Numerical modeling of effect of polyurea on response of steel plates to impulsive loads in direct pressure-pulse experiments

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Abstract

Results of computational modeling and simulation of the response of monolithic DH-36 steel plates and bilayer steel-polyurea plates to impulsive loads in direct pressure-pulse experiments (Amini et al., in press-b), are presented and discussed. The corresponding experiments and their results are presented in an accompanying paper (Amini et al., 2010). 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 (2003b) and an experimentally supported temperature-, rate-, and pressure-sensitive constitutive model for polyurea, developed and incorporated into the computer code, LS-DYNA, by Amirkhizi et al. (2006), have been implemented.

The transient response of the plates under impulsive pressure loads is 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 reported by Amini et al. (2010).

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1. Introduction

It has been shown by direct pressure-pulse experiments 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 (Amini et al., this issue). Here we provide numerical simulations of those experiments, using the finite-element code, LS-DYNA. The aim is to understand the transient response and the underpinning mechanisms of deformation and failure of the plates under pressure-pulse loading conditions.

A comprehensive review of various theoretical and numerical studies of the deformation of thin metal plates subjected to impulsive loads is given by Nurick and Martin (1989) and also Jones (1989). Zhu (1996) 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 (2005a,b) 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 (2005) 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. (2006) have studied the effect of interlayer elastomeric polyurea on the dynamic response of sandwich plates under dynamic loads. It has been shown by reverse impact and direct pressure-pulse experiments 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 (Amini et al. 2010a,b).

There are few numerical studies on the effect of polyurea coating on the performance of steel plates. Amini
et al. (2010a) numerically studied the deformation and failure of monolithic steel and bilayer steel-polyurea plates under reverse impulsive loads. In these experiments, a hollow cylindrical projectile carries the plate and impacts on a soft-polyurethane layer resting on a steel plate within a confining steel cylinder. Because of the nature of the experimental setup, the plate failure cannot be photographed. Also, for the same reason, water cannot be used as the loading medium.

Comparing the numerical prediction with the reverse impact experimental results, they found that the presence of polyurea coating on the impact face (front face) can aggravate the shock loading and enhance fracturing of the steel plates, but it can mitigate failure if the polyurea is cast on the face opposite to the impact side (back face). 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 when it is cast on the front face. On the other hand, when polyurea is cast onto the 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 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 and the polyurea coating remains bonded to the steel. Xue and Hutchinson (2007) studied the neck retardation under biaxial stretching of bilayer elastomer-metal plates. They show that a substantial increase in the necking limit and the consequent energy absorption can be achieved in metal-elastomer bilayers compared to monolithic steel plates of the same area density.

In the present paper, we perform a full-scale finite-element modeling of the direct pressure-pulse impact experiments reported by Amini et al. (2010). The significance of these finite-element models is that they allow: (1) to study the transient response of the plate; (2) to incorporate complex temperature-, pressure-, and rate-dependent constitutive models in the finite-element code; and (3) to conduct parametric studies to gain insight into the dynamic response of the plates. The effect of the relative position of the polyurea layer with respect to the loading direction, the effect of the thickness of the polyurea layer, and finally, the effect of the polyurea-steel interface bonding strength on the dynamic deformation and fracturing of the steel plates are also studied numerically.

2. Finite-element modeling procedure

In this section, the direct pressure-pulse experimental setup is briefly mentioned and the corresponding finite-element model is detailed.

2.1. Direct impact-induced pressure-pulse experimental setup

In the direct impact-induced pressure-pulse experiments (Amini et al., 2010), performed at UCSD/CEAM’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 or 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-in. 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 the accompanying paper (Amini et al., 2010).

2.2. Finite-element model

The direct impact-induced pressure-pulse experiments are modeled in full-scale, using the commercially available finite-element code, LS-DYNA. Two views of the three-dimensional model used in the finite-element simulation are shown in Fig. 1. The simulation begins at the stage when the aluminum projectile of a uniform initial velocity, \( V_0 \), impacts a cylinder that loads a medium (polyurethane or water) which rests against the sample. The interface between the projectile and the cylinder is modeled as frictionless contact. The projectile mass is 832 g or 1682 g 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 only for sliding in the longitudinal \( (Z) \) direction. The loading medium in turn loads the sample 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 or polyurethane) is modeled with one-point ALE multi-material brick elements and the remaining components are modeled using a Lagrangian formulation. The simulation uses the contact algorithm option of LS-DYNA, entitled CON-STRAINED_LAGRANGE_IN_SOLID to model the interactions of the Lagrangian and Eulerian domains; see Hallquist (1998) for more details. The CONTACT AUTOMATIC SURFACE TO SURFACE contact algorithm of LS-DYNA is employed to model the interaction between the plate and the cylindrical support. In the modeling, all steel plates have a diameter \( D_{st} = 76.00 \) mm, gauge section thickness \( t = 1.02 \) mm, rim thickness \( t_{rim} = 4.79 \) mm, and rim width \( w_{rim} = 9.52 \) mm; see Fig. 2. 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. Fig. 3 presents the spatial discretization of the three sample 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, the outer portion and the central portion, so the...
stretch enhancement in the central region of the plates would 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.

3. Material constitutive models

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

3.1. DH-36 steel

The physics-based (PB) model, proposed by Nemat-Nasser and Guo (2003b) 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

\[
\tau = 70 + 750 \gamma^{0.25} + 1500 \left[ 1 - \left( -6.6 \times 10^{-5} T \left( \ln \frac{\dot{\gamma}}{2 \times 10^{16}} \right) \right)^{\frac{1}{2}} \right],
\]

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

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

where \( T_c = \left( -6.6 \times 10^{-5} \left( \ln \frac{\dot{\gamma}}{2 \times 10^{16}} \right) \right)^{-1} \).
Fig. 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.

3.2. Polyurea

An experimentally based viscoelastic constitutive model, including pressure and temperature sensitivity, introduced and implemented into LS-DYNA by Amirkhizi et al.
(2006), has been employed for modeling the polyurea. For bulk deformations, this model assumes that the trace of the Cauchy stress tensor, \( \sigma \), is given by,

\[
\text{tr}(\sigma) = 3k \ln J,
\]

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

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

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

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

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

\[
\zeta(t) = \int_0^t \frac{dt}{a_t(\tau)},
\]

\[
a_t = 10^\frac{\Delta T - C_{\text{ref}} p - T_{\text{ref}}}{C_{\text{ref}}},
\]

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

In this study the four-term (\( n = 4 \)) Prony series option of the Amirkhizi et al. (2006) code is used to define the relaxation function,

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

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,

\[
\frac{\partial W_d}{\partial t} = G_\infty \frac{T(t)}{T_{\text{ref}}} \sum_{i=0}^{n} p_i \varepsilon_d'(t) \cdot \varepsilon_d(t).
\]

where,

\[
\varepsilon_d'(t) = \int_0^t e^{-(\zeta(t) - \zeta(\tau))/q_i} D'(\tau)d\tau.
\]

The numerical values of the parameters used in this model are listed in Table 1.

### 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,

\[
\dot{p} = -K_e i_{\text{eq}},
\]

where \( K = 2202 \text{ MPa} \) is the bulk modulus and \( i_{\text{eq}} \) are the strain rate components. A tensor viscosity is used which acts only on the deviatoric stress, \( \sigma'_{\text{eq}} \), given in terms of the damping coefficient as:

\[
\sigma'_{\text{eq}} = VC \cdot \Delta L \cdot a_p \dot{i}_{\text{eq}},
\]

where \( VC = 0.1 \) is the viscosity coefficient, \( \Delta L \) is a characteristic element length calculated by the software, \( a \) is the fluid bulk sound speed, \( \rho = 1.00 \text{ g/cm}^3 \) is the density, and \( \dot{i}_{\text{eq}} \) 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 \( \rho = 1.19 \text{ g/cm}^3 \); for more details, see LS-DYNA theoretical manual (Hallquist, 1998).

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

#### 4.1. Calculating the principal stretches

To validate the finite-element models, the variation of the three principal stretches along the radial line for several 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 a number of trapezoidal elements. Since the plastic deformation of steel can be considered nearly isochoric, the volume of the ring, generated by the rotation of trapezoidal 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 farther element can be obtained. The deformation gradient of each element can then be calculated; see Amini et al. (2010a) 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 functions 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.

4.2. Validation of the monolithic steel plate model

The experiment S-57 is used to validate the finite-element model of the monolithic steel plate. The details of this experiment are presented in Amini et al. (2010). The parameters, used for the finite-element simulation are provided in Table 2. Fig. 5 shows the finite-element predictions of the three principal stretches and thickness profiles, and compares these with the corresponding experimental results, revealing that the finite-element model could be considered capable of predicting the dynamic response and deformation of monolithic steel plates. Fig. 6 shows the predicted sequence of deformed configurations of the S-57 sample at 40 μs time intervals. The results show that the deformation initiates at the rim while the center part of the plate is undeformed, and 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 predicted deformation process is experimentally verified using high-speed photography.

4.3. Validation of bilayer steel-polyurea plate model

A bilayer steel-polyurea plate, SP-130, 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 that includes a debonding algorithm. The parameters, used for the finite-element simulation are provided in Table 2. The predicted principal stretches and thickness profiles for the model with a perfect steel-polyurea bonding are presented in Figs. 7 and 8, and the corresponding predicted deformation sequence is given. Comparison of the predicted principal stretch profiles with the experimental measurements reveals that the finite-element model under-predicts the stretching of the plate. This 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 Hallquist (1998) for details. In this model, it is assumed that the interface debonding is governed by the following failure criterion:

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

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 a final simulation, the $SFLS$ and $NFLS$ are taken to be 100 MPa and 64 MPa, respectively. Fig. 9 shows the corresponding finite-element model predictions of the principal stretches and the thickness profiles.

Table 2

Values of the parameters used in finite-element simulations.

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Plate type</th>
<th>Loading direction</th>
<th>Thickness (mm)</th>
<th>Loading target</th>
<th>Projectile Mass (g)</th>
<th>Initial conditions</th>
</tr>
</thead>
<tbody>
<tr>
<td>S-57</td>
<td>Monolithic</td>
<td>Flat side</td>
<td>1.02</td>
<td>Polyurethane</td>
<td>832</td>
<td>61.58, 1577</td>
</tr>
<tr>
<td>SP-130</td>
<td>Bilayer</td>
<td>Flat side</td>
<td>1.02</td>
<td>Polyurethane</td>
<td>832</td>
<td>63.8, 1691</td>
</tr>
<tr>
<td>MW-1</td>
<td>Monolithic</td>
<td>Flat side</td>
<td>1.02</td>
<td>Water</td>
<td>1682</td>
<td>48.4, 1970</td>
</tr>
</tbody>
</table>

Fig. 5. Comparison of the experimental and numerically predicted results (ALE formulation) of monolithic plate S-57: (a) principal stretches (left), and (b) thickness (right).
The comparison of the experimental results and the numerical predictions, presented in Fig. 9, shows that excellent correlation between experimental and numerical results can be obtained by introducing the debonding algorithm with suitable interface strength values. Fig. 10 shows a sequence of predicted deformed configurations of the SP-130 sample at 40 ms time intervals, showing a complete debonding of the steel and polyurea layers. The debonding initiates from the edge and propagates towards the center.

4.4. Transmitted force: finite-element vs. experiment

As explained in Section 2 of this paper, the force transmitted through the plate is measured by the strain gauges that are mounted on the output bar, for a set of selected

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**Fig. 6.** Sequence of the side view of the numerical estimate of the deflection of S-57 monolithic steel plate at 40 ms time intervals; note that a quarter of the model is masked to provide a better view of the thickness profile.

**Fig. 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 = ∞.
experiments. The detailed description of the experimental conditions is presented in Amini et al. (2010). To further assess the finite-element models, the forces measured for samples S-117 and S-119 are compared with the corresponding finite-element results. The parameters used for the finite-element simulation (MW-1) are provided in Table 2. The results are shown in Fig. 11, revealing that the model does predict the four experimentally observed stages of loading: shock loading (pre-cavitation), cavitation, post-cavitation loading, and unloading. The predicted transmitted force has an excellent correlation with the experimental measurements of the pulse shape, its duration, and its amplitude.

5. Parametric study results

This section gives the results of a parametric study of the bilayer plates under impulsive pressure loads. The main parameters that are investigated are:

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

The accumulated effective plastic strains of the elements within a 4 mm (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 (Amini et al., 2010).

The projectile used in the finite-element models reported in this section has a mass of 1682 g and an initial velocity of 48.4 m/s that provides the same initial kinetic energy for all the modeled samples. All the steel plate models have a gauge section thickness of 1.02 mm and are loaded through a polyurethane medium. The details of the finite-element model parameters are given in Table 3.
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 Fig. 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 the 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

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Fig. 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 = 100 MPa, NSFS = 64 MPa.

Fig. 10. Sequence of the side view of the numerical estimate of the deflection of SP-130 bilayer plate at 40 ms time intervals with SFLS = 100 MPa and NSFS = 64 MPa; note that a quarter of the model is masked to present a better view of the profile.
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 a 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 (2004), Clifton and Jiao (2004), and Nemat-Nasser et al. (2003a) revealed a significant stiffening under high pressures and high-strain rates that produce a closer match between the impedance of the steel and that of polyurea. In addition, the viscoelastic damping of the initial shock load transferred to polyurea, results in the better performance of the bilayer plate with polyurea on the back face.

As the 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 transmitted and the incident wave pressures can be approximated using the following relation from elastic wave propagation theory in layered media:

$$\frac{P_T}{P_I} = \frac{2\rho_T V_T}{\rho_T V_T + \rho_I V_I}, \quad (15)$$

where, $P_T$ and $P_I$ are the transmitted and incident pressures, respectively; $\rho_T$ and $\rho_I$ are the densities and $V_T$ and $V_I$ are the velocities of the two media, respectively.

### Table 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 assumed 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>$\infty$</td>
<td>$\infty$</td>
</tr>
<tr>
<td>B2</td>
<td>Back face</td>
<td>$\infty$</td>
<td>$\infty$</td>
</tr>
<tr>
<td>B3</td>
<td>Back face</td>
<td>$\infty$</td>
<td>$\infty$</td>
</tr>
<tr>
<td>F1</td>
<td>Front face</td>
<td>$\infty$</td>
<td>$\infty$</td>
</tr>
<tr>
<td>F2</td>
<td>Front face</td>
<td>$\infty$</td>
<td>$\infty$</td>
</tr>
<tr>
<td>F3</td>
<td>Front face</td>
<td>$\infty$</td>
<td>$\infty$</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>$\infty$</td>
<td>100</td>
</tr>
<tr>
<td>F6</td>
<td>Front face</td>
<td>$\infty$</td>
<td>100</td>
</tr>
<tr>
<td>B7</td>
<td>Back face</td>
<td>$\infty$</td>
<td>140</td>
</tr>
<tr>
<td>F7</td>
<td>Front face</td>
<td>$\infty$</td>
<td>140</td>
</tr>
<tr>
<td>B8</td>
<td>Back face</td>
<td>$\infty$</td>
<td>180</td>
</tr>
</tbody>
</table>
The density and longitudinal wave velocity of steel, polyurea, and water are presented in Table 4. It has been shown before (Yi and Boyce, 2004; Clifton and Jiao 2004; Nemat-Nasser et al., 2003a) 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 layer is calculated based on a plain-strain finite-element simulation. In the case that polyurea is on the 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 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 transmitted pressure-pulse reaches the polyurea-steel interface, the ratio of the pressure transmitted to the steel to the incident pressure in the polyurea is 1.80. If the assumption of no dissipation of energy in the polyurea (in compression only) is valid, then the ratio of the pressure transmitted to the steel to the initial incident pressure in water for the bilayer plate with the polyurea on the front face is 2.99 which is about 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. Nevertheless, they seem to correspond, at least qualitatively, with the experimentally observed results.

Xue and Hutchinson (2007) have shown by simulations, 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 applies only if the bilayer plate does not fail during the initial shock loading and the polyurea remains bonded to the steel layer.

5.2. Effect of thickness of polyurea layer

A set of simulations is performed to study the effect of the polyurea layer thickness on the performance of the bilayer plates. In these simulations the thicknesses of the polyurea layer used for the simulations are 3.78 mm, 2.52 mm, and 1.26 mm. The simulations are performed for both bilayer configurations. A perfect steel-polyurea interface bond is assumed. The details of the simulations are given in Table 3. Fig. 12 compares the averaged effective plastic strain histories obtained in these simulations. The comparison reveals that the increase in the thickness of the polyurea layer placed on the back face, improves the overall performance of the bilayer plates. On the other hand, as the thickness of the polyurea layer placed on the front face increases, the initial shock becomes more pronounced. However the increase in the polyurea layer thickness also increases the overall effective tangent modulus of the plate and leads to a superior performance of the bilayer plates in this case (perfect interface bond). We note that the thickness of the steel plate has kept constant in these as well as in the Xue and Hutchinson (2007) simulations. Recently, Samiee et al. (in preparation) have performed a systematic three-dimensional simulation of a 1 m diameter steel plate with and without polyurea, keeping the areal density the same. They observe a similar trend as reported above, even when a thicker steel plate is used in order to maintain equivalent areal densities.

5.3. Effect of steel-polyurea interface bonding strength

It is experimentally observed (Amini et al., 2010) 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 input. 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 3. The results are compared with those obtained for the perfectly bonded steel-polyurea bilayers. Fig. 13 compares the corresponding averaged effective plastic strain histories. 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 the 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 polyurea is on the front face, the delamination initiates from the center and proceeds to the edge. On the other hand, when polyurea is on the back face, the delamination initiates from the edge and proceeds towards 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.

6. Summary and conclusions

The response of monolithic steel and bilayer steel-polyurea plates in direct impact-induced pressure-pulse experiments is evaluated using finite-element methods. The finite-element simulations model the entire experimental setup detailed in Amini et al. (2010). 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
Our study also shows that, when polyurea is on the front face, then the increase of the polyurea layer thickness has two effects: first, it aggravates the initial shock effect on the steel layer, and, second, it increases the effective tangent modulus of the plate. These two factors have an opposing influence on the overall performance of the bilayer plate, and, depending on the conditions, the increase of the polyurea layer thickness on the front face might improve or aggravate the shock response of the plate. On the other hand, when polyurea is on the back face, the increase in its thickness improves the overall performance of the bilayer plate.

The finite-element simulations of the polyurea-steel bilayers of different assumed interface bonding strengths reveal that the performance of the bilayer plate is dependent on the strength of the interface 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.

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