Compression and impact testing of two-layer composite pyramidal-core sandwich panels

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ABSTRACT

Quasi-static uniform compression tests and low-velocity concentrated impact tests were conducted to reveal the failure mechanisms and energy absorption capacity of two-layer carbon fiber composite sandwich panels with pyramidal truss cores. Three different volume-fraction cores (i.e., with different relative densities) were fabricated: 1.25%, 1.81%, and 2.27%. Two-layer sandwich panels with identical volume-fraction cores (either 1.25% or 2.27%), and also stepwise graded panels consisting of one light and one heavy core, were investigated under uniform quasi-static compression. Under quasi-static compression, load peaks were identified with complete failure of individual truss layers due to strut buckling or strut crushing, and specific energy absorption was estimated for different core configurations. In the impact test, the damage resulting from low-velocity concentrated impact was investigated. Our results show that compared with glass fiber woven textile truss cores, two-layer carbon fiber composite pyramidal truss cores have comparable specific energy absorptions, and thus could be used in the development of novel light-weight multifunctional structures.

1. Introduction

Sandwich panels with low-density cores have attracted significant interest as multifunctional structures. These systems show promise for supporting mechanical loads and mitigating blast effects [1–4]. Such panels are traditionally manufactured using metallic honeycombs or stochastic foams [5–7]. The emergence of manufacturing techniques for constructing metallic three-dimensional periodic cores [8–12], and the more recent development of techniques to fabricate composite truss cores have opened new opportunities for optimizing multifunctional structures. Most studies of the mechanical response of composite panels have dealt with the quasi-static behavior of sandwich panels with a single layer of core material bonded to two face sheets [13–18]. On the other hand, multi-layer metallic structures have been shown to be effective at dispersing high-intensity impulses, and at reducing the transmitted pressures of an underwater impulsive load [19,20]. Wang et al. [21] found that in compression testing, failure of a multi-layer corrugated sandwich proceeds one layer at a time, that the energy absorption of multi-layer corrugated sandwich structures is significantly higher than with a single layer, and that they have the capability to survive greater shocks. Fan et al. [22,23] indicated that thicker individual monolayers weakened a stack, and encouraged sequential buckling. They manufactured multi-layered glass fiber reinforced composite sandwich panels as a stack of thinner layers in order to improve the energy absorption ability of the woven textile sandwich. Multi-layer panels also allow development of semi-functionally graded cores, which can exhibit superior energy absorption compared a uniform foam with equal mass [24–27].

In the present work, we studied the mechanical response and failure mechanisms of two-layer carbon fiber composite sandwich panels with pyramidal truss cores under uniform quasi-static compressive loading and low-speed concentrated impact, as an initial step in understanding the performance of multi-layer composite panels. The pyramidal cores were fabricated from continuous strips of prepreg using a hot-press molding technique described in our recently published study [17]. In this manufacturing method, the continuous fibers of the composite are aligned with each strut’s axis, allowing the truss structure to fully exploit the intrinsic strength of the fibers. We also showed that truss structures fabricated by the above method have strength close to the theoretical limit of lattice structures. For this study, two-layer sandwich panels were fabricated by adhesively bonding the pyramidal truss cores between three carbon-fiber reinforced face sheets. In fabricating a multi-layer panel, the two most obvious core alignment choices for the truss layers are (a) node-to-node, and (b) node-to-space. Since the former simply results in a sheet-stabilized variant of the well-known Kagome truss core [28], we explored instead the conventional (but weaker and more flexible) node-to-space configuration, achieved by vertically translating one truss to define the placement of the next (effectively, stacking identical panels).
2. Experiments

2.1. Materials and specimens

The tested panels are comprised of truss-like carbon fiber pyramidal structures fabricated by the hot press mold method and separated by a flat carbon fiber composite sheet as sketched in Fig. 1b. In manufacturing the pyramidal truss cores, we used layers of unidirectional carbon fiber/epoxy prepreg of individual thickness 0.15 mm (T700/epoxy composite, Beijing Institute of Aeronautical Materials, China). The unidirectional prepreg properties are listed in Table 1. Once the core is bonded to its face sheets (using epoxy adhesive 08-57, Heilongjiang Institute of Petrochemistry), the unit cell is as shown in Fig. 1a. The dimensionless relative density (i.e., volume fraction) \( \overline{\rho} \) of two pyramidal cores combined with a medial sheet is given by:

\[
\overline{\rho} = \frac{\rho_1 h_1 + \rho_2 h_2 + t_f}{h_1 + h_2 + t_f},
\]

(1)

where \( h_1 \) and \( h_2 \) are the core heights for each layer, \( t_f \) is the thickness of the medial sheet, and \( \rho_i \) is the core relative density calculated accurately as

\[
\rho_i = \frac{2[2b + (h_i - t_i)]/\sin \omega_i - t_i \sin \omega_i - \frac{d_i}{2}]}{2b + (h_i - t_i)/\tan \omega_i - t_i \sin \omega_i/2} h_i.
\]

(2)

We have previously [17] used the following simpler expression, which is less accurate for large \( t_f \):

\[
\rho_i = \frac{2[2b + (h_i - 2t_i)]/\sin \omega_i d_i}{(2b + h_i/\tan \omega_i)} (i = 1 \text{ or } 2).
\]

(3)

The geometrical parameters \( d_1, d_2, \omega_1, \omega_2 \) and \( b \) are shown in the schematic figure of a unit cell sketched in Fig. 1a. Fig. 1b illustrates an assembled two-layer sandwich panel and Fig. 2a shows the additional parameters \( h_1, h_2, t_1 \) and \( t_2 \). Three grades of truss core were fabricated for this investigation. The ‘light weight’ core was made with four layers (i.e., ribbons) of prepreg and had the following geometrical parameters: \( t = 0.6 \text{ mm}, b = 4 \text{ mm}, h = 15 \text{ mm}, d = 3 \text{ mm}, \omega = 45^\circ \), resulting in a relative density 1.25%. ‘Medium weight’ truss core used six layers of prepreg, for a thickness \( t = 0.9 \text{ mm} \), with other dimensions essentially unchanged, resulting in a relative density of 1.81%. The ‘heavy weight’ truss core employed 8 layers of prepreg with \( t = 1.2 \text{ mm} \) to achieve a relative density of 2.27%. It should be noted that all truss cores considered have very low relative density, and the terms “light”, “medium” and “heavy” simply indicate their relative strength and stiffness. The medial sheets match conventional aerospace practice: four prepreg layers \( [0^\circ/90^\circ/90^\circ/0^\circ] \) with a total thickness of 0.5 mm. To understand the relative mass contributions of each component, note that the ‘equivalent solid sheet thickness’ of 1.25% core is 0.19 mm, while 1.81% core is equivalent to 0.27 mm, and 2.27% core is equivalent to 0.34 mm. The two-layer panels therefore contain from 69% to 80% of their mass in the face and medial sheets.

2.2. Quasi-static compression and low velocity impact tests

We carried out uniaxial compression testing using a screw-driven machine (INSTRON 5569) following ASTM C365/C 364 M-05. The compression tests were carried out in the quasi-static displacement-controlled regime with a nominal displacement rate of 0.5 mm/min. Samples with 3 × 3 = 9 unit cells were used as shown schematically in Fig. 2a.

Analysis revealed that the node-to-space core alignment illustrated in Fig. 2a exhibits an edge effect: the outermost core struts of the upper layer (20 out of 36 total) cannot transmit as much load from the top plate due to their flexible support by the free edge of the medial sheet (which is cantilevered, and also unable to develop membrane tension). Thus extra load is carried by the interior struts. Unfortunately, we have no way to estimate the post-buckling peak load, but it seems likely that it will be reduced somewhat. Future studies should either explore or eliminate this effect. Because of the pronounced load drop when a core layer fails, the compression of multiple layers exhibits a sequence of peaks. Once all layers (two in this case) were densified, the test was terminated at a load comparable to the previously observed peaks.

Low-velocity penetrating impact tests were carried out using a guided drop-weight test rig (Instron Dynatup Model 9250HV) with adjustable rebound catchers (to prevent multiple impacts when the indentor rebounds). It is capable of striking samples at energies

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![Fig. 1](image-url)
up to 826 J utilizing a spring-assist. For the present paper, all samples were struck with a 6.17 kg impactor whose nose is a 12.7 mm diameter hemisphere. Three nominal impact energies, 20, 40 and 60 J, provided by nominal drop heights 330 mm, 660 mm, and 990 mm, were used for the tests with cores of three different relative densities $\rho_1 = \rho_2 = 1.25\%$, 1.81% and 2.27%. These energy magnitudes were selected to cause partial damage and then rebound. A specimen, 140 mm $\times$ 100 mm, was clamped into a rectangular fixture with an open window of 100 mm $\times$ 80 mm. The face and medial sheets were clamped at their edges. The impact position on a sandwich panel was located in the center of the specimen, as shown in Fig. 3. All tests were performed at room temperature. For the 20 J test (1.25%, 1.25%) the force–time history was recorded digitally. After impact, images of the sample exterior deformation of the medial sheet. The core’s quasi-static peak modulus values, a series-compliance calculation indicates that:

$$E = \frac{2E_1d_t \sin^3 \theta}{(h - t)((h - t)/\tan \theta + 2b - t \sin(\theta/2))^2}$$

In our calculations, the following simplified equation was used, however it is not very accurate for large thickness:

$$E_i = \frac{2E_1d_t \sin^3 \theta}{(2b + h_i/\tan \theta)^2} \quad (i = 1 \ or \ 2).$$

If the layers are identical, the overall modulus equals that of a component layer. However, if the layers have different individual modulus values, a series-compliance calculation indicates that:

$$E = \frac{(h_1 + h_2)E_1E_2}{h_1E_2 + h_2E_1}$$

From Table 2, it can be seen that discrepancies between experiments and analytical predictions typically exceed 60%. The main reason is probably the un-modeled ‘checkerboard’ out of plane deformation of the intermediate face sheet.

The compressive strength of a single-layer panel is governed by strut failure due to elastic buckling or crushing. For multi-layer panels, Fan et al. [23] argued that the strength and the compressibility could be understood from the component monolayer properties. We assume that a two-layer pyramidal core panel exhibits the separate deformations of the two cores, plus the out-of-plane deformation of the medial sheet. The core’s quasi-static peak macroscopic compressive strength can be derived from our previous paper [17] as:

$$\sigma_s = \frac{2\pi^2 E_1d_t \sin^3 \theta}{3(h_1 - t_i)^2([h_1 - t_i]/\tan \theta + 2b - t_i \sin(\theta/2))^2}$$

$$\sigma_d = \frac{2\sigma_{cd}d_t \sin \theta}{([h_1 - t_i]/\tan \theta + 2b - t_i \sin(\theta/2))^2}$$

The following approximate expressions were used in this paper:

Euler buckling of struts $\sigma_e = \frac{32\pi^2 E_1I_i \sin^2 \theta(h)}{(4b + 2h_i)(h_1 - 2t_i)^2}$

Compressively crushing struts $\sigma_d = \frac{2\sigma_{cd}d_t \sin \theta}{(2b + h_i/\tan \theta)^2}$

Here, $E_i$ denotes the apparent axial elastic modulus of individual struts, and $\sigma_{cd}$ is the delamination strength in compressive loading of uniaxial composites (i.e., the fiber-oriented compressive stress level at which the material loses its integrity), both of which are provided in our previous paper [17]. and $I_i = d_t^4/12$ ($i = 1, 2$).

For each fabricated type of pyramidal core (light = 4 layers, medium = 6 layers, heavy = 8 layers, along with the other dimensions from Section 2.1), the calculated macroscopic failure stresses are given in Table 3. While the intermediate sheets are explicitly assumed not to contribute to the multi-layer panel compressive strength, they do add to the apparent compressive strain as mentioned above.

4. Results and discussions

4.1. Quasi-static compression tests

4.1.1. Two-layer sandwich panels with equal relative densities

The load–displacement curves feature dual peaks, each corresponding to the failure of a layer. For each peak, the curve first rises up elastically and then drops due to the Euler buckling or crushing/debonding of struts. The resulting essentially horizontal mat of failed struts can be compacted to support any desired load and transmit it to an undamaged layer. The curve then climbs up to form a second peak corresponding to the failure of the second layer. When both layers have been crushed to a load-bearing condition, the curve rises steeply. At loads able to fail a given type of core, each sandwich panel was compressed to 30% of its original thickness, or even thinner.

The compressive stress–strain responses of equal-density two-layer sandwich panels are shown in Figs. 4 and 5. They are similar to some metal foams [5] and metal multilayer sandwich panels...
with pyramidal truss cores [29]. The deformation histories for the sandwich panels have been marked with red dots. The two peaks in the stress–strain response shown in these figures corresponded to the strains at which cooperative buckling, fracture and debonding of the pyramidal truss layers occurred. In all equal-density panels tested, the upper layer failed first, perhaps due to the lack of medial-sheet edge support.

For two-layer specimens with \( q_1 = q_2 = 1.25\% \), as shown in Fig. 4, the measured first (upper layer) and second (lower layer) peak average compressive stresses are about 0.26 MPa and 0.34 MPa, with the respective standard deviations listed in Table 3. Visual observations indicate that the peak strength coincides with the onset of elastic buckling of the upper layer. The analytical estimate for the Euler buckling strength of the core based on Eq. (9) gives \( \sigma_{E} = 0.307 \) MPa, which is in a relatively good agreement with the measured peak stress values.

After the first peak stress, when the additional strain was sufficient to bring the failed core members into contact with their

### Table 2
Calculated and measured modulus of pyramidal truss core.

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Experimental results (MPa)</th>
<th>Analytical results (MPa)</th>
<th>Error (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>( q_1 = q_2 = 1.25% )</td>
<td>20.3 (19.5, 18.7)</td>
<td>49.3</td>
<td>-58.8</td>
</tr>
<tr>
<td>( q_1 = q_2 = 2.27% )</td>
<td>35.9 (41.3, 30.5)</td>
<td>104.89</td>
<td>-65.8</td>
</tr>
<tr>
<td>( q_1 = 1.25%, q_2 = 2.27% )</td>
<td>25.6 (29.2, 22)</td>
<td>67.07</td>
<td>-61.8</td>
</tr>
</tbody>
</table>

For 1.25%, \( E_s = 20.5 \) MPa and \( \sigma_{cd} = 493.76 \) MPa. For 2.27%, \( E_s = 21.8 \) MPa and \( \sigma_{cd} = 298.25 \) MPa.

### Table 3
Calculated and measured average failure stress for strut Euler buckling or axial crushing, for different grades of pyramidal truss core.

<table>
<thead>
<tr>
<th>Type of truss core (MPa)</th>
<th>( \sigma_{E} ) Euler buckling (MPa)</th>
<th>( \sigma_{c} ) Compressive crushing (MPa)</th>
<th>Experimental results and Standard deviations</th>
</tr>
</thead>
<tbody>
<tr>
<td>( q_1 = q_2 = 1.25% )</td>
<td>0.307</td>
<td>2.38</td>
<td>0.26 ± 0.05, 0.34 ± 0.06</td>
</tr>
<tr>
<td>( q_1 = q_2 = 2.27% )</td>
<td>3.13</td>
<td>2.87</td>
<td>1.95 ± 0.14, 2.14 ± 0.21</td>
</tr>
<tr>
<td>( q_1 = 1.25%, q_2 = 2.27% )</td>
<td>0.307 for 1.25%</td>
<td>2.87 for 2.27%</td>
<td>0.35 ± 0.06, 1.95 ± 0.25</td>
</tr>
</tbody>
</table>

For the experimental results, there are two peak loads for two-layer sandwich panels.
Fig. 4. Stress–strain curves and failure modes for static compression tests of two-layer pyramidal truss core with relative densities $\rho_1 = \rho_2 = 1.25\%$. The deformation history, I: top layer-buckling, bottom layer-elastic deformation; II: top layer-compacted, bottom layer-elastic deformation; III: top layer-compacted, bottom layer-buckling; IV: top layer-compacted, bottom layer-compacted.

Fig. 5. Stress–strain curves and failure modes for static compression tests of two-layer pyramidal truss core with relative densities $\rho_1 = \rho_2 = 2.27\%$. The deformation history, I: top layer-debonding, bottom layer-elastic deformation; II: top layer-compacted, bottom layer-elastic deformation; III: top layer-compacted, bottom layer-debonding; IV: top layer-compacted, bottom layer-compacted.
adjoining faces (single-layer strain ~0.6, equivalent to overall strain ~0.3), densification and hardening of the upper layer occurred. This was followed by a second (peak) buckling event in the lower layer as shown in Fig. 4a. In the images of Fig. 4b, the bottom layer exhibits the deformation soon after the onset of buckling, while the top layer illustrates contact between the interlayer and the elastic deformed core members. At an overall strain approaching 60%, the densification of the bottom layer caused rapid hardening.

The stronger-core specimen also exhibited two peak strengths like specimen 1 – see Fig. 5a. The first peak strength and second peak strength are about 1.95 MPa and 2.14 MPa, relatively. Both peak strengths were much lower than the expected theoretical failure stress $\sigma_0 = 2.87$ MPa due to strut crushing and fiber debonding. Prior to the first peak load, medial sheet elastic deformation was observed. For this heavier, thicker-truss core material, visual observations indicated that the first peak strength coincides with the onset of joint debonding and fracture between the top face sheet and upper layer, rather than with Euler buckling. When the strain was sufficient to cause contact between the core members and faces (event II in Fig. 5b), hardening of the upper layer occurred, followed by debonding, delamination and fracture in the lower layer at event III. In this region, the strain resulted in truss elastic deformation followed by truss debonding, delamination, fracture and medial sheet elastic deformation. At a strain approaching 60% (event IV), the onset of densification of the lower layer caused final rapid hardening.

4.1.2. Two-layer sandwich panels with different relative densities

In the present quasi-static crushing and low-velocity penetration studies, simple discretely graded cellular structures (achieved by having different truss core relative densities in the two layers) were investigated in order to identify the failure mechanisms. Fig. 6 represents the stress-strain curve and failure mechanism for sandwich panels under quasi-static crushing. The top core layer relative density was $\rho_1 = 1.25\%$ and the bottom core layer relative density was $\rho_2 = 2.27\%$. The weak top layer with relative density $\rho_1 = 1.25\%$ failed first due to Euler buckling, and then the bottom layer with relative density $\rho_2 = 2.27\%$ failed due to debonding, as shown in Fig. 6b. The observed first peak strength $\sigma_{P1} = 0.351$ MPa

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is a little larger than the calculated $\sigma_E = 0.307$ MPa. For debonding and fracture of the second, stronger layer, the measured strength $\sigma_{P2} = 1.954$ MPa is again lower than the expected $\sigma_{D} = 2.87$ MPa calculated on the basis of strut compressive failure.

4.1.3. Energy absorption in quasi-static uniform compression

It is convenient to evaluate the panel’s energy-absorbing ability by compressing it quasi-statically normal to its plane. The compression curves reveal long deformation plateaus, suggesting that multilayered sandwich panels may potentially be good energy-absorbing materials. Fan et al. [23] pointed out that multilayered woven textile sandwich panels could deform up to half of their thickness. In the present paper we found that the overall compression ratio of the two-layer sandwich panels is closer to 70%. At 50% crushing strain, the specific energy absorption of the panels is about 0.64 J/g for specimen 1 with relative densities $\rho_1 = \rho_2 = 1.25\%$, 6.06 J/g for specimen 2 with relative densities $\rho_1 = \rho_2 = 2.27\%$ and 3.41 J/g for the specimen with relative densities $\rho_1 = 1.25\%, \rho_2 = 2.27\%$ – see Fig. 7.

Specific energy absorption of the woven textile truss is 4.8 J/g at 55% strain for nichrome [30] and 4.6 J/g at 70% strain for steel [31]. The specific energy absorption of a four layer prismatic steel panel is about 3.3 J/g [19]. The maximum specific energy absorption of the present two-layer sandwich panels is about 6.06 J/g. Compared to the referenced panels with woven textile truss cores, the present sandwich structures have comparable specific energy absorbing ability.

4.2. Low-velocity concentrated impact tests

Local damage in two-layer sandwich panels with pyramidal truss cores was investigated in low velocity concentrated impact tests, where a different energy level was used in striking each different density of core, as seen in Figs. 8–10. Fig. 8 shows the outcome of the 20 J impact test on ‘light weight’ pyramidal truss cores ($\rho_1 = \rho_2 = 1.25\%$), and includes a partial plot of contact force versus time measured by the sensor in the impactor. A load drop occurred after a first peak point due to initiation of major damage. This was followed by an irregular plateau with many large oscillations. A nearly undamaged area was found below the impactor position, inside a pattern of inclined matrix cracks and delamination. However, both layers of pyramidal truss cores failed completely due to buckling, debonding and fracturing, as shown in Fig. 8b.

Fig. 9 shows the specimen with the relative densities $\rho_1 = \rho_2 = 1.81\%$ after a 40 J impact test. From the images it can be seen that the impact damage included: (a) penetrating into the top face sheet, (b) interacting with the top pyramidal truss core, (c) contacting with the medial sheet, (d) penetrating into the intermediate sheet and (e) interacting with the bottom...
pyramidal truss cores. It can be seen that the bottom face sheets were bulged between four nodes of the pyramidal truss.

Fig. 10 shows the typical failure observed at 60 J impact energy for two-layer sandwich panels with core relative densities $p_1 = p_2 = 2.27\%$. The top and intermediate face sheets exhibited tearing, accompanied by fracture and debonding of some of the struts under the impact location. The bottom face sheets were also bulged between four nodes of the pyramidal truss core. Debonding, fracture and delamination were observed in both pyramidal truss cores underneath the impact area.

5. Conclusions

The failure modes and deformation mechanisms of carbon fiber composite two-layer sandwich panels with pyramidal truss core were investigated under uniform quasi-static compression and low velocity concentrated impact. Under quasi-static compression, force–displacement response was measured both for two-layer sandwich panels with the same relative densities, and also for panel layers having different relative densities. Compared with a referenced glass fiber woven textile truss core, the present two-layer sandwich structures with continuous carbon fiber truss core have similar or better energy absorbing capability per unit mass. In low-velocity penetrating impact, damage mechanisms were determined. For the two-layer panel with relative densities $p_1 = p_2 = 1.25\%$, the impact damage was mainly crushing of pyramidal truss cores. For the relative densities $p_1 = 1.81\%$ and $p_1 = p_2 = 2.27\%$, the impact damage in sandwich panels included tearing of the face sheets, debonding, fracturing and delamination of pyramidal truss cores. The results provide insight into the mechanical behavior of multi-layer composite cores under uniform compression and low velocity concentrated impact. Knowledge of the failure mechanisms will be useful for developing pyramidal truss cores as part of novel light-weight multifunctional structures.

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