Impact response of ductile self-reinforced composite corrugated sandwich beams

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Abstract

Corrugated sandwich beams made entirely from self-reinforced poly (ethylene terephthalate) SrPET are manufactured and tested dynamically. The beams are subjected to impact loading at the mid-span using a metal foam projectile and the beam deflection is measured. For sandwich beams with a constant areal mass, beams with a high mass portion in the core webs outperform configurations with a high mass portion in the face sheets (given that the face sheets are thick enough to carry the transversal loads induced by the core webs). Reinforcing the face sheet – core web interface further improves the impact response. The strain rate sensitivity of SrPET has also been investigated. A three-dimensional finite element (FE) model has been developed to simulate the impact event and a good agreement is found with the measured response. It is found that corrugated sandwich beams made from SrPET has competitive impact performance compared to similar sandwich beams with equal mass and geometry out of aerospace grade aluminium and carbon fiber / high performance foam sandwich.

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1. Introduction

Composite sandwich structures are commonly used in lightweight structures and it is of high importance to understand their behaviour under both quasi-static and dynamic loading conditions. Extensive work has been performed to measure and to predict the properties of composite sandwich structures under quasi-static loading [1, 2]. The three-point bending response of composite sandwich beams comprising of glass and carbon fiber reinforced plastic face sheets with foam cores as well as with prismatic corrugated cores has for instance been investigated in detail [3-7]. To date, most research on the impact behaviour of composite sandwich structures has focused on systems made from brittle glass or carbon fibres. As an example, the impact behaviour of clamped and simply supported glass and carbon fiber based composite sandwich beams has been investigated by Tagarielli et al., Russell et al and Tekalur et al [8-10]. A fundamental issue with the inherent brittleness of these systems is that although they perform well when the structures deform in their elastic regime, they tend to fail catastrophically when a specific level of impact momentum is reached [9]. In contrast, metals show a more benign transition at increased levels of impact momentum due their ability to deform plastically [11]. The chief disadvantage with metal alloys is their high density; this necessitates that the structures become very thin walled and buckling dominated structures at low areal densities.

An alternate approach has recently been explored by Schneider et al. [12] where ductile self-reinforced polymer (SrP) composites have been used to create sandwich structures. SrP composites (also referred to as single-polymer or all-polymer composites) are an emerging group of composite materials where the reinforcing fibers and the matrix material are made from the same base polymers [13, 14]. As these are fully thermoplastic, they can be manufactured in a cost effective way and are recyclable. The fundamental drawback with

SrPs is their low stiffness compared to traditional glass or carbon fibre composites but they show great promise in their ability to absorb energy [15].

Although a growing area of research, few studies have been performed on making composite sandwich structures out of SrPs [12, 16-19]. The quasi-static three-point bending properties of sandwich beams with a prismatic core has however recently been investigated by Schneider et al. [19]. These beams showed high energy absorption capability and outperformed their Aluminium alloy counterparts (of similar geometry and mass) in both peak load and energy absorption. In addition, their performance was on par with sandwich beams made from carbon/epoxy face sheets and high performance polymer cores.

The objective of the present work is to investigate the impact behaviour of corrugated sandwich beams out of SrPs in order to understand if the high quasi-static energy absorbing capability of the SrP material can also be utilized to create high performing impact resistant composite structures. Two different manufacturing techniques will be explored, one where the joints between the sandwich core members and the face sheets are bonded using the base polymer as adhesive and one where the joints are stitched together using Kevlar wrapped steel thread.

2. Materials and Manufacturing

2.1. Constituent material and consolidation routine

A self-reinforced poly(ethylene terephthalate) (SrPET) fabric, comprising of commingled yarns with 50 wt.% high tenacity PET (HTPET) reinforcing fibres and 50 wt.% amorphous PET fibres (matrix material) was used. The reinforcing HTPET fibres melt at 260 °C. The chemically modified PET matrix material has a glass transition temperature at about 60°C and will at 160 – 180 °C have sufficiently low viscosity to fully impregnate the reinforcing PET fibers. The yarns are woven to a 4/1 warp/weft direction plain weave with four times more reinforcing fibres in the warp (termed $x1$ direction) than in the weft (termed $x2$ direction).

For the sake of completeness, the thickness direction is termed x_3 . The fabric, with an areal mass of 0.555 kg m^{-2} , was woven and supplied by Comfil® APS [20].

Before consolidating the fabric to a laminate, the SrPET fabric was dried for 24 h in a climate chamber at a relative humidity of 15 % and a temperature of 50 °C. Immediately after, the fabric is consolidated under 1 bar pressure above the ambient pressure at a consolidation temperature of 220 °C for 20 min and thereafter cooled at 10 °C / min back to room temperature. The consolidation routine was suggested by the material supplier [20]. A consolidated laminate had the density of 1350 kg m^{-3} .

2.2. Manufacturing of high rate tensile test specimens

The tensile test specimens were made from $\sim 1.5 \text{ mm}$ thickness flat sheet of SrPET material (4 layers of fabric) manufactured using the process described previously. All four layers of fabric were placed with the same principal fiber directions and tensile test specimens were cut-out in the x_1 direction (see also Fig. 1). The specimens had a dog-bone shaped geometry (although not following any particular standard) with a gauge length, $l_0=60 \text{ mm}$. The specimen width at the gripping area was 25 mm whereas the width of the test gauge length was 8 mm. The distance between the shoulders was 95 mm.

2.3. Manufacturing of flat monolithic beams

Flat monolithic beams were manufactured using 10 layers of SrPET (all placed with the fibers in the same direction) resulting in a laminate thickness of approximately 3 mm (more layers build slightly lower ply-thicknesses than few). From this laminate, beams of a total length of 300 mm and a width of 50 mm were cut-out where the long and short edge were parallel to the x_1 direction and x_2 direction, respectively. These tests were performed to validate the finite element model, see section 5.

2.4. Manufacturing of corrugated sandwich beams

Two different manufacturing techniques were used to produce the corrugated sandwich beams where the main difference lies in the joints between the core webs and the face sheets. In the first case, joints are not reinforced but only rely on the base polymer to transfer the loads and in the second case the joints were reinforced by Kevlar/Steel stitching. For both cases, the same aluminium mould with a trapezoidal cross-section was used with dimensions according to Fig. 1. To ensure successful de-moulding after consolidation, all parts of the mould were coated with a Tygovac RF260 Fluoropolymer FEP release film prior to consolidation.

The corrugated sandwich beams with reinforced joints are manufactured by firstly pre-consolidating the desired amount of SrPET fabric layers. **The PET matrix shrinks at about 75 °C [21] and to ensure that the matrix will not shrink, pre-consolidation was performed at 50 °C and applying 1 bar pressure above the ambient pressure to the fabric stack.** This procedure partially adheres the different fabric layers to each other making the handling of the fabrics considerably easier during the stitching process. The stitching was performed by the sail maker Gransegel AB [22] using a sail sewing-machine and a Kevlar/Stainless steel thread. Firstly, the pre-consolidated fabric stack for the bottom face was stitched to the core web fabric stack as shown in Fig. 1 ① where the seams are presented with dashed lines. In order to achieve a high degree of reinforcement, two parallel seams were stitched in each contact area between the core webs and the face sheets. The core web fabric stack was then stitched to the top face sheet fabric (see ② in Fig. 1). Subsequently, the first aluminium mould profile was placed between the core web fabric and the bottom face sheet fabric (see ③ in Fig. 1) and thereafter core web fabric could be stitched to the bottom face sheet fabric to form one unit (see ④ in Fig. 1). This sequence of placing and stitching would then be repeated (see ⑤ -⑥ in Fig. 1) until the desired sandwich panel width had been achieved. Finally, the stack of

SrPET fabric and aluminium moulds was consolidated in a hot-press under the same thermo- and pressure cycle as described in Section 2.2.

For the manufacturing of corrugated sandwich beams with unreinforced interfaces, the exact same manufacturing route was used as for the beams manufactured in previous study by Schneider et al [19]. It follows approximately the same steps as for the reinforced sandwich beams, except for the pre-consolidation and stitching. In this case, all fabric layers were placed in the mould at the same time.

Two different sandwich panel configurations with approximately the same areal mass of $8\text{--}8.5\text{ kg m}^{-2}$ were manufactured. To manufacture corrugated sandwich beams within the above defined areal weight range, a total of 14 layers of SrPET fabric were used.

The two different geometrical configurations of corrugated sandwich beams are labelled 383 and 545 where the 383 sandwich configuration has three fabric layers in the face sheets and eight fabric layers in the core web. Similarly, the 545 sandwich beams have five layers of fabric in the face sheets and four fabric layers in the core web. The mass distributions and overall beam geometry of the two configurations are given in Tab. 1 and Fig. 2. Due to the stitching performed with Kevlar/Steel, sandwich beams with reinforcements had an overall higher areal density than the ones without. In Tab. 1, areal densities for the reinforced sandwich beams are presented in brackets.

3. Experimental protocol

Two material tests and two structural tests have been performed. Material tests were performed both at low loading rates and at high loading rates (up to 1 m s^{-1}). The compression properties have been investigated and discussed in previous work [18] and therefore only the tensile properties are investigated here. Structural impact tests were performed on clamped monolithic beams and on simply supported corrugated sandwich beams. For the material tests, each loading velocity and configuration was tested at least three times and the average data is

reported. For the structural tests one specimen per loading velocity and configuration were tested.

3.1 Low rate testing of material properties

Low strain rate tensile tests were performed on a screw-driven uniaxial loading Instron 4045 testing machine equipped with a 30 kN load cell. All tests were performed with a constant cross head displacement of 1 mm / min. The strain was measured using a clip gauge extensometer with a gauge length of 50 mm resulting in a strain rate of

3.2 High rate testing of material properties

A servo-hydraulic Instron VHS160/20 with a maximum load capacity of 200 kN was used to perform dynamic testing at velocities v_0 in the range of $0.1-1 \text{ m s}^{-1}$ resulting in strain rates $\dot{\epsilon}$. Load was measured using a piezoelectric load cell and elongation Δl was measured with an optical length measurement system from Zimmer AG. The logarithmic strain was calculated by $\epsilon = \ln(1 + \Delta l / l_0)$ where l_0 is the gauge length of the tensile specimen. The strain rate was given as $\dot{\epsilon} = v_0 / l_0$. High speed photography was performed using a Phantom v.1600.1 to validate the strain rate and specimen elongation. More details about the testing machine, loading and clamping fixture are found in [23].

3.3 Impact loading of monolithic clamped beams

Monolithic beams were clamped on both sides so that the free span was 150 mm. Both ends of the beams were clamped over a 75 mm length using six M5 bolts.

Aluminium foam projectiles were used to simulate a blast loading of the monolithic beam [24]. Aluminium foam projectiles of length $l_p = 50 \text{ mm}$, diameter $d_p = 50.6 \text{ mm}$ were electro-discharged from a foam block of density $\rho_p \approx 300 \text{ kg m}^{-3}$. A gas gun of bore

diameter 50.8 mm and length of 4.5 m was used to fire the projectile at a velocity v_p in the range 50 m s^{-1} – 230 m s^{-1} . This provides a projectile momentum per unit area of $IO = \rho_p l_p v_p$ in the range 750 N s m^{-2} – 3450 N s m^{-2} . High speed photography (Phantom v.1600.1) was used to observe beam failure mechanisms, measure the projectile velocity and the beam rear face mid-span displacement. To assess the impact performance of the beams, two metrics were hence evaluated; (i) the mid-span deflection of the beams and (ii) the beam's ability to sustain different level of impact energies (analogue to the energy absorption capability of the beams).

3.4 Impact loading of simply supported sandwich beams

The same gas gun apparatus as for the impact loading of the monolithic beams was used to fire the same size and type aluminium projectiles and to measure the beam response. The support condition for the corrugated sandwich beams was however simply supported and not clamped as for the monolithic beam impact. The corrugated sandwich beams were placed on two support rollers of 25 mm diameter and a free span of 150 mm.

4. Summary of experimental results

4.1 High rate material mechanical properties

The measured uniaxial true tensile stress versus logarithmic strain histories for the SrPET material loaded in the x direction at a strain rate range $\dot{\epsilon} = 10^{-4} \text{ s}^{-1}$ – 10^1 s^{-1} is presented in Fig. 3a. For the sake of completeness, the uniaxial true compression stress versus logarithmic strain histories of the same is reproduced from [25] and given in Fig. 3b.

When loaded in tension, the SrPET composites show an initial elastic response followed by a strain hardening post yielding up to the ultimate fiber tensile failure strain of 15-17 %. No obvious changes in tensile modulus, tensile strength or tensile strain to failure could be

observed for the strain rates investigated here. A small change in yield strength from 74 MPa for the lowest tested strain rate to 87 MPa for the highest was however noted. As discussed in previous work [18], synthetic polymer fiber materials tend to show small strain rate dependence up to a specific transition point where the polymer chains “lock-up” and significant increase in stiffness and yield strength is observed. This transition point has previously been measured to 102 s^{-1} in compression loading of the SrPET material [18].

Using the currently available high rate tensile test equipment we were however not able to perform test at strain rates above 101 s^{-1} with a high degree of accuracy due to practical limitations in specimen gripping, ensuring uniform strain rates over the full specimen gauge length, reducing load cell ringing etc.

The compression response as function of strain rate is shown in Fig. 3b and a detailed discussion on the behaviour is found in [18, 25]. A difference in tension and compression is seen in the post-yield behaviour at large strains where the compression specimens suffer from shear band formation and eventually also delamination [18].

4.2 Impact loading of clamped monolithic beams

An overview of the results for the impact loaded clamped monolithic beams is found in Fig. 4 where the rear face maximum deflections at mid-span has been plotted as a function of the impulse per area, $IO=\rho plvp$. The beam mid-span displacements as a function of time are also presented in Fig. 5 for three different impulse levels. The maximum mid-span displacement δ_{max} increases approximately linearly with increasing impulse level. At an impulse level of 2604 N s m^{-2} , significant delamination damage is observed close to the supports as well as initiation of fiber tearing. At a loading impulse of 3450 N s m^{-2} , the beam fails completely via fiber tearing at the clamped supports.

A montage of a high speed photography sequence captured during testing at a loading impulse of 2604 N s m^{-2} is shown in Fig. 6. Time $t=0 \text{ s}$ is defined as the first time step where the projectile comes into contact with the beam. Between frames 1 and 4 ($t=240 \mu\text{s}$), a flexural wave travels from the beam mid-span and out towards the supports. As the flexural wave hits the supports the beam goes into a membrane stretching mode as the mid-span displacement increases and reaches its maximum at 31.3 mm for $t=500 \mu\text{s}$. At the maximum deflection, fiber fracture is observed followed by delamination close to the supports.

4.3 Impact loading of simply supported sandwich beams

4.3.1 Overview of results

An overview of the results for the simply supported sandwich beams is shown in Fig. 7 where the maximum rear face mid-span displacement is plotted as a function of loading impulse. For the sake of clarity, if two sandwich beams are subjected to the same impulse, the one with the smaller mid-span deformation exhibits higher performance. It can be observed that sandwich beam configurations which have a larger portion of their mass distributed to the core (configuration 383) outperform the same mass sandwich configurations that have a relatively more mass distributed to the face sheets. The 545 sandwich beams were able to sustain a loading impulse of approximately 2000 N s m^{-2} before failure (see Fig. 7a) whereas the 383 sandwich beams configurations could resist loading impulse above 3000 N s m^{-2} (see Fig. 7b). At low impulse levels, the difference in performance between the 545 and the 383 configuration however diminish and both configurations show approximately the same rear face deflections.

Comparing the sandwich beams with reinforced interfaces to the ones with unreinforced interfaces, the configurations with reinforced interfaces show lower rear face maximum deflections and fail at notably higher impulse levels.

At an impulse level of 2000 N s m^{-2} , we observed the largest differences between the two sandwich configurations where the 545 configuration deforms significantly more than the 383 configurations.

4.3.2 545 sandwich beam configurations

The measured rear face mid-span displacements of the 545 sandwich configuration subjected to an impulse level of 2000 N s m^{-2} is plotted as a function of time in Fig. 8. Here, the effect of reinforced interfaces can be seen clearly as the reinforced configuration deforms considerably less than the un-reinforced configuration. For the same impulse level, the sandwich beam with un-reinforced interfaces reaches a 50 % higher maximum displacement than the one with the reinforced interfaces.

A montage of high speed photography images captured during testing of the 545 sandwich beam with reinforced interfaces is presented in Fig.9. The impact occurs at $t=0 \mu\text{s}$ and by the second frame ($t=120 \mu\text{s}$) a flexural wave travels in the top face from the impact site towards the supports. Simultaneously, the first cracks in the core webs can be observed immediately under the projectile edge (marked with arrows). In the third frame ($t=280 \mu\text{s}$), the top face sheet bends towards the bottom face and the core webs shows several more cracks. At the maximum mid-span displacement ($t=760 \mu\text{s}$), the top face sheet and the core web separate from each other as the reinforcing Kevlar stitches rupture (arrows show the broken Kevlar stitches). For the unreinforced 545 sandwich configuration, the front face sheet completely detaches from the core web causing the significantly higher deflections compared to the reinforced configuration (see post-impact photographs in Fig. 10).

The delamination between the face sheet and core webs for the 545 configuration is already seen at relatively low loading impulse, see Fig 10a. In addition, vertical cracks in the core

webs are also observed. With increasing loading impulse, all of the above mentioned failure modes become more distinct and the rear face sheet starts to separate from the core web.

It is clear from the data presented above and the post-impact photographs in Fig 10b that reinforcing the core web – face sheet interface of the 545 sandwich configuration delays the onset of separation and enhances the beam's ability to sustain dynamic loading. As could be seen in the high speed photography montage in Fig. 10, the current reinforcement is however not sufficient to suppress core web – face sheet separation at the higher impulses resulting in the ultimate failure of the sandwich beam. Investigating the amount of stitching required for different configurations to suppress delamination onset and growth would be part of future work.

4.3.3 383 sandwich beam configurations

The measured rear face mid-span displacements of the 383 sandwich configuration subjected to an impulse level of 2000 N s m^{-2} is plotted as a function of time in Fig. 11. The sandwich beam configuration with un-reinforced interfaces shows a 20 % higher mid-span displacement compared to the reinforced configuration. A montage of high speed photography of the 383 sandwich beam with reinforced interfaces subjected to a loading impulse level of 2000 N s m^{-2} is shown in Fig.12. Immediately after impact, a flexural wave travels in the top face sheet from the impact site towards the supports. Subsequently, the front face sheet bends towards the rear face and fully folds over the core members. At this loading impulse, only limited damage occurred in the vicinity of the supports of the reinforced 383 sandwich beam (see Fig. 13b) while the un-reinforced beam of the same configuration also showed a number of vertical cracks in the core webs (see Fig. 13a). With increasing loading impulse, the damage area around the supports increases, the core webs show more vertical cracks and the top face sheet deforms significantly.

5. Finite element analysis

A finite element (FE) model was developed with the objective to develop a predictive capability to simulate the complex deformations of a corrugated sandwich beam out of SrPET composite material subject to impact loading. The model is validated against two types of experiments: impact on clamped monolithic beams and impact on simply supported corrugated sandwich beams. The former of these is a considerably more simplified load case, where structural interface damage plays a smaller role, making the validation of the constitutive law more straight forward. Both configurations were modelled as three dimensional solids in the commercial FE package ABAQUS Explicit (6.13).

5.1. Constitutive laws and material models

The SrPET composite material was modeled using a material model for ductile anisotropic SrPET composites developed by Kazemahvazi et al. [25]. The model includes anisotropic elastic and plastic deformations and the observed tension / compression asymmetry of the solid SrPET composite but does not include damage laws and hence material failure or delamination is not captured. The SrPET material model was implemented as a user material (VUMAT) using a backward Euler integration scheme. The elastic material properties are presented in Tab.2 and readers are referred to [25] for more details on the constitutive law and the material properties used.

The projectile foam was modelled using the in-built ABAQUS crushable foam material model with isotropic hardening [26]. The foam is assumed to have an elastic modulus of 1 GPa, an elastic Poisson's ratio $\nu=0.3$ and a plastic Poisson's ratio of $\nu_p=0$ and modelled using the same approach as Rubino et al [27].

5.2. Description of finite element model

The beams were discretized using eight-noded linear brick elements (C3D8R) with reduced integration. The mesh size of the largest element was a 1.6mm. A further mesh refinement did not improve the accuracy of the predictions significantly.

The foam projectiles were modelled using the same eight-noded linear brick elements (C3D8R) as for the beams. At the start of the simulation, the projectile was given a uniform initial velocity and was brought into contact with beam at its mid-span. A frictionless contact was assumed using the in-built ABAQUS general contact algorithm to model contact between the projectile and the beam as well as with the different parts of the sandwich beam.

5.2.1. Clamped monolithic beam

Due to the symmetry of the loading and boundary conditions, one quarter of the beam was modelled and symmetry boundary conditions employed along the two central mid-planes. Clamped boundary conditions (constraining all degrees of freedom) were imposed at the supports of the beam.

5.2.2. Simply supported corrugated sandwich beam

A quarter of the beam was modelled using symmetry boundary conditions employed along the two central mid-planes similar to the monolithic beams. It was assumed that the different parts of the corrugated beams (core webs and the face sheets) were perfectly bonded together and thus no separation between the parts was allowed (no fracture or delamination criteria are implemented yet). The support rolls were modelled as analytical rigid shells. Contact conditions were modelled using the same technique as for the monolithic beams.

5.3. Results and comparison to experimental observations

The FE-predictions are included in the Figs. 4-6 for the monolithic beams and in Figs. 7-9 and Figs. 11-12 for the sandwich beams.

5.3.1. Clamped monolithic beams

The predictions of the maximum mid-span deflections versus loading impulse are shown in Fig. 4. For the lower impulse levels, the model predicts the maximum deflection to a high degree of accuracy but as the loading intensity increases the FE-model underestimates the deflections. The mid-span deflection versus time response of the monolithic beam subjected to a loading impulse of 2604 N s m^{-2} is shown in Fig. 5 and the deformation shapes are shown in Fig. 6. The displacement time-response is predicted very accurately by the model and the deformation shapes are similar to the experimentally observed ones. At the later stages of deformation, the test specimen fails through delamination and fiber tearing at the supports. As the developed FE-model does not have the ability to predict the delamination and fiber tearing failure mechanisms, the additional beam deformation caused by these mechanisms is not predicted and hence the maximum beam deflections at higher impulses are underestimated.

Although the deflection – time history up to the peak is predicted accurately for all cases, it can be seen that the elastic spring back of the beams are over predicted by the FE-model, see Fig. 5. This is believed to be attributable to the non-recoverable micro damage that occurs in the laminates (see [15] where damage between the fiber-matrix interface was observed at tensile strains as small as 1 %) which is not fully captured by the simplified elastic-plastic constitutive law.

5.3.2. Simply supported corrugated sandwich beam

The predictions of the maximum rear face mid-span deflections versus loading impulse are shown in for the both sandwich beam configurations in Fig. 7. The FE model predicts the maximum deflection to a reasonable degree of accuracy when compared to the stitched sandwich configurations. Similarly to the monolithic beams, the higher the loading intensity, the more the FE-model underestimates the maximum displacements as delamination, material failure and core web – face sheet separation is not considered.

For the 545 sandwich beam, the predicted rear face mid-span displacement versus time response for an impact impulse of 2000 N s m^{-2} agrees well with the experimentally measured response of the sandwich beam with reinforced interfaces (see Fig. 8). Recall the top face sheet – core web separation for the sandwich beam with reinforced interfaces which was observed at peak mid-span displacement (see Fig. 9). When comparing to the FE-predictions, it becomes clear that this late stage separation is not significantly influencing the peak mid-span displacement. The reason why the mid-span displacement of the unreinforced sandwich beam configuration is significantly higher is that the core web - face sheet separations occur at a much earlier stage of the deformation process.

Turning to the deformation shapes predicted by the FE-model (Fig. 9), it can be seen that the predicted deformation of the 545 sandwich beam agree well with the measurements until the later stages of deformation where top face sheet separates from the core web. The predicted sandwich beam cross-section deformation (View A-A) have also been include in Fig. 9 and this shows that the bottom face sheets deforms considerably and bends towards the center of the sandwich beam (marked with arrows) at $t = 120 \mu\text{s}$.

For the 383 sandwich beam the predicted rear face mid-span displacement is almost identical to the measurement (see Fig. 11). Similar to the experimental observations, the top face sheet completely folds over the core members undergoing significant plastic deformation and then partially deforms back as the beam elastically springs back.

6. Discussion and Benchmarking

To benchmark the performance of SrPET sandwich beams we compare the corrugated sandwich beams with reinforced interfaces to an aluminum alloy and a carbon fiber reinforced epoxy (CFRP) sandwich beam that have been tested in a separate study [28]. The beams have the same overall geometry, i.e. a total thickness of $\sim 23 \text{ mm}$, length of 225 mm and width of 50 mm . A detailed description of the beam manufacturing and performance is given by

Kazemahvazi et al [28] and can be summarized as follows. The CFRP/foam sandwich beam was designed for maximized energy absorption with as high core density, core ductility and core thickness as possible. In order to achieve the same areal mass with the 6061-T6 aluminum beam, the thicknesses of the core webs and face sheets had to be scaled down to approximately 0.8 mm. This posed significant challenges in the manufacturing of the beams and in order to achieve sufficient geometrical accuracy, the beams were manufactured by CNC folding the core webs followed by a dip brazing to bond the core web to the face sheets. The governing collapse mode of the 6061-T6 beams is buckling of the core web which could be improved by further thickening the core webs and thinning the face sheets but this was not achievable in practice as the thin face sheets significantly warped during the heat treatment. Hence, the CFRP and the 6061-T6 beams are as close to the optimal configurations as we could manufacture within reasonable development work. It is however important to highlight that if the same comparisons would be made at considerable higher areal mass; the performance differences would be smaller as the 6061-T6 beam cores would be less sensitive to buckling/indentation failure. The chosen areal mass is however representative of many lightweight structure applications.

Figure 14 presents the mid-span displacement versus the impulse per area for the SrPET, AL and CFRP sandwich beams. The AL sandwich beam used herein exhibits the largest deflection for a given impulse. The CRFP-beam performs very well at low impulses but suffers from substantial damage and loss of stiffness at higher impulse loads. The SrPET 545 sandwich beam exhibits a higher deformation for a given impulse and a lower maximum impulse load at collapse than both the SrPET 383 and CRFP beams. However, by placing more material in the core as in the SrPET 383 beam the performance is clearly improved.

It is worth highlighting that the SrPET material cost is considerably lower than both CFRP and the aerospace aluminum alloy. To make the SrPET sandwich beams with reinforced

interfaces recyclable, the Kevlar/Stainless steel thread has only be replaced with a HTPET thread resulting in a fully recyclable sandwich structure. Low material cost and recyclable structures are key factors in e.g. the automobile industry hence SrPET sandwich beams might serve as interesting candidates for these applications.

Simulation of impact behaviour is an important issue for many application areas. In order to make more confident simulations, a fracture model should be added to the material model. It may also, as already mentioned, be valuable to investigate the amount of stitching required to suppress failure by delamination. However, such an investigation needs to include the design parameters of the structure (thickness of faces and core).

7. Concluding remarks

Corrugated sandwich beams with un-reinforced and reinforced face sheet – core web interfaces have been manufactured and impact tests have been performed. The following conclusions have been made:

- (i) Corrugated sandwich beams with a higher mass portion in the core web have a higher dynamic resistance. This resistance can be improved if the face sheet-core web interfaces are reinforced.
- (ii) FE-prediction showed excellent agreement with measurements from the monolithic beams. For the sandwich beams, the FE- predictions showed good agreement with the measurements. Discrepancy results from material failure which is not captured in the current material model.
- (iii) When compared to similar weight and geometry structures of different materials, the corrugated SrPET sandwich beams are very competitive candidates to corrugated sandwich beams out of aerospace grade aluminium and CFRP /foam sandwich beams.

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Table 1: Geometry of the corrugated beams, corrugation angle $\omega = 60^\circ$, height $h = 19\text{ mm}$, distance $d = 10\text{ mm}$ and the total beam length was of 300 mm was held constant for all configurations. The areal densities of sandwich beams with reinforced interfaces are presented in parentheses.

Layers of SrPET fabric	Sandwich beam mass distribution (%)	W (mm)	H (mm)	t_c (mm)	t_{ft} (mm)	t_{fb} (mm)	$\bar{\rho}$ ($kg\ m^{-2}$)
5/4/5	24/53/23	57.6	24.9	1.6	1.8	1.7	8.2 (8.6)
3/8/3	16/69/15	57.5	24.7	2.7	1.0	0.9	8.4 (8.8)

Table 2: Elastic material properties for SrPET composite [25]

$E_{11}(MPa)$	$E_{22}(MPa)$	$E_{33}(MPa)$	$G_{12}(MPa)$	$G_{23}(MPa)$	$G_{13}(MPa)$	ν_{12}	ν_{23}	ν_{13}
6000	4000	3300	2000	2000	2000	0.29	0.64	0.48

Figure 1: Schematic illustration of the assembly steps to manufacture the SrPET corrugated sandwich panels with reinforced face sheet – core web interface. (1) Stitching bottom face sheet fabric to core web fabric, (2) Stitching top face sheet fabric to core web fabric, (3) Placement of aluminium profile, (4) Stitching bottom face sheet to core web, (5) Placement of aluminium profile and (6) Stitching top face sheet fabric to core web fabric.

Figure 2: Schematic of corrugated sandwich beam cross section

Figure 3: a) The measured true tensile stress versus logarithmic tensile strain responses for a strain rate range $10^{-4} \text{ s}^{-1} - 10^1 \text{ s}^{-1}$ and b) true compression stress versus logarithmic compression strain response for a strain rate range $10^{-4} \text{ s}^{-1} - 2000 \text{ s}^{-1}$ [25].

Figure 4: Measured and predicted maximum rear face displacements at the mid-span of the clamped monolithic beam as a function of the foam projectile momentum I_0 .

Figure 5: Measured (solid lines) and predicted (dashed lines) mid-span displacement versus time for the clamped monolithic beams.

Figure 6: Montage of high speed photography images and FE - predictions of the monolithic beam tested at a loading impulse of $2604 N s m^{-2}$

Figure 7: Measured and predicted maximum displacement at the mid-span of the corrugated sandwich beam as a function of the foam projectile momentum I_0 . a) 545 sandwich beam and b) 383 sandwich beam

Figure 8: Measured (reinforced and unreinforced face sheet-core web interfaces) and predicted rear face displacements at the mid-span of the corrugated sandwich beam as a function of time for the sandwich beam 545 loaded at an impulse of 2000 N s m^{-2} .

Figure 9: Captured images of the sandwich beam 545 with reinforced face sheet- core web interfaces and predicted deformations for an impulse level of 2000 N s m^{-2}

Figure 10: Post impact photographs of the 545 sandwich beam for various impact velocities a) with unreinforced face sheet – core web interface, b) reinforced interface. (1) top face sheet-core web separation (2) bottom face – core separation, (3) core web cracks in between support and projectile, (4) core web cracks at the projectile corner, (5) core web cracks at the support, (6) onset of top face separation and (7) onset of bottom face separation.

Figure 11: Measured (reinforced and unreinforced face sheet-core web interfaces) and predicted maximum displacement at the mid-span of the corrugated sandwich beam as a function of time for the sandwich beam 383 at an impulse per area of 2000 N s m^{-2}

Figure 12: Captured images of the sandwich beam 383 with reinforced face sheet- core web interfaces and predicted deformation for an impulse per area of 2000 N s m^{-2} .

Figure 13: Post impact photographs of the 383 sandwich beam for various impact velocities a) with unreinforced face sheet – core web interface, b) reinforced interface. (1) Core web cracks at the support, (2) core web cracks at the projectile corner, (3) core web cracks in between support and projectile.

Figure 14: Mid-span displacement δ versus impulse per area for the SrPET sandwich beams with reinforced interfaces, CFRP [23] and AA6061-T6 [23]