Composite sandwich panels are a promising solution for the rehabilitation of building floors and pedestrian bridges. However, previous studies on relatively thin sandwich panels have pointed out their particular susceptibility to the action of concentrated loads. In this respect, studies developed for relatively thick sandwich panels like those used in civil engineering structural applications are scarce. The present work presents experimental, numerical and analytical studies about the indentation-punching and perforation behaviour of relatively thick FRP composite sandwich panels under concentrated loads. In the experimental study, the influence of the indenter geometry (shape and diameter) and of different core materials in the indentation stiffness, first damage and peak resistances and energy absorption capacity of the sandwich panels are investigated. The results obtained show that the sandwich panels, despite exhibiting high resistance values, present a reduced energy absorption capacity for the first damage. Furthermore, the results show that different core materials lead slight variations of resistance values. The numerical study comprised the development of 3D finite element models of sandwich panels; it allowed concluding that the Hashin (failure initiation) criterion gives a very reasonable prediction of the first damage resistance (12% relative difference compared to the test value selected as a case study). For the two analytical models analysed, a parametric study was developed and a new formulation was proposed; the results obtained show that the models can provide very reasonable predictions of first damage resistance.

Keywords: FRP composite sandwich panels, foam core, balsa core, quasi-static indentation, low-velocity impact, numerical models, analytical models

1 Introduction

Composite sandwich panels are a promising solution for the rehabilitation of building floors and pedestrian bridges [1]. The reduced installation periods, the high strength and stiffness to weight ratios and the reduced maintenance costs are some of the advantages that explain the increasing interest in this solution as an alternative to traditional materials. However, composite sandwich panels are a relatively recent solution in the construction sector, and their widespread use is being limited by the absence of specific regulation and the limited understanding about some aspects of their behaviour.

Previous studies developed on relatively thin composite sandwich panels pointed out their particular susceptibility to the action of concentrated loads [2,3]. The dropping of tools during the installation phase and the action of localised loads during the service stage, caused by furniture or high heels, are some of the examples of actions that can cause localized damage with significant implication for the behaviour of the sandwich panels.

According to Hildebrand [4], the impact behaviour of composite sandwich panels is mainly dependent on the material properties and on the impact action characteristics (namely the indenter geometry and the boundary conditions).

The influence of indenter geometry on the impact behaviour of composite sandwich panels was studied by Zhu et al. [5]. Tests with hemispherical and flat indenters were performed in sandwich panels with an aluminium honeycomb core. As expected, the results showed that the use of flat indenters leads to higher elastic stiffness, and higher first damage and peak resistances of the sandwich panels. According to the authors, this may be explained by the higher stress concentration that is verified for the hemispherical indenters.

Atas and Potoglu [6] experimentally studied the low-velocity impact behaviour of composite sandwich panels composed of PVC and PET foam cores and by glass/epoxy skins of variable thickness. The results showed that the sandwich panels with thicker skins present higher perforation resistance and higher energy absorption capacity. However, comparing the results obtained for the two different core materials, no significant variations of the resistance values were verified. According to the authors, this may be explained by the relatively similar shear properties of the two core materials used in their study.

The influence of the core material was also studied by Atas and Sevim [7]. Composite sandwich panels composed of a PVC foam core (62 kg/m³) and a balsa wood core (157 kg/m³) were tested for different impact energies. For higher impact energies, matrix and fibre damages at the rear skins were observed. The sandwich panels with the balsa wood core presented higher elastic stiffness and slightly higher strength values for the perforation of the impacted skin. In contrast, for the perforation of the second skin, higher values were observed for sandwich panels with the PVC foam core due to the core densification.

Many other studies about the impact behaviour of composite sandwich panels have been presented. However, the studies presented in the literature were mostly developed in the context of naval and aerospace applications, in which the sandwich panels’ thicknesses are clearly lower than those currently used in civil engineering structural applications.

In addition to experimental studies, there are also numerical and analytical modelling studies in the literature. However, the scientific development in this area is still limited, and the use of these models is restricted to certain conditions. Nevertheless, some considerations can be made about these models. Regarding the numerical models, like those presented by Rizov et al. [8], Foo et al. [9] and Zhou et al. [10], it is verified that, to simulate the quasi-static behaviour of composite sandwich panels, self-routines based on the Hashin criterion [11] are mostly used.
A good agreement between the experimental data and the numerical models was observed. However, none of these models were developed for sandwich panels used in civil engineering structural applications. In contrast to the panels used in those numerical studies, the ones used in civil construction are composed of thick skins and, thereby, the impact behaviour may be different.

According to the literature reviews developed by Abrate [2] and Zhu and Chai [3], the Hertz contact model, energy balance models and spring-mass models are the ones most often used to predict the impact behaviour of composite sandwich panels. However, these models are only capable of describing the elastic phase of the impact behaviour. After the first damage, they are no longer valid. Analytical models proposed for the prediction of the first damage resistance, that may be used in the preliminary design of composite sandwich panels, are still very scarce.

In this context, this paper presents experimental, numerical and analytical studies about the low-velocity impact behaviour of thick FRP composite sandwich panels. The suitability of the Olsson [12] and Wen et al. [13] formulations, proposed for the prediction of the first damage resistance, is studied. Based on the Olsson model, a new analytical formula is proposed and studied.

## 2 Experimental programme

### 2.1 Objectives and materials

In order to evaluate the impact behaviour of thick FRP composite sandwich panels, the following three alternative composite sandwich panels with different cores were tested:

- Rigid polyurethane (PUR) foam core;
- Polyethylene terephthalate (PET) foam core;
- Balsa wood (BAL) core.

The sandwich panels have a total thickness of 134 mm, comprising two 7 mm thick skins separated by a 120 mm thick core. They were manufactured by Polo de Inovação em Engenharia de Polímeros (PIEP) and fully characterised in a previous study developed by Garrido [1]. Table 1 presents a summary of the most relevant properties (namely density ($\rho$), tensile strength ($\sigma_{\text{tu}}$), compressive strength ($\sigma_{\text{cu}}$), shear strength ($\tau_u$), shear modulus ($G$) of the core materials as well as the indication of their respective suppliers.

<table>
<thead>
<tr>
<th>Core materials (supplier)</th>
<th>Properties</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$\rho$ [kg/m$^3$]</td>
</tr>
<tr>
<td><strong>PUR</strong> (Polirígido)</td>
<td>87.4</td>
</tr>
<tr>
<td><strong>PET</strong> (3A Composites)</td>
<td>105.4</td>
</tr>
<tr>
<td><strong>BAL</strong> (3A Composites)</td>
<td>101.4</td>
</tr>
</tbody>
</table>

Both GFRP skin laminates of the sandwich panels comprised unidirectional continuous E-Glass fibres (with symmetrical fibre layup of [0/0/30/−30/90/0][s]) embedded in an orthophthalic polyester resin through a vacuum infusion process. Table 2 presents the results of mechanical characterisation tests performed by Garrido [1], in which tensile ($\sigma_{\text{tu}}$), compressive ($\sigma_{\text{cu}}$) strengths, tensile elasticity modulus ($E_t$), shear strength ($\tau_u$), shear modulus ($G$) and Poisson ratios ($\nu$) were assessed for longitudinal (subscript $L$) and transverse (subscript $T$) directions. The information presented in Table 2 is complemented by the results of interlaminar shear tests.
(namely longitudinal ($\tau_{\text{L},u,L}$) and transverse ($\tau_{\text{T},u,T}$) interlaminar shear strengths), conducted in the context of the present experimental study according to the test method suggested in ASTM D2344/D2344M - 16 [14].

2.2 Experimental design

In the development of this experimental study, two different tests were performed: (i) quasi-static indentation tests (QS) and (ii) low-velocity impact tests (Imp). Several combinations of different core materials and indenters with different geometries (diameter and shape) were tested. A summary of the experimental programme is presented in Table 3. In the first row, the indenters are identified, respectively, by the diameter (in mm) and by the shape (Hemispherical or Flat). For the repetition of the impact tests, the use of the two sides of the specimens was considered.

### Table 3 – Experimental programme

<table>
<thead>
<tr>
<th>Core</th>
<th>10H</th>
<th>10F</th>
<th>20H</th>
<th>20F</th>
<th>30H</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>QS</td>
<td>Imp</td>
<td>QS</td>
<td>Imp</td>
<td>QS</td>
</tr>
<tr>
<td>PUR</td>
<td>1</td>
<td>3</td>
<td>1</td>
<td>2</td>
<td>1</td>
</tr>
<tr>
<td>PET</td>
<td>1</td>
<td>3</td>
<td>1</td>
<td>2</td>
<td>1</td>
</tr>
<tr>
<td>BAL</td>
<td>2</td>
<td>3</td>
<td>1</td>
<td>3</td>
<td>-</td>
</tr>
</tbody>
</table>

#### 2.2.1 Quasi-static tests

For the development of the quasi-static tests, an Instron 1343 universal test machine was used. The specimens were fully supported by a rigid solid 50 mm thick steel plate (Fig. 1), and a quasi-static (2 mm/min) concentrated load was applied in the centre of the sandwich panels.

![Fig. 1 – Quasi-static tests setup and instrumentation](image)

In order to validate the numerical models, a video extensometer [composed by a Sony XCG-500SE high-definition camera and Fujinon Fujifilm HF50SA-1 lens] was used to measure the displacement of 15 points, marked on one side of the panels. Two displacement transducers were also used to measure vertical deflections at two different points, along the longitudinal and transverse directions.

#### 2.2.2 Low-velocity impact tests

The experience gained with quasi-static static tests was used in the design of the impact test experimental programme. To implement this part of the experimental study, a Rosand IFW5 instrumented drop weight machine (Fig. 2) was used. Fully supported specimens were impacted by a large variable mass, dropped from a fixed height of 0.75 m. The force was measured by a load cell positioned between the impact mass and the indenter. A metal flag of accurately known height, attached to the falling weight, gave the incident velocity as it passed through an optical gate.

![Fig. 2 – Low-velocity impact tests setup and instrumentation](image)

#### 3 Experimental results and discussion

##### 3.1 Quasi-static tests

Previous investigations have shown that the behaviour of composite materials, when subjected to a local load, may well be highly dependent on the test set-up [15]. So, it must be ensured that the considered geometry can simulate the behaviour of a sandwich panel on a real scale in a sufficiently accurate manner. Since the quantity of material available for this experimental study was limited, economising the amount of material required was an important concern. Thus, a preliminary study with different geometries for the specimens was performed to find the adequate dimensions for the final study.

After defining the dimensions to be adopted, the quasi-static tests were performed. Fig. 3 presents the typical load-displacement (cross-head displacement given by the test machine) behaviour for one representative specimen (PET foam core) of each indenter shape, used to apply the indentation load.

![Fig. 3 – Typical load-displacement behaviour curve of a sandwich panel subjected to hemispherical and flat indenters](image)

For both indenters, two approximately linear stages were observed. The first visible damage corresponds to the notable point that marks the transition between them. In the
load-displacement curves, this damage translated into a first reduction in the force for flat indenters and a decrease in the stiffness for hemispherical ones. During the tests, the damage was characterized by the appearance of a whitish region around the contact area between the indenter and the specimen. This was presumably associated with the delamination between adjacent layers of the skin.

After this first damage, the load increased again until the onset of the perforation process (represented in the plot by the maximum force). After this point, depending on the indenter shape, a progressive decrease (hemispherical) or a sudden drop (flat) in the force values was observed. These differences may be explained by the variable contact area of the hemispherical indenters that causes progressive damage and shearing of the fibres (Fig. 4 (a)). In contrast, the constant contact area of the flat indenters led to sudden shear of the fibres (Fig. 4 (b)).

It is worth mentioning that, even in the first linear stage, before any significant damage was observed, some cracking sounds were audible starting between 2.8 and 5.0 kN (independently of the specimen core material), presumably caused by microcracking in the matrix of the GFRP laminates.

Fig. 4 – Damage caused by the action of (a) hemispherical and (b) flat indenters

Fig. 5 compares the first linear stage stiffness values for sandwich panels composed of different core materials subjected to the action of indenters with different geometries.

The first linear stage stiffness is mainly influenced by the core material. The use of a stiffer core material led to an improvement in the sandwich panels’ indentation stiffness. For specimens with the same core material subjected to the action of indenters with different geometries, non-significant variations in the indentation stiffness values were observed. Furthermore, the slight variations observed did not follow a clear trend.

Figs. 6 (a) and (b) present a comparative plot of the first damage resistance and maximum resistance values for different combinations of core material and indenter geometry.

It can be seen that the different core materials led to slight variations in the force values; in fact, it is possible to observe that there is a slight increase in the indentation resistance of the panels comprising stiffer core materials.

As expected, indenters of same type with a larger diameter (leading to a force distribution over a larger area) usually require higher force values to achieve first damage and peak load. This trend was more marked for the flat indenters, but was also observed for the hemispherical indenters. This may be explained by the fact that the contact areas of hemispherical indenters of different diameters are relatively similar to each other, especially at small deformations.

Figs. 7 (a), (b) and (c) present a comparison between the absorbed energy values, estimated for the different core materials and different indenters. For this effect, the first damage, maximum force and total perforation instances were considered. A reduction in the force values of 90% between the maximum force and the core ductility plateau was considered to define the total perforation of the sandwich panel skin.

Regarding the tests performed with the flat indenters, sandwich panels with softer cores led to an increase in the absorbed energy values, mainly caused by their ability to exhibit higher deformations compared to the panels with stiffer core materials. In the tests performed with hemispherical indenters, this trend was not always followed.
Fig. 7 – Comparison of the absorbed energy values for different core materials and indenters determined at: (a) first damage, (b) maximum force and (c) total perforation.

Usually, indenters of a same type with larger diameters lead to higher values of absorbed energy. Similarly to what was previously discussed for the influence of indenter size regarding the first damage and peak loads, this effect was more marked for the flat indenters and less significant for the hemispherical ones (Fig. 7 (a)).

However, the results obtained for the maximum force (Fig. 7 (b)) and for the total perforation (Fig. 7 (c)) instances, show that the action of hemispherical indenters leads to higher increases in the absorbed energy for those instances when compared to the indenters. This may be explained by the different failure modes of the sandwich panels, under the action of the indenters with different shapes, according to the discussion of Fig. 3.

Due to the limited quantity of material available for this experimental study, it was not possible to carry out the number of tests required to establish statistical parameters. In order to analyse, to some extent, the variability of the results, Fig. 8 presents a comparison of preliminary and final test results, performed in the sandwich panels with polyurethane foam core.

Fig. 8 – Comparison of preliminary and final test results

The good agreement between the tests carried out with the same indenter suggests a reduced variability of the results. Therefore, the results seem to be representative of the indentation behaviour of the sandwich panels. Nevertheless, it is important to confirm these hypotheses through an experimental study with an adequate number of specimens.

3.2 Low-velocity impact tests

Typically, the results obtained directly from the test machine used for low-velocity impact tests are characterised by an oscillation in the signal, which poses some difficulties in the data treatment. Thus, for a better analysis of the results, noise smoothing was implemented using the moving averages method, as illustrated for one representative case in Fig. 9.

Figs. 10, 11, 12 and 13 present comparisons between quasi-static (QS) and impact test results, regarding load-displacement and absorbed energy-displacement curves. As previously mentioned, the two sides of the specimens were used for the impact tests. Nevertheless, the results for the different repetitions of the same test (same indenter and same core material) were considerably consistent. This suggests that, due to the high thicknesses of the sandwich panels, there was no influence of the first test in the impact behaviour of the non-tested skin.

Regarding the different tests, the quasi-static results provided a good approximation of the overall development of the load-displacement curves obtained in impact tests. Although the resistance values were not coincident, quasi-static tests led to similar or even lower resistance values when compared to impact tests. Therefore, it is assumed that quasi-static tests (easier and cheaper than the impact tests) can be used to predict the sandwich panels impact behaviour.

It is important to refer that the stiffness of the first linear stage and first damage force are strongly dependent on the number of points considered in the moving average smoothing. For this reason, these two parameters was not considered in the following analysis.
Fig. 10 – Comparison of the quasi-static and impact load-displacement curves for: (a) PUR 10H and (b) PUR 10P

Fig. 11 – Comparison of the quasi-static and impact load-displacement curves for: (a) PET 10H and (b) PET 10P

Fig. 12 – Comparison of the quasi-static and impact load-displacement curves for: (a) BAL 10H and (b) BAL 10P

Fig. 13 – Comparison of the quasi-static and impact load-displacement curves for: (a) PUR 20H and (b) PET 20P
Fig. 14 plots a comparative analysis for the maximum force obtained in quasi-static and impact tests.

![Comparison between the maximum strength of quasi-static and impact tests](image)

Fig. 14 shows that the different core materials did not induced significant changes in the maximum force values. Comparing the results obtained in both types of tests for homologous specimens, two distinct scenarios were observed. For 10H indenters, the obtained force values were very similar. For the remaining indenters (namely 10P and 20H), the quasi-static tests led to much lower maximum strength than those achieved in impact tests (ranging between 62% to 72% of the impact test values). This may suggest that the behaviour is different for sandwich panels subjected to the action of hemispherical indenters with diameters similar to skin thickness comparing to sandwich panels subject to the action of indenters of higher diameters.

4 Numerical study

4.1 Objectives

One of the main purposes of the numerical study was to investigate the suitability of conventional FE numerical models to simulate the indentation phenomenon in GFRP composite sandwich panels. In particular, the models were expected to replicate the experimental test results previously presented. For this study, in order to simulate the first damage, the Tsai-Hill [16] and the Hashin [11,17] criteria were considered.

Beyond these general goals, this study aimed (i) to evaluate the reliability of the numerical models in predicting the elastic indentation stiffness in composite sandwich panels and (ii) to analyse the stress and strain fields induced by the indentation phenomenon and their relationship with the failure modes observed during the experimental campaign.

4.2 Model description

In this numerical study, carried out using the commercial software Abaqus, the indentation tests performed with flat indenters were simulated. The dimensions adopted for the modelled parts were identical those used in the experiments. A total of 2830 triangular (S3) Continuum Shell elements were used to model the GFRP skins, while a total of 65166 solid ten-node tetrahedral elements (C3D10) were used to model the core material. Fig. 15 presents an example of the geometry of the numerical model developed to simulate the indentation behaviour of the composite sandwich panels.

![Finite element model (mesh and geometry) of the composite sandwich panels](image)

The contacts between all adherent surfaces (face-core interfaces) were modelled using the “cohesive behaviour” option for the interaction properties in ABAQUS/CAE, adopting the package’s default contact enforcement method regarding traction-separation behaviour. This approach does not allow significant slipping or separation to occur between the adherent surfaces.

The boundary conditions were applied to the lower skin. The vertical displacements and the rotations in the three directions were restrained, in order to model the support provided by the steel plate used in the experimental tests. The concentrated load was applied by defining an imposed displacement at a couple node region that represents the flat indenter surface. Non-linear geometrical effects were not considered since the analysis is limited to the simulation of the response up to the first damage, in which only small displacements were observed.

For the simulation of the core material, a bilinear elastic behaviour was considered with a reduction of 90% in the elastic modulus after exceeding the core failure stress. The properties presented in the Table 2 and Table 3 were adopted to model respectively the core materials and the GFRP skins. Table 4 presents the fracture energies ($G_f$) considered in this study to simulate damage evolution according to the Hashin criterion.

<table>
<thead>
<tr>
<th>Material</th>
<th>$G_{ft}$ [N/mm]</th>
<th>$G_{fr}$ [N/mm]</th>
<th>$G_{ff}$ [N/mm]</th>
<th>$G_{fr}$ [N/mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>GFRP</td>
<td>2.38</td>
<td>5.28</td>
<td>0.423</td>
<td>0.948</td>
</tr>
</tbody>
</table>

The values presented in the Table 4 were proposed by Nunes et al. [18]. They were assumed for this study due to the lack of knowledge about the fracture energies for the GFRP skins used in the composite sandwich panels tested.

4.3 Results and discussion

A comparison between the experimental and numerical elastic stiffness values is presented in Table 5.

<table>
<thead>
<tr>
<th>Core</th>
<th>Elastic Stiffness</th>
<th>Ratio</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Experimental</td>
<td>Numerical</td>
</tr>
<tr>
<td>PUR 10P</td>
<td>3.4</td>
<td>3.7</td>
</tr>
<tr>
<td>PET 10P</td>
<td>5.8</td>
<td>6.1</td>
</tr>
<tr>
<td>BAL 10P</td>
<td>9.5</td>
<td>18.9</td>
</tr>
<tr>
<td>PUR 20P</td>
<td>3.9</td>
<td>4.7</td>
</tr>
<tr>
<td>PET 20P</td>
<td>5.3</td>
<td>7.4</td>
</tr>
</tbody>
</table>

Table 5 – Experimental and numerical elastic stiffness values
In the numerical models the possible material imperfections and their local effects are not taken into account. This may explain why numerical elastic stiffness values were globally higher than experimental ones.

The numerical models were able to simulate the increase in stiffness indentation for sandwich panels with stiffer core materials. For lower indenter diameters, the numerical models presented a good agreement with the experimental results (differences of 5% and 9%) regarding the indentation stiffness observed in specimens with foam cores (PUR and PET). For higher diameters acting in homologous specimens, higher differences were obtained (21% and 40%). Increases of the indenter diameter showed to have a greater impact in the indentation stiffness compared to the increase observed in the experiments. Regarding the sandwich panel with a balsa core, there was a significant discrepancy between the numerical and the experimental stiffness values. This may be explained by the highly heterogeneous nature of the balsa wood, which exhibits different properties depending on the quality of the wood actually present in the load application area. The properties of the balsa core, obtained through characterisation tests, reflect the average properties of a set of blocks and not of a particular one.

Figs. 16 (a) and (b) plot the numerical model results for one representative case (PUR 10P) according to Tsai-Hill and Hashin criteria, respectively. The notable points, set in the load-displacement curves, represent the initial failure mode considered in each criterion. The effect of the initial adjustments in the contact between the specimens and the test system is not considered in the numerical simulations.

![Fig. 16 – Numerical results for PUR 10P using the: (a) Tsai-Hill criteria and (b) Hashin criteria](image)

Regarding the Tsai-Hill index results, in this analysis there is no reduction in the elastic stiffness after the first damage. As expected, according to previous numerical studies concerning local loads in GFRP elements [18,19], the Tsai-Hill criterion presents fairly conservative predictions of the first damage force (between 44% to 72% of the experimental values). This may be explained by the fact that Tsai-Hill criterion does not consider the GFRP stress redistribution ability, particularly important for the action of local loads [19].

The Hashin model considers four different failure modes and the development of damage (namely longitudinal tension, longitudinal compression, transverse tension and transverse compression). In this case, the simulation stopped at some point of the analysis due to convergence issues and only three failure modes were identified. Considering the order of magnitude associated with the first mode (tension in transverse direction), it is presumed that it may represent the first audible cracking reported in the experimental study. For compression in the transverse direction, it was not possible to correlate it with any event observed during the experimental campaign. For the last failure mode (tension in longitudinal direction) predicted by Hashin criterion, a good approximation to the experimental first damage force was observed (88% of the experimental value). Nevertheless, it is important to mention that the Hashin criterion does not consider the delamination phenomenon.

Fig. 17 presents the damage evolution according to the Hashin criterion for tensile failure along the transverse direction. The numerical results suggest that the damage onset is firstly caused by stress concentration around the indenter periphery. It is observed that the damage propagates preferentially along the longitudinal direction, which is consistent with the lower tensile strength in the transverse direction. After the numerical first damage is achieved, the damage starts to propagate in the other directions.

![Fig. 17 – Damage evolution according to Hashin criterion: (a) immediately before the numerical first damage and (b) after the first damage](image)

The influence of the core failure in the indentation behaviour of the sandwich panels was also investigated. Fig. 18 presents the core failure considering the moment when compression strength is first attained in the numerical model. Eventually, due to the high thickness of sandwich panels skins or due to large core ductility plateau, there is a non-significant variation of load-displacement curve caused by the core failure.
5 Analytical study

5.1 Objectives

Modelling the impact behaviour of composite sandwich panels is complex. Usually, low-velocity impact leads to the development of internal failure modes in composite materials, which are barely visible during experimental tests. Furthermore, different combinations of materials can be used in the composite sandwich panels, which influence the impact behaviour. Thus, the analytical models for the impact design of composite sandwich panels are still very scarce.

However, there are some formulations available in the literature that try to predict the delamination onset, a parameter which, according to Sutherland and Soares [20], can be considered one of the key parameters in the impact resistance design of composite materials.

The main objective of this analytical study is to evaluate the suitability of two analytical models available in the literature, which are presented and assessed below.

5.2 Model description

5.2.1 Model of Wen et al.

Wen et al. [13] proposed an analytical model to simulate the behaviour of composite sandwich panels with thick skins impacted by flat indenters. According to the authors, for thick skins, the membrane effect can be neglected. The first damage resistance of the sandwich panels is thus given by:

$$P_f = 2\pi Rh\tau_{13} + K_c\pi R^2q$$

(1)

in which, $R$ is the indenter radius, $h$ is the skin thickness, $\tau_{13}$ is the laminate shear strength (through the thickness), $K_c$ is a constraint factor and $q$ is the compression strength of the core.

In the previous equation, the first term gives the skin laminate contribution and the second one expresses the core contribution. The constraint factor $K_c$ represents the effect of the surrounding material in the core.

5.2.2 Model of Olsson

Olsson [12] proposed an analytical model to simulate the behaviour of composite sandwich panels impacted by hemispherical indenters. According to this model, the core contribution to the first delamination ($F_d$) is neglected. It is suggested that the first delamination resistance is only dependent on skin variables, namely the bending stiffness of the skins ($D_f$) and the critical strain energy release rate in mode II ($G_{IIc}$), being given by the following expression,

$$F_d = \frac{32D_fG_{IIc}}{3\pi}$$

(2)

in which, for orthotropic laminates, $D_f$ is given by:

$$D_f = \pi \left( \frac{D_{11}D_{22}(n+1)}{2} \right)$$

$$\eta = \frac{D_{12} + 2D_{13}}{D_{11}D_{22}}$$

(3)

In the previous equations, it is considered that the stiffness matrix components $D_{11} = D_{22} = D_{13} = 0$. For the laminate used in this study, these components are not zero, but are considerably lower than the remaining components of stiffness matrix. Thus, the use of the previous approach was considered reasonable. Additionally, a modification to this model was proposed in this study, to include the core contribution to the first delamination (as detailed in section 5.3.2).

5.3 Results and discussion

5.3.1 Wen et al. model

The constraint factor $K_c$ is an empirical parameter. Reference values ($1.7 < K_c < 2.5$) were presented by Reddy et al. [21] for sandwich panels with metallic skins and a foam core. These materials, namely the skins, are very different from those used in this study.

In order to evaluate the influence of the constraint factor, the results of a parametrical study, where different values of $K_c$ were considered, is presented in Fig. 19. Note that the core contribution is neglected for $K_c = 0$.

Regarding the sandwich panels with lower stiffness values (PUR 10P, PUR 20P, PET 10P and PET 20P), the core contribution to the first damage is not quite relevant. For these cases, for which values $K_c = 0$ and $K_c = 4$ were considered, relative differences between predicted values range between 2% and 9%.

In a homologous comparison for sandwich panels with balsa, the core contribution was considerably more significant, with a maximum difference between predicted values of 17%.

Comparing the predicted values with the experimental ones, one concludes that a perfect fit of the analytical model to test data would involve physically unreasonable scenarios ($K_c < 0$ and $K_c \gg 1$). The uncertainty associated to the material
properties and their variability (namely for $q$ and $r_{13}$), and the variation of the interlaminar shear strength with the considered direction (not considered in this model) may explain the differences between the predicted and the experimental values. Nevertheless, overall the model provided reasonable accurate estimates, with predictions varying between -21% and +13%, in relation to the experimental values, in the most unfavourable scenarios (namely assuming $K_c = 0$ for the prediction of PET 10P and assuming $K_c = 4$ for the prediction of PUR 20P).

5.3.2 Olsson modified model

As previously mentioned, the Olsson model neglects the core contribution to the first damage resistance. Nevertheless, the experimental study presented in this paper showed that stiffer sandwich panels lead to slightly higher load values at first damage.

In order to consider the influence of the core material, an adaptation is proposed to the Olsson model. The second term of the Wen et al. formula was added to the Olsson model, leading to the following equation:

$$F_d = \pi \sqrt{\frac{32D_G G_{IIc}}{3} + K_c \pi R^2 q}$$

(5)

Due to the lack of knowledge about the $G_{IIc}$ values for the skin laminates used in this study, the direct use of the previous expression was not possible. In order to study the applicability of this model to thick composite sandwich panels as well as to regard its differences to the Olsson Model, a parametric study was developed.

Fig. 20 presents the variation of two statistical parameters, namely the mean square error (MSE) and the mean percentage error (MPE), for a variation of $G_{IIc}$ between 500 J.m$^2$ and 950 J.m$^2$. Based on the proposal of Fatt and Park [22], a $K_c = 2$ value was adopted. For this analysis, the variation of $K_c$ with the indenter diameter and with the core material was neglected.

The MSE curve shows that, on average, the analytical predictions present the best approximation to the experimental results for $G_{IIc} = 720$ J.m$^2$. The MPE bars show the average distribution of the predicted values.

In order to study the influence of the core, predictions with different $K_c$ values were computed and compared to the experimental results, as depicted in Fig. 21. Based on the previous discussion, a $G_{IIc} = 720$ J.m$^2$ was assumed. Note that $K_c = 0$ represents the predictions according to the original model by Olsson.

Fig. 21 – Comparison between the predicted first damage resistance and the experimental values

Regarding the results obtained with the Olsson model, the same predicted values were obtained for the different scenarios. The relative differences to the experimental values range from 23% to 27%.

The adaptation of the Olsson model provides the possibility of considering the core contribution and the indenter diameter. With the new formula, there is a considerable improvement of the predicted values for the bigger diameters and stiffer sandwich panels scenarios – the relative differences to test data now range from -7% to +10% (assuming $K_c = 2$). Nevertheless, it is worth mentioning that this analysis is based on several assumptions. It is quite important to clarify the adopted values, since a considerable uncertainty regarding several properties (namely $G_{IIc}$ and $K_c$) is involved in these formulas.

6 Conclusions

This paper presented experimental, numerical and analytical studies about the low-velocity impact response of thick GFRP composite sandwich panels. The following main conclusions may be drawn from these studies:

1. The analysed composite sandwich panels present high indentation resistance, although they present reduced values of energy absorption capacity corresponding to the first damage.
2. The indenter geometry is the most influential parameter in the low-velocity impact behaviour of thick FRP composite sandwich panels.
3. The indentation stiffness is mainly influenced by the material used in the core. Regarding the resistance values, the influence of the core materials was less marked.
4. Quasi-static tests provide a good estimate for the impact behaviour of composite sandwich panels.
5. The Hashin criterion provides closer predictions for the first damage resistance of the composite sandwich panels compared to the Tsai-Hill criterion.
6. According to the assumptions adopted, the analytical models assessed in this paper can provide reasonable predictions for the first damage resistance measured in quasi-static tests.
7. Both numerical and analytical models still cannot explain some particularities of the indentation phenomena in composite sandwich panels.
References


