Wet-sand impulse loading of metallic plates and corrugated core sandwich panels

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ARTICLE INFO

Article history: 
Received 11 November 2010 
Received in revised form 17 May 2011 
Accepted 20 May 2011 
Available online 30 May 2011

Keywords: 
Sandwich panels 
Blast loading 
Impulse mitigation

ABSTRACT

We have utilized a combination of experimental and modeling methods to investigate the mechanical response of edge-clamped sandwich panels subject to the impact of explosively driven wet sand. A porthole extrusion process followed by friction stir welding was utilized to fabricate 6061-T6 aluminum sandwich panels with corrugated cores. The panels were edge clamped and subjected to localized high intensity dynamic loading by the detonation of spherical explosive charges encased by a concentric shell of wet sand placed at different standoff distances. Monolithic plates of the same alloy and mass per unit area were also tested in an identical manner and found to suffer 15–20% larger permanent deflections. A decoupled wet sand loading model was developed and incorporated into a parallel finite-element simulation capability. The loading model was calibrated to one of the experiments. The model predictions for the remaining tests were found to be in close agreement with experimental observations for both sandwich panels and monolithic plates. The simulation tool was then utilized to explore sandwich panel designs with improved performance. It was found that the performance of the sandwich panel to wet sand blast loading can be varied by redistributing the mass among the core webs and the face sheets. Sandwich panel designs that suffer 30% smaller deflections than equivalent solid plates have been identified.

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1. Introduction

Metallic sandwich panel structures with cellular cores made from ductile, ferritic, and in some cases corrosion resistant stainless steels have attracted significant interest for protecting structures from impulse loading. The performance and advantages of sandwich panel construction have been thoroughly studied in the case of water blast, and to a lesser extent for air blast. In each of these two cases the sandwich structure performance is controlled by the design of the panels [i.e., core topology and geometry] [1,2], the mechanical properties and density of the material [3], the integrity of the joints [4], and the nature of the impulse transfer from the surrounding medium to the structure [5–9]. There has been relatively less literature on impulse loading with soil as the medium.

In the case of water and air blast, efforts have focused on exploiting the so-called fluid structure interaction (FSI) effect to reduce the impulse imparted to the structure. For explosive loading in water and air (in the acoustic weak shock limit), the impulse transferred to a massive plate is twice the incident impulse due to complete shock reflection. However, when low inertia (thin and light) plates are impacted by these shocks, the reflection factor can be decreased and the impulse acquired by the structure is then less than that of the high inertia system [3,9]. As deduced in Taylor’s seminal work [5], the ratio of transferred to incident impulse, \( I/I_0 \), is controlled by a dimensionless parameter \( \beta = \rho_f c_w (1 - \beta) \), where \( \rho_f \) and \( c_w \) are the density and sound speed of the fluid respectively, \( t_0 \) is the characteristic decay time for the pulse, and \( I/e \) is the mass per unit area of the front face sheet. The acoustic limit for the impulse transfer ratio derived by Taylor is given by \( I/I_0 = 2\beta^{(1-\beta)} \). In water, sandwich panels may exploit the FSI effect by using light face sheets supported by soft cellular structure cores [3]. Reductions of impulse as high as 25% have been experimentally reported for steel face sheets that are several millimeters thick supported by cores with a compressive strength of 5–10 MPa [10]. Improvements to Taylor’s expression have been proposed by Hutchinson et al. [11] to account for the resistance to the motion of the front face sheet offered by the core in sandwich panels. The level of impulse reduction depends upon the time and location of cavitation after the shock reflection, which in turn is affected by panel design [12].

Panel designs that seek to exploit the FSI effect in water have utilized a variety of core topologies and geometries including: Y-truss [2], corrugated [13], triangular honeycomb [14], square honeycomb [15], and lattice truss cores [16], among others. These
concepts use a tough outer face sheet that is impacted by the waterborne shock and a relatively low-strength core. The impulsive loading causes the outer face sheet to accelerate and to crush the core. At larger deflections, additional resisting forces are activated by membrane stretching of the front face sheet, and for severe impulsive loading, by stretching of the back face sheet and core [17,18].

The FSI effect for air blasts has been studied theoretically by Kambouchiev et al. [8,9,19]. They extended Taylor’s analysis to the nonlinear regime where the compressibility of the fluid is pressure dependent [8]. Their approach allowed a decoupling of the structural loading from the fluids dynamics. It was found that nonlinear compressibility effects further decrease the transmitted impulse in comparison to the acoustic case. However, the effect is only present for very high intensity loading and very low inertia face sheets. In practice, the requirement of high intensity and light face sheets may limit the practicality of exploiting FSI effects in the design of air blast-mitigating systems [7,19]. Even so, it is important to recognize that sandwich panels have still demonstrated improved performance over equivalent mass per unit area solid plates by their increased plastic bending resistance [18].

An alternative to coupled simulation for determining the soil blast loading is to use empirical models, such as that of Westine et al [28]. This model provides a (spatially varying) specific impulse given the problem geometry and the size of the charge. Load is applied as an initial velocity presuming that the structural response time is long compared to impulsive loading from the soil. The approach is simple, although its validity is restricted to the range of the original experimental conditions. Furthermore, when compared to other loading models that incorporate a pressure time history, the initial velocity approach can significantly overestimate the core crushing of sandwich panels [7].

The objectives of the present study are to (i) understand the mechanical response of edge-clamped sandwich panels subject to the impact of explosively driven wet sand, (ii) develop a modeling approach that enables the prediction of such response as a function of the impulse spatio-temporal distribution, the geometry of the panels and their material properties, and (iii) explore a model-based optimization of the panel design. To address these objectives, a combined experimental and modeling approach was adopted.

For the experimental section of the study, a set of 6061-T6 aluminum sandwich panels with corrugated cores were fabricated. The panels were subsequently edge clamped and subjected to localized high intensity dynamic loading by explosively accelerated wet sand. Spherical test charges were detonated at different standoff distances from the target panels, and a post-mortem analysis of the specimens was performed and their permanent deformation measured. Monolithic plates with same mass per unit area and made from the same alloy were also tested in an identical manner.

A decoupled loading model was developed and incorporated into a parallel finite-element simulation capability to analyze the experiments. The loading model was calibrated to match the results of one experiment of the test matrix, and the values obtained through the calibration procedure were then adopted for all subsequent calculations. The simulation tool was then utilized to explore alternative sandwich panel designs.

The remainder of this article is organized as follows: In Section 2 we describe the experimental test setup. In Section 3 we present the computational model. In Section 4 experimental results are presented and compared to simulation results as a means for model assessment and experimental interpretation. The model is then used to investigate the panel design space and potential improvements to the panel design are presented. It is shown that significant panel deflection reductions can be achieved by redistributing panel mass from the core to the back face sheet.

2. Experimental procedures

Experiments were conducted on a simplified system to understand the structural response of metallic sandwich panels to wet sand blast (in particular as a function of blast standoff). The experimental design, similar to that of Deshpande et al. [20], aims to produce a controlled loading environment that reproduces the relevant physical interactions. We note that the experimental design is not intended to reproduce the conditions of any particular explosive device or scenario.

The experiments were conducted on aluminum alloy sandwich panels with a triangular corrugated and I-core and 1.7 cm thick solid plates of the same alloy and areal density (mass per unit area). Spherical test charges were placed at different standoff distances from the center of the target panels and plates and detonated to evaluate the effects of the impulsive loading on the structures. After testing, the plates and panels were water-jet sectioned and their permanent deflections and deformation modes were evaluated.

A detailed explanation of the panel fabrication processes, the preparation of the test charge, the panel testing configuration, and the test matrix adopted for this work is given in the subsections below.

2.1. Panel fabrication

The sandwich panels were fabricated from a 6061 aluminum alloy using a combination of porthole die extrusion and friction stir welding. The panels were extruded by Mid East Aluminum (Mountaintop, PA) into 0.143 m wide, ~ 3 m long stick extrusions with the corrugated structure shown in Fig. 1, using a 17.8 cm diameter, 300 ton direct extrusion press operating at 482 °C. The panels were heat-treated to the T6 (peak strength) condition after extrusion. The core of the extruded structures had a web...
inclination angle of 60°, an inclined web thickness of 3.2 mm, a core height of 19.3 mm, and 5.2 mm thick top and bottom face sheets and (Fig. 1). The relative density of the corrugated core was 25.2%. The core’s areal density was 13 kg/m² while that of each of the faces was 14 kg/m². The edges of the extrusions were terminated with vertical solid webs to facilitate the fabrication of wider square panels. These vertical webs were machined to a thickness of 4.75 mm.

To create panels of the required width for testing, five extrusions were friction stir welded side-by-side at Friction Stir Link, Inc. (Waukesha, WI). The process was performed using a 17 mm diameter welding tool turning at a speed of 1000 RPM as schematically illustrated in Fig. 2. The 4.75 mm wide vertical trusses at the two sides of the extrusion (Fig. 1) were retained to minimize the distortion of the structure due to the high pressures generated during the stir welding process. The vertical trusses also shielded the welds from some stress during subsequent testing and compensated for the local decrease in mechanical properties associated with the welding process.

The resulting test panels were 0.632 m wide and 0.610 m long (Fig. 2). The relative density of the hybrid core including the vertical webs was 29% and the areal density of the entire sandwich panel was 46 kg/m² (equivalent to a solid 6061-T6 aluminum panel of 1.7 cm thickness).

Since the sandwich panel face sheets were vulnerable to pull-out at the bolts used to edge clamp the panels during testing, four 10.6 cm wide strips along the panel edges were filled with a Crosslink Epoxy (CLR1061 resin/CLH6930 hardener) to create an outer strengthened panel border.

2.2. Charge preparation

Model test charges were made by packing nominally 200 μm diameter glass microspheres (Mo-Sci Corp, Rolla, MO) in a 152 mm
diameter, thin-walled polyester glycol plastic sphere, concentric with an internal 80 mm diameter, thin-walled acrylic shell filled with 375 g of C-4 explosive. The test charge assembly sequence is shown in Fig. 3.

First, a measured mass of explosive was tightly packed inside the 80 mm diameter plastic sphere. A thin wooden rod was then inserted through the explosive sphere to facilitate subsequent suspension of the charge. To enable later insertion of a detonator, a thin-walled polymer tube was then inserted into a hole cut in the north pole of the plastic sphere and hot glued to the surface (Fig. 3a). This 80 mm plastic sphere was then centered inside a hemispherical, 152 mm diameter plastic shell. The hemispherical shell was partially filled with 200 μm diameter glass microspheres (artificial “sand”), periodically vibrating the hemisphere so that the microspheres were tightly packed. The top hemispherical half of the outer sphere shell was then positioned on the bottom hemisphere, and the seam joint sealed with hot glue (Fig. 3b). The internal volume of the outer sphere was then completely filled with more glass microspheres (Fig. 3c) and the weight of sand measured. The mass of sand used in the charges was 2.466 kg. Finally, water was poured into the larger sphere to fill the void space between the glass microspheres (Fig. 3d). The mass of water added to the charges was 0.617 kg and the total mass of the “water saturated sand” was 3.083 kg.

2.3. Panel testing configuration

The impulsive loading experiments were conducted at the Force Protection Inc. Explosives Test range located near Edgefield, SC using a test geometry shown schematically in Fig. 4. Approximately square (61 cm × 61 cm) test panels were mounted horizontally to a 3.8 cm thick steel plate bolted to a welded I-beam frame resting on a concrete support base. A 19.5 mm diameter, 32-hole pattern in the steel plate allowed the test panels to be securely edge clamped in the test configuration. The 3.8 cm thick steel plate contained a 41 cm × 41 cm square center opening to enable the unrestricted deformation of the test panel when subjected to a centrally applied impulse load. The sand-encased spherical charges were suspended above the center of the test panels at varying standoff distances, defined as the distance from the center of the charge to the impact surface of the test panels, Fig. 5.

2.4. Test matrix

A series of 10 experiments were conducted. Five of these were performed at standoff distances of 15, 19, 22, 25 and 30 cm (measured from the front face) with an equivalent weight...
monolithic 6061-T6 plate of thickness 1.7 cm, Fig. 5a, and the remaining five by replacing the solid plates with five corrugated core sandwich panels, at the same standoff distances, Fig. 5b. The panels were water-jet sectioned after testing and the panel deflections and deformation modes were evaluated.

3. Computational modeling

We conducted computer simulations of the plates and sandwich panels subjected to impulsive wet sand loading (i) to study details of the experiments that are difficult to measure; and (ii) to identify potential improvements to the panel geometry.

In the course of this endeavor, we utilized a simplified, decoupled method for applying the soil impact loads to the structures. This spatio-temporal distribution of the loading was calibrated against one experiment from the testing matrix based upon a recently proposed soil model developed by Deshpande et al. [20]. In the final step, we compared two strategies for improving the sandwich panels (measured in terms of core crush and maximum back face deflection) with numerical simulations.

3.1. Finite element modeling

The responses of the plates and sandwich panels were simulated using a parallel finite-element computational framework [29] based on a Lagrangian finite deformation formulation of the equations of motion [30]. The spatial discretization of the plate and panel geometries utilized 10-node quadratic tetrahedral elements. The mesh used for the analysis of the solid plate comprised approximately 160,000 elements while that for the extruded panel contained approximately 200,000 elements. Time integration was performed with the explicit, second order accurate, central-difference scheme, which falls within the Newmark family of time stepping algorithms.

3.2. Constitutive modeling

The constitutive response of 6061-T6 aluminum was modeled using an isotropic, finite deformation variation of the Johnson-Cook viscoplastic model [31], formulated within the framework of variational constitutive updates developed by Radovitzky and Ortiz [32], Ortiz and Stainier [33], Yang et al. [34], and Ortiz and Pandolfi [35]. To avoid complications from fracture, we disregarded the experimental results that display large-scale failure (the sandwich panel at 15 cm standoff) since we did not attempt to model fracture.

We adopted a modified Johnson—Cook flow stress $Y$ defined as

$$Y = \sigma_0 \left[ 1 + \left( \frac{\dot{e}_p}{\dot{e}_0} \right)^n \right] \left[ 1 + \text{Clog} \left( \frac{\dot{e}_p}{2\dot{e}_0} + \sqrt{1 + \left( \frac{\dot{e}_p}{2\dot{e}_0} \right)^2} \right) \right]$$

where $\dot{e}_p$ is the equivalent plastic strain, $\dot{e}_0$ is the equivalent plastic strain rate,
is a reference plastic strain, and \( \dot{\varepsilon}_p^0 \) is a reference plastic strain rate. The choice of this flow stress avoided singularities in the traditional Johnson-Cook hardening as \( \dot{\varepsilon}_p \to 0 \) and in the hardening modulus as \( \dot{\varepsilon}_p \to 0 \), in accordance with the variational update method requirements. The constitutive model is supplemented with an isotropic Hencky hyperelasticity extension of linear elasticity.

The strain-rate effect parameter \( C \) was obtained from the literature [36], fitted at a reference strain rate \( \dot{\varepsilon}_p = 1 \text{ s}^{-1} \). The Young's modulus \( E \) and the strain hardening parameters \( n, B, \) and \( \sigma_0 \) were calibrated to quasi-static tensile tests, conducted on coupons cut from the faces of the finished panels and measured in the direction of extrusion. Fig. 6 compares the measured uniaxial true stress-strain data to the constitutive law prediction. The derived constitutive parameter values are shown in Table 1, along with the density \( \rho \).

3.3. Wet sand blast loading model

Traditionally, the modeling of blast loads on solids due to explosions have been based on empirical formulas, such as the Westine model [28]. This method has proven fruitful in design; however, it is limited in applicability to conditions similar to the original calibration experiments. Another approach has been to develop constitutive models for soils, e.g. Grujicic et al. [21] and Grujicic and Cheeseman [23]. Such models may then be used in coupled FSI codes to simulate virtually any scenario, within the range of validity of the constitutive model. In the present context, this approach has the disadvantage that significant computational effort is expended on the soil mechanics, which here is of less interest than the response of the structure. Since the FSI effect during impulsive
soil loading is small, we created a decoupled loading model with a priori determined tractions that incorporates information from a constitutive model applicable to impulsively loaded soil [20]. This approach may be viewed as a compromise between the two previous methods, with some of the advantages of both.

Deshpande et al. [20] performed simulations of dry and wet sand impact from test charges similar to those investigated here. These simulations provided values for the time dependence of the impulse \( I \) and the average pressure \( p \) on a target panel or plate as a function of the radial distance \( r \). Our loading consists of a time-dependent surface traction based upon the impulse and pressure data obtained from this model. The introduced coefficients were calibrated by fitting to one of the sandwich panel experiments (at 30 cm standoff). A brief description of this procedure follows; additional mathematical details are contained in an appendix.

Consider a point \( P \) on the target surface located a distance \( r \) from the center of the charge \( O \) as depicted in Fig. 7. As shown in the Appendix, the load at the point \( P \) can be reduced to the application of a time-dependent pressure \( p^P(t) \) given by

\[
p^P(t) = \begin{cases} \frac{p(r) \sin^2(\theta)}{\alpha} & \text{if } t \in [t^P_a, t^P_b + t^P_d] \\ 0 & \text{otherwise} \end{cases}
\]

where \( p(r) \) is the pressure as a function of distance to the charge center, and the arrival time \( t^P_a \) and the duration of the pulse \( t^P_d \) are given by

\[
t^P_a = \frac{\rho_0 V_0}{4\pi \alpha (1 + \beta)} \left[ \frac{1}{r^1_0} - \frac{1}{r^{1+\beta}_0} \right]
\]

\[
t^P_d = \frac{\alpha r^\beta}{p(r)}
\]

In these equations \( \rho_0 \) and \( V_0 \) are the initial average density and volume of the sand-water mix surrounding the explosive charge. In addition, we assumed that the impulse, defined as

\[ I(r) = \int^t_{t^P_a} p(t) dt \]

is a function of the distance \( r \) to the charge center, and takes the form

\[ I(r) = a r^3 \]

The expressions for the impulse \( I(r) \) and the pressure \( p(r) \) were obtained by a procedure which utilizes calculated wet sand blast data for a similar experimental setup (from [20]), followed by calibration against the current experimental data. The calibration procedure is discussed in Section 4.2.

4. Results and validation of the model

4.1. Experimental results

The effect of changing the standoff distance upon the deflection of a 17 mm thick monolithic aluminum 6061-T6 plate (with a mass per unit area identical to the sandwich panels) is shown in Fig. 8. All of the tests caused significant bending and plastic stretching of the panels. This increased in severity as the standoff distance was decreased. A small crack formed on the loaded surface near the mid-point of the gripped edge of the most intensely loaded panel.

The effect of the same impulsive load upon the sandwich panel structures is shown in Fig. 9. Sandwich panel permanent deflection were 15–20% less than those of the solid plates with the exception of the most intensely loaded panel, Fig. 9a. In this case, face sheet fracture at the edge and center of the panel occurred. Edge cracking was also evident in the front and back faces of the panel tested at a standoff of 19 cm (Fig. 9b) and facilitated additional panel deflection over that due to face sheet stretching and panel bending. Face sheet stretching and deflection between corrugation web nodes in the most intensely loaded regions of the panels is also evident (see Fig. 9a and b). At lower impulses, the core was only modestly compressed by inelastic buckling of the corrugation webs and sandwich action was preserved for a significant fraction of the loading event.

4.2. Model analysis

The three loading parameters were obtained by incorporating results obtained from Deshpande’s calculations [20] with a calibration against one experimental test result. For wet-sand blasts, Deshpande’s model predicts that the pressure is approximately

![Fig. 7. Sand blast front schematics. The dashed lines represent the position of the wet sand blast front for times \( t = t^P_a \) and \( t = t^P_d \).](image)

![Fig. 8. Sectioned photos of equivalent weight solid plates tested at standoff distances of, (a) 15 (b) 19 (c) 22 (d) 25 and (e) 30 cm. A small crack formed at the region circled in (a).](image)
constant for the range of standoff distances considered in this work. Additionally, the impulse decay with distance is well characterized by a power law:

$$I(r) = a r^b p(r) = p_0$$ (8)

In our calculations, the exponent $b$ is derived from published simulation data in Eq. (20) for a similar experimental setup; the impulse versus standoff distance data for fully saturated wet sand were fitted to the power law (Eq. (8)) and the resulting exponent was adopted for the current simulations. The remaining loading parameters $p_0$ and $a$ were calibrated through simulation by matching the values of the core strain and the permanent deflection at the center of the back face of the corrugated panel for a standoff of 30 cm. The loading parameters found through this procedure are listed in Table 2. From Eq. (8), the predicted peak impulses ranged from 3.28 kN s/m² at $r = 30$ cm to 6.00 kN s/m² at $r = 19$ cm. We note that a $b$ value less than $-2$ implies a weaker than inverse square law dependence of impulse upon standoff distance for fully saturated wet sand. Further assessments of this finding await fully coupled simulations that account for the direction of the explosives detonation wave front (in this case towards the target), impulse transfer from the explosive to the wet sand and the interaction of the radially stretching wet sand shell with a dynamically deforming target.

Fig. 10 shows the displacements at the centers of both panel face sheets as a function of time for the calibration case. The application of the wet sand blast load to the panel produces an initial rapid deflection, peaking at $t_p = 0.36$ ms. The initial peak is followed by a series of oscillations with period $T \approx 0.61$ ms. The permanent deflection of the front and back face sheets is estimated as the average of the peak values of the corresponding oscillation:

$$d = \frac{d_{\text{max}} - d_{\text{min}}}{2}$$ (9)

The last oscillation of the simulation window $t = [0, 0.002]$ s is used for this calculation. From Fig. 10, it can be observed that the front face exhibits a larger permanent deflection than the back face. This difference, divided by the core height defines the permanent core strain:

$$e_{\text{core}} = \frac{d_{\text{front}} - d_{\text{back}}}{h_{\text{core}}}$$ (10)

where $h_{\text{core}} = 19.1$ mm.

After calibrating the loading parameters, the other experiments were simulated using the same loading parameters. In total, the simulation suite comprises both plates and corrugated panels subjected to wet sand blast loads at standoffs of 19 cm, 22 cm, 25 cm and 30 cm. Fig. 11 shows the deformed shape of sectioned panels after the simulation concluded. Fig. 12 shows the numerical results for the corrugated panels together with their experimental counterparts. As expected, the deflection for the 30 cm standoff distance case exactly matched the experimental observation since this was the calibration case. The remaining three cases (at standoffs of 25 cm, 22 cm and 19 cm) all are in good agreement.

Fig. 13 compares the numerical deflection results and the experimental observations for the monolithic plates. Good agreement is again observed. The worst case, at 19 cm standoff, exhibits an 8% difference with the corresponding experiment. It bears emphasis that the good agreement observed in both the plate and sandwich panel simulations was obtained with identical loading parameters $a$, $b$, and $p_0$ for every simulation, using only one case for calibration (the sandwich panel at 30 cm standoff).

Comparisons of the center permanent deflections of the sandwich panel, Fig. 12, and monolithic plate, Fig. 13, show that the sandwich panel suffered 15–20% smaller deflections than the monolithic plates. Examination of Fig. 11 shows that the

<table>
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<th>Table 2</th>
<th>Calibrated loading parameters.</th>
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<tr>
<td>$\alpha$</td>
<td>$\beta$</td>
</tr>
<tr>
<td>0.665 kN s/m²</td>
<td>−1.325</td>
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2 Simulation results for the standoff distance of 15 cm are not reported since the current model neglects failure. The experimental results indicate that extensive failure occurs in the sandwich panel at this standoff, see Fig. 9.
simulations predicted modest core compression, in agreement with experimental observations, cf. Fig. 9.

4.3. Exploration of the design space

The validated model provides a simple means for exploring the panel design space by calculating the response of corrugated panels with variations of the original design. We have restricted our analysis to cases where the core compression strain did not exceed 50% (whereupon the panels’ sandwich action is lost). Panel modifications explored two different strategies:

- **Case 1: core mass fraction variation.** The overall core area was reduced by evenly decreasing the thickness of both vertical and inclined webs. The total panel mass was kept constant by adding the subtracted mass to the back face sheet. In addition to the original panel, two cases were considered, with 10% and 20% core density reduction.

- **Case 2: vertical web variation.** The core mass was reduced by decreasing the thickness of the I-core (vertical) webs only. The total panel mass was again kept constant by adding the subtracted mass to the back face sheet. In addition to the original panel, three cases were considered, with 20%, 60% and 100% reduction of the vertical web thickness. The latter case corresponds to a panel without vertical webs.

Fig. 14 shows the permanent deflection results obtained for case 1. A reduction of the total core mass produced a slight decrease of the permanent back face deflection for each standoff distance. Larger reductions in core mass result in decreasing back face deflections but significantly more core crushing, Fig. 17a.

Fig. 15 summarizes the permanent deflection trends for case 2. Here, reduction of the total core mass decreased the permanent back face deflection for all standoff distances. The limiting case of no vertical webs exhibited the best performance of any case considered resulting in 30% reduction in deflection compared to the solid plate. No interpenetration was observed for any of these simulations.
The successful reduction of back face deflection relative to the original design is tempered by increments in core crush strain at all standoff distances, particularly for case 1. Fig. 16 compares the core crush strain of the original design to the best performing panels from cases 1 and 2. Fig. 17 illustrates this effect for the 19 cm standoff distance. The difference in the core crush strains can be explained by the occurrence of different failure mechanisms.

In case 1, the stubby vertical webs imposed significant constraint upon front face motion, limiting the buckling of neighboring inclined webs. This constraint resulted in an inhomogeneous buckling (crushing) of the core. As a consequence, failure localized between the innermost vertical webs, and energy was not dissipated uniformly (See Fig. 17a). The effective plastic strain attained a maximum value $\varepsilon_p = 0.48$ in the inclined webs, which is significantly greater than the value reached in the original design, $\varepsilon_p = 0.25$. This localized buckling in turn caused greater bending and stretching in the top face sheet, implying an inefficient use of material.

The response is markedly different when no vertical webs are present. In this scenario buckling proceeded more homogeneously throughout the core; see Fig. 17b. Consequently, energy was initially dissipated though distributed plastic buckling of the core webs across the entire panel, and later through membrane stretching of both face sheets. The effective plastic strain in the core webs attained a maximum value $\varepsilon_p = 0.35$ in the original design, $\varepsilon_p = 0.34$ for the design shown in Fig. 17a, and $\varepsilon_p = 0.37$ for the design depicted in Fig. 17b.

In all cases considered, high effective plastic strains were formed in the face sheets near the clamped edges. Their location corresponded to the region where face sheet failure occurred in the most intensely loaded experiments. The deformation in this region was little affected by design modifications. The maximum effective plastic strain was $\varepsilon_p = 0.33$ in the original design, $\varepsilon_p = 0.34$ for the design shown in Fig. 17a, and $\varepsilon_p = 0.37$ for the design depicted in Fig. 17b.

Fig. 18 summarizes the gains realized through the design space exploration. The limited space design exploration led to the identification of panel designs that suffer about 30% smaller center deflections than equivalent solid plates under localized wet-sand blast loading.

5. Summary and conclusions

We have used a combined experimental and modeling approach to investigate wet sand blast loading response of 6061-T6 aluminum plates and sandwich panels with corrugated cores of the same mass per unit area.

The corrugated core panels suffered 15–20% smaller maximum permanent deflections than equivalent mass per unit area monolithic plates of the same alloy. The sandwich panel deflection was accommodated by a small (5–10%) crushing of the core and by face sheet stretching. The permanent center deflection of the sandwich panels increased with load intensity and failed by face-sheet fracture at the gripped panel edges and in the panel center for the most intensely loaded panels.
A parallel finite-element simulation capability based on a Lagrangian large-deformation formulation of the equations of motion was used to simulate the response of plates and panels under wet sand blast loading. A modified Johnson-Cook constitutive model was adopted and its material constants were fitted to match the experimentally obtained stress-strain response of the aluminum. We developed a decoupled loading model informed with Deshpande’s results which predict that the pressure on the target is approximated constant for the range of standoff distances considered in this work. By calibrating the loading model for one single experiment (corrugated panel, 30 cm standoff distance) we were able to predict the remaining 7 experiments with excellent accuracy.

The validated computational model provided the means for performing a brief exploration of the design parameter space of the structures. Modifications to the original panel design were studied by pursuing two different strategies: (i) core mass fraction variation, and (ii) vertical web variation. Different structural failure mechanisms were observed during this exploration: heterogeneous core crushing for panels with vertical webs, and membrane deformation of the panels as a whole for the case with no vertical webs. The latter case corresponds to the best design obtained from our brief exploration of the design space. In this case, the energy dissipation is distributed evenly as elastic webs. The latter case corresponds to the best design obtained from the explosive charge center of a time-dependent pressure.

We are grateful to Vikram Deshpande for helpful discussions of this work. The experiments were conducted at the Force Protection Industries Explosives Test Range (Edgefield, South Carolina) with the considerable assistance of Keith Williams. Research was supported by the Office of Naval Research (ONR grant number N00014-07-1-0764) as part of a Multidisciplinary University Research Initiative (David Shiffer, Program manager).

Appendix. Derivation of simplified wet sand blast loading model

Let us consider a point $P$ on the target surface at a distance $r$ from the explosive charge center $O$ as depicted in Fig. 7. The wet sand velocity, pressure and density at the arrival point are related by

$$p(t) = \rho(t)v(t)^2$$

In addition, let us consider the mass conservation equation assuming constant density across the sand blast

$$\rho_0 V_0 = \rho(r)V(r)$$

where $\rho_0$ and $V_0$ are the initial average density and volume of the explosive charge respectively and $V(r)$ is the volume of the expanded wet sand shell, approximated by

$$V(r) = 4\pi r^2 w(r)$$

In the previous equation, $w(r)$ is the thickness of the expanded wet sand shell, related to the velocity and duration of the load $t_d$ by

$$w(r) = t_d(r)v(r) = \frac{v(r)l(r)}{p(r)}$$

Combining the last three equations, the conservation of mass reduces to

$$\rho_0 V_0 = \frac{\rho(r)4\pi r^2 v(r)l(r)}{p(r)}$$

Solving equations (11) and (15) simultaneously we obtain standoff-dependent expressions for the arrival velocity $v$ and the density $p$:

$$\rho(r) = \frac{\rho_0 V_0}{4\pi r^2} \frac{1}{l(r) r^2}$$

$$v(r) = \frac{4\pi}{\rho_0 V_0} l(r) r^2$$

Since $v$ is not constant, the arrival time $t_a$ must be obtained through integration of equation 17. In order to solve for the arrival time, let us assume that the impulse can be fit with a power law:

$$I(r) = \alpha t^b$$

This assumption, combined with eq. (17), implies a representation of the velocity of the form

$$v(r) = \frac{4\pi \alpha}{\rho_0 V_0} t^{b+2}$$

Assuming that $t = 0$ for $r = r_0$ and integrating the previous equation we obtain the position of the blast front as a function of time. In this manner, by simple inversion, the arrival time can be obtained as:

$$t_a = \frac{\rho_0 V_0}{4\pi \alpha (1+\beta)} \left[ \frac{1}{t_0^{1+\beta}} - \frac{1}{t^{1+\beta}} \right]$$

Furthermore, since $p(r)$ is the average pressure, the duration $t_d$ of the pressure pulse is, by definition, given by

$$t_d = \frac{I(r)}{p(r)}$$

At this point, we know that the point $P$ is loaded by a constant pressure from $t_d^L$ to $t_d^P$, but we must be careful regarding the definition of such a pressure. The data provided by Deshpande et al. considers the point where the target surface is perpendicular to the sand velocity, namely $P_0$. Therefore, we must only consider the normal component of the sand velocity at the arrival point:

$$v_n = v \sin(\theta)$$

In this way, the load at the point $P$ is reduced to the application of a time-dependent pressure $p(t)$ given by

$$p(t) = \begin{cases} p(r) \sin^2(\theta) & \text{if } t \in [t_d^L, t_d^P] \\ 0 & \text{otherwise} \end{cases}$$

References


[27] V.S. Deshpande Submitted.


