<http://www.diva-portal.org>

Postprint

This is the accepted version of a paper published in *Materials & Design*. This paper has been peer-reviewed but does not include the final publisher proof-corrections or journal pagination.

Citation for the original published paper (version of record):

Schneider, C., Velea, M., Kazemahvazi, S., Zenkert, D. (2015)

Compression properties of novel thermoplastic carbon fibre and poly-ethylene terephthalate fibre composite lattice structures.

Materials & Design, 65: 1110-1120

<http://dx.doi.org/10.1016/j.matdes.2014.08.032>

Access to the published version may require subscription.

N.B. When citing this work, cite the original published paper.

Permanent link to this version:

<http://urn.kb.se/resolve?urn=urn:nbn:se:kth:diva-158376>

Compression properties of novel thermoplastic carbon fibre and PET fibre composite lattice structures

C. Schneider¹, M.N. Velea^{1,2*}, S. Kazemahvazi³, D. Zenkert¹

¹*KTH, Department of Aeronautical and Vehicle Engineering, Stockholm, Sweden*

²*Transilvania University of Braşov, Department of Mechanical Engineering, Romania*

³*University of Cambridge, Department of Engineering, Cambridge, United Kingdom*

Abstract

A novel manufacturing route to efficiently produce fibre composite lattice structures has been developed. By using thermoplastic composite materials, flat sheets have been continuously folded, cut into a lattice shape and joined into a sandwich structure. Carbon fibre reinforced PET and PET fibre reinforced PET materials have been used to explore two different core options; a carbon fibre option which gives high performance but low recyclability and a single polymer PET option which gives lower performance but full recyclability. Parametric numerical simulations have been used to investigate how the various manufacturing parameters affect the mechanical performance of the core. The carbon fibre composite cores have mechanical performance on-par or better than existing metallic and composite lattice cores presented in literature. Single polymer PET cores show better performance compared to high-end foam cores but have considerable lower performance than carbon fibre lattice cores.

Keywords: Lattice structures, self-reinforced polymers, thermoplastic composites

* Corresponding author, Tel.: +40723676387

Email address: marian.velea@unitbv.ro

1 Introduction

Usage of composite materials and sandwich structures have become a common approach to reduce weight of structures in the aerospace and, more recently, in the automotive industry. While the relatively low-volume production of aerospace industry has its main focus on performance and quality, the high volume automotive industry puts more focus on manufacturing lead times and costs. In addition, the automotive industry is currently operating under stringent legislation on CO₂-emissions and recyclability of materials. Thus, although composite materials and sandwich structures can significantly contribute in reducing structural weight and thereby in-service emissions, they still need to prove themselves as “high volume manufacturing materials” with recycling potential.

Numerous studies have focused on further enhancing the weight specific mechanical properties of composite sandwich structures by arranging the material in a more geometrically efficient way. Such efforts include all-composite corrugated cores [1-2], square honeycomb [3] cores and pyramidal lattice truss cores [4-8]. Although many of these novel structures show exceptional weight specific strength and stiffness, they are often complicated to manufacture and more suitable for laboratory scale production. There are currently a number of different routes to manufacture composite lattice truss cores. Xiong et al [7] used an approach where small manually cut carbon fibre/epoxy pre-preg strips were placed into a mould to create each of the four struts of a pyramidal lattice truss. Yin et al [9] used an approach where a corrugated carbon fibre reinforced epoxy plate was manufactured in a mould and then small strips were cut in the transverse direction of the corrugation. The strips were subsequently assembled and bonded together perpendicularly to form a pyramidal lattice truss structure. George et al

[10] used a similar approach where small strips were water-jet cut out from a flat carbon fibre reinforced epoxy sheet in a corrugated type shape. These were then assembled and bonded together perpendicularly in the same manner to form a pyramidal lattice truss structure. Although these examples of manufacturing methods for carbon fibre lattice truss cores are innovative, they do not lend themselves for high volume and low-cost production.

In the present paper we use fibre reinforced thermoplastic materials to develop a novel continuous (and potentially high volume) manufacturing method for composite lattice truss structures. The manufacturing process is reminiscent of that developed for metal structures by Velea et al [11-12]. We employ two different material concepts; Carbon fibre reinforced PET for high mechanical performance and PET fibre reinforced PET (self-reinforced composite) for low material cost and full recyclability.

The aims of the study are as follows.

- i. Demonstrate a continuous and potential high volume manufacturing method to produce composite lattice truss cores.
- ii. Investigate how different geometrical parameters of the manufacturing method affect the mechanical properties of the core.
- iii. Investigate the potential of using a low-performance material (self-reinforced PET) in a geometrically efficient design to enhance overall structural performance.

The paper is outlined as follows. We will start by describing the constituent materials and provide an in-depth description of the novel manufacturing method. This is

followed by a summary of the results from the experimental programme and a parametric investigation of properties using finite element analysis. The paper will be finalized by comparing the performance of the novel lattice truss cores with currently available commercial materials and selected lattice truss structures available in literature.

2 Materials and manufacturing route

2.1 Description of constituent materials

Two different materials are used in this study; poly-ethylene terephthalate fibre reinforced poly-ethylene terephthalate (SrPET) and carbon fibre reinforced poly-ethylene terephthalate (CPET).

The SrPET composite consists of a low melting temperature matrix PET (termed LPET) and a high tenacity PET (termed HTPET) as fibre material. The melting temperature of the LPET is approximately 170°C whereas the HTPET melts at 260°C. During consolidation the temperature should be as high enough to melt the LPET and wet the fibers but not too high so that the HTPET fibers degrade and lose their reinforcing properties. A previous study showed that sound laminates with good mechanical properties can be consolidated at 220°C for 20 min under a pressure of 1.5bar [14].

The used SrPET material used in this study is a commingled balanced 2/2 twill fabric with an areal weight of 0.75 kgm⁻² and 50% reinforcement fibres measured by weight (supplied by Comfil®APS [13]). Using the above mentioned process parameters, one layer of woven fabric results in a lamina with a thickness of 0.5mm and a material density of 1380kgm⁻³.

The second material is a carbon fibre composite where LPET fibres are commingled with carbon fibres to produce yarns. The commingled yarns are then woven into a balanced 2/2 twill fabric with an areal weight of 0.50 kgm^{-2} and 54% carbon fibres measured by weight (supplied by Comfil®APS [13]). The fabric was consolidated using the same processing parameters as for the SrPET composite. One layer of carbon fibre / LPET weave produces a lamina with a thickness of 0.3mm and a density of 1600 kgm^{-3} .

2.2 *Quasi-static response of the constituent materials*

The quasi-static in-plane compression stress-strain histories for the CPET and SrPET are presented in figure 1. For details on test procedures and specimens dimension readers are referred to previous work performed by Schneider et al [14]. The compression modulus and ultimate compression strength for the SrPET composite is $5.3 \pm 0.2 \text{ GPa}$ and $94.7 \pm 0.7 \text{ MPa}$ respectively. A material yield point is observed at 35 MPa after which the stiffness of the material reduces and results in softening. For the CPET composite the compression modulus is $48.9 \pm 3 \text{ GPa}$ and the compressive strength $285.3 \pm 9 \text{ MPa}$. The strain-to-failure for the CPET is $0.7 \pm 0.1\%$ whereas the more ductile SrPET show failure strains of more than 20% [14].

2.3 *Manufacturing route for lattice truss core material*

The manufacturing route for the composite lattice truss core material consists of three steps as depicted schematically in figure 2 and shown for a CPET material in figure 3. The first step (I) is to form a corrugated plate shape. In a mass production setting this would be performed using a continuous process such as hot-roll forming of the thermoplastic sheet material but in this laboratory setting we have hot formed the

material using machined corrugated aluminium moulds (using the same process as described in section 2.1). In the second step (II), slots with a width, c , are machined out with a distance, d , from each other. In the third and final step (III), the core is heated and expanded into its final shape with the desired longitudinal truss angle β (in the y -direction). These three steps can all be performed simultaneously while the core is expanded in the y -direction. The final result of an expanded CPET lattice truss core is shown in figure 3.

2.4 Manufacturing of face sheets and complete sandwich structure

In order to enhance the bonding between the core and face structures, an approach is presented where the face sheets have stringers which provide support for the lattice truss core as shown in figure 4a. This both increases the surface area of the bonding between the lattice truss core and face sheets but it also changes the load introduction so that axial loads in the core struts translate into compressive or tension forces on the stringers.

The face sheets used in this laboratory set-up have been made using 4 layers of the fabric for the SrPET structure and 4 layers of fabric in the CPET structure. This results in a face sheet thickness of 1.8mm and 1.1mm for the SrPET and CPET structure respectively. To build up the triangular section of the stringers, additional SrPET or CPET material was used. An alternative to adding SrPET or CPET material is to instead fill the stringers with polymer foam in order to not add significant weight to the face sheets, this has however not been investigated further in the present study.

When the face sheets have been pressed into their final shape they are bonded to the core using a hot-plate bonding method. In this method, the bond surfaces are heated to about 200°C instantly (which is above the matrix melting temperature) using pre-heated aluminium plates. When both surfaces have been heated, they are pressed together for a few seconds until the material has cooled down and solidified. This method allows for a rapid and strong bond of the material.

The final lattice truss sandwich plate, shown in figure 4b, was then cut into individual unit cells in order to perform the out-of-plane compression experiments. A detailed description of the unit cell geometry is given in proceeding section.

3 Experimental Protocol

3.1 Selection of lattice truss core geometrical parameters

The manufacturing route for the lattice truss core presented in this study provides the possibility of selecting seven independent geometrical variables as shown in figure 2. By altering these geometrical variables, different core relative densities can be achieved with different resulting mechanical properties. The relative density of the core, defined as the density of the core normalised by the density of the constituent material, is defined as,

$$\rho_r = \frac{t[2c(l_1 + l_2) - b(2l_1 + l_2)]}{(l_2 + l_1 \cos \alpha)[(t + 2l_1 \sin \alpha) \cos \beta + 2c \sin \beta][(c - b) \cos \beta + (t + l_1 \sin \alpha) \sin \beta]} \quad (1)$$

where each individual variable is depicted in figure 2. Equation 1 has its minimum (i.e. lowest relative density) for core configurations where the expanding angle, β , is 45° and

the corrugation angle, ω , is 56° . In order to manufacture a core with a low relative density but yet keep the laboratory scale manufacturing straight forward, these two parameters have been fixed to 45° in the experimental part of this study. In order to simplify manufacturing of the cores, we have also chosen a fixed l_1/d ratio of 2.9 where the strut length $l_1=28.3$ mm and the cut distance $d=9.7$ mm.

3.2 *Unit-cell geometry for out-of-plane compression experiments*

In order to investigate the out-of-plane compression properties of the sandwich structure, individual unit cells of dimensions $L=42$ mm and $W=60$ mm were cut-out from plates, see figure 5a. After being cut-out, the unit cell face sheets were then bonded to 5 mm CPET plates in order to minimize face sheet deformation during testing and therefore characterize the core properties only. For each material configuration, two types of strut slenderness ratios (t/l_1) were investigated as summarized in table 1.

3.3 *Compression experiment apparatus*

All experiments were performed in a screw-driven Instron 5567 testing machine at a constant compression velocity of 1 mm per minute resulting in a compressive strain rate of 10^{-3} per second over the core thickness. Load was measured using a 30 kN load cell and compressive displacements were measured using a clip-gauge extensometer mounted between the steel loading platens. Compressive stress over the specimen was calculated by dividing the measured load by the surface area of the unit cell ($42 \times 60 \text{ mm}^2$ in figure 5a). Compressive strain was calculated by dividing the measured displacement by the core height under the assumption that the face sheets exhibit small out-of-plane displacements compared to the core.

4 Description of finite element model

4.1 *Model geometry and loading conditions*

A unit cell finite element model has been developed in the commercial finite element software Hypermesh [20] to investigate how different geometrical parameters affect the mechanical properties of the lattice truss core. Figure 5b shows the theoretical unit-cell used to model the specimens. The only difference between the manufactured specimens and the theoretical unit-cell is the size of the face stringers. In order to reduce the amount of elements in the finite element model, the part of the stringers that do not contribute as support for the core struts have been removed. This does, however, not affect the out-of-plane compressive response of the unit cell. The modelled unit cell and the applied boundary conditions are shown in figure 6. At each boundary (where core struts meet the face stringers), a tie condition has been employed using the assumption that core struts are perfectly bonded to the face stringers. The tie condition imply that all translational and rotational degrees of freedom are the same as for the face sheet stringers. The bottom face sheet stringers have been assumed to be in a clamped condition so that translational degrees of freedom of the solid brick elements are restricted. An evenly distributed pressure has then been applied to the upper face sheet stringer to simulate a compressive force on the unit cell. Each core configuration was modelled using both a linear elastic buckling analysis and a non-linear implicit analysis in order to capture both instability and material failure mechanisms. Solid brick elements of type CHEXA was used to model the face sheet stringers and composite shell elements of type CQUAD4 were used to model the individual core member struts.

4.2 *Initial geometrical imperfections*

To simulate the structural imperfections caused during the manufacturing process of the core material, initial geometrical imperfections have been applied to the core. It has been assumed that the geometrical imperfections of each core member have the shape of its first eigenmode. To introduce this imperfection a linear buckling analysis was performed in Hypermesh and the first eigenmode was computed. The deformed core member geometry, in the shape of the first eigenmode, was then used as the initial geometry of the non-linear implicit analysis. As CPET is more sensitive to geometric imperfections than SrPET, the magnitude of the deformed shape was scaled differently, by trying to reach the best correlation with the experiments, so that the maximum initial transverse deflection was 10% and 5% of the core member thickness for SrPET and CPET respectively.

4.3 *Material models*

The SrPET material was modelled as an elastic-plastic material (using the MAT1 card and its extension - MATS1, in Hypermesh). The material modulus, yield point and stress-strain history measured experimentally were used as input data for the FE model. The CPET material was modelled using the in-built orthotropic material model in Hypermesh (MAT8 card and its extension - MATX43) where the experimentally measured value for the both the longitudinal and the transversal moduli was used. The transverse shear moduli G_{13} and G_{23} and the in-plane shear modulus were assumed to be the same, having a value of 2000 MPa. We assume that the CPET material fails when the maximum longitudinal strain attains the experimentally measured quasi-static failure strain of 0.7%. This relatively straight forward failure modelling approach has previously been proven successful by Tagarielli et al [15].

5 Summary and discussion of experimental results and FE predictions

In order to validate the accuracy of the FE model we will start by giving a comparison between the experimentally measured stress-strain responses and the different failure mechanisms to the predictions made by the FE model.

5.1 Summary of results for SrPET lattice cores

The stress-strain response and the corresponding *in-situ* photographs for the slender SrPET-1 core configuration is shown in figure 7. The overall stress-strain response predicted by the FE model shows good agreement with experimental findings. At low compressive strains ($<2.5\%$, point 1 in figure 7a), a linear elastic loading behaviour is observed experimentally and in the numerical FE model. There is minimal out-of-plane deformation of the core struts and no plastic deformation is recorded in the FE model. At a compressive strain of $\sim 4.5\%$ (point 2 in figure 7a), the core members buckle into a shape similar to a half sinus wave which is followed by a rapid drop in stress level. This drop in load level occurs while the material is still in its elastic range. Shortly after the immediate load drop, the stress strain curve starts to flatten out. At this strain point (around point 3 in figure 7a), we observe onset of plastic deformations in the FE model which results in a less dramatic load drop and a more controlled softening type of deformation (point 3-4 in figure 7a). The FE-model predicts a lower post buckling load drop ($\sim 20\%$) as compared to the experimental findings where a somewhat higher post buckling load drop is observed ($\sim 35\%$). As the specimen is compressed further, a mid-span plastic hinge is formed on the core members and eventually they fold and come into contact with the face sheets which results in increased load levels and core densification.

Turning to the more stubby SrPET-2 core (figure 8), a similar stress-strain response can be observed but with a less pronounced immediate load drop after the peak load. In this case, the onset of plastic deformation in the FE model occurs directly at the peak load (point 2 in figure 8a) causing the less pronounced load drop. This is an effect of the higher core member thickness which provides better resistance to buckling (higher bending stiffness) causing the core members to yield first prior to onset of buckling. After a plastic hinge has formed, approximately mid-span of the core member, the load however continues to drop beyond compressive strains above 40%.

It is noted that the FE model is able predict the stiffness of the slender core structures with adequate accuracy (typically within 10%) but larger discrepancy is found for the stubby cores (up to 23%). No apparent explanation could be found for this discrepancy.

5.2 *Summary of results for CPET lattice cores*

The measured and predicted stress-strain history of the slender CPET-1 core is shown in figure 9. A linear elastic loading behaviour is observed up to $\sim 2\%$ compressive strain (close to point 2 in Figure 9a) both in the experiments and the numerical model after which a sudden load drop is observed. Core member compressive material failure is noted in both the experimental measurements and numerical predictions as indicated by the black arrow in figure 9b. A considerable variation in stiffness is noted between the experimental measurements elucidating the sensitivity to manufacturing imperfections.

The measured and predicted stress-strain history for the stubbier CPET-2 core is shown in figure 10. The peak stress of 2 MPa occurs at a compressive strain of about 2.7% (point 2 in Figure 13a). At 2.7% core compressive strain, the strain in each core member

strut is approximately 0.7% which is close to the measured material failure strain. Compressive material failure of the core members could be observed in the experiments and FE model resulting in a catastrophic failure mode with sudden drop in stress. The peak stress and loading stiffness is predicted with good accuracy for both the CPET-1 and CPET-2 configuration.

6 Parametric investigation and comparisons to existing core materials

In the previous section it was shown that the FE model developed herein is able to predict stress-strain response and failure mechanism of most core configurations with good accuracy. The only exception is the core stiffness prediction for the stubby SrPET cores where discrepancies up to 20% were found. For the purpose of providing an approximation of the core performance, this level of accuracy is adequate and comparable to commonly used theoretical models. In this section we will use the FE model to investigate how the different manufacturing parameters, depicted in figure 2, affects the out-of-plane stiffness and strength of the core material.

6.1 Effect of core strut cross-section geometry on out-of-plane compressive response

We start by investigating the effect of core strut cross-section geometry on the weight normalized compressive strength and stiffness of the core. The strut cross-section geometry is defined by the two variables d and t which describe the cross section width and thickness respectively.

In figure 11 we show the effect of core strut slenderness by keeping all variables fixed ($\omega=45^\circ$, $\beta=45^\circ$, $d=7.7\text{mm}$, $c=2\text{mm}$, $l_1=22.3\text{mm}$, $l_2=10\text{mm}$) while changing the strut thickness, t .

Starting with the SrPET core, we note a major and linear increase of the weight normalized strength up to $t/l_I = 0.09$ and a minor one further on. This is the point where the failure mode switches from buckling to material compression failure. When the failure is governed by strut buckling, the core strength increases approximately in cube to the strut thickness while the density increases linearly with the strut thickness resulting in an overall increase in normalized core strength. When the governing failure mode is material compression failure, strength and density increase linearly with thickness resulting in a plateau for the weight normalized strength. The stiffness of the core is constant with changing strut thickness with one exception: At very low strut slenderness ratios, the effect of initial imperfection on core stiffness becomes significant as the core struts have very low bending stiffness.

The CPET core shows the same stiffness and strength behaviour as the SrPET core with the difference that the major increase in normalized strength is up to $t/l_I = 0.05$ instead of $t/l_I = 0.09$. This is since the ratio between material strength and material stiffness is an order of magnitude smaller for the CPET material compared to the SrPET material. The effect of this is that the switch in failure mode from buckling to material compression failure will occur at a lower strut slenderness ratio.

Turning to the strut width, d , we explain its effect on the mechanical behaviour as follows. When the struts are stocky enough so that failure is governed by material compression failure, the compressive strength of the core will change linearly with the increased strut cross section area (t times d). As the core density changes linearly with the strut cross section area, the density normalized core strength will be constant when t/d changes. In the case where failure is governed by strut buckling we will see a different behaviour: The core strut will always buckle in the direction where we have

the lowest second moment of inertia meaning that the smaller of the two dimensions, t and d , will define the direction where we get buckling. For low core relative densities where failure is governed by core strut buckling, the optimal width to thickness ratio, t/d , will thus be 1.

6.2 *Effect of expansion and corrugation angle on out-of-plane compressive response*

We proceed by investigating the effect of core expansion angle, β , and corrugation angle, ω , on the weight normalized compressive strength and stiffness of the core.

6.2.1 *Effect of expansion angle*

In figure 12a we show the effect of expansion angle on the weight normalized stiffness and strength of the core. We vary the expansion angle in the interval of 45° to 80° while keeping all other variables fixed. An expansion angle of 90° would result in the structure in stage II of the manufacturing process (see figure 2); i.e. a corrugated structure with slots at a distance d from each other.

An increasing weight specific stiffness and strength is observed for both CPET and SrPET cores as the expansion angle increases. For the weight specific stiffness, this increase continues towards 75° - 80° where a plateau is reached. Looking at the core strength, we notice a similar increase in weight specific strength but the peak weight specific strength is found for an expansion angle of approximately 60° after which the weight specific strength starts to drop again. Although the stiffness and strength increases continuously with the expansion angle the core density increases more rapidly for $\beta > 60^\circ$, which causes the plateau and the drop in weight specific stiffness and strength.

6.2.2 *Effect of corrugation angle*

In figure 12b we show the effect of corrugation angle, ω , on the weight specific strength and stiffness of the core. Similar to the core expansion angle, we note an increasing weight specific strength as function of corrugation angle. The weight specific stiffness of both the CPET and SrPET increase up to the maximum corrugation angle of 90° while the weight specific strength shows a peak at a corrugation angle of approximately 75° . The drop in weight specific strength is caused by the rapidly increasing core density as the corrugation angle increases beyond 70° as can be derived from equation 1.

It is worth emphasising that the optimum expansion and corrugation angle for cores subjected to shear load, is likely to be different from the one found for out-of-plane compression.

6.3 *Comparisons with existing core materials*

Based on the parametric studies described above, we now choose the manufacturing parameters for the expanded lattice core so that we create a close to optimal geometry for out-of-plane compressive loading. The core strut cross-section ratio, t/d , is chosen as close to unity as possible. The expansion angle, β , is chosen to be fixed at 60° and the corrugation angle, ω , is chosen to be fixed at 75° . We then vary the core density by changing the core strut thickness, t , while keeping the core strut length, l_l , fixed.

The out-of-plane stiffness as function of density is plotted for a number of sandwich core materials in figure 13. The CPET lattice cores shows very competitive stiffness compared to both traditional foam cores and to recently developed pyramidal lattice cores [6]. At higher core densities, the CPET core have considerably higher out-of-plane compressive stiffness than both pyramidal titanium [16] and carbon fibre cores [6,7].

The SrPET lattice cores show stiffness similar to high-end PMI Rohacell cores but perform considerably worse than carbon fibre composite lattice cores at densities above 20kgm^{-3} .

The out-of-plane compressive strength as function of density is plotted in figure 14. At low core densities ($<40\text{kgm}^{-3}$), the CPET lattice cores have a strength comparable to the structures presented in [6] and [7]. At these low core densities no competing metallic cores exist. The strength of the CPET structures increases rapidly with increasing density as the core members become more resistant to buckling failure. Similar trend is seen for the pyramidal cores presented in [6] which have relatively low strength at very low densities (20kgm^{-3}) and then increase rapidly. At higher densities, the CPET lattice core outperforms other core materials except for the corrugated carbon fibre core presented in [1]. Worth noting is that the non-optimal CPET lattice core, which have been tested experimentally in this study, shows similar performance as the tetrahedral AA6061 [21] and the pyramidal titanium core [16].

The performance of the SrPET lattice core is competitive at very low densities. This is mainly since SrPET has lower density than CPET and the core members are therefore thicker and more buckling resistant. At intermediate densities, the SrPET cores have strength performance which is better than high-end foam cores and comparable to metallic pyramidal and tetrahedral lattice cores. The non-optimal SrPET cores, which have been tested experimentally within this study, shows relatively poor performance on par with commercially available polymeric foams.

7 Concluding remarks and discussion

It has been demonstrated that thermoplastic composite materials can be used to create lattice structures using an efficient and scalable manufacturing method. The following

conclusions can be made regarding the mechanical performance of these lattice materials:

- i. The lattice structures manufactured from carbon fibre reinforced PET show both competitive strength and stiffness at a range of densities. The experimentally tested non-optimal configurations shows mechanical properties that are on par with high end metallic cores. It has however been demonstrated that a significant improvement in mechanical properties can be achieved by changing the geometrical properties of the cores so they are more suited for out-of-plane compressive loading.
- ii. The lattice structures manufactured from PET fibre reinforced PET show similar stiffness and strength performance as high-end polymeric foams but perform considerably worse than metallic and carbon fibre composite lattice structures, especially in terms of stiffness. If a more optimal core geometry is chosen, performance similar to metallic lattice cores can be achieved. Further, the SrPET lattice cores show non-catastrophic and ductile failure modes (as opposed to the carbon fibre based cores) which is beneficial from an energy absorption point of view.

Although the SrPET lattice structures show limited benefits over traditional foam structures when it comes to stiffness, they can still be interesting for strength dimensioned designs where low weight and high energy absorption is important. Further, SrPET lattice structures are fully recyclable as opposed to PVC and PMI foam cores which is key for e.g. automotive applications (such as car bumper beams).

Acknowledgements

The financial support for this investigation has been provided by the Centre of ECO2 Vehicle Design at the Department of Aeronautical and Vehicle Engineering, Royal Institute of Technology, KTH Sweden and the Swedish Agency for Innovation Systems (VINNOVA). Dan Zenkert was partly funded by Vinnova SIO Lighter. We acknowledge the support from Comfil in providing material.

References

- [1] Kazemahvazi, S., Tanner, D., Zenkert, D., (2009), Corrugated all-composite sandwich structures. Part 2: Failure mechanisms and experimental programme, *Composites Science and Technology* 69:920 - 925
- [2] Kazemahvazi, S., Zenkert, D., (2009) Corrugated all-composite sandwich structures. Part 1: Modeling, *Composites Science and Technology*, 69:913 – 919
- [3] Russell, B.P., Liu, T., Fleck, N.A., Deshpande, V.S., (2011), Quasi-static three-point bending of carbon fiber sandwich beams with square honeycomb cores, *Journal of Applied Mechanics, Transactions ASME*, 78
- [4] K. Finnegan, G. Kooistraa, H. N. G. Wadley, V. S. Deshpande, (2007), The compressive response of carbon fiber composite pyramidal truss sandwich cores, *Int. J. Mat. Res.* 98
- [5] Bing Wang, Guoqi Zhang, Qilin He, Li Ma, Linzhi Wu, Jicai Feng, (2014), Mechanical behavior of carbon fiber reinforced polymer composite sandwich panels with 2-D lattice truss cores, *Materials and Design* 55:591–596
- [6] Li, M., Wu, L.Z., Ma, L., Wang, B., Guan, Z.X., (2011), Mechanical response of all-composite pyramidal lattice truss core sandwich structures. *J. Mater. Sci. Technol.* 27:570–576.

- [7] Xiong, J., Ma, L., Wu, L.Z., Wang, B., Vaziri, A., (2010). Fabrication and crushing behavior of low density carbon fiber composite pyramidal truss structures. *Compos. Struct.* 92:2695–2702.
- [8] Xiong, J., Ma, L., Pan, S., Wu, L., Papadopoulos, J., Vaziri, A., (2012). Shear and bending performance of carbon fiber composite sandwich panels with pyramidal truss cores. *Acta Mater.* 60:1455–1466
- [9] Sha Yin, Linzhi Wua, Steven Nutt, (2013), Stretch–bend-hybrid hierarchical composite pyramidal lattice cores, *Composite Structures* 98, 153–159
- [10] Tochukwu George, Vikram S. Deshpande, Haydn N.G. Wadley, (2013), Mechanical response of carbon fiber composite sandwich panels with pyramidal truss cores, *Composites: Part A* 47, 31–40
- [11] Velea, M.N., Wennhage, P., Lache, S., (2012), Out-of-plane effective shear elastic properties of a novel cellular core for sandwich structures, *Materials & Design*, 36, 679–686.
- [12] Velea, M.N., Lache, S., (2012), In-plane effective elastic properties of a novel cellular core for sandwich structures, *Mechanics of Materials*, 43, 377–388
- [13] Comfil® APS. Thermoplastic Composites, DK 8883 Gjer, Denmark, 08.2013, www.comfil.biz.
- [14] C. Schneider, S. Kazemahvazi, M. Åkermo, D. Zenkert. Compression and tensile properties of self-reinforced poly(ethylene terephthalate)-composites. *Polymer Testing*, 32(2):221-230, 2013
- [15] V.L. Tagarielli, V.S. Deshpande, N.A. Fleck, (2010), Prediction of the dynamic response of composite sandwich beams under shock loading, *International Journal of Impact Engineering*, 37, 854–864

- [16] D.T. Queheillalt, H.N.G. Wadley, (2009), Titanium alloy lattice truss structures, *Materials and Design*, 30, 1966–1975
- [17] DIAB Group, Divinycell® H Technical data, SE- 312 22 Laholm, Sweden.
www.diabgroup.com
- [18] DIAB Group, Divinycell® PR Technical data SE- 312 22 Laholm, Sweden.
www.diabgroup.com
- [19] Evonik Industries AG, ROHACELL® IG/IG-F Properties, 64293 Darmstadt, Germany. www.rohacell.com
- [20] Altair HyperWorks 12, Online Help and Documentation; 2013.
- [21] Gregory W Kooistra, Vikram S Deshpande, Haydn N.G Wadley, (2004), Compressive behavior of age hardenable tetrahedral lattice truss structures made from aluminium, *Acta Materialia*, 52:4229-4237

Figure 1: Compression stress strain diagram for CPET and SrPET [14]

Figure 2: Three key steps in manufacturing the novel expanded lattice truss structure

Figure 3: CPET material in the process of expansion from a corrugation to a lattice truss structure (left) and final result of an expanded lattice truss core structure made of CPET with $\beta=\omega=45^\circ$, $l_1=28.28$ mm, $l_2=10$ mm, $t=1.8$ mm, $d=9.7$ mm and $c=2$ mm (right).

Figure 4: (a) SrPET lattice truss core which has been hot-plate bonded to one face sheet with reinforcing stringers and (b) final sandwich plate with stringer reinforced face sheets and a lattice truss core

Figure 5: (a) Unit-cell specimen geometry used to perform out-of-plane compression experiments and (b) theoretical unit-cell used in finite element model

Figure 6: Finite element model unit cell showing boundary conditions and applied loads from 4 different viewing angles

Position	Experiment	FE-model
1		
2		
3		
4		

Figure 7: (a) Stress-strain response of SrPET-1 as measured experimentally and predicted by finite element model. (b) Deformation mechanism at various strain levels

Position	Experiment	FE-model
1		
2		
3		
4		

Figure 8: (a) Stress-strain response of SrPET-2 as measured experimentally and predicted by finite element model. (b) Deformation mechanism at various strain levels

Position	Experiment	FE-model
1		
2		
3		

Figure 9: (a) Stress-strain response of CPET-1 as measured experimentally and predicted by finite element model. (b) Deformation mechanism at various strain levels

Position	Experiment	FE-model
1		
2		
3		

Figure 10: (a) Stress-strain response of CPET-2 as measured experimentally and predicted by finite element model. (b) Deformation mechanism at various strain levels

Figure 11: Effect of strut slenderness ratio (t/l_1) on out-of-plane compressive stiffness and strength

Figure 12: (a) Effect of expansion angle (β) on out-of-plane compressive stiffness and strength and (b) effect of corrugation angle (ω) on out-of-plane compressive stiffness and strength

Figure 13: Core stiffness as function of core density for a range of different sandwich core materials

Figure 14: Core strength as function of core density for a range of different sandwich core materials

Name	Material	Strut thickness t , (mm)	Strut length l_i , (mm)	Relative core density, (-)	Core density, (kgm ⁻³)
SrPET-1	HTPET/LPET	1.8	28.28	0.041	57
SrPET-2	HTPET/LPET	2.7	28.28	0.062	86
CPET-1	Carbon/LPET	1.1	28.28	0.024	40
CPET-2	Carbon/LPET	1.6	28.28	0.037	60

Table 1: Four different core configurations tested in the experimental program. All geometries have $\beta=\omega=45^\circ$, $l_2=10$ mm, $d=7.7$ mm and $c=2$ mm.