Titanium matrix composite lattice structures

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Abstract

Cellular materials made from high temperature composites with efficient load supporting lattice topologies and small cell sizes would create new options for light weight, high temperature structures. A method has been developed for fabricating millimeter cell size cellular lattice structures with a square truss topology from 240 μm diameter Ti–6Al–4V coated SiC monofilaments. The compressive stiffness of the lattice structures was well approximated by that of the monofilament fraction in the loading direction. The strength of the lattices was controlled by elastic buckling and the results were accurately represented by an elastic buckling model. The post peak compressive deformation was accommodated by elastic buckling of the SiC fibers and plastic deformation of the titanium coating until the onset of monofilament fracture. The specific stiffness and strength of the lattices were found to be between 2 and 10 times that of other cellular structures and thus appear to be promising candidates for high temperature, ultralight weight load supporting applications.

Keywords: Cellular materials; A. Metal–matrix composites (MMCs); B. Mechanical properties

1. Introduction

Lightweight, multifunctional load supporting structures based upon sandwich panel concepts have many applications [1–10]. Their cellular cores usually have either a prismatic (corrugations) or honeycomb topology and are usually adheresively bonded to the face sheets [2–10]. The cores are made of very low density materials such as Nomex [3,4], polymer foams [5], or light metal constructions [6–9] that maintain the face sheet separation needed for sandwich panel action at minimum parasitic mass [1]. Metallic core systems based upon light metals such as aluminum [6,7] and titanium [8,9] alloys are utilized where high compressive strengths are needed to avoid local indentation. They can also be used for higher temperature applications provided metallurgical bonding is used to attach the core to the face sheets [6–9]. The maximum use temperature of these structures is then limited by the creep rate of the alloy system.

While honeycomb topology systems usually offer significantly superior structural performance [1,2,10,11], their closed-cell topology limits some potential multifunctional applications [10]. They are also susceptible to corrosion and suffer from delamination problems [2,9]. Stochastic metal foam core systems with fully interconnected pores have attracted interest because they provide multifunctional capabilities such as cross flow heat exchange [11–14], dynamic load protection [11,15–17], and acoustic damping [11,18]. However, their use in structural applications is severely constrained by their low elastic moduli and indentation strengths [1,11].

Methods have recently begun to be developed for fabricating millimeter scale, open-cell metallic lattices from a range of light metals and high temperature alloys [19–31]. The truss members in lattice materials can be topologically configured to experience predominantly axial stresses (i.e. tension or compression) when they are used in sandwich panels that are loaded in bending (i.e. the cores are stretch dominated) [19,32–34]. This results in significantly superior mechanical performance compared to stochastic metal foams whose ligaments deform by bending [35].
These lattice topologies also possess greater compressive strength and therefore better resistance to indentation.

Numerous cell topologies have been fabricated including those with tetrahedral [19–23], pyramidal [21, 24–26], 3D-Kagomé [23,27], woven textile [28,29], and diamond or square collinear [30,31] structures. Examples of these lattices, configured as the cores of sandwich panels, are schematically illustrated in Fig. 1. The textile and collinear lattice structures exploit metallic wires for their fabrication by a simple lay-up and transient liquid bonding process [24,29–31].

The strength of lattice core sandwich panels is governed by the stress at which panel failure mechanisms are initiated under the various modes of loading (through thickness, longitudinal compression, bending, tension etc.) [37–40]. These failure mechanisms are well established for metallic materials and include face sheet yielding [34,36,38], face sheet wrinkling [34,36,38], core shear [28,37,38], core plastic yielding [34,36,38], fracture under tension [33], and plastic buckling [27,33,34] of compressively loaded core truss members. Although the most common loading environments for sandwich panels are shear and bending, the practical implementation of sandwich panels is often limited by concentrated compressive stresses at the loading points. In such cases, the core suffers local yield or crushing (indentation) and can be accompanied by face sheet stretching/tearing [11,36,37]. These are affected by the compressive strength of the core and the cell size (which controls the stretching periodicity) [34].

The compressive stiffness and strength of lattice structures could be significantly increased by the use of stiffer, higher strength core materials. The highest specific strength lattice structures fabricated to date have utilized Ti–6Al–4V for the truss structures [26]. This alloy has a density of 4.43 g/cm³, Young’s moduli in the range of 100–120 GPa, and yield strengths of 750–1250 MPa [39]. A Ti–6Al–4V coated SCS-6 SiC monofilament developed in the 1990’s for titanium matrix composite (TMC) manufacture [40–42] appears to be an even stronger (but less ductile) candidate material for lattice structures. For a 35% fiber volume fraction, the axial Young’s modulus of the monofilament is about 215 GPa and its ambient temperature tensile fracture strength exceeds 1800 MPa. Both are approximately two times higher than that of Ti–6Al–4V alloy, while the density of such a monofilament would be 3.93 g/cm³, roughly 12% less than that of Ti–6Al–4V. These composite materials also offer much better creep resistance than their all-metallic counterparts [43]. With these considerations, cellular structures fabricated from such composites might be attractive for high temperature and/or weight sensitive structural applications.

The aim of the current paper is to report a method for the fabrication of TMC lattice core sandwich panels. The out-of-plane compressive behavior of the lattice structure is investigated experimentally and the micromechanisms of lattice deformation as a function of lattice relative density are identified. The mechanical properties are then compared to analytic estimates based upon the observed failure modes. While these materials/structures have limited ductility, they appear to be one of the highest specific strength cellular structures fabricated to date.

2. Lattice truss fabrication

Collinear lattice structures with a square orientation (Fig. 1) and a relative density, \( \rho \), in the range of 5–17% were fabricated from Ti–6Al–4V coated SCS-6 SiC monofilaments supplied by FMW Composite Systems, Inc. (Clarksburg, WV). Each monofilament was approximately 240 \( \mu \)m in diameter and consisted of a 140 \( \mu \)m diameter SiC (SCS-6) fiber surrounded by a 50 \( \mu \)m thick physical vapor deposited Ti–6Al–4V coating, Fig. 2. The SCS-6 fibers were made by chemical vapor deposition on carbon cylindrical substrates (33 \( \mu \)m diameter fibers) [44,45]. The densities of SCS-6 fiber, Ti–6Al–4V, and the composite monofilament are 3.00, 4.43, and 3.93 g/cm³, respectively.

![Fig. 1. Examples of sandwich panel structures with open-cell, lattice core topologies. Metallic pyramidal, tetrahedral and kagomé core structures are made by folding perforated metal sheets or by investment casting. The textile and collinear core structures are made by wire lay-up and brazing methods.](attachment://image.png)
Lattice structures were assembled from the monofilaments using a simple fixture (constructed of stainless steel and spray coated with BN to prevent sticking) to align the monofilaments in collinear layers, Fig. 3a. The orientation of consecutive layers was alternated to create a square lattice truss topology structure. Fig. 4 shows a stacking sequence and a unit cell of the lattice structure. Once assembly of the TMC filament lay-up was complete, a dead weight was used to apply a force of $3.7 \times 10^{-2}$ N (equivalent to an applied pressure of 1.5–5 MPa) to each contact (truss–truss node). The assembly was diffusion bonded by placing it in a vacuum furnace at a base pressure of $10^{-7}$ Torr. It was heated at 20 $^\circ$C/min to 900 $^\circ$C and then held for 4 h under pressure, Fig. 3b. The lattice structures were then removed from the tooling and cut, using wire electro-discharge machining, to create samples that were four cells high and six cells in length, Fig. 3c. The sample width was approximately the same as the sample height,
i.e. \( W \approx H = 4l \), where \( l \), \( W \), and \( H \) are the unit cell length, the lattice width, and the lattice height, respectively.

The relative density of the as laid-up lattice, \( \bar{\rho}_o \), is simply the volume fraction of the unit cell occupied by solid truss material:

\[
\bar{\rho}_o = \frac{V_s}{V_c} = \frac{2\pi a^2 l}{w_o l^2} = \frac{2\pi a^2 l}{4a l^2} = \frac{\pi a}{2l},
\]

where \( V_c \), \( V_s \), \( w_o \), \( a \), and \( l \) are the unit cell volume, the volume occupied by the solid trusses, the cell width, the filament radius, and the cell length, respectively.

Fig. 5 shows nodal regions of the lattice structure after the diffusion bonding step. It can be seen that excellent metallurgical uniformity was achieved in the bonded region. Equilaxed \( \alpha \)-grains and an intergranular \( \beta \)-phase microstructure was observed with an average grain size of 8 \( \mu \)m. During diffusion bonding, the spacing between consecutive layers and hence the macroscopic lattice width decreased as the titanium alloy coating at the contact points deformed and interdiffused. Fig. 4 schematically shows a unit cell of the square lattice before and after diffusion bonding.

A diffusion bonding coefficient, \( \beta \), can be introduced by assuming the titanium alloy coating was redistributed similarly in each collinear layer (see Fig. 4). The \( \beta \) coefficient is defined as \( w/w_o \), where \( w \) and \( w_o \) are the unit cell widths prior to and after the diffusion bonding process (Figs. 4b and c), correspondingly. The unit cell volume of the diffusion bonded lattice can then be written:

\[
V_c = w l^2 = \beta w_o l^2 = \beta 4a l^2.
\]

Even though the titanium alloy coating is deformed at the contacts, the volume occupied by the solid composite in the unit cell remains the same as for the as laid-up unit cell. The relative density of the diffusion bonded square lattice structure, \( \bar{\rho} \), is therefore:

\[
\bar{\rho} = \frac{V_s}{V_c} = \frac{2\pi a^2 l}{\beta 4a l^2} = \frac{\pi a}{2 \beta l^2} = \frac{\bar{\rho}_o}{\beta}.
\]

Table 1 summarizes cell parameters and corresponding relative densities of the square lattices prior to and after diffusion bonding. For the diffusion bonding conditions used here, \( \beta \) was 0.9–0.92, and the relative density of the lattices therefore increased by 8–10% during the bonding process.

After being cut to size, the lattices were brazed to 2 mm thick Ti–6Al–4V face sheets using a TiCuNi-60\% paste braze alloy supplied by Lucas-Milhaupt, Inc. (Cudahy, WI), Figs. 3c–d. This alloy powder had a nominal composition of Ti-15Cu-25Ni (wt\%) and was held in a polymer binder. The solidus and liquidus temperatures of the brazing alloy are 890 °C and 940 °C, respectively. The sandwich panel samples were vacuum brazed by heating at 20 °C/min to 550 °C, holding for 5 min (to volatilize and remove the polymer binder), and finally heating to 975 °C for 30 min at a base pressure of \(~10^{-7}\) Torr. Micrographs of a truss-face sheet node made by the brazing technique are shown in Fig. 6. It can be seen that the TiCuNi-60\% braze provided good wettability to the SCS-6 fiber surface as well as good penetration to the Ti–6Al–4V alloy coating and face sheet. Photographs of sandwich panel samples of each core relative density are shown in Fig. 7.

3. Experimental compressive response

The square lattice structures were tested at ambient temperature in compression with a displacement rate of
The measured load cell force was used to calculate the nominal stress applied to the sandwich, and the nominal through thickness strain was obtained from a laser extensometer positioned about the sample centerline. The gage length for strain measurement for the compression samples was the core height, $H$ (see Table 2).

The through thickness, compressive stress–strain responses for square lattice structures at each $\rho$ are shown in Fig. 8. All the cellular structures deformed in an elastic–brittle manner with a variable degree of post peak strength retention that decreased with increasing relative density. The stiffnesses and peak strengths are plotted against $\rho$ in Figs. 9a and b. The stiffness exhibited a linear dependence on $\rho$. The measured load cell force was used to calculate the nominal stress applied to the sandwich, and the nominal through thickness strain was obtained from a laser extensometer positioned about the sample centerline. The gage length for strain measurement for the compression samples was the core height, $H$ (see Table 2).

### Table 1

| Lattice Cell Parameters $a$ and corresponding relative densities prior to and after core diffusion bonding ($T = 900 \, ^\circ\text{C}, t = 4 \, \text{h}, P = 1.5–5 \, \text{MPa}$) |
|---|---|---|---|---|
| Center-to-center cell length, $l$ (mm) | Prior to diffusion bonding | After diffusion bonding | $\beta (\text{v/v})$ |
| Relative density, $\rho_0$ (%) | Core width, $W_0$ (mm) | Core width, $W$ (mm) | Relative density, $\rho$ (%) |
| 1.26 | 15.0 | 6.03 | 5.43 ± 0.015 | 16.7 ± 0.05 | 0.90 ± 0.003 |
| 1.88 | 10.0 | 9.05 | 8.35 ± 0.025 | 10.8 ± 0.03 | 0.92 ± 0.003 |
| 2.51 | 7.5 | 12.06 | 10.91 ± 0.043 | 8.3 ± 0.03 | 0.90 ± 0.004 |
| 3.77 | 5.0 | 18.10 | 16.75 ± 0.147 | 5.4 ± 0.05 | 0.92 ± 0.008 |

$a$ The monofilament diameter was 240 $\mu$m for all samples. The center-to-center cell length $l$ is unaffected by diffusion bonding.

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**Table 2**

| Lattice Cell Parameters $a$ and corresponding relative densities prior to and after core diffusion bonding ($T = 900 \, ^\circ\text{C}, t = 4 \, \text{h}, P = 1.5–5 \, \text{MPa}$) |
|---|---|---|---|---|
| Center-to-center cell length, $l$ (mm) | Prior to diffusion bonding | After diffusion bonding | $\beta (\text{v/v})$ |
| Relative density, $\rho_0$ (%) | Core width, $W_0$ (mm) | Core width, $W$ (mm) | Relative density, $\rho$ (%) |
| 1.26 | 15.0 | 6.03 | 5.43 ± 0.015 | 16.7 ± 0.05 | 0.90 ± 0.003 |
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| 2.51 | 7.5 | 12.06 | 10.91 ± 0.043 | 8.3 ± 0.03 | 0.90 ± 0.004 |
| 3.77 | 5.0 | 18.10 | 16.75 ± 0.147 | 5.4 ± 0.05 | 0.92 ± 0.008 |

$a$ The monofilament diameter was 240 $\mu$m for all samples. The center-to-center cell length $l$ is unaffected by diffusion bonding.
upon $\rho$, while the strength appeared to depend on $(\rho)^3$. Figs. 10 and 11 show stress–strain responses and photographs of the lattice taken during the compression of samples with high ($\rho = 16.7\%$) and low ($\rho = 5.4\%$) lattice relative densities, correspondingly. As the samples were compressed, trusses parallel to the loading direction deformed elastically and then buckled cooperatively as a consequence of being joined by the horizontal trusses. The peak stress in the stress–strain curve coincided with the onset of this buckling instability. Unload/reload tests indicated that prior to the peak compressive strength the strain was fully recoverable, indicating that the strength of these structures is controlled by elastic buckling of the vertical trusses. Note that Fig. 8 indicates that the lower the relative density (the more slender the trusses) the lower the strength and the smaller the core strain at the onset of elastic buckling.

In the highest relative density lattice (with $\rho = 16.7\%$), the trusses had a relatively low aspect ratio, and fracture of the monofilament was observed instantaneously with buckling (Fig. 10). Fig. 11 shows clearly that for the lattices with lower relative densities (more slender trusses), the trusses continued to buckle cooperatively as the strain progressed beyond the load peak. The buckling half wavelength was equal to the core height. This buckling-accommodated straining was accompanied by a significant decrease in applied stress, i.e. core softening. As compression continued, the truss nodes were subjected to increasingly significant shear forces and eventually debonded. Fig. 12 shows micrographs of a vertical truss after the compression test where shear debonding has occurred. In addition to debonding at the nodal contact, fracture of the metallic coating and fiber also occurred in this case.

While the peak compressive strengths appear to be simply controlled by the elastic buckling instability, the post peak stress behavior is controlled by less obvious mechanisms. From Fig. 11, it is clear that the unloading paths were not parallel to the initial elastic response and did not return to zero strain. This is indicative of inelastic deformation of the metallic coating. It appears that after the onset of the buckling instability, the SCS-6 fiber contin-

<table>
<thead>
<tr>
<th>Sample relative density, $\rho (%)$</th>
<th>Core height, $H$ (mm)</th>
<th>Strain rate, $\dot{\varepsilon}$ ($10^{-3}$ s$^{-1}$)</th>
</tr>
</thead>
<tbody>
<tr>
<td>16.7</td>
<td>6.20 ± 0.031</td>
<td>5.38 ± 0.027</td>
</tr>
<tr>
<td>10.8</td>
<td>9.38 ± 0.010</td>
<td>3.56 ± 0.004</td>
</tr>
<tr>
<td>8.3</td>
<td>12.66 ± 0.015</td>
<td>2.68 ± 0.003</td>
</tr>
<tr>
<td>5.4</td>
<td>19.26 ± 0.022</td>
<td>1.73 ± 0.002</td>
</tr>
</tbody>
</table>

Table 2: Compression test parameters

Fig. 8. Compressive stress–strain response for square lattice truss structures with $\rho = 16.7, 10.8, 8.3$, and 5.4%.

Fig. 9. Experimental data for (a) out-of-plane compressive stiffness and (b) out-of-plane compressive strength of the TMC square truss cores.
Fig. 10. Compressive stress–strain response and corresponding photographs of a square lattice truss structure with $\bar{\tau} = 16.7\%$: (a) prior to initial loading; (b) prior to peak load; (c) immediately after the peak load; and (d) after the peak load. Events (b)-(d) occurred within a few seconds.

Fig. 11. Compressive stress–strain response and corresponding photographs of a square oriented lattice truss structure with $\bar{\tau} = 5.4\%$: (a) prior to initial loading; (b) after the peak load and prior to first unloading; (c) following first unloading; (d) prior to second unloading; (e) following second unloading; (f) prior to third unloading; (g) following third unloading; (h) prior to fourth unloading; and (i) following fourth unloading.
ued to buckle elastically, while Ti–6Al–4V coating deformed plastically to accommodate the bending strain and node shear. At the highest core strains, beyond the elastic limit of the SCC-6 fiber and ductility of the Ti alloy coating, truss node debonding, coating fracture, and eventually SCS-6 fiber fracture accommodated the imposed strain and were responsible for the core softening.

The reloading behavior of these structures also revealed interesting effects, Fig. 11. In particular, it can be seen that when the lattice structure was reloaded, it followed a different stress–strain path to that during unloading. During unloading, the applied stress at a fixed core strain was less than that during the subsequent reloading cycle. This appears to be a result of elastic SiC fiber spring back and the release of the stored elastic energy in the fiber.

4. Discussion

Simple micromechanical arguments can be used to rationalize the initial loading modulus and peak strength of the composite cellular materials tested here.

4.1. Compressive stiffness

Under out-of-plane compression, only half of the trusses (the vertical trusses) carry load. Prior to buckling of the compressively loaded columns, the elastic modulus, \( E_c \), of the collinear lattice is simply [46]:

\[
E_c = \frac{1}{2} E_s \bar{\rho},
\]

where \( E_s \) is the axial Young’s modulus of the parent material. The axial Young’s modulus of the TMC monofilament can be estimated by the rule of mixtures:

\[
E_s = (1 - f)E_m + fE_f,
\]

where \( f \) is the volume fraction of the SCS-6 fiber, and \( E_m \) and \( E_f \) are the Young’s moduli of the Ti–6Al–4V matrix and SCS-6 fiber, respectively. For \( E_f = 400 \text{ GPa} \) [44,45], \( E_m = 115 \text{ GPa} \) [39], and \( f = 0.35 \), Eq. (5) gives \( E_s = 215 \text{ GPa} \).

A comparison between the measured and predicted stiffness (plotted as non-dimensional compressive stiffness, \( \Pi = E_c / (E_s\bar{\rho}) \)) as a function of the lattice relative density is shown in Fig. 13a. While most of the experimental data lie within 30% of the predicted value, some data lie well

![Fig. 12. Nodal shear and coating/fiber fracture near the nodes of a compressively loaded square lattice truss structure with \( \bar{\rho} = 10.8\% \).](image)

![Fig. 13. Analytical prediction and experimental data for (a) dimensionless stiffness and (b) strength of TMC square lattices.](image)
below the prediction. Such discrepancies have been frequently reported for cellular materials [20–31] and the origin of the effect appears to result from imperfections introduced in the fabrication process. The discrepancy observed here is believed to be a consequence of small variations in the length of the load-carrying trusses (core height) due to the cutting process. During initial loading, the applied load would then be disproportionately shared between the trusses. The compressive platen would apply the load onto longer trusses first. Because the platen moved with a constant rate, lower load would then be required than if all the trusses were load carrying. As compression proceeds, the load would be applied onto more and eventually all trusses. This makes the initial slope of the stress–strain plot (where a constant load supporting area is assumed) relatively low, but it continues to increase until the applied load is proportionately shared among all the trusses.

4.2. Compressive strength

The peak compressive strengths of the TMC square collinear cores can be estimated by analyzing the failure modes of truss members as individual axially loaded columns. It is assumed that the lattice collapses by elastic buckling of the constituent struts over the full height of the sandwich core as schematically illustrated in Fig. 14. If the vertical trusses are not joined together by horizontal trusses, the buckling stress acting on each vertical truss can be described by the elastic bifurcation stress, $\sigma_{cr}$, of a compressively loaded circular column [47,48]:

$$\sigma_{cr} = \frac{\pi^2 k^2 E_s}{4} \left(\frac{a}{l_b}\right)^2,$$  

(6)

where $E_s$ is the column elastic modulus, $a$ the column radius and $l_b$ the length of the column between two supporting ends. The factor $k$ depends upon the rotational stiffness of the end nodes with $k = 1$ corresponding to a freely rotating pin-joint and $k = 2$ corresponding to built in nodes which cannot rotate [47,48]. For the square lattices, the trusses length was $H$ (the core height) and we assume $k = 2$. Using this expression for the predicted critical strength of individual truss members, the mechanical properties of the sandwich panel that has no interaction among the truss members can be globalized and the compressive collapse strength of a core structure, $\sigma'_{pk}$, can be expressed by [21]:

$$\sigma'_{pk} = \Sigma \sigma_{cr} \bar{p},$$  

(7)

where $\Sigma$ is a lattice topology dependent scaling factor. The scaling factor $\Sigma = 1/2(\sin^2 \omega_1 + \sin^2 \omega_2)$ accounts for the fact that trusses oriented in the loading direction are the most efficient for load bearing, while those that are inclined are limited by force resolution considerations [49]. For square truss samples, half of the trusses have $\omega_1 = 0^\circ$ and another half have $\omega_2 = 90^\circ$ so the effective value of $\Sigma = 0.5$.

By substituting Eq. (6) into Eq. (7), the peak compressive strength of a square lattice truss structure failing by elastic buckling is then:

$$\sigma'_{pk} = \frac{\pi^2 E_s}{2} \left(\frac{a}{H}\right)^2 \bar{p}.$$  

(8)

Substituting $H = nl$ (where $n$ = number of unit cells along the height of the core), Eqs. (1) and (3) into Eq. (8) gives a relation between core strength, lattice relative density, and the number of cells in the principle loading direction:

$$\sigma'_{pk} = \frac{2E_s}{n^2 \bar{p}} \bar{p} \left[\frac{3}{\beta} \right].$$  

(9)

However, since the vertical trusses are joined together and hence are forced to buckle cooperatively, an additional shear stress at the truss nodes is also present (as suggested by experiment, see Fig. 12). This cooperative buckling mode is similar to the shear buckling mode of a fiber composite material modeled by Rosen [50]. In Rosen’s model, adjacent fibers buckle with the same wavelength and in phase with one another. The deformation of the matrix material between adjacent fibers is assumed to be primarily a shear deformation. Rosen found that the compressive strength of a fiber composite $\sigma_c$ due to (cooperative) shear buckling is:

$$\sigma_c = \frac{G_m}{1 - \nu_f} + \nu_f \sigma_{cr},$$  

(10)

where $G_m$, $\nu_f$, and $\sigma_{cr}$ are the shear modulus of the matrix, the volumetric fraction of the fibers, and the buckling strength of the fibers. The first term on the right-hand side of Eq. (10) is the contribution to the composite compressive strength from matrix shear and the remaining term is the contribution to the compressive strength associated
with the finite-bending resistance of the fibers. Fleck [51] subsequently argued that the matrix shear contribution, \( G_m(1 - v_f) \), should be replaced by the in-plane shear modulus of the composite when the compressive strength of the composite material is to be estimated.

Using this composite analogy, the cooperative buckling strength of a square lattice is then controlled by the resistance to lattice buckling (Eq. (9)) and the resistance to lattice shear given by the in-plane shear modulus of the lattice structure, \( G \):

\[
\sigma_{pk} = G + G_{pk}.
\]

An analytical expression of the in-plane shear modulus of square lattice \( G \) has been recently derived by Hutchinson [52]. He finds that the structure investigated here has a shear modulus:

\[
G = \frac{1}{16} E_s p^3.
\]

By substituting Eqs. (12) and (9) into Eq. (11), the peak compressive strength of a square lattice truss structure that fails by cooperative elastic buckling then becomes:

\[
\sigma_{pk} = \left( \frac{1}{16} + \frac{2\rho^2}{n^2} \right) E_s p^3.
\]

A comparison between the measured and predicted compressive peak strength (plotted as non-dimensional peak compressive strength, \( \Sigma = \sigma_{pk}/(E_s p^3) \) against \( \rho \)) of the TMC square collinear cores is shown in Fig. 13b. The cooperative elastic buckling model is seen to capture the peak compressive strengths of these square lattices well. The analytically predicted strength coefficient is 0.13. Most of the experimental data lie within 20% of this estimate.

4.3. Comparisons with other cellular structures

The highest specific strength lattice structures previously reported [19–31] utilized a Ti-6Al-4V alloy for the truss structure. The specific strength of such lattices was found to be approximately 100 kN m/kg [26]. The specific strength of the titanium composite lattices studied here was 185 kN m/kg. These TMC lattices therefore appear to be the highest specific strength cellular materials reported to date.

The titanium composite lattices investigated here are expected to retain good dimensional stability during loading at temperature up to 500°C (above this, creep of the metallic component becomes significant). The stiffness and peak strength of the square TMC lattice structures can be compared with those of similar lattices made from stainless steel [30] and ceramic foams [53–62] whose service temperatures also extend to 500°C and above, Fig. 15. Examination of the figure indicates that the higher relative density TMC lattices possess a higher specific stiffness and strength than any of the other structures/materials. These materials may therefore provide interesting high temperature multifunctional opportunities provided their limited ductility is not a significant constraint.

5. Conclusions

1. A method for making titanium sandwich panels with millimeter-scale titanium matrix composite (TMC) lattice cores has been developed. The method involves laying up linear arrays of TMC monofilaments, alternating the direction of consecutive layers, and using diffusion bonding to join the nodes. The relative density of the
lattice was controlled by the diameter of the TMC monofilaments, the spacing between the filaments in each collinear layer (cell length), and the degree of diffusion bonding.

2. The out-of-plane modulus of square TMC lattice structures has been shown to scale linearly with $\bar{p}$. The compressive peak strength was controlled by cooperative elastic buckling and varied with $(\bar{p})^3$.

3. The specific strength and stiffness of the lattices significantly exceeds that of other lattice structures fabricated to date.

4. These TMC lattices appear promising candidates for elevated temperature, multifunctional applications.

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