/A</td>
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<td>S-66</td>
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<tr>
<td>SP-35</td>
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<td>101.9</td>
<td>832.6</td>
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<tr>
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<td>SP-97</td>
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<td>3.75</td>
<td>103.8</td>
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</table>
Table 4.2. Conditions of the direct ballistic experiments: DB-II. (samples denoted by SP are bilayer and samples denoted by S are monolithic)

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Test</th>
<th>Projectile</th>
<th>Thickness (mm)</th>
<th>Loading device</th>
<th>Mass (g)</th>
<th>Velocity (m/s)</th>
</tr>
</thead>
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<td></td>
<td>Polyurea cast</td>
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<td></td>
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<td>Water</td>
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<td>Water</td>
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<td>43.9</td>
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<td>S-117</td>
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<td>Water</td>
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<td>48.1</td>
</tr>
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<td>S-119</td>
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<td>1.03</td>
<td>Water</td>
<td>1685.4</td>
<td>48.5</td>
</tr>
<tr>
<td>SP-95</td>
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<td>Polyurea</td>
<td>1.00</td>
<td>Water</td>
<td>1685.4</td>
<td>48.4</td>
</tr>
<tr>
<td>SP-98</td>
<td>Flat side</td>
<td>Polyurea</td>
<td>1.00</td>
<td>Water</td>
<td>1685.4</td>
<td>48.6</td>
</tr>
<tr>
<td>SP-116</td>
<td>Dish side</td>
<td>Polyurea</td>
<td>1.01</td>
<td>Water</td>
<td>1685.4</td>
<td>48.1</td>
</tr>
<tr>
<td>S-121</td>
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<td>Polyurethene</td>
<td>1685.4</td>
<td>47.8</td>
</tr>
<tr>
<td>S-123</td>
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<td>Polyurethane</td>
<td>1.01</td>
<td>Polyurethene</td>
<td>1685.4</td>
<td>48.5</td>
</tr>
</tbody>
</table>

4.2.3 Steel-polyurea bilayer samples

The steel-polyurea bilayer samples have two configurations: polyurea on the back face and polyurea on the front face. In the former case, polyurea is cast into the inner portion of the sample’s dish side. In the latter case, polyurea is cast onto the flat side of the plate, first on the central region of 57mm diameter (corresponding to the opposite-face of the dish portion). Then a thin Teflon band is placed on the lateral surface of the polyurea and new polyurea is cast onto the remaining portion of the plate’s flat side. Figure 4.2 shows a monolithic steel plate (right) and two configurations of the polyurea-steel bilayer plate. By casting the polyurea on the flat side in two steps, the stretch enhancement in the central region of the plates will be similar for the two configurations. The thickness of the polyurea layer is varied from 1.13mm to 3.75mm, as indicated in Tables 4.1 and 4.2.
4.3. Experimental Techniques

Two sets of direct ballistic experimental setups, denoted by DB-I and DB-II, are used to characterize the dynamic response and fracture resistance of the samples. In the first set, each sample is loaded by a projectile that impacts a confined soft polyurethane layer that rests against the sample, while, in the second set, a layer of confined water is used in some of the tests and soft polyurethane in others. These techniques are developed at UCSD’s Center of Excellence for Advanced Materials’ gas gun facilities laboratory. They are discussed in what follows.

![Figure 4.3. Schematic view of the DB-I experimental setup.](image)

4.3.1. DB-I setup

A schematic view of the cross section of this experimental setup is presented in Figure 4.3. In this setup, a 7075 aluminum projectile with a
nominal mass of 832gr is propelled by a gas gun at a controlled velocity, which is measured using velocity sensors placed at the end of the gas gun muzzle. The aluminum projectile loads a 25.4mm thick cylindrical soft polyurethane of 40A durometer hardness, resting against the sample within the confining cylinder. The polyurethane loads the plate that sits on a cylindrical step machined as an integral part of the confining cylinder. A pair of steel bars and sleeves is used to transfer the load from the cylindrical support to a 3-inch Hopkinson output bar.

Figure 4.4. Various views of the DB-I experimental setup and components.
The experimental setup is housed in a metal enclosure to prevent damage due to flying debris. The shield has a transparent window to allow for the high-speed photographic recording of the deformation and possible fracturing of the sample, using a Hadland Imacon 200 image acquisition system. The camera has sixteen channels which can be programmed to record a sequence of separate images at prescribed time intervals. Images are acquired at an angle relative to the path of the projectile. The camera is geometrically calibrated for parallax and alignment to achieve accurate results. The camera is indirectly triggered by the velocity sensor, through a digital delay generator, and it in turn triggers the flash strobes. Figure 4.4 shows various views of the setup, showing different aspects of the system. The experimental conditions and the corresponding specifications are summarized in Table 4.1. The projectile velocities range from 60m/sec to 72m/sec, providing a projectile kinetic energy of up to 2,129J.
4.3.2. DB-II setup

A schematic view of the cross section of this experimental setup is presented in Figure 4.5. In this setup confined water or polyurethane is used as a loading medium. This setup is used to assess the use of polyurethane instead of water to load the samples. This assessment is done by comparing the force transmitted through the plate for each loading medium (polyurethane or water). In this setup, a 7075 aluminum projectile with a nominal mass of 1,685gr is propelled by a gas gun at a controlled velocity, which is measured using velocity sensors placed at the end of the gas gun muzzle. The projectile loads an impedance-matched 7075 aluminum piston within a cylindrical confinement. The piston loads the confined water column (or a soft polyurethane cylinder) that in turn loads the plate that rests on a cylindrical step at the end of the confining cylinder. The load is transferred from the
support to a 3-inch Hopkinson output bar and measured by the strain gauges that are mounted on the bar.

Table 4.3. Results of the direct ballistic experiments: DB-I. (* This sample did not fail by stretching at the central region of the plate but sheared off at the edge)

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Test condition</th>
<th>Result</th>
</tr>
</thead>
<tbody>
<tr>
<td>Test</td>
<td>Plate type</td>
<td>Load-receiving layer</td>
</tr>
<tr>
<td>S-52</td>
<td>Monolithic</td>
<td>Steel</td>
</tr>
<tr>
<td>S-53</td>
<td>Monolithic</td>
<td>Steel</td>
</tr>
<tr>
<td>S-55</td>
<td>Monolithic</td>
<td>Steel</td>
</tr>
<tr>
<td>S-57</td>
<td>Monolithic</td>
<td>Steel</td>
</tr>
<tr>
<td>S-58</td>
<td>Monolithic</td>
<td>Steel</td>
</tr>
<tr>
<td>S-59</td>
<td>Monolithic</td>
<td>Steel</td>
</tr>
<tr>
<td>S-60</td>
<td>Monolithic</td>
<td>Steel</td>
</tr>
<tr>
<td>S-66</td>
<td>Monolithic</td>
<td>Steel</td>
</tr>
<tr>
<td>S-62</td>
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<tr>
<td>S-63</td>
<td>Monolithic</td>
<td>Steel</td>
</tr>
<tr>
<td>S-64</td>
<td>Monolithic</td>
<td>Steel</td>
</tr>
<tr>
<td>SP-35</td>
<td>Bilayer</td>
<td>Steel</td>
</tr>
<tr>
<td>SP-50</td>
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<td>Steel</td>
</tr>
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<td>SP-33</td>
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<td>Steel</td>
</tr>
<tr>
<td>SP-37</td>
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<td>SP-40</td>
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<tr>
<td>S-67</td>
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<td>Steel</td>
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<td>S-69</td>
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</tr>
<tr>
<td>SP-97</td>
<td>Bilayer</td>
<td>Polyurea</td>
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</table>
The water column is set up by partially filling a steel confinement of internal diameter 76.2mm, making a 25.4mm thick layer. The confinement is capped at one end with the piston, while the plate is placed at the opposite end. The piston and the hollow cylinder have O-ring seals to prevent leakage. In addition, the piston has a bleed valve to ensure that air is not trapped in the water column. To use polyurethane as the loading medium, the water column is replaced by a 25.4mm thick cylindrical polyurethane of 40A durometer hardness. Figure 4.6 shows various parts employed in this setup. The experimental conditions and the corresponding specifications are summarized in Table 4.2. The projectile velocities range from 42m/sec to 49m/sec, providing a projectile kinetic energy of up to 1,981J. It should be noted that the DB-II setup projectile has a lower velocity compared to the DB-I setup one, so as to compensate for its mass which is approximately twice that of the projectile used in the DB-I setup.
Figure 4.6. DB-II experimental setup and components.
4.4. Results and discussions

A total of 36 plates were tested using the DB-I setup and 9 samples were tested using the DB-II setup. The deformed plates are carefully examined for any visible signs of failure. Based on their response, the deformed plates are subdivided into three categories: No Failure, Moderate Failure, and Severe Failure. The plates in the first category did not have any cracks but multiple parallel necks were visible in the central region of some of them. The second category samples developed severe necking, with crack initiation and minor petaling. Finally, the last category samples had radial and circumferential cracks with petaling and possibly disking or edge tearing. A typical sample of each category is shown in Figure 4.7. The experimental conditions and the corresponding results are summarized in Tables 4.3 and 4.4.

### Table 4.4. Results of the direct ballistic experiments: DB-II.

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Test condition</th>
<th>Result</th>
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</tr>
<tr>
<td>S-123</td>
<td>Monolithic Steel</td>
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4.4.1. Effect of steel plate thickness

The membrane stiffness varies linearly with the plate thickness while the bending stiffness relates linearly to the square of the thickness. At low velocities, the deflection is small and hence the bending is dominant, while the stretching effect becomes significant at high impact velocities which produce large tensile deformation. For large deformations, the plastically dissipated work in the membrane deformation is larger than that of the bending deformation. Hence, the major portion of the energy absorbed by the specimen’s plastic deformation has a linear relation with the thickness at high velocities and large deformations. In other words, at constant impact velocity,
as the thickness of the specimen increases, the kinetic energy converted into the deformation energy increases linearly with thickness. Therefore, initial kinetic energy per unit thickness of the steel layer may be used to quantify the ballistic efficiency of the structure. It is experimentally verified that this parameter can be used to predict the failure onset of the plates. The experimental results suggest that when the impact kinetic energy per unit thickness is greater than an experimentally-obtained critical value, the sample fails. This critical value depends on the experimental conditions. All the monolithic steel plates tested in the DB-I setup with energy per unit thickness of greater than 15,600J/cm failed severely. Monolithic steel plates S-67 and S-69 failed at slightly lower energy per unit thickness values, which may have been caused by imperfection in their thicknesses.

Figure 4.8. Selected bilayer samples deformed at different input energy per unit thickness. Bilayer samples with polyurea cast onto front face (top row), and bilayer samples with polyurea cast onto back face (bottom row).
4.4.2. Effect of relative position of polyurea

Figure 4.8 shows selected bilayer samples loaded with polyurea on the back face (top row) and polyurea on the front face (bottom row). While the monolithic plates all failed at energy per unit thickness of 15,600J/cm and greater, the samples with a 3.7mm thick polyurea on the back face did not fail at 21,682J/cm energy per unit thickness. This shows that the presence of a polyurea layer on the back face (opposite to the impact face) improves the ballistic performance of the plates by more than 38%. On the other hand, it is observed that when a polyurea layer of the same thickness is present on the front face (impact face) the sample fails at energy per unit thickness as low as 15,692J/cm. This comparison reveals the significance of the relative position of the polyurea layer with respect to the loading direction.

Figure 4.9. Selected set of photos showing the sequence of the deformation and edge tearing (Frame 10) of steel-polyurea bilayer plate, SP-33 (polyurea on the back face). The frame number is marked at the upper right corner of each photograph. The frames are taken

Experiment SP-33 was performed with an initial kinetic energy per unit thickness of 21,681J/cm. A sequence of photographs showing the transient
response of this bilayer plate at 40µs time intervals, is presented in Figure 4.9. These photos reveal that SP-33 did not fail because of biaxial stretching at the center but sheared off at the edge. This may be explained by considering the initial shock effect. When polyurea is cast on the impact side (*front face*), it is loaded in compression. Experimental study at CEAM/UCSD [7] has shown that polyurea is a highly pressure-sensitive elastomer, with its stiffness increasing remarkably with increasing pressure. When the confined polyurea is loaded in compression, its stiffness can increase more than 10-fold, thereby attaining a better impedance match with the steel plate. Consequently, more energy is transferred to the plate. On the other hand, when polyurea is cast onto the opposite side of the impact (*back face*), the soft polyurethane (or water) loads the steel plate first and then a part of this energy is transferred to the polyurea, compressing it and thereby increasing its stiffness, and hence increasing the amount of the energy that it captures and damps because of its viscoelasticity. In addition to the significant shock effect, the polyurea layer has a secondary effect. Provided that the plate does not fail during the initial shock loading, the presence of a polyurea layer, either on the *front* or on the *back face* of the steel plate, increases the effective tangent modulus of the plate. Therefore, if the steel plate in a bilayer system does not fail and the polyurea does not detach during the initial shock loading, the presence of the polyurea tends to delay the onset of the necking instability, as has been pointed out by Xue and Hutchinson [8].
4.4.3. Effect of polyurea layer thickness

The effect of the thickness of the polyurea layer on the fracture resistance of bilayer plates is investigated by testing bilayer plates with various polyurea thicknesses, ranging from 1.1mm to 3.7mm. Figure 4.10 shows tested bilayer samples with polyurea on the front face (top row) and samples with polyurea on the back face (bottom row). All the samples with polyurea on the front face failed whereas the samples with polyurea on the back face did not fail. Sample SP-115 with 1.2mm polyurea cast on the back face did not fail at energy per unit thickness of 17,832J/cm which is 15% above the monolithic plate’s failure threshold. It is also observed that the thickness of the polyurea layer affects the de-bonding of the polyurea such that the reduction of polyurea layer thickness retarded the de-bonding. A more comprehensive set of experiments is necessary to completely understand the effect of the thickness of the polyurea layer on the dynamic response and fracture resistance of the bilayer plates.

Figure 4.10. Selected bilayer samples with indicated polyurea thickness, deformed at different (indicated) input energy per unit thickness. Bilayer samples with polyurea cast onto front face (top row), and bilayer samples with polyurea cast onto back face (bottom row).
4.4.4. Dynamic loading of plates

The force transmitted through the sample in the DB-II setup is measured using strain gauges mounted on the 3-inch Hopkinson output bar. The histories of the transmitted load demonstrate a consistent pattern with only some slight differences depending on the loading medium and the mode of the sample failure; see Figure 4.11. Four distinct stages of loading can be detected in all cases: Shock loading (pre-cavitation), cavitation, post-cavitation loading and unloading. These stages can be described as follows:

1-Shock loading (pre-cavitation loading)

In this stage, the initial transmitted shock pulse is transferred to the sample causing its initial deformation. The duration of this loading stage is less than 50µs and the amplitude of the load peak is approximately 350KN.

2-Cavitation

During this stage which approximately lasts 100µs, the particle velocity of the sample is higher than the particle velocity of the loading medium (water or polyurethane). This particle velocity discontinuity causes a pressure drop in the loading medium and consequently a pressure relief stage results, during which the plate continues to deform under its own kinetic energy.

3-Post-cavitation loading

The plate’s particle velocity decreases during the cavitation stage and continues until the water (or polyurethane) catches up and loads the sample
again. The duration of this stage is approximately 300μs and the amplitude of
the loading plateau is approximately 175KN, displaying distinctly different
features than those of the pre-cavitation. As shown in Figure 4.11, comparing
the load history of the failed and unfailed samples, it follows that the failure of
the sample reduces the loading duration of this stage by 75μs.

4-Unloading

After the post-cavitation loading stage, the sample is unloaded and the
average pressure in the loading medium drops back to zero. This stage is
completed within 150μs after the post-cavitation loading.

![Force history curves recorded by the strain gauges mounted on the 3-inch Hopkinson output bar. Polyurethane is used as loading medium for experiments S-121 and S-123 and water is used as the loading medium for the rest of the experiments.](image-url)
4.4.5. Comparison of water and polyurethane

To assess the use of polyurethane instead of water to load the samples, the corresponding transmitted load-histories are presented in Figure 4.11. The first stages for both loading media have the same characteristics in terms of peak force and load duration, while the cavitation period of the plates loaded through the polyurethane is shorter by ~20µs compared to the samples loaded through water. However, the main difference between the two cases occurs during the post-cavitation loading stage, since polyurethane can sustain tension which results in its unloading about 100µs earlier than that for water. This comparison shows that we can use polyurethane in place of water as the loading medium and thereby simplify the experimental process considerably.

4.4.6. Transient response of plates

The high-speed photography of the transient response of the plates provides additional information to understand their deformation and failure process. It is shown that this failure has three stages: hoop cracking, radial cracking, and tangential cracking. In the first stage, the hoop cracking, a crack
is formed in a circular pattern near the center of the plate. The hoop cracking then leads to radial cracks that extend toward the edge of the plate. As the radial cracks propagate toward the edge, they may change direction and then extend further in the tangential direction, provided that sufficient driving energy exists. Figure 4.12 consists of a sequence of photographs, showing the deformation and failure of a monolithic steel plate, S-64, at 40µs time intervals. The photographs depict all three cracking stages; see also Figure 4.13.

Figure 4.13. Three crack types developed in sample S-64: hoop crack, radial crack, and tangential crack.
Since the initiation of the failure is imperfection sensitive, the radius of the initial hoop crack is sensitive to the geometrical and material imperfections. In certain cases, these imperfections may eliminate the initial hoop cracking, resulting in fracture initiation by the radial cracks emanating from the near center of the plate, as is demonstrated in Figure 4.14 for a monolithic steel plate, S-70; the photographs are taken at 40µs time intervals. It is also noted that the de-bonding of the polyurea and steel in the samples with polyurea on the back face initiates at the edge and develops towards the center. The extent of polyurea and steel de-bonding varies from a complete de-bonding to a partial de-bonding, depending on the energy per unit thickness of the steel plate and the thickness of the polyurea layer.

Figure 4.14. Selected set of photos showing the sequence of the deformation and failure of the monolithic steel plate, S-70. The frame number is marked on the upper right corner of each frame. The frames are taken at 40µs time intervals.
Figure 4.15 shows a sequence of photographs of the deformation of the bilayer sample, SP-37. The high-speed photography also reveals that the failure process of the bilayer plates with polyurea on the front face is similar to that observed in the monolithic plates. Figure 4.16 shows a sequence of photographs of the deformation and failure of the bilayer sample, SP-74. Visual inspection of the bilayer samples with polyurea on the front face reveals that the de-bonding of polyurea and steel initiates at the center and develops toward the edge.

Figure 4.15. Selected set of photos showing the sequence of the deformation and de-bonding of the steel-polyurea bilayer plate, SP-37 (polyurea on the back face). The frame number is marked on the upper top right corner of each frame. The frames are taken at 40µs time intervals

Figure 4.16. Selected set of photos showing the sequence of the deformation and fracture of the steel-polyurea bilayer plate, SP-74 (polyurea on the front face). The frame number is marked on the upper right corner side of each frame. The frames are at 40µs time intervals
4.5. Summary and conclusions

Two experimental setups are used to study the fracture resistance and dynamic response of monolithic and bilayer plates. The transient response and fracture process of these plates are captured employing high-speed photography. In addition, the time-history of the force transmitted through the samples is measured. The most significant result of our study is that, when a polyurea layer is cast onto front face (the impact side), its presence promotes failure during the initial shock effect. Under pressure, the stiffness of the polyurea layer increases substantially, attaining a better impedance match with the steel and thereby increasing the energy that is transferred to the plate. On the other hand, when polyurea is cast onto the back face (opposite to the impact side), the initial shock loads the steel plate first and then a part of the shock is captured and dissipated by the polyurea layer because of its viscoelasticity. Polyurea can also increase the effective tangent modulus of the bilayer and thus delay the onset of the necking instability, if the steel plate does not fail during the initial shock loading. The other noteworthy results are as follows:

- Four stages of loading are detected: Shock loading (pre-cavitation), cavitation, post-cavitation loading, and unloading.
- Use of polyurethane instead of water as the loading medium is assessed. It is shown that the time-history of the pressure imparted to the plate through soft polyurethane is essentially the same as that
imparted through water, i.e., the pulse shape, its duration, and its magnitude.

- Three stages of failure of the plates are detected: hoop cracking, radial cracking, and tangential cracking.
- It is observed that the de-bonding of the bilayer plates with polyurea on the back face initiates at the edge and develops towards the center, whereas when the polyurea is cast onto the front face, the detaching initiates at the center of the plate.

These experimental results are supported by systematic computational simulations of the entire experiment, employing realistic physics-based constitutive models for the steel (DH-36, in the present work) and polyurea, the results of which are reported in a separate article [1].

### 4.6. Acknowledgments

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4.7. References


Chapter Five

Numerical modeling of effect of polyurea on response of steel plates to impulsive loads in direct ballistic experiments

The results of the finite-element modeling of the response of monolithic DH-36 steel plates and bilayer steel-polyurea plates to impulsive loads (pressure) in direct ballistic experiments [1], are presented and discussed. The experiments and their results are presented in a separate paper [1], briefly summarized here. The entire experimental setup is modeled using the finite-element code, LS-DYNA, in which a physics-based temperature- and strain rate-sensitive constitutive model for DH-36 steel, developed by Nemat-Nasser and Guo [2], and an experimentally-supported temperature-, rate-, and pressure-sensitive constitutive model for polyurea, developed and implemented by Amirkhizi et al. [3], have been implemented.

The transients response of the plates under impulsive pressure loads are studied focusing on the effects of the relative position of polyurea with respect
to the loading direction, the thickness of the polyurea layer, and the polyurea-
steel interface bonding strength. The numerical simulations of the entire
experiment support the experimentally observed results provided one employs
realistic physics-based constitutive models for all constituents.

5.1. Introduction

It has been shown by direct ballistic experimentally by Amini et al. [1]
that the failure resistance of steel plates to impulsive pressure loads can be
enhanced by spray casting a layer of polyurea on the back face of the plates.
Here we provide numerical simulations of those experiments, using finite-
element code, LS-DYNA. The numerical study is aimed at understanding the
transient response and the underpinning mechanisms of failure and
deformation of the plates in such loading conditions.

A comprehensive review of various theoretical and numerical studies
on the deformation of thin metal plates subjected to impulsive loads is given by
Nurick and Martin [4] and also Jones [5]. Zhu [6] performed a numerical
investigation to understand the transient behavior of thin plates under
explosive loads and compared his predictions with the experimental results.
Lee and Wierzbicki [7,8] employed commercially available codes PAM-
CRASH and ABAQUS to model various aspects of transient deformation and
fracture of thin plates under localized impulsive loads. Yuen and Nurick [9]
used ABAQUS to predict the response of quadrangular stiffened plates subjected to uniform blast loads and compared their predictions with experimental results. Bahei-El-Din et al. [10] has studied the effect of interlayer elastomeric polyurea on the dynamic response of sandwich plates under dynamic loads. It has been shown by reverse ballistic and direct ballistic experiments by Amini et al. [11, 1] that the failure resistance of steel plates to impulsive pressure loads can be enhanced by spray casting a layer of polyurea on the back face of the plates.

There are few numerical studies on the effect of polyurea coating on the performance of steel plates. Amini et al. [12] numerically studied the deformation and failure of monolithic steel and bilayer steel-polyurea plates under impulsive loads in reverse ballistic experiments. Comparing the numerical prediction with the reverse ballistic experimental results, they found that the presence of polyurea coating can enhance the fracture resistance of steel plates if it is on the face opposite to the impact side (back face). This is due to the viscoelastic damping of the initial shock load transferred to polyurea and also increasing of the overall effective tangent modulus of the plate. On the other hand they reported that when polyurea is on the impact face (front face) the polyurea layer can aggravate the shock loading. However, if the steel plate survives the initial shock loading and the polyurea coating remains bonded to the steel then its presence increases the effective tangent modulus of the plate and hence retards the necking. Xue and Hutchinson [13] studied the
neck retardation under biaxial stretching of bilayer elastomer-metal plates. They show that substantial increase in necking limits and consequent energy absorption can be achieved in metal-elastomer bilayers compared to metal plate of same area density.

In the present paper, we perform a full-scale finite-element modeling of the direct ballistic experiments reported by Amini et al. [1]. The significance of these finite-element models is that they allow the first, to study the transient response of the plate, second, to incorporate complex temperature-, pressure-, and rate-dependent constitutive models in the finite-element code, and finally, to conduct parametric studies to gain insight into the ballistic response of the plates. The effect of the relative position of the polyurea layer with respect to loading direction, the effect of the thickness of the polyurea layer, and finally, the effect of the polyurea-steel interface bonding strength on the ballistic performance of steel plates are studied numerically.

5.2. Finite-element modeling procedure

In this section, the direct ballistic experiments setup is briefly reviewed and the finite-element model is detailed.
5.2.1. Direct ballistic experiments setup

In the direct ballistic experiments [1], performed at CEAM/UCSD’s gas gun facilities, a projectile is propelled by a gas gun at a controlled velocity toward an impedance-matched piston that loads a confined soft polyurethane
or water medium. The loading medium (water of polyurethane) in turn loads the plate that rests on a cylindrical step (support) within a confining cylinder. The load is, directly or through a steel bar/sleeve system, transferred from the support to a 3-inch Hopkinson output bar. For some cases the transmitted force is measured by the strain gauges that are mounted on the bar. The detailed description of the experimental setup and the results are presented in [1].

5.2.2. Finite-element model

The direct ballistic experiments are modeled in full-scale using the commercially available finite-element code, LS-DYNA. Two views of the 3-dimensional model used in the finite-element simulation are shown in Figure 5.1. A simulation is initiated when an aluminum projectile is brought into frictionless contact with a cylindrical loading medium (polyurethane/water) at a uniform initial velocity $V_0$. The projectile mass is 832gr or 1682gr depending on the experimental conditions. To model the confinement of the loading medium (nearly incompressible) under pressure, the side nodes of the loading medium that are in contact with the confining cylinder are restricted to allow sliding only in the longitudinal ($Z$) direction. The loading medium in turn loads the plate that rests against a hollow cylindrical support. The cylindrical support nodes in contact with the output Hopkinson bar are fixed by requiring that the radial, tangential, and longitudinal displacement components be zero, i.e., $u_r = u_t = u_z = 0$. To increase the stability and the accuracy of the finite-element
model, an Arbitrary Lagrangian-Eulerian (ALE) formulation is used in these calculations. The loading medium (water/polyurethane) is modeled with one-point ALE multi-material brick elements and the rest of components are modeled using elements with Lagrangian formulation. The simulation uses the contact algorithm option of LS-DYNA, entitled CONSTRAINED_LAGRANGE_IN_SOLID to model the interactions of the Lagrangian and Eulerian domains; see [14] for more details. The CONTACT_AUTOMATIC_SURFACE_TO_SURFACE contact algorithm of LS-DYNA is employed to model the interactions of the plate and the cylindrical support. All steel plates have a diameter $D_{st} = 76.00\text{mm}$, gauge section thickness of $t = 1.02\text{mm}$, rim thickness of $t_{\text{rim}} = 4.79\text{mm}$, and rim width of $w_{\text{rim}} = 9.52\text{mm}$; see Figure 5.2.

![Figure 5.2. Angled view of the specially discretized monolithic plate, with outer diameter $D_{st} = 76.00\text{mm}$, rim thickness of $t_{\text{rim}} = 4.79\text{mm}$, and rim width of $w_{\text{rim}} = 9.52\text{mm}$.](image)

The plate is modeled using eight-node brick elements with one integration point whereas the rim (outer part of the plate) is modeled using the
fully integrated quadratic eight-node brick elements with nodal rotation to avoid the undesired hourglass energy. There are 56 elements along the diameter and three elements per millimeter through the thickness of the plate. Figure 5.3 presents the spatial discretization of the three plate configurations: the monolithic steel plate (left), the bilayer plate with polyurea on the dish side/back face (middle), and the bilayer plate with polyurea on the flat side/front face (right). The polyurea on the flat side is modeled in two separate parts, outer portion and central portion, so the stretch enhancement in the central region of the plates will be similar for the two bilayer plate configurations. The 7075 aluminum projectile is modeled by fully integrated selectively-reduced eight-node brick elements. The Flanagan-Belytschko integration hourglass-control algorithm is used to damp out the overall zero-energy modes.

Figure 5.3. Angled view of spatially discretized plates: (a) monolithic steel plate (left), (b) bilayer plate with polyurea on the dish side (middle), and (c) bilayer plate with polyurea on the flat side (right).
5.3. Material constitutive models

The material constitutive models used for DH-36 steel, polyurea, water, and polyurethane are discussed in this section.

Figure 5.4. Comparison of the PB-model predictions with the experimental results at indicated strain rates and initial room temperature.

5.3.1. DH-36 steel

The physics-based (PB) model, proposed by Nemat-Nasser and Guo [2] for DH-36 steel, is implemented into LS-DYNA through a user-defined material subroutine in FORTRAN. This PB model expresses the flow stress (effective stress), $\tau$, as a function of temperature, $T$, effective plastic strain, $\gamma$, and effective plastic strain rate, $\dot{\gamma}$, as
\[ \tau = 70 + 750\gamma^{0.25} + 1500 \left\{ 1 - \left[ -6.6 \times 10^{-5} T \left( \ln \frac{\dot{\gamma}}{2 \times 10^{10}} \right) \right]^{\frac{3}{2}} \right\}, \quad (1) \]

for \( T \leq T_c \), where the stress is in MPa and \( T \) is in degrees Kelvin. On the other hand, for \( T \geq T_c \), we use

\[ \tau = 70 + 750\gamma^{0.25}, \quad (2) \]

where \( T_c = \left[ -6.6 \times 10^{-5} \left( \ln \frac{\dot{\gamma}}{2 \times 10^{10}} \right) \right]^{-1} \).

Figure 5.4 compares three experimentally obtained stress-strain curves with their corresponding PB-model predictions at the indicated strain rates. As is seen, excellent correlation between the experimental data and the model predictions is obtained.

### 5.3.2. Polyurea

An experimentally-based viscoelastic constitutive model, including pressure and temperature sensitivity, introduced and implemented into LS-DYNA by Amirkhiz et al. [3], has been employed for modeling the polyurea. For bulk deformations, this model assumes the trace of the Cauchy stress tensor, \( \sigma \), which is given by,

\[ tr(\sigma) = 3k \frac{\ln J}{J}, \quad (3) \]
where \( J = \det \mathbf{F} \) is the Jacobian of the deformation, and \( k \) is a temperature-dependent bulk modulus, assumed to be given by,

\[
k(T) = k(T_{\text{ref}}) + m(T - T_{\text{ref}}),
\]

(4)

with \( T_{\text{ref}} \) being the reference (room) temperature. A hereditary integral is employed to express the deviatoric part of the stress,

\[
\mathbf{\sigma}'(t) = \int_{0}^{t} \frac{T(\tau)}{T_{\text{ref}}} 2G_{\text{ref}} (\xi(t) - \xi(\tau)) \mathbf{D}'(\tau) d\tau,
\]

(5)

where \( \mathbf{D}' \) is the deviatoric part of the deformation rate tensor, and \( \xi \) is a pressure sensitive reduced time, related to the actual time, \( t \), through the following equation:

\[
\xi(t) = \int_{0}^{t} \frac{d\tau}{a_{T}(\tau)},
\]

(6)

\[
a_{T} = 10^{\frac{A(T-C_{p}P-T_{\text{ref}})}{B+T-C_{p}P-T_{\text{ref}}}}.
\]

(7)

Here, \( P \) is the pressure and \( C_{p} \) is a time-pressure coefficient.

In this study the four-term \((n = 4)\) Prony series option of this code is used to define the relaxation function,

\[
G_{\text{ref}}(t) = G_{\infty} \left[ 1 + \sum_{i=1}^{n} p_{i} e^{-t/\tau_{i}} \right].
\]

(8)

The local temperature is calculated using

\[
\frac{\partial T}{\partial \hat{t}} = \frac{1}{C_{v}} \frac{\partial W_{d}}{\partial t},
\]

(9)
where $C_v$ is the heat capacity at constant volume, and $W_d$ is the dissipated work, both measured per unit original volume. The right-hand side of this equation is expressed as,

$$\frac{\partial W_d}{\partial t} = G\frac{T(t)}{T_{ref}} \sum_{i=0}^{n} p_i \varepsilon_i'(t) : \varepsilon_i'(t).$$

(10)

where,

$$\varepsilon_i'(t) = \int_{0}^{t} e^{-(\xi(t) - \xi(\tau))}/q_i \mathbf{D}'(\tau) d\tau.$$  

(11)

The numerical values of the parameters used in this model are listed in Table 5.1.

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5.3.3. Water and polyurethane

An elastic fluid model, entitled MAT_ELASTIC_FLUID, of LS-DYNA is used to model water. In this model, the pressure rate, $\dot{p}$, is given by,
\[ \dot{p} = -K \dot{\varepsilon}_{ij}, \]  
(12)

where \( K=2202\, \text{MPa} \) is the bulk modulus and \( \dot{\varepsilon}_{ij} \) are the strain rate components.

A tensor viscosity is used which acts only on the deviatoric stress, \( \sigma'_{ij} \), given in terms of the damping coefficient as:

\[ \sigma'_{ij} = V C \cdot \Delta L \cdot a \rho \dot{\varepsilon}'_{ij}, \]  
(13)

where \( V C=0.1 \) is the tensor viscosity coefficient, \( \Delta L \) is a characteristic element length calculated by the software, \( a \) is the fluid bulk sound speed, \( \rho = 1.00 \, \text{g/cc} \) is the density and \( \dot{\varepsilon}'_{ij} \) is the deviatoric strain rate component.

The polyurethane is modeled using a Mooney-Rivlin rubber constitutive model with shear modulus \( G = 16\, \text{MPa} \) and Poisson's ratio \( \nu = 0.495 \), the density being \( \rho = 1.19 \, \text{g/cc} \); for more details, see LS-DYNA theoretical manual [14].

### 5.4. Comparison of numerical predictions and experimental results

#### 5.4.1. Calculating the principal stretches profiles

To validate the finite-element models, the variation of the three principal stretches along the radial line for the selected plates is compared with their corresponding finite-element model predictions. To this end, the deformed plates are sectioned along their diametral plane and scanned. The scanned cross section of the deformed plate is discretized into trapezoid
elements. Since the plastic deformation of steel can be considered nearly isochoric, the volume of the ring, generated by the rotation of trapezoid elements along the center line of the model, is the same in the deformed and undeformed states. Using this fact and the known constant thickness of the undeformed steel plate, one can calculate the length of the central ring in the radial direction in the undeformed state. By repeating this calculation and marching from the center line towards the rim of the plate and given the volume of each ring, its constant thickness, and its distance from the center, the radial length of each element in the radial direction and the location of the next further element can be obtained. The deformation gradient of each element can be calculated afterward; see [12] for details of the measurements and analysis. After calculating the deformation gradient of each element, a MATLAB program is used to calculate the principal stretches and thickness of the analyzed plates as a function of the distance from the center of the plate in the undeformed state. The same computational procedure is applied to the numerical model of the deformed plates to calculate the final principal stretches and thickness profiles.

Table 5.2. Values of the parameters used in finite-element simulations. (polyurea is on the back face of sample SP-130)

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Plate type</th>
<th>Loading medium</th>
<th>Projectile Mass (gr)</th>
<th>Initial conditions Velosity (m/s)</th>
<th>Kinetic energy (J)</th>
</tr>
</thead>
<tbody>
<tr>
<td>S-57</td>
<td>Monolithic</td>
<td>polyurethane</td>
<td>832</td>
<td>61.58</td>
<td>1577</td>
</tr>
<tr>
<td>SP-130</td>
<td>Bilayer</td>
<td>polyurethane</td>
<td>832</td>
<td>63.8</td>
<td>1691</td>
</tr>
<tr>
<td>MW-1</td>
<td>Monolithic</td>
<td>water</td>
<td>1682</td>
<td>48.4</td>
<td>1970</td>
</tr>
</tbody>
</table>
5.4.2. Validation of the monolithic steel plate model

To validate the monolithic steel plate finite-element model, experiment S-57 is modeled. The details of this experiment are presented in [1]. The parameters, used for the finite-element simulation are provided in Table 5.2. Figure 5.5 shows the finite-element predictions of the three principal stretches and thickness profiles, and compares these with the corresponding experimental results. The comparison of experimental measurements and numerical predictions reveals that the finite-element model is capable of predicting the ballistic response and deformation of monolithic steel plates. Figure 5.6 shows a sequence of deformed configurations of the S-57 sample at 40μs time intervals. The numerical model reveals that the deformation initiates at the rim while the center part is still un-deformed, and the deformation then proceeds toward the center until the maximum mid-span deflection is attained and the plate is unloaded. The unloading is followed by small elastic vibrations. This numerically predicted deformation process is experimentally verified using high-speed photography.

Figure 5.5. Comparison of the experimental and numerically-predicted results (ALE formulation) of monolithic plate S-57: (a) principal stretches (left), and (b) thickness (right).
Figure 5.6. Sequence of photos of the side view of the deflection of S-57 monolithic steel plate at 40ms time intervals; note that a quarter of the model is masked to provide a better view of the thickness profile.

5.4.3. Validation of bilayer steel-polyurea plate model

A bilayer steel-polyurea plate, SP-130, direct ballistic experiment is modeled and the finite-element prediction is compared with the experimental measurements. Two simulations are performed, one with a perfect steel-polyurea interface bond and the other with employing a debonding algorithm. The parameters, used for the finite-element simulation are provided in Table 5.2. The finite-element prediction of the principal stretches and thickness
profile for the model with perfect steel-polyurea bond are presented in Figure 5.7. In addition, Figure 5.8 shows the predicted deformation sequence of SP-130 with perfect interface bond.

Figure 5.7. Comparison of the experimental and numerically-predicted results (SP-130): (a) principal stretches (left), and (b) thickness (right) as functions of the initial distance from the center of the plate for $SFLS=\infty$.

Figure 5.8. Sequence of photos of the side view of the deflection of SP-130 bilayer plate at 40ms time intervals with $SFLS=\infty$; note that a quarter of the model is masked to obtain a better view of the profile.
Comparison the principal stretch profiles with the experimental measurements reveals that the finite-element model under-predicts the stretching of the plate. This under-prediction is due to the assumption of a perfect steel-polyurea interface bond. To model the experimentally-observed debonding of the steel and polyurea layers, the contact algorithm option of LS-DYNA, entitled CONTACT_AUTOMATIC_SURFACE_TO_SURFACE_TIEBREAK is used; see [14] for more details. In this model, it is assumed that interface debonding is governed by the following failure criteria:

$$\frac{\tau}{SFLS} + \frac{\sigma}{NFLS} \leq 1.0,$$

where \( \tau \) and \( \sigma \) are the shear and normal stresses at the interface, and \( SFLS \) and \( NFLS \) are the interface shearing and normal strengths. Since no experimental data is available for the interface bonding strength, a trial and error method is employed to reproduce the experimentally observed debonding by adjusting the value of the interface bonding strength. In this simulation, the \( SFLS \) and \( NFLS \) are assumed to be 100 MPa and 64 MPa respectively. Figure 5.9 shows the finite-element model predictions of the principal stretches and the thickness profiles for \( SFLS=100 \text{MPa} \) and \( NFLS=64 \text{MPa} \). The comparison of the experimental results and the numerical predictions, presented in Figure 5.9, reveals that excellent correlation between experimental and numerical results can be obtained by introducing the debonding algorithm. Figure 5.10 shows a sequence of deformed configurations of the SP-130 sample at 40\( \mu \)s.
time intervals. This numerical model predicts that complete debonding of the steel and polyurea layers. The debonding initiates from the edge and propagates toward the center.

Figure 5.9. Comparison of the experimental and numerically-predicted results (SP-130): (a) principal stretches (left), and (b) thickness (right) as functions of the initial distance from the center of the plate for SFLS= 100MPa, NSFS=64MPa.

Figure 5.10. Sequence of photos of the side view of the deflection of SP-130 bilayer plate at 40ms time intervals with SFLS= 100MPa and NSFS=64MPa; note that a quarter of the model is masked to obtain a better view of the profile.
5.4.4. Transmitted force: finite-element vs. experiment

As explained in Section 5.2 of this chapter, the force transmitted through the plate is measured by the strain gauges that are mounted on the output bar, for selected experiments. The detailed description of the experimental conditions is presented in [1]. To further assess the finite-element models, the forces measured for selected samples (S-117, S-119) are compared to the finite-element prediction. The parameters, used for the finite-element simulation (MW-1) are provided in Table 5.2. The comparison of the experimentally measured force history curves for selected experiments and prediction of the finite-element model are shown in Figure 5.11. This comparison reveals that the finite-element model can predict the four stages of loading: shock loading (pre-cavitation), cavitation, post-cavitation loading and unloading. The finite-element model prediction of the transmitted force has an excellent correlation with the experimental measurements in the pulse shape, its duration and its amplitude.
5.5. Parametric study results

In this section, a parametric study is performed on the bilayer plates under impulsive pressure loads. The three main parameters that are investigated include:

- the relative position of the polyurea layer with respect to the loading direction,
- the strength of the polyurea-steel interface bonding, and
- the thickness of the polyurea layer.

Figure 5511. Comparison of the experimentally measured force history curves recorded by the strain gauges mounted on the 3-inch Hopkinson output bar for selected experiments and prediction of the finite-element model.
The accumulated effective plastic strains of the elements within a 4mm (original) distance from the center of the steel plates are averaged and used to assess the response of the plates, since it has been observed experimentally that failure initiates within the central region of the steel plates [1].

Table 5.3. Values of the parameters used in the finite-element simulations of the effect of relative position of polyurea with respect to the loading direction, thickness of polyurea layer and the polyurea-steel interface bonding strength.

<table>
<thead>
<tr>
<th>Model</th>
<th>Polyurea</th>
<th>Strength on interface bond (MPa)</th>
<th>Polyurea thickness (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Normal (NFLS)</td>
<td>Shear (SFLS)</td>
</tr>
<tr>
<td>M1</td>
<td>N/A</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>B1</td>
<td>back face</td>
<td>∞</td>
<td>∞</td>
</tr>
<tr>
<td>B2</td>
<td>back face</td>
<td>∞</td>
<td>∞</td>
</tr>
<tr>
<td>B3</td>
<td>back face</td>
<td>∞</td>
<td>∞</td>
</tr>
<tr>
<td>F1</td>
<td>front face</td>
<td>∞</td>
<td>∞</td>
</tr>
<tr>
<td>F2</td>
<td>front face</td>
<td>∞</td>
<td>∞</td>
</tr>
<tr>
<td>F3</td>
<td>front face</td>
<td>∞</td>
<td>∞</td>
</tr>
<tr>
<td>B4</td>
<td>back face</td>
<td>64</td>
<td>140</td>
</tr>
<tr>
<td>B5</td>
<td>back face</td>
<td>64</td>
<td>100</td>
</tr>
<tr>
<td>F4</td>
<td>front face</td>
<td>64</td>
<td>140</td>
</tr>
<tr>
<td>F5</td>
<td>front face</td>
<td>64</td>
<td>100</td>
</tr>
<tr>
<td>B6</td>
<td>back face</td>
<td>∞</td>
<td>100</td>
</tr>
<tr>
<td>F6</td>
<td>front face</td>
<td>∞</td>
<td>100</td>
</tr>
<tr>
<td>B7</td>
<td>back face</td>
<td>∞</td>
<td>140</td>
</tr>
<tr>
<td>F7</td>
<td>front face</td>
<td>∞</td>
<td>140</td>
</tr>
<tr>
<td>B8</td>
<td>back face</td>
<td>∞</td>
<td>180</td>
</tr>
</tbody>
</table>

The projectile used in the finite-element models reported in this section has a mass of 1682gr and an initial velocity of 48.4m/sec that provides a uniform initial kinetic energy for all the models. All the steel plate models have a gauge section thickness of 1.02mm and are loaded by polyurethane. The details of the finite-element model parameters are tabulated in Table 5.3.
5.5.1. Effect of relative position of polyurea layer

To study the effect of the relative position of polyurea, three simulations are performed: A monolithic steel plate (M1), a bilayer plate with the polyurea layer on the impact receiving side, front face (F3), and a bilayer plate with the polyurea layer on the back face (B3). A perfect bond between the polyurea and steel is assumed for the bilayer plate models. The computed average effective plastic strain histories of the three simulations are presented in Figure 5.12. The effective plastic strain history curve of the monolithic
plate, M1, clearly reveals the presence of the four loading stages. The initial increase of effective plastic strain corresponds to the shock loading. The first plateau that follows the initial rise indicates the cavitation stage where the pressure applied by the loading medium on the plate is released. At the end of the first plateau, there is a second rise in the effective plastic strain which is due to the post-cavitation loading. These stages can also be detected in the effective plastic strain histories of the bilayer plates. The comparison of the three curves shows that the presence of the polyurea improves the overall performance of the plate (if it remains bonded to the steel layer). However, the presence of polyurea on the front face aggravates the initial shock loading effect. This can be explained by considering that when polyurea is on the front face of a bilayer plate, it would transmit a greater amount of the impact energy to the steel plate due to better impedance matching, in comparison to when it is on the back face of the plate. High-strain rate impact experiments performed on polyurea elastomers by Yi and Boyce [15], Clifton and Jiao [16], and Nemat-Nasser et al. [17] revealed significant stiffening under pressure and high strain-rate that can aggravate this effect. In addition, the viscoelastic damping of the initial shock load transferred to polyurea, results in better performance of the bilayer plate with polyurea on the back face.

Table 5.4. Density and longitudinal wave velocity of steel, polyurea and water.

<table>
<thead>
<tr>
<th>Material</th>
<th>Density (gr/cc)</th>
<th>Longitudinal wave velocity (m/s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>polyurea</td>
<td>1.10</td>
<td>4800</td>
</tr>
<tr>
<td>steel</td>
<td>7.83</td>
<td>6042</td>
</tr>
<tr>
<td>water</td>
<td>1.00</td>
<td>1098</td>
</tr>
</tbody>
</table>
It is known that as an incident pressure pulse reaches a material interface, a part of the pressure pulse is transmitted into the second material and a part of it is reflected back. The ratio of the pressure between the transmitted and the incident waves can be expressed using the following relation:

\[
\frac{P_T}{P_i} = \frac{2 \rho_T V_T}{\rho_T V_T + \rho_I V_I}
\]  

(15)

where, \(P_T\) and \(P_i\) are the transmitted and incident pressures, respectively; \(\rho_T\) and \(\rho_I\) are the densities of the transmitted and incident layers; and \(V_T\) and \(V_I\) are the longitudinal wave velocities of the transmitted and incident layers. The density and longitudinal wave velocity of steel, polyurea and water are presented in Table 5.4. It has been shown before [15, 16, 17] that the stiffness of polyurea is highly pressure-dependent and can increase by orders of magnitude with compression. Thus, the longitudinal wave velocity of the polyurea is calculated based on a plain-strain finite-element simulation. In the case that polyurea is on the \textit{back face}, as the wave traveling in the water arrives at the water-steel interface, the ratio of the pressure transmitted to steel to the incident pressure in the water is 1.95. On the other hand, when the polyurea is on the \textit{front face}, as the wave traveling in the water arrives at the water-polyurea interface the ratio of the pressure transmitted to the polyurea to the incident pressure in the water is 1.66. Then, as this aggravated transmitted pressure pulse reaches the polyurea-steel interface, the ratio of the pressure
transmitted to steel to the incident pressure in the polyurea is 1.80. If the assumption of no dissipation of energy in the polyurea is valid then the ratio of the pressure transmitted to steel to the initial incident pressure in water for the bilayer plate with the polyurea on the front face is 2.99 which is more than 53% greater than the corresponding value for the bilayer plate with the polyurea on the back face. It should be noted that these calculations are based on one-dimensional elastic wave propagation theory and ignore the three-dimensional effects.

Xue and Hutchinson [13] proved that, the presence of the polyurea on either face would increase the corresponding overall tangent modulus, thereby retarding the necking of the plate which has been observed to precede failure in thin DH-36 steel plates. However this is valid only if the bilayer plate does not fail during the initial shock loading and the polyurea remains bonded to the steel layer.

5.5.2. Effect of thickness of polyurea layer

A set of simulations are performed to study the effect of the polyurea layer thickness on the performance of the bilayer plates. In these simulations the thickness of the polyurea layer is set to 3.78mm, 2.52mm and 1.26mm. The simulations are performed for both bilayer configurations. A perfect steel-polyurea interface bond between the polyurea and steel is assumed for the
bilayer plate models. The details of the simulations are tabulated in Table 5.3. Figure 5.12 shows the comparison of the averaged effective plastic strain histories for these simulations. The comparison reveals that the increase of the thickness of the polyurea layer on the back face improves the overall performance of the bilayer plates. On the other hand, as the thickness of the polyurea on front face increases, the initial shock effect becomes more pronounced. However the increase of the polyurea layer thickness also increases the overall effective tangent modulus of the plate that leads to superior performance of the bilayer plates in this case (perfect interface bond).

Figure 5.13. Average effective plastic strain history of a monolithic plate and bilayer plates of different polyurea-steel interface bonding strengths. The thickness of polyurea layer is 3.78mm for all bilayer samples. M1: monolithic plate; F3 through F7: bilayer p
5.5.3. Effect of steel-polyurea interface bonding strength

It is experimentally observed [1] that the polyurea layer may detach from the steel plate either partially or completely, depending on the interface bonding strength and the initial kinetic energy. Simulations B4 through B8 and F4 through F7 are aimed at evaluating the effect of the polyurea-steel interface bonding strength on the performance of the bilayer plates. The values of the parameters used for these simulations are given in Table 5.3. The results of these simulations are compared to the results obtained for perfectly bonded steel-polyurea bilayers. Figure 5.13 compares the results of these simulations in terms of the averaged effective plastic strain history. Generally it can be observed that the performance of the bilayer plates in the shock loading stage (pre-cavitation) is not significantly affected by the steel-polyurea debonding. However, after the cavitation stage the effective plastic strain increases due to debonding of the layers. The effect of debonding on the performance of the bilayer plate is more pronounced for the case when polyurea is on the back face. When the debonding algorithm is employed, in some cases, it is observed that the bilayer plate with polyurea on the front face has a superior performance compared to the bilayer plate with polyurea on the back face. In the case that the polyurea is on the front face the delamination initiates from the center and develops to the edge. On the other hand, when polyurea is on the back face the delamination initiates from the edge and developed toward the center resulting in partial or complete debonding. It should be noted that the
delamination criterion used in this study needs to be investigated systematically for more accurate predictions.

5.6. Summary and conclusions

The response of monolithic steel and bilayer steel-polyurea plates in direct ballistic experiments is evaluated using finite-element method. The finite-element simulations model the entire direct ballistic experimental setup detailed in [1]. To model these direct ballistic experiments an explicit version of the commercially available finite-element code, LS-DYNA, is used. User-defined material models are implemented into the software for steel and polyurea. The numerically predicted, spatial variations along the radial line of the three principal stretches of the steel plates for selected monolithic and bilayer plates are compared to their corresponding experimental measurement. The experimentally observed delamination of the steel and polyurea layers is modeled by employing a failure algorithm based on the normal and shear strength of the polyurea-steel interface bond. The numerical predictions of the plate deformation are in excellent correlation with the experimental results for both the monolithic and the bilayer plates. In addition the experimentally measured transferred force to the output bar is compared to the corresponding finite-element prediction. It is found that the finite-element model can predict the four stages of loading (i.e., shock loading, cavitation, post-cavitation
loading and unloading) with great accuracy in pulse shape, its duration and its amplitude.

A set of finite-element simulations are performed to understand the effects of, the relative position of the polyurea layer with respect to the loading direction, the thickness of the polyurea layer, and the polyurea-steel interface bonding strength on the response of the bilayer plates under impulsive loads. The accumulated effective plastic strains of the elements within a 4mm (original) distance from the center of the steel plates are averaged and used to assess the response of the plates.

It is found that the presence of the polyurea layer improves the overall performance of the plate (if it remains bonded to the steel layer). However, the presence of polyurea on the front face aggravates the initial shock loading effect. The explanation for this observation is that, when polyurea is on the front face of a bilayer plate, it would transmit a greater amount of the impact energy to the steel plate due to better impedance matching, in comparison to when it is on the back face of the plate. A simple one-dimensional wave propagation theory is also used to understand the behavior of the initial shock that is transmitted from water to a steel-polyurea bilayer plate. The ratio of the transmitted pressure to incident pressure depends on the ratio of the specific acoustic resistance of the two materials that make the interface. If the assumption of no dissipation of energy in the polyurea is valid then the ratio of
the pressure transmitted to steel to the initial incident pressure in water for the bilayer plate with polyurea on the front face is more than 53% greater than the corresponding value for the bilayer plate with polyurea on the back face. In addition, the viscoelastic damping of the initial shock load transferred to polyurea, results in better performance of the bilayer plate with the polyurea on the back face.

The study also shows that when polyurea is on the front face then the increase of the polyurea layer thickness has two effects: first that it increases the effective tangent modulus of the plate and second that it aggravates the initial shock effect. These two factors influence the overall performance of the bilayer plate in opposite directions and depending on the conditions, the increase of the polyurea layer thickness on the front face might improve or aggravate the ballistic response of the plate. On the other hand, when polyurea is on the back face the increase of polyurea layer thickness improves the overall performance of the plate.

Additional finite-element simulations on the polyurea-steel interface bonding strength reveals that the performance of the bilayer plate is dependent on the strength of the polyurea-steel bond. This dependence is more pronounced when polyurea is on the back face. It should be noted that the delamination criterion used in this study needs to be investigated systematically for more accurate predictions.
5.7. Acknowledgment

Chapter 5 is prepared for publication by M.R. Amini, J. Simon, and S. Nemat-Nasser. This work has been supported by the ONR (MURI) grant N000140210666 to the University of California, San Diego, under Dr. Roshdy G. Barsoum's research program. The dissertation author was the primary investigator of this paper.

5.8. Reference


Chapter Six

Micro-scale analysis of ductile fracture of

monolithic steel and bilayer steel-polyurea plates

under impulsive loads

In this chapter, the ductile fracture of monolithic DH-36 steel and bilayer steel-polyurea plates under impulsive loads is studied experimentally and numerically. The plates are loaded in a direct ballistic experimental setup presented and discussed in [1]. Selected tested samples are analyzed using optical microscope and scanning electron microscope (SEM) to examine the microstructure of the failed samples, and also understand the fracture and necking patterns, and the underpinning mechanisms of failure. The ductile fracture observed herein involves void nucleation, growth and coalescence, causing a dimpled fracture surface. This failure mechanism is observed both by optical microscope and SEM. The examination of the microstructure of the deformed steel samples also reveals that the microstructure does not change significantly and no shear banding is observed at the fracture zone. Finite-
element models are developed based on the micro-scale examinations to further study the fracturing process of the steel plates. For the numerical simulations, we have used physics-based and experimentally-supported temperature- and rate-sensitive constitutive model for steel. The finite-element models are capable of predicting the fracture process of the steel plates. Additional finite-element simulations are performed to investigate the effect of polyurea coating on the fracture resistance of the steel plates.

6.1. Introduction

The ductile fracture of steel structures has been the subject of extensive studies in the past few decades, both experimentally and numerically. DH-36 steel is a high strength structural steel used in naval and other structural applications (i.e. as ship hulls). As a plate structure it may be subjected to underwater impulsive loads. In a previous study [1], the response of monolithic steel and bilayer steel-polyurea plates to impulsive loads is studied focusing on the effect of polyurea on the performance of the steel plates using a direct ballistic experimental technique. To further understand the process of deformation and failure of the steel plates and the effect of polyurea on their performance, a micro-scale study is performed. In this paper, we study the fracture of steel plates under ballistic loads using optical microscopy and scanning electron microscopy (SEM) focusing on the fracture patterns, and the
mechanisms of failure. These experimental studies are paralleled with numerical modeling of the failure of the plates.

Steel exhibits a wide variety of failure mechanisms depending on the environmental conditions, the microstructure of the steel and the loading conditions. Cleavage fracture and ductile fracture are the two main fracture modes observed in failed steel structures. On the other hand, two ductile fracture mechanisms have been identified in high strength steels depending on loading triaxiality. First is void coalescence process in which relatively equiaxed voids grow to impingment under positive triaxiality loading condition. Second is a void-sheet mechanism in which failure occurs by a micro-void shear localization process under shear loading (zero or negative triaxiality).

The nucleation, growth and coalescence of voids under positive triaxiality loading condition have been studied extensively by many researchers [2,3,4]. McClintock [5] performed the first theoretical analysis on void growth by studying the growth of cylindrical holes of elliptical cross-section with axes parallel to the principle directions of the applied stress. Rice and Tracey [6] studied the growth of a spherical void in a general remote field. They found that, for any remote strain rate field, the enlargement of spherical voids is amplified by a factor of an exponential function of the stress triaxiality. McVeigh et al. [7] studied the micro-void shear localization
softening mechanism and modeled the process at the scale of the secondary particles under pure shear loading condition using commercially available finite-element software ABAQUS. A comprehensive review of the past research on ductile fracture of solids is presented by Curran et al. [8] and Garrison and Moody [9].

In a recent study, Nemat-Nasser and Guo [10] studied the microstructure of DH-36 samples under high strain-rate loads over a wide range of temperatures. They found that the microstructural evolution of the material is not very sensitive to changes in strain rates and temperatures.

Figure 6.1. DH-36 steel microstructure perpendicular to the rolling direction obtained by optical microscope at ×400.

Figure 6.2. DH-36 steel microstructure in the rolling direction obtained by optical microscope at ×400.
In this study, the microstructural evolution of impulsively loaded DH-36 steel plates in direct ballistic experiments is checked more closely. The microstructure of several deformed and undeformed samples are examined by optical microscope and scanning electron microscope focusing on understanding the fracture patterns, and the mechanisms of failure. Finite-element models are developed based on the micro-scale examination of the failed samples. The numerical models predict the experimentally-observed fracture patterns. In addition a simulation is performed to study the void growth process. Finally, the effect of polyurea coating on the fracture resistance of the steel plates is studied using finite-element analysis.

6.2. Materials and samples

6.2.1. DH-36 steel microstructure

The chemical composition of high strength low alloy DH-36 structural steel is given in Table 6.1 [10]. This steel is a hypo-eutectoid alloy (0.14 wt % carbon) with ferrite and pearlite as the prime constituents. Using the lever rule it can be shown that volume fraction of pearlite and ferrite are 17.5% and 82.5% respectively. The lack of pearlite makes this alloy a relatively more rust resistant compared to other carbon steel alloys. Figure 6.1 and 6.2 shows a microstructure of an undeformed DH-36 steel plate sample perpendicular to
and along the rolling direction. In these figures, white regions are ferrite and the black regions are pearlite. Ferrite or alpha iron (α-Fe) has a body centered cubic (BCC) crystal and has a lower strength and hardness but higher plasticity and toughness. On the other hand, pearlite, is a two-phase, lamellar structure composed of alternating layers of α-Fe (88 wt %) and cementite or iron carbide (12 wt %) and accounts for the higher strength and hardness and lower plasticity and toughness. DH-36 is microalloyed by grain refining elements such as aluminum and vanadium.

<table>
<thead>
<tr>
<th></th>
<th>C</th>
<th>Mn</th>
<th>Cu</th>
<th>Si</th>
<th>Cr</th>
<th>Mo</th>
<th>V</th>
<th>Ti</th>
<th>Al</th>
<th>Nb</th>
<th>P</th>
<th>S</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>0.14</td>
<td>1.37</td>
<td>0.14</td>
<td>0.22</td>
<td>0.08</td>
<td>0.03</td>
<td>0.001</td>
<td>0.003</td>
<td>0.017</td>
<td>0.03</td>
<td>0.007</td>
<td>0.001</td>
</tr>
</tbody>
</table>

6.2.2. Sample specifications

The plates studied here are EDM machined from a DH-36 steel base plate. Figure 6.3 shows a DH-36 monolithic steel plate with the indicated nominal dimensions. The plates have a nominal thickness of 1mm at the inner portion and a nominal outer diameter of 76mm. All of the plates have a rim on the outside with the nominal inner diameter of 57mm, a nominal outer diameter of 76mm and a nominal thickness of 4.75mm. The plates have a radius of curvature on the edge of the inner section of 1.6mm.
6.3. Experiments and specimen preparation

6.3.1. Experimental setup

The dynamic loading of the plates are performed at CEAM/UCSD’s gas gun facilities using a direct ballistic experimental setup. In this experimental setup, an aluminum projectile of known mass is propelled by a gas gun at a controlled velocity toward an impedance-matched piston that loads a confined soft polyurethane or water medium. The loading medium in turn loads the plate that rests on a cylindrical step (support) within a confining cylinder. The load is transferred from the support to a 3-inch Hopkinson output bar. The velocity of the projectile is recorded by velocity sensors placed at the end of the gas gun muzzle. The detailed description of the experimental setup and the results are presented in [1]. The details of the experimental conditions of the samples used in this study are presented in Table 6.2.
Table 6.2. Conditions of the selected direct ballistic experiments (SP-97 is a bilayer plate and the other two samples are monolithic plates).

<table>
<thead>
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<th>Thickness (mm)</th>
<th>Loading device</th>
<th>Mass (g)</th>
<th>Velocity (m/s)</th>
</tr>
</thead>
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<td>1.02</td>
<td>N/A</td>
<td>Water</td>
<td>1685.4</td>
</tr>
<tr>
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<td>1.00</td>
<td>3.75</td>
<td>Polyurethane</td>
<td>831.8</td>
</tr>
<tr>
<td>S-246</td>
<td>N/A</td>
<td>1.03</td>
<td>N/A</td>
<td>Polyurethane</td>
<td>1685.4</td>
</tr>
</tbody>
</table>

6.3.2. Specimen preparation for optical microscopy

To examine the microstructure of the undeformed and deformed samples, selected samples are sectioned and cut to proper size. The small sectioned pieces are then mounted into an epoxy using a Teflon mold. The mounted samples are then wet grinded, polished and etched. The wet grinding is performed in three steps using 500, 1200 and 4000 grit papers respectively. The specimen is polished next with 3µm and 1µm diameter diamond particles on a nappless cloth to produce a scratch-free mirror finish. To see the grain boundaries and the microstructural constituents of the specimen, it is chemically etched. The etching agent is Nital that is prepared by a mixture of 98ml ethyl alcohol and 2ml H₂NO₃. Figure 6.4 shows the process of preparing a specimen for analysis by cutting, mounting, wet grinding, polishing and etching to reveal the microstructural constituents.
6.4. Micro-scale analysis

6.4.1. Microscopy: failure process

In this section the result of the microscopy of selected tested samples are reported. Two marginally failed samples, SP-97 and S-117X, and a severely failed sample, SP-246, are prepared for microscopy. Figure 6.5 shows the top view of the plates SP-97 and S-117X. Several parallel cuts, along the direction normal to the crack openings are made to study the necking and fracturing patterns.

![Diagram showing the process of preparing a specimen for analysis by cutting, mounting, wet grinding, polishing and etching to reveal the microstructural constituents.](image)

Figure 6.4. The process of preparing a specimen for analysis by cutting, mounting, wet grinding, polishing and etching to reveal the microstructural constituents.
Figure 6.5. Top view of the marginally failed samples, SP-97 and S-117X, selected for microscopy. The samples are cut normal to the crack direction to study their fracture pattern.

It is known that when ductile solids are deformed sufficiently into the plastic regime, a smooth and continuously varying deformation pattern gives way to highly localized deformation in the form of a neck. Visual inspection of the highly deformed steel plates, tested in the direct ballistic experiments, reveals that multiple parallel necks form at the central region of the plates. Figure 6.6 shows the cross section of the mounted steel specimen taken from steel plate SP-97 obtained by SEM.
The photos are taken from a cut at the crack tip in the direction normal to the crack. As the deformation localizes into the necked regions, voids form around some brittle inclusions and second phase particles (e.g., titanium nitrides). The voids form either by cracking of the particles, or by decohesion at the particle/matrix interface [11]. The growths of the primary voids terminate by the formation of a band of localized deformation extending between the voids. Within the band a number of very much smaller voids, micro-voids, are formed. The micro-voids enlarge with the plastic deformation and link up with adjacent micro-voids forming micro-necks. These micro-necks progressively develop leading to a ductile fracture surface with a highly dimpled appearance and final atomic scale rupture of the plate. The same process of failure initiation has been reported by Cox and Low [12] for high strength AISI-4340 steel. Figure 6.7 shows several fractographs of steel plate S-117X dimpled fracture surface obtained by SEM. The diameter of the
primary voids present in Figures 6.7a and 6.7b range from 5µm to 50µm. On the other hand, the diameters of the micro-voids shown in Figure 6.7c are less than 1µm.

![Figure 6.7. Fractographs of the failed sample S-117X obtained by SEM. a and b) primary voids on the fracture surface, c) micro-voids form a highly dimpled appearance](image)

Figure 6.8 shows several views of the dimpled fracture surface of severely failed sample, SP-246, at various magnifications. Figure 6.9 also shows several micrographs of steel plate S-117X at different magnifications obtained by optical microscopy. These micrographs reveal two facts: first that micro-voids are present at the fracture surface and second that no shear band is formed at the region adjacent to the fracture surface. It is also noted that the microstructure of the DH-36 steel plates does not show remarkable changes in the tested samples.
Figure 6.8. Fractographs of the failed sample SP-246 obtained by SEM at several magnifications showing the dimple structure of the fracture surface.

Figure 6.9. Several micrographs of monolithic steel plate S-117X at different magnifications obtained by optical microscopy. a) cross section of the sample showing the two fracture surfaces, b) micro-voids are clear at the fracture surface, c and d) no shear band
6.4.2. Finite-element setup

To understand the failure process of the steel plates under impulsive loads a 3-dimensional finite-element model is developed using commercially available code, LS-DYNA. The model geometry is based on a micrograph of steel plate SP-97 at the necked region. The top part of Figure 6.10 shows the dimensions of the steel plate at the necked region and the bottom part of Figure 6.10b shows the dimensions of the finite-element model and its spatial discretization. The plate is modeled using eight-node brick elements with one integration point; see LS-DYNA theoretical manual [13]. To model the plain strain condition, one element is used in the Z direction and all the nodes are restricted to allow moving only in the X and Y directions. To model the deformation caused by the impulsive loads, the nodes on the left side of the model move with constant velocity of 1.3m/sec in the negative X direction and the nodes on the right side of the model rotate with the constant angular velocity of 872Rad/sec around the Z axes; see Figure 6.10. The imposed deformation conditions are based on the results of the full-scale simulations of the plate in the direct ballistic experiments, reported in [14]. The boundary and initial conditions are imposed on the model as if the plate is loaded from the top, so the top face is the load-receiving face (front face) and the bottom face is the opposite to the impact face (back face).
To model the steel plate, the physics-based (PB) model, proposed by Nemat-Nasser and Guo [10] for DH-36 steel, is implemented into LS-DYNA through a user-defined material subroutine in FORTRAN. This PB model expresses the flow stress (effective stress), $\tau$, as a function of temperature, $T$, effective plastic strain, $\gamma$, and effective plastic strain rate, $\dot{\gamma}$, as

$$
\tau = 70 + 750\gamma^{0.25} + 1500 \left[ 1 - 6.6 \times 10^{-5} T \left( \ln \frac{\dot{\gamma}}{2 \times 10^{10}} \right)^{\frac{1}{2}} \right]^{\frac{3}{2}}, \quad (1)
$$
for $T \leq T_c$, where the stress is in MPa and $T$ is in degrees Kelvin. On the other hand, for $T \geq T_c$, we use

$$\tau = 70 + 750\gamma^{0.25},$$

(2)

where $T_c = \left[-6.6 \times 10^{-5} \left(\ln \frac{\dot{\gamma}}{2 \times 10^{10}}\right)\right]^{-1}$.

In addition, a failure model is implemented into LS-DYNA though a user-defined subroutine. In this failure model the fracture occurs when the accumulated effective plastic strain, $\gamma$, reaches a critical value, $\gamma_f$, by eliminating that element. The purpose of modeling failure is to understand the failure process, so the critical effective plastic strain value is arbitrary in that regards. In this study we set $\gamma_f = 0.6$. 
6.4.3. Numerical predictions

Failure initiation

Figure 6.11 shows the results of the finite-element predictions of the deformation and failure of the necked steel plate. The figure shows the time variation of the effective plastic strain contours of the plat. The numerical model suggests that the failure initiates from the back face at the center of the neck and then propagates to the front face along the fracture line at a $60^\circ$ angle with respect to the plate midplane, until the plate fails completely. The failure is such that the neck on the front face is on one side of the fracture line while
the fracture line passes though the center of the neck on the back face. This prediction is confirmed by examination of the failed samples, SP-97 and S-117X; see Figure 6.12.

To be consistent with the finite-element models, the micrographs are oriented such that the plates are loaded from the top. Figure 6.13 shows the effective plastic strain time variation of two elements along the fracture line, one at the back face and the other on the front face. When the effective plastic strain of the element on the back face is 0.65 at $t=250\mu$s, the effective plastic strain of the element on the front face is 0.4 which is 38% less than the one on the back face. Comparison of the micrographs and finite-element model results reveals that the finite-element model is able to predict the failure process.
Void growth

Ductile fracture nucleation in steel alloys involves decohesion of an embedded particle from the surrounding matrix or particle fracture [11]. In this high strength steel, the embedded particles are generally primary particles (e.g. titanium nitrides) on the order of several micrometers and secondary particles (e.g. carbides and manganese) on the order of several nanometers. A finite-element simulation is performed to study the growth of primary voids in the neck region. Figure 6.14 shows the initial distribution of the voids, the dimensions and the spatial discretization of the finite-element model. The
initial radius of the voids is 15µm. The impulsive load is modeled by applying the same initial and boundary conditions explained in previous section.

Figure 6.14. Spatial discretization of the finite-element model with indicated dimensions and boundary condition.

Figure 6.15 shows the time variation of the effective plastic strain contours of the model. The voids grow as the deformation proceeds until the strain localization bands are formed that in turn promote the nucleation and growth of micro-voids, as also pointed out by Bandstra and Koss [15] and Bandstra et al. [16]. They used finite-element micro-mechanical modeling to show that the strain localization arising between larger voids in HY-100 steel is sufficient to drive the nucleation of micro-voids from secondary particles.
Figure 6.15. A sequence of photos showing the void growth and effective plastic strain contours in the neck region of the plate.

To further understand the void growth in the neck region the radius of the selected voids are plotted as a function of time; see Figure 6.16. This comparison of the time variation of the void radius reveals two facts. First that the voids at the center of the neck have a greater growth rate compared to the voids that are further away from the neck. Second that the voids that are close to the back face have a greater growth rate compared to the voids that are close to the front face. The study of the void growth in the neck model reveals that
the voids at the center of the neck on the \textit{back face} have a greater growth rate compared to the other voids.

**Figure 6.16.** Time variation of the void radius for selected voids.

\textit{Effect of polyurea coating}

Two simulations are performed to study the effect of polyurea coating on the fracture resistance of the steel plates in the neck region and the performances are compared to the monolithic steel plate. In the first simulation a 600µm polyurea layer is present on the \textit{back face} of the steel plate and in the second model a 600µm polyurea layer is present on its \textit{front face}. Figure 6.17 shows the spatial discretization of the three finite-element models: monolithic
steel plate (M), bilayer plate with polyurea on the back face (BB) and bilayer plate with polyurea on the front face (BF). The polyurea layer is modeled using eight-node brick elements with one integration point.

An experimentally-based temperature-, rate-, and pressure-sensitive constitutive model for polyurea, developed and implemented by Amirkhiz et al. [17], have been used to model the polyurea. The monolithic steel plate model has the same dimensions as the one shown in Figure 6.10. In addition, the models use the same initial and boundary conditions explained in previous section. For each model two elements, one on the front face and the other on the back face at the center of the neck region of the steel layer are selected. The time variations of the effective plastic strain of these elements are compared in Figure 6.18 for each configuration (i.e. M, BB, and BF). It is concluded that the effective plastic strain of the elements on the back face of the monolithic plate (M) and bilayer plate with polyurea on the front face (BF) is higher that
the elements on the *front face*, whereas, this is reversed for the bilayer plate with polyurea on the back face (BB). In addition, the results reveal that the presence of polyurea layer either on the *front face* or *back face* retards the neck development. This has been also pointed out by Xue and Hutchinson [18]. Among the three configurations, the bilayer plate with polyurea on the *back face* (i.e. BB) has a superior performance over the other two configurations.

![Graph showing time variation of the effective plastic strain](image)

**Figure 6.18.** Time variation of the effective plastic strain at the center of the neck, on the back face and front face for three plate configurations: monolithic plate (M), bilayer plate with polyurea on the front face (BF) and bilayer plate with polyurea on the back face (BB).

### 6.5. Summary and conclusions

In this chapter the microstructure of impulsively loaded monolithic steel plates and bilayer steel-polyurea plates are studied using numerical and experimental methods. Few plates that are loaded in a direct ballistic
The experimental setup is selected for the purpose of this analysis. Using optical microscope and scanning electron microscope, the microstructure evolution of the failed samples is examined focusing on understanding the fracture and necking patterns, and the underpinning mechanisms of failure. Highly dimpled fracture surface indicates the ductile fracture based on void nucleation, growth and coalescence. The examined samples show no sign of shear banding or significant microstructural changes. Finite-element models are developed based on the micro-scale examination of the failed samples using the commercially available finite-element code LS-DYNA. For the numerical simulations, we have used physics-based and experimentally-supported temperature- and rate-sensitive constitutive model for steel. This constitutive model is implemented into the finite-element code. The finite-element models are capable of predicting the fracturing and void growth. The finite-element also reveals that the failure of the plates initiates at the back face and propagates toward the front face until the sample is failed completely. Moreover, the primary voids that are present near the back face have a significantly greater growth rate compared to the voids near the front face. In addition, a set of simulations are performed to study the effect of polyurea coating on the fracture resistance of the steel plates. The finite-element model reveals that the presence of polyurea layer either on the front face or back face retards the neck development. However, when polyurea is on the back face it provides a local enhancement and retards the failure initiation such that the failure initiates form the front face.
6.6. Acknowledgment

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6.7. Reference


Chapter Seven

Experimental and computational evaluation of compressive response of single and hex-arrayed aluminum tubes

Here we report the results of an experimental and numerical investigation of dynamic and quasi-static compressive response of single and hex-arrayed thick aluminum tubes. The numerical simulations produce results that show excellent correlation with the results of the corresponding experiments. The investigation is aimed at characterizing the energy absorbing capability of tube-based sandwich structures. The energy absorbing characteristics of the sandwich plate material have been previously investigated, using dynamic inertia tests and detailed finite-element simulations [1].

First, the quasi-static buckling behavior of single tubes of 7075 aluminum is studied through a set of compression tests. This aluminum alloy shows excellent ductility and plasticity which make it potentially a good choice
for energy absorbing devices. The experimental results show different buckling
modes, depending on the geometry of the tubes. The finite-element numerical
simulation produces results that correlate well with the test results in
estimating the maximum load level, and the buckling and post buckling
responses of the tubes, thereby providing a reliable tool to predict tube
buckling characteristics.

Second, the dynamic behavior of hex-arrayed 3003 aluminum tubes,
also used in a sandwich structure, is studied in detail. The numerical and
experimental results correlate well with one another, and both show
remarkable increase in the energy absorbing ability of the hex-arrayed tubes,
essentially caused by the post buckling interaction of the neighboring tubes.
From the experiments and finite-element simulations, it is also found that, as
the tube-spacing is decreased, the overall energy absorbed by the system
increases significantly. In addition, the effect of the length and thickness of the
tubes on the buckling behavior of the hex-arrayed tubes is studied numerically
for dynamic loading.

7.1. Introduction

Fundamental to the use of many metal components as energy absorbers
is the ductility which permits large plastic deformations prior to the failure of
the structural system. Plastic deformation and specifically plastic tube buckling
are effective mechanisms by which large amounts of energy can be dissipated. Circular tubes have comparatively high energy absorbing capacity, with a favorable stroke length per unit mass.

Sandwich structures consisting of two plates separated by metal tubes, have been considered as potential candidates to mitigate impulsive (short duration) loads. Some investigations suggest excellent energy absorbing characteristics of tubes under high velocity impact loading conditions [2]. Several methods for improving crashworthiness of the tube-based structures have been examined, including varying the geometry of the cross section. For example, among all the cross sections that have been examined by Mamalis et al. [3] the thick-walled circular tubes exhibited more stable and ideal crash mode. The experimental investigations, e.g., by Batterman [4], Lee [5], Horton et al. [6], Sobel and Newman [7], and Johnson et al. [8], have shown that the relatively thick shells (radius, $R$, to thickness, $t$, less than 50) buckle axisymmetrically, whereas thinner shells buckle in a diamond pattern with a circumferential wave-number dependent on the thickness. Batterman [4], Tennyson and Muggeridege [9] and Lee [5] have found that the effect of initial imperfections is enormous in the case of the diamond-shaped circumferential mode buckling (radius to thickness greater than 100), according to an incremental theory. Gerard [10] reported that, in contrast to the elastic response, the inelastic axisymmetric solution always corresponds to a lower buckling stress than the solution which allows for nonsymmetric
circumferential modes, and that the imperfections are of no significance in either case. Other theoretical investigations based on a \( J_2 \)-deformation theory, conducted by Gellin [11] on thick shells with sinusoidal axisymmetric imperfections, also suggests less sensitivity to geometrical imperfection in the case of plastic buckling, as compared to the elastic buckling. Using a \( J_2 \) corner theory plasticity model, Tvergaard [12] has analyzed the elastic-plastic response of cylindrical shells with initial axisymmetric imperfections, showing that they bifurcate into a nonaxisymmetric shape. He found that, for sufficiently thin-walled shells, the bifurcation occurs before the load maximum; but in the case of the thick-walled shells, the axisymmetric deformations are stable beyond the maximum load, at which localization into a single outward buckled mode takes place. His investigations also show that, localization delays bifurcation considerably, such that a sufficiently thick-walled shell collapses in an axisymmetric mode. Budiansky and Hutchinson [13] have shown that the value of the dynamic buckling load of a cylindrical tube under axial compression is always less than that of the static buckling load of a tube with the same characteristics. Karagiozova et al. [14] have studied the effect of inertia on axisymmetrically buckled cylindrical shells. Weingarten et al. [15] have experimentally shown that the buckling coefficient varies with the radius to thickness ratio.

In this paper, we report the results of our experimental and numerical investigation of the dynamic and quasi-static response of thick \((R/t < 20)\) hex-
arrayed and single aluminum tubes under axial compressive loads. The considered aluminum alloys show good ductility and plasticity at low temperatures and high strain rates, without fracturing [16]. The effects of edge constraint, imperfection, and length of the tubes have not been addressed in this research, since they don’t have significant impact on the plastic buckling and post buckling response of such thick cylindrical tubes, as supported by extensive experimental and numerical investigations of the last 50 years.

The results of two series of experiments that are performed to study the quasi-static and dynamic response of this class of structures are presented. One consists of quasi-static experiments of single tubes, and the other involves dynamic impact experiments of hex-arrayed tubes. An Instron hydraulic testing machine and a 3-inch Hopkinson bar apparatus are used for these experiments, respectively.

First, we study experimentally the energy absorbing characteristics of single 7075 aluminum tubes under a quasi-static loading condition, and parallel this with detailed finite-element simulations. In the next step, the dynamic behavior of hex-arrayed 3003 aluminum tubes in a sandwich structure is studied through dynamic impact (compression) tests, again paralleled with finite-element simulations. The experimental and numerical results show that, when the hex-array tubes are tightly spaced, they maintain their level of load carrying capacity after the initial buckling, due to their interaction effects. In
addition, the effect of length and thickness of the tubes on the energy absorbing characteristics of the structure is studied numerically, keeping the outer diameter of the tubes constant. From these finite-element simulations, it is found that, as the thickness and length of the tubes increase, the absorbed energy per unit mass of the system also increases to a maximum level; however, if the two geometrical parameters are disproportionately increased, a noticeable drop in the dissipated energy per unit mass of the system may result.

Based on these sets of experimental and numerical studies of the dynamic behavior of sandwich structures with tube cores, it is concluded that the finite-element models that use proper experimentally-based material models can reliably predict the overall behavior of complex tube-based sandwich structures, making such numerical modeling a powerful tool for an effective design of energy absorbing structures. In this manner, it is possible to optimize the material, geometry, and the overall configuration of a complex sandwich structure in order to maximize its energy absorption capacity under dynamic and static loads.

7.2. Experimental procedure and results

The design of crashworthy structures, i.e., structures capable of withstanding and mitigating the effect of impact, requires both knowledge of structural dynamics and an understanding of the properties and deformation
mechanisms of the material and the structural components. Aluminum tubes in axial compression provide a particularly efficient energy-absorbing mechanism by their plastic buckling deformation modes. In the present section, the quasi-static compressive behavior of aluminum tubes as a core material for sandwich structures is studied experimentally, paralleled by numerical modeling to better understand their deformation mechanisms. All the tubes examined in this research are thick-walled with radius to thickness ratio, $R/t$, of less than 20. Consequently, the tubes are found to collapse plastically with the buckling mode and buckling load being imperfection insensitive. Moreover, the effect of length to radius of the tube on the buckling stress and buckling mode is not significant above the proportional limit region. This can be best explained by the axisymmetric localization, occurring mainly in thick cylindrical shells, as pointed out by Tvergaard [12]. This localization has also been observed in the experiments presented herein.

The detail of the quasi-static experiments performed on 7075 aluminum tubes and the dynamic experiments performed on the 3003 aluminum hex-arrayed tubes are being discussed in the following sections:

7.2.1 Quasi-static experiments and results

Two quasi-static experiments are performed on two 7075 aluminum tubes with different geometries using an Instron hydraulic testing machine with
a specially designed arbor. The sample, denoted as Tube-I, in the first experiment is a tube that has a radius to thickness ratio of 18, while that, denoted as Tube-II, in the second experiment has a radius to thickness ratio of 6. Based on the theory of cylindrical shell-buckling, both of these samples are considered thick shells ($R/t < 100$) and they both buckle in the plastic regime.

![Figure 7.1. Experimental set-up for quasi-static buckling test of 7075 aluminum tube.](image)

**Tube-I experiment**

As the first experiment, a thick-walled tube of 4.50mm nominal outer diameter, 0.127mm nominal wall thickness ($R/t = 18$), and 11.80mm nominal length is machined from a 7075 aluminum rod. The specimen has a uniform thickness with accurately cut ends. Figure 7.1 shows a photograph of the arbor consisting of two steel bars (of maraging steel), an aluminum tube specimen, and an extensometer. The specimen is sandwiched between the two bars in the
arbor. The experimental setup is such that the tube's ends are fixed against lateral displacement but are free to rotate. The first quasi-static buckling test is conducted at 295K (room temperature) under a displacement-controlled loading condition with a crosshead speed of about $10^{-3}$ mm/s. The axial displacement is measured by extensometer, attached to the arbor and calibrated before testing. Load signals measured from load cell and extensometer signals are collected through data acquisition system, SCXI 1001, using Labview software. These acquired data are analyzed using suitable calibration factors. Simultaneously, photographs are taken by a digital camera with close-up lens during the buckling test. When taking a photograph, at each buckling step, the electrical signals are also used to record the corresponding load-state. Figure 7.2 displays the load-displacement history for a crosshead speed of $6.71 \times 10^{-3}$ mm/s.

![Figure 7.2. Variation of displacement with time for aluminum tube-I; displacement-controlled loading with crosshead speed of $6.71 \times 10^{-3}$ mm/s. and corresponding load history recorded.](image)
Initially, the tube deforms in an axisymmetric pattern near one end. The photographs in Figure 7.3 show this buckling pattern. As the axial load is slowly increased, a short axisymmetric bulge is observed to develop near one end of the tube (step 1). This bulge continues to grow upon further increase in the axial load until the load reaches a maximum value (step 2). The load-displacement curve shows a linear elastic behavior prior to this maximum peak corresponding to the first buckling fold. The load then drops off while the bulge develops into an axisymmetric ring (from step 3 to step 6). The maximum load that the tube can sustain is defined to be the buckling load, with the corresponding mode being the buckling mode. This buckling mode is called a ring mode in the literature.

Figure 7.3. Photographs of aluminum tube-I buckling in uniaxial compression; displacement-controlled loading with crosshead speed 6.71 $10^{-3}$mm/s: numbers indicate the load state in Figure 7.2.
The initially symmetric or concentric deformation close to one end of the tube is then followed by the formation of a non-symmetric or diamond (third harmonic) deformation mode at a load level below the previously reached maximum (buckling) value (steps 9 and 11 in Figure 7.3). We note that the static axial crushing tests by Mamalis and Johnson [17] and Abramowicz and Jones [18] on aluminum circular cylinders also exhibited a similar phenomenon.

![Figure 7.4. Variation of displacement with time for tube-II; displacement-controlled loading with crosshead speed of 5.33 \times 10^{-2} \text{mm/s.} and corresponding load history recorded.]

**Tube-II experiment**
As a second quasi-static tube-buckling experiment, a 7075 aluminum tube with a nominal outer diameter of 4.52mm, nominal thickness of 0.381mm and length of 8.76mm ($R/t = 6$) is considered. The experimental setup is exactly the same as in the first experiment, with similar edge conditions, i.e., fixed against lateral displacement and free to rotate. The total displacement in this test is less than 1mm, and no post buckling folding is observed. Figure 7.4 displays the variation of the load and displacement with time, under a displacement-controlled loading with a crosshead speed of $5.33 \times 10^{-2}$ mm/s.

![Figure 7.5. Schematic cross-sectional and lateral views of a hex-arrayed tube structure](image)

### 7.2.2 Dynamic experiments and results

The hex-arrayed tube-structure consists of two square plates and seven 3003 aluminum tubes, each of a nominal outer diameter of 15.87mm, nominal
wall thickness of 1.65mm, and nominal height of 12.7mm ($R/t = 5$). Figure 7.5 shows the cross-sectional and lateral views of a typical sample. Each sample is individually fabricated, and hence it may have slight geometric and dimensional imperfections.

![Diagram of dynamic compression test](image)

**Figure 7.6. Schematic view of the dynamic compression test of hex-arrayed tubes and Hopkinson bar equipment set-up.**

The dynamic tests are performed using a single 3-inch Hopkinson bar setup, shown in Figures 7.6, 7.7 and 7.8. In this experiment, a hex-arrayed sandwich structure is impacted by a striker bar at 32m/s. The deformation process is documented using an Imacon 200 high-speed camera. Just before impact, the velocity of the striker bar is measured by the velocity sensors near the end of the gas gun. The force transmitted through the structure is measured by a strain gauge attached to the 3-inch output bar. The striker bar is a 3inch...
(7.62 cm) diameter, 4.5 inch (11.43 cm) long, 7075 aluminum bar, weighing 1,460 g. Below, we report our experimental results for three such tests, denoted as Test-I, Test-II, and Test-III, corresponding to tube spacing of less than 0.1 inch (2.5 mm), 0.2 inch (5 mm), and 0.5 inch (12.5 mm), respectively. Figure 7.9 shows the top view of two hex-arrayed structures of different tube spacing.

![Image](image_url)

**Figure 7.7. The Photo of hex-arrayed structure attached to the output Hopkinson bar.**

Figure 7.10 shows the deformed configuration of the Test-II specimen, captured by the high-speed camera at 40 μs time intervals. In Figure 7.11 the vertical lines mark the camera timing, and the curve presents the history of the compressive load transmitted through the sandwich structure and recorded by the strain gauge attached to the output bar.
The load-displacement curves of 3 tests are presented in Figure 7.12. In this figure the load is normalized by the mass of the corresponding structure in order to properly compare arrayed tube-structures of different length, thickness, and outer diameter. From the load-displacement curves of Figure
7.12, it can be concluded that the transmitted post buckling load decreases as the tube spacing is increased. The response of the two structures with greater tube spacing (0.2inch and 0.5inch) follows the classic tube buckling pattern with the load dropping off after reaching a peak value with some slight post buckling interaction in the case of the 0.2inch spaced tubes. On the other hand, because of the interaction between adjacent tubes in the case of the tightly-packed structure (0.1inch tube spacing), the post buckling plastic deformation of the tubes is interrelated and more complex, having a slightly larger buckling load which is essentially maintained over the entire deformation process, and even is increased at large displacements.

Figure 7.10. The deformed shape of the test II specimen captured by an Imacon-200 high-speed camera at 40 sec time intervals.
7.3. Material models

We present the details of the material constitutive models used in this study. For 7075 aluminum tubes, we use the Johnson-Cook material model that is already provided by the LS-DYNA, using the material parameters that have been experimentally established by Rule and Jones [19], for this specific metal. In this model the flow stress, $\tau$, is expressed as,

$$\tau = (A + B\dot{\gamma}^n)(1 + C\ln\dot{\varepsilon}^\ast)(1 - T^\ast),$$

(3.1)

where $\dot{\varepsilon}^\ast = \dot{\gamma} / \dot{\gamma}_0$ is the dimensionless strain rate, ($\dot{\gamma}_0$ is normally taken to be 1.0/s), the parameters $A$, $B$ and $C$, are material constants, $\gamma$ is the effective plastic strain, and $T^\ast$ is a normalized temperature defined by,
where \( T_m = 877K \) is the melting temperature of the material. \( T \) is the material's actual temperature, and \( T_r \) is a reference temperature which must be less than the lowest temperature of interest; we have used \( T_r = 298K \). The final expression for the Johnson-Cook model for 7075 aluminum now is (in MPa),

\[
\tau = (452 + 457 \gamma^{0.357})(1 + 0.01 \ln \dot{\varepsilon}^*)(1 - T^{*1.1}),
\]

(3.3)

where,

\[
T^* = \frac{T - 298}{579}.
\]

(3.4)

Figure 7.12. Illustration of normalized load-displacement experimental relations for tests I, test II and test III; Load is normalized by the mass of the system. Tubes are of dimensions: OD=15.87mm; Wall thickness=1.65mm; and Length=12.7mm.
To model the 3003 aluminum tubes, we have implemented a user-defined physics-based material model [16] into LS-DYNA. This is an elastic-viscoplastic constitutive model developed using the kinetics and kinematics of dislocation motion; see Nemat-Nasser [20] for details. The resulting macroscopic constitutive parameters are evaluated using a series of quasi-static and dynamic experiments over a broad range of temperatures and strain rates. The model is thus reliable and can be used for computational simulation of high strain-rate deformations involved for example in blast-induced plastic deformations and shear banding. In this model the flow stress, $\tau$, is expressed as a function of temperature, $T$, effective plastic strain, $\gamma$, and effective plastic strain rate, $\dot{\gamma}$, as,

$$
\tau = c_0 + c_1\gamma^n + \tau^0 \left\{ 1 - \left[ \frac{-kT}{G_0} \left( \ln \frac{\dot{\gamma}}{\gamma_0} + \ln f(\gamma, T) \right) \right]^{\frac{1}{p}} \right\} f(\gamma, T), \text{ for } T \leq T_c,
$$

where,

$$
f(\gamma, T) = 1 + a \left[ 1 - \left( \frac{T}{T_m} \right) \gamma_m \right], \text{ and } T_c = \frac{-G_0}{k} \left( \ln \frac{\dot{\gamma}}{\gamma_0} + \ln f(\gamma, T) \right)^{-1}, \quad (3.5)
$$

and

$$
\tau = c_0 + c_1\gamma^n, \text{ for } T \geq T_c, \quad (3.6)
$$

where $G_0$ is the total energy of the short-range barrier to the motion of dislocations, measured per atom, $k$ is the Boltzmann constant, $\dot{\gamma}_0$ is a reference strain rate, $T_m$ is the melting temperature, and $c_0$, $c_1$, $\tau^0$, $n$, $m$, $p$, $q$
and $a$ are material parameters. The details of the parameters used in this physics–based model are reported by Nemat-Nasser [20]. The final constitutive relation (denoted as the PB-model, for physics-based) for this material is, for $T \leq T_c$,

$$
\tau = 64\gamma^{0.4} + 72\left[1 - \left(-3.2 \times 10^{-5} T \left(\ln \frac{\dot{\gamma}}{2 \times 10^{10}} + \ln f(\gamma, T)\right)\right)^{\frac{3}{2}}\right] f(\gamma, T)
$$

where $f(\gamma, T) = 1 + 6\left[1 - \left(\frac{T}{916}\right)^2\right]^{0.05}$, \hspace{1cm} (3.7)

where the stress is in MPa and $T$ is in degrees Kelvin. On the other hand, for $T \geq T_c$,

$$
\tau = 64\gamma^{0.4},
$$

where $T_c = \left[-3.2 \times 10^{-5} \left(\ln \frac{\dot{\gamma}}{2 \times 10^{10}} + \ln f(\gamma, T)\right)\right]^{-1}$. \hspace{1cm} (3.8)
Figure 7.13. Comparison of PB-model (physical-based model) predictions with experimental results at a strain rate of 0.0001/s and indicated initial temperatures obtained by Nemat-Nasser [16].

Figures 7.13 and 7.14 compare the experimental results with the PB-model predictions at strain rates of $10^{-3}$/s and 9,500/s, for indicated initial temperatures. As is seen, good correlation between the experimental data and model predictions is obtained.
7.4. Numerical simulation procedures

We now report the results of our numerical analysis of the nonlinear dynamic and static deformation and buckling of single and hex-arrayed tubes, using the finite-element LS-DYNA 970 code that incorporates experimentally-based nonlinear material models, including strain-rate and temperature effects. The code is based on an updated Lagrangian formulation; it is fully vectorized, and can be used to analyze the transient dynamic response of solids and structures [21].

Figure 7.14. Comparison of PB-model (physical-based model) predictions with experimental results at a strain rate of 9500/s and indicated initial temperatures obtained by Nemat-Nasser [16].
7.4.1 Finite-element model

The finite-element model of hex-arrayed and single tubes is performed, using 4-node Belytschko-Tasy shell elements with two Gauss integration points through the thickness and an hourglass viscosity to damp out the zero-energy modes. The mesh density is optimized in such a way that the geometrical distortional nature of the buckling and collapse of the structure can be captured in a smooth manner with reasonable computational cost. The contact between the aluminum tubes is modeled using the code’s automatic single-surface contact option. The sandwich plates are modeled using rigid walls and the nodes of the tubes are considered as slave nodes. Fixed boundary conditions are assumed at both ends of all the tubes, since, in our experiments, the tubes are welded into the top and bottom plates of the sandwich structure.

7.4.2 Numerical integration of plasticity model

To apply the constitutive models presented in section 3 for the three-dimensional calculations, the J2-plasticity model is integrated using a radial return algorithm. In this semi-implicit backward Euler method, the increments in the effective plastic strain and temperature are calculated at the end of the time step and the flow-stress condition is then enforced. In this algorithm, given the total strain tensor, \( \varepsilon_n \), plastic strain tensor, \( \varepsilon_n^p \), effective plastic strain \( \gamma_n \), stress tensor, \( \sigma_n \), at time \( n \), and the total strain-increment tensor, \( \Delta \varepsilon = \dot{\varepsilon} \Delta t \), the code computes the quantities (\( \varepsilon_{n+1}^p, \varepsilon_{n+1}^p, \gamma_{n+1}, \sigma_{n+1} \)), as follows:
1- The trial stress, denoted here by $\sigma^{(0)}$, is computed by the elastic predictor using,

$$\sigma^{(0)} = \sigma_n + 2G\Delta\varepsilon' + K \text{trace}(\Delta\varepsilon'),$$

where $\Delta\varepsilon'$ is the deviatoric part of total strain-increment tensor, $G$ is the shear modulus, and $K$ is the bulk modulus.

2- The effective plastic strain rate, $\dot{\gamma}_{n+1}$, is computed assuming the strain increment is all plastic. This is a reasonable approximation since the elastic part of the strain increment is negligibly small.

3- The effective trial stress, $\tilde{\sigma}^{(0)}$, is computed and compared with the current flow stress, $\tau(\gamma_n, \dot{\gamma}_{n+1}, T_n)$ (discussed in section 3). If the trial state is elastic then the effective plastic strain, plastic strain-rate, and temperature are not updated. The trial stress is used as the stress at the end of the time step. However, if the effective trial stress exceeds the flow stress, then the radial return algorithm for plastic loading is applied in four steps:

3.1- Initialization:

$$k = 0, \varepsilon^{(0)} = \varepsilon^p, \dot{\gamma}^{(0)} = \gamma_n, \Delta\gamma^{(0)} = 0,$$

where $k$ is the iteration counter and superscripts correspond to the iteration number.
3.2- Check flow stress at the $k^{th}$ iteration:

$$f^{(k)} = \bar{\sigma}^{(k)} - \tau\left(\gamma^{(k)}, \dot{\gamma}_{n+1}, T_n\right).$$

If: $f^{(k)} < TOL$ (arbitrary small number, in this case $10^{-5}$) then the iteration has converged and the temperature is updated, otherwise the process is continued.

3.3- Compute the increment in the effective plastic strain:

$$\delta\gamma = \frac{\left(\bar{\sigma}^{(0)} - 3G\Delta\gamma - \tau\left(\gamma^{(k)}, \dot{\gamma}_{n+1}, T_n\right)\right) - \Delta\gamma^{(k+1)}}{3G + \frac{\partial\tau}{\partial\gamma}\left(\gamma^{(k)}, \dot{\gamma}_{n+1}, T_n\right)} = \Delta\gamma^{(k)} + \delta\gamma.$$

3.4- Update the plastic strain tensor, effective plastic strain and total stress, set $k \leftarrow k+1$ and go to step 3.2.

7.5. Numerical simulation of single-tube buckling

In this section, the results of the finite-element simulation of the quasi-static tube buckling are presented and compared with the experimental results. Both the buckling load and mode are compared. Attention is focused on the post buckling behavior. Some relevant published analytical results are also discussed.
In general, the finite-element simulations are in excellent agreement with the experimental results, both for the buckling load and the buckling mode. On the other hand, the analytical predictions that we have examined do not seem to yield accurate estimates of the buckling load, nor do they predict the post buckling behavior that is observed.
7.5.1 Computational results

The simulation of the first thick 7075 aluminum tube, Tube-I, predicts that the tube initially buckles at one end in an axisymmetric mode, and then the crushing continues while the buckling mode is changing from an axisymmetric bulge into a triangular shape. This change of the mode during the post buckling process is due to the change of the boundary condition for the remaining unbuckled portion of the cylinder. The simulated buckling mode and its changes during the course of loading are in relatively good agreement with the experimental results; see Figures 7.15 and 7.16. The displacement boundary conditions discussed for the experiments in Figure 7.2, are applied for the simulation. The simulation predicts the buckling load to be 910N, whereas the
experimental buckling load is 1051N resulting in 13.4% computational error. Three post buckling peaks are detected in the simulation. They agree with the experimental results both in the number of peaks and their corresponding approximate values.

![Diagram of axisymmetric axial buckling collapse mechanism](image)

**Figure 7.17. Axisymmetric axial buckling collapse mechanism due to Alexander [22].**

Abramowicz and Jones (1985) have provided a comprehensive review and substantial data on the axisymmetric buckling modes of cylindrical shells. Their analysis is based on a work done by Alexander (1960); the essence of their assumed mechanism is illustrated in Figure 7.17. The buckling process is assumed to take place in a section of length $2H$ and to consist of a set of three stationary (relative to material) plastic hinges separating two outward moving portions which undergo circumferential stretching. The mean value of the crushing force, $P_\alpha$, is determined in terms of the material properties and
geometry of the tube by minimization, invoking a global minimum work hypothesis that leads to the following expression:

$$\frac{4P_B}{\sigma_0 t^2} = 20.79 \left( \frac{D}{t} \right)^{\frac{\sqrt{2}}{2}} + 11.90,$$

(5.1)

where $\sigma_0$ is the yield stress of the material, and $t$ is the thickness and $D$ is the outer diameter of the tube. For our experiment described above, (5.1) gives $P_B = 225.5\text{N}$, while the experimental mean value of the crushing force is approximately $452\text{N}$. This significant underestimation is fairly typical of the predictions of this analysis and is described by Abramowicz and Jones [22] to be due to the assumption that the convolutions flatten into discs. This assumption is inconsistent with our experimental observations and numerical predictions. Ignoring the plastic hardening can be considered another reason for this underestimation.

![Figure 7.18. Sequence of plots from deformation and buckling of the thick 7075 aluminum tube finite-element model in uniaxial compression; displacement-controlled loading with crosshead speed 5.33 \times 10^{-2}\text{mm/s}, presented in Figure 7.4.](image)
For the second aluminum tube, Tube-II, as shown in Figures 7.18 and 7.19, the numerical model of the tube buckling provides a reasonable representation of the experimental results, both for the buckling load and the buckling mode. The simulation predicts the buckling load to be around 2,600N, whereas, the experiment shows a buckling load of 2,950N, resulting in an 11.8% computational error. Figure 7.19 shows the comparison of the experimental and numerical load-displacement curves. The simulation predicts the buckling to start at the end of the tube and when the displacement reaches a specific level; see Figure 7.18. The simulation predicts a stable post buckling response that agrees with the experimental observations.

Figure 7.19. Comparison of experimental and numerical load variation with displacement of the cross head for 7075 aluminum tube, tube-II, of dimensions: OD=4.52mm; Wall thickness=0.381mm; and Length=8.76mm.
Two important observations about the characteristics of the buckling mode are apparent from an examination of the foregoing numerical and experimental results. First, the buckling mode is axisymmetric, and, second, the buckling is initiated near the ends of the tubes. These observations are explained as follows.

![Figure 7.20. The numerical results of normalized load per mass with displacement variation of the sandwich plates for the hex-arrayed 3003 aluminum tubes compared at three different spacing levels, using Nemat-Nasser’s physics-based (PB) material model. Tubes are of dimensions: OD=15.9mm; Wall thickness=1.65mm; and Length=13.0mm.](image)

Axisymmetric buckling of the tube consists of only stretching in the hoop direction, whereas non-axisymmetric buckling would primarily involve bending in the hoop direction. The energy required for each mode, hoop direction stretching or hoop direction bending, is a function of the thickness of
the tube. As the thickness of the tube increases the energy required for the hoop direction stretching increases linearly. On the other hand the bending in the hoop direction has a quadratic relationship with the thickness. Accordingly, the hoop bending energy increases at a faster rate with the increase in thickness. For sufficiently thick tubes (roughly $R/t < 40$), the hoop bending energy exceeds the hoop membrane energy. Hence, for such thick tubes the buckling occurs in the lower energy axisymmetric mode [23].

The initiation of the buckling at the ends can be explained by the end boundary conditions and axisymmetric buckling localization theory [24]. If the tubes were not constrained at the ends, then as the load increases the axial shortening is accompanied by a radial expansion that is uniform along the axial direction (Poisson effect). However, the tubes are constrained at both ends due to friction. This additional constraint in the radial direction causes an axisymmetric shear stress at the end of the tubes in the radial direction that decays rapidly with distance from boundary. This additional shear stress triggers the tendency of axisymmetric buckling localization, and results in the initiation of the buckling at the boundaries. This behavior is observed in the quasi-static experiments and predicted by the finite-element simulations.
7.6. Numerical simulation of hex-arrayed tube-buckling

To enhance energy absorption while keeping the weight of the system as low as possible, one may exploit the post buckling interaction of adjacent tubes to increase the energy absorption capacity of the hex-arrayed structure. As in the case of a single tube, hex-arrayed tubes initially have a linear response under a compressive load. However, once the buckling starts, the buckled tubes interact with one another, enhancing the energy absorption of the structure.

The response of hex-arrayed tubes to a compressive load is influenced by the spacing between the members. Here, the experimental and numerical results of dynamic compression tests of hex-arrayed tubes are presented and the effect of the tube spacing, length, and thickness on the mechanical response of the structure is investigated for a fixed outer diameter.

Figure 7.21. Final finite-element configuration of the hex-arrayed 3003 aluminum tubes at maximum deformation; spacing equal to 0.2 inch, using Nemat-Nasser’s physics-based material model.
7.6.1 Computational results

Each tube has a nominal outer diameter of 15.87mm, nominal wall thickness of 1.65mm, and nominal height of 13.0mm \((R/t=5)\). All the simulations are performed with the same setup, using an impact speed of 32m/s and also with the same fixed boundary conditions representing the welding of the tubes to the plates. The load histories of the three simulations are compared in Figure 7.20. The simulation predicts a buckling load per unit mass of approximately 3KN/g for all three different tube spacings. The experimental results show a lower buckling load per unit mass (2.6KN) for all three cases. Although the numerical models qualitatively predict the experimental trends, the reason for quantitative discrepancy between the experimental and numerical results is not fully understood. This discrepancy may be due to introduction of the fabrication technique that has introduced various imperfections in the samples [25], while the simulation assumes perfectly arranged hex-array structures.
The finite-element model predicts that the structure with a tight spacing (< 0.1 inch) maintains the load level due to the post-buckling interaction among the tubes. The corresponding experimental results show the same effect. On the other hand, the load histories of the structures with tube spacings of 0.2 inch and 0.5 inch are almost the same with minor post-buckling tube interaction in the case of 0.2 inch tube spacing. For these cases, the post-buckling loads obtained from the finite-element simulations are less than those for the closely spaced tubes (< 0.1 inch), which is in good agreement with the experimental results; see Figure 7.19. The overall behavior of the hex-arrayed tube structure with different spacing levels and the effect of the tube spacing on the energy absorption of the structure are predicted reasonably well by the finite-element simulations. The predicted final deformed configuration of the hex-arrayed
tubes with 0.2inch tube spacing is presented in Figure 7.21, and the corresponding deformation sequences are given in Figure 7.22.

![Graph showing load normalized by mass variation with displacement for different thicknesses.]

Figure 7.23. Effect of thickness on the normalized load per unit mass variation with displacement of the sandwich plates for tubes spacing less than 0.1inch. The length of the tubes is 1.3cm and the outer diameter is 1.59cm.

7.6.2 Parametric study of hex-arrayed tube-buckling

We have also used computational simulations to investigate the effect of thickness and length of the tubes on their response, keeping the outer diameter equal to 1.59cm. The dissipated energy and the applied load are normalized by the mass of the system for comparison purposes. All simulations are performed with the same fixed boundary conditions, using 32m/s impact velocity. The aluminum 3003 material is considered using Nemat-Nasser’s physics-based model, presented in Section 2.
To analyze the tube thickness effect, five different thicknesses, 0.851, 1.051, 1.251, 1.451, and 1.651 mm are simulated for the same three tube spacings levels. The nominal length of the tubes is 1.30 cm and their nominal outer diameter is 1.59 cm. The simulation results are presented in Figures 7.23, 7.24, and 7.25, for indicated cases.
The increase in thickness from 1.051 to 1.451mm causes an increase in the load per unit mass of the system for all three different tube spacings. However, increasing the thickness from 1.451 to 1.651mm does not seem to cause a significant drop in the buckling load per unit mass, nor for the normalized dissipated energy per unit mass (maximum dissipated energy per unit mass obtained at the compact spacing for 1.451mm tube thickness is set to one for the comparison purposes); see Figures 7.26 and 7.27.
A second set of simulations is conducted to investigate the effect of tube length on the energy absorption of the structure. The tubes have 1.59cm
outer diameter and 1.451mm thickness, and spacing of less than 0.1inch. Different lengths, varying from 0.32cm to 2.60cm, are used. The numerical results shows that, as the ratio of the length to the outer diameter increases, the buckling mode changes from a single barrel in the middle to a double barrel at the ends. This transition from single barrel to double barrel buckling mode happens when the length is changed from $L=1.3\text{cm}$ (single barrel) to $L=1.95\text{cm}$ (double barrel). Figure 7.28 illustrates the variation of the normalized load per unit mass of the structure versus the deflection per unit length of the tubes. As the common length of the tubes increases, starting from 0.325cm, the absorbed energy (per unit mass per unit length) increases and reaches a maximum at 1.3cm; however, increasing the length further does seem to result in an increase in the absorbed energy (per unit mass, per unit length), as is illustrated in Figure 7.29, where the squares are the computational data points. Among the simulations conducted in this study, the hex-arrayed tube structure with an outer diameter to thickness ($OD/t$) and outer diameter to length ($OD/L$) ratios of approximately 11 and 1.22, respectively, showed the best energy-absorption performance.
Figure 7.28. Variation of normalized load per unit mass of the hex-arrayed tube structure with deflection of unit length for tubes of different lengths and compact spacing (<0.1inch). The outer diameter of the 3003 aluminum tubes is 1.59cm.

Figure 7.29. Variation of energy absorbed per unit mass and length of the hex-arrayed aluminum tube structure with different length to outer diameter ratios. The Spacing is compacts (<0.1inch) and the thickness is 0.145cm.
7.7. Conclusions

Experiments are performed to investigate the quasi-static buckling of single and dynamic buckling of hex-arrayed aluminum tubes. The experimental results are compared with the corresponding finite-element simulation results. Excellent agreement is obtained for the buckling load, buckling mode, and the post buckling behavior. The finite-element calculations incorporate rate- and temperature-sensitive physics-based constitutive models for aluminum 3003 that have been integrated into LS-DYNA using a $J_2$-plasticity model and a radial-return algorithm.

The single tube experiments and simulations have confirmed that the buckling mode is dependent on the geometrical ratios and the tube thickness, being the deformed configuration corresponding to the minimum energy level. The single tube initially deformed into an axisymmetric pattern near one end, once the buckling load was reached. This then was followed by the formation of a diamond deformation mode at a load level significantly less than the buckling load.

The experimental and numerical investigations of the hex-arrayed tube structures suggest that their energy absorption increases with a decrease in their spacing due to post buckling interaction between the adjacent tubes. Finally, the effect of the geometrical parameters of the hex-arrayed tubes on
their energy absorption is examined by a few illustrative numerical simulations.

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7.9. References


