Structural Modelling Validation of Cork Composites for Aeronautical Applications

by

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of the degree of Master of Science in Mechanical Engineering

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November 2009
Afonso Freire Novais dos Santos Tiago (1981-2009)

in memoriam
Abstract

Recently, the application of composites in the aerospace industry has seen a dramatic increase and in this context, cork composites may provide a viable option in the design of certain components. Their low density, excellent thermal and mechanical properties and small environmental footprint make them prime candidates for substituting components made out of carbon-derived products. However, the fundamental parameters for their mechanical performance are not very well known. An extensive validation process needs to be undertaken in order for the recently proposed constitutive models to be mature enough for aerospace applications design cycle.

This thesis focuses on the computational structural modelling of aeronautical shell components made of sandwich composites with cork cores. The purpose of the study is to validate finite element models by comparing the experimental test data under a three-point bending setup with that predicted by MSC Patran™ 2008, a commercial FEA software. The overall results do not show a clear correlation between the experimental and computational results, however some improvements in the model are suggested for further research.

The research work documented in this thesis has been conducted in the framework of the Aerocork project, where the ultra-lightweight aircraft manufacturer Dyn’Aero Ibérica (DAI) requested industry and academia partners to assist in the feasibility study of the substitution of oil-derived materials Sphere.Tex and Polyvinyl chloride (PVC) components by cork-derived components.

Keywords: cork, composites, finite element analysis, sandwich core structures
Resumo

A utilização de materiais compósitos na indústria aeroespacial tem aumentado de uma forma assinalável nas últimas décadas. Neste contexto, os materiais compósitos contendo aglomerados de cortiça demonstram potencial para surgir como uma alternativa viável no projecto de alguns componentes e sistemas. Estes aglomerados são caracterizados pela sua baixa densidade, desempenho mecânico e térmico superior, e por uma reduzida pegada ecológica. No entanto, numerosos parâmetros relativos ao seu comportamento mecânico são ainda em grande medida desconhecidos. Um processo de validação terá de ser levado a cabo até que algunos modelos constitutivos desenvolvidos nos últimos anos tenham maturidade suficiente para serem aplicados na indústria aeroespacial.

Esta dissertação tem como enfoque a modelação estrutural computacional de estruturas sanduíche com núcleos de aglomerados de cortiça no âmbito das aplicações aeronáuticas. O objectivo do estudo aqui documentado é o de validar técnicas de modelação em elementos finitos neste tipo de estruturas. Este estudo consiste numa primeira fase por ensaios laboratoriais de flexão em três pontos, sendo que os resultados obtidos são comparados com aqueles previstos pelo software MSC Patran\textsuperscript{TM} 2008. De uma forma geral, os resultados não demonstram uma correlação exacta. No entanto, são apresentadas sugestões de melhoria para eventuais desenvolvimentos futuros deste estudo.

Este trabalho de pesquisa foi conduzido e financiado no âmbito do projecto Aerocork. Este projecto tem como objectivo o desenvolvimento, produção, ensaio e certificação de aviões ultra-leves com compósitos de cortiça incorporados.

**Palavras-chave:** cortiça, compósitos, análise de elementos finitos, núcleos de estruturas sanduíche
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Finally, my heartfelt thanks to my family, for the encouragement for pursuing this additional MSc. thesis nearly three years into my professional life, and their
patience in bearing with me in so many moments.
This dissertation starts with a description of cork as a material and its multiple applications, in Chapter 1. The importance of cork in the context of composite materials is also addressed.

An overview of the state of the art in cork composites within the aerospace industry follows in Chapter 2. Historical and present examples are mentioned, although the applications of cork composites in aeronautics are not as abundant as those in space. A distinction is made between aeronautical and space applications, due to the different natures of the environmental conditions between both fields.

The Aerocork project scope is introduced in Chapter 3, in which a listing of the main structural components to be phased out and substituted by cork composites can be found. The cork core sandwich structures analyzed in this study are also addressed.

In Chapter 4 the theoretical background of the classical plate-bending theory mechanical behavior is described, addressing also plates of isotropic and quasi-isotropic multi-layers. This classical theory served as the foundation of the computational methods used in the correlation study.

In Chapter 5 a correlation study is performed. The goal of this study is to acquire enough confidence in FE models of cork core sandwich composites. Specimens of these are experimentally tested under a three-point setup. A FE model is then developed to reproduce the mechanical behaviour of these specimens under the same conditions, leading to a correlation study between the results obtained via FEA and those obtained in the laboratory.

Finally, in Chapter 6 conclusions of this study are stated, and suggestions of improvements for subsequent studies are also addressed.
Acronyms and Notation
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<td>ARD</td>
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<td>TPS</td>
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<td>UK</td>
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ACRONYMS AND NOTATION

\begin{itemize}
  \item \(a, b\) dimensions
  \item \(D\) flexural rigidity
  \item \(D_t\) transformed flexural rigidity
  \item \(D_u\) flexural strength
  \item \(E\) modulus of Young
  \item \(E_c\) compressive modulus
  \item \(E_t\) tensile modulus
  \item \(G\) modulus of elasticity under shear
  \item \(K\) strain gage factor
  \item \(p\) pressure
  \item \(p_0\) uniform distributed load
  \item \(R\) strain gage nominal resistance
  \item \(t\) thickness
  \item \(t_c\) core thickness
  \item \(t_f\) facesheet thickness
  \item \(u\) deflection component along the \(x\) axis
  \item \(v\) deflection component along the \(y\) axis
  \item \(w\) deflection component along the \(z\) axis
  \item \(\beta_g\) coefficient of thermal expansion of the strain gage materials
  \item \(\beta_s\) coefficient of thermal expansion of the specimen material
  \item \(\sigma\) stress
  \item \(\sigma_u\) ultimate strength
  \item \(\varepsilon\) strain
  \item \(\varepsilon_0\) apparent strain
  \item \(\varepsilon_u\) ultimate strain
  \item \(\rho\) density
  \item \(\nu\) coefficient of Poisson
  \item \(\theta\) orientation of the material coordinate system relative to the element
  \item \(\theta_x\) rotation around the \(x\) axis
  \item \(\theta_y\) rotation around the \(y\) axis
\end{itemize}
Chapter 1

Cork and Cork Composites

1.1 Cork

Cork is a light substance extracted from the bark of a type of oak tree named *Quercus suber* L., which grows around the Mediterranean basin. The process of bark extraction from each tree is performed about every 9 years, during which time the bark grows to its full size. Cork has been used for many centuries as a prime material for the manufacturing of stoppers, floorings, and tiles.

The bark is extracted from the trees in the shape of rough convex-shaped boards. These boards are then boiled for about one hour, in order to relieve residual stresses, to decrease radius of curvature of the boards, and to reduce the size of the pores [1]. Once this preparation step is completed, the boards are ready for processing.

1.2 Cork Applications Overview

Cork products can be divided in two categories: natural cork products and agglomerate products. The former comprise products which involve no processes other than preparation, cutting and finishing. Stoppers and disks are included in this category. The latter comprise products formed from cork granules, which are the waste derived from the natural cork products manufacturing, thus max-
imizing the usage of cork as a material.\footnote{Only 25\% of the cork in the boards is used for stoppers. Up until the 1960’s, all remaning cork was regarded as scrap material and discarded. Since then, a great effort has been devoted to extract as much value as possible from it.}

Agglomerate products are then divided into two subcategories: white agglomerates and black agglomerates. The former are composed by cork granules bonded by an adhesive product. A subgroup of these which also includes rubber is often known as rubbercork. The latter are composed by cork granules only, and are obtained from an autoclave heating process, during which a process of self-agglomeration of the granules occurs.

White agglomerates are mostly used for composite stoppers, walls, flooring, insulation, and tiles. Rubbercork is mostly used for gaskets and engine rings, due to its heat resistance, high recoverability and high degree of impermeability even under low bolting pressures \cite{2}. Black agglomerates are mostly used for vibration damping and acoustic insulation.

Generally speaking, cork composite agglomerates are highly regarded for building applications, for their superior performance in terms of passive acoustic control and energy conservation. The same level of acceptance is seen in sports accessories industries, due to properties such as low density and resistance to wearing. In most applications, grain size can usually be tailored to meet specific customer mechanical and thermal performance needs.

\section*{1.3 Sandwich Composites with Cork Core}

One of the most common applications of composites is sandwich construction consisting of two, thin high-strength skins covering both sides of a low-density core. Figure 1.1 provides an illustration of this concept.

The material of the facesheets may be plywood, wood pulp fibers, Aluminium alloys, glass fibers, or Carbon fibers. That of the core may be cork, balsa wood, or synthetic materials. Sandwich composites such as those illustrated in Figure 1.1 are usually designed so as to have their bending and in-plane strength provided by their facesheets, while a low-density core provides shear strength. This type of sandwich composites have combine the high mechanical performance of the facesheet materials with the the low-density of the core
CHAPTER 1. CORK AND CORK COMPOSITES

Figure 1.1: Sandwich concept illustration

Figure 1.2: Use of cork agglomerates in sandwich components for touring and competition kayaks (credit: ACC)

Sandwich composites with cork agglomerate cores have been used in applications such as sports and leisure, building, and energy. A case in consideration is that of CORECORK, which is the commercial designation of a cork agglomerates range developed, produced and commercialized by ACC [3]. CORECORK has been successfully used in touring and competition kayaks (see Figure 1.2), surf boards, and acoustic insulation panels. Benefits from the use of cork agglomerates in sandwich composites include improved manufacturing cycle times, reduction of environmental impact (due to the transitioning from oil-derived materials to a natural recyclable material), lower resin usage (due to reduced porosity) and consequent weight savings, and improved damping performance.
Chapter 2

Aerospace Applications

2.1 Aeronautics

Composite materials have been used in one way or another in aeronautics from relatively early stages of the industry. Engineers soon realized their advantages in terms of weight reduction, corrosion resistance, and the possibility of tailoring components in order to better withstand predicted mechanical loads.

Sandwich composites gained their own room in aeronautical design, as they offer the same benefits of buckling resistance as aluminum-alloy sheets, without the drawback of heavy and atomized integration efforts. The process of riveting several aluminum-alloy thin-walled elements is a costly one, in terms of the number of rivets and the integration time and labour costs that riveting involves.

According to Hoff and Mautner [4], it is unclear when the first sandwich constructions were applied in aeronautical construction, but a prime example can be tracked down to 1924, when a sandwich wall patent was granted to von Karman and Stock. S. E. Mautner would later develop the first applied sandwich structure to an existing airplane in 1934, at the premises of the Schneider-Creusot airplane factory in Le Creusot, France. Curiously, this was made with a ply-cork sandwich structure. In 1938, the same engineer presented a low-cost mono-wing plane, with a sandwich wing.

The first airplanes built with fiber reinforced plastic were gliders, in the 1950’s [5]. Engineers realized the benefits of these materials both in terms of
aerodynamic drag quality, as well as the simplicity of manufacturing of large integrated assemblies. Until then, gliders were built mostly with wooden materials, which facilitated the transition, due to the fact that both materials share anisotropic properties.

2.2 Space

Cork composites have been extensively used in the space industry. The benefit of weight reduction becomes even more important in the space context, as its reduction yields substantial cost savings for launchers.

One of the leading applications of cork agglomerates is that of ablative materials. These materials provide structures with protection from high thermal energy sources. Ablation is a phenomenon by which a material dissipates energy via its own vaporization instead of energy absorption via heat [6].

2.2.1 Thermal Protection Systems (TPS) for Re-Entry

One of the most prominent examples of usage of cork agglomerates for space is that of TPS for atmospheric re-entry. A material named Norcoat Liege has been developed and commercialized by EADS, in order to suit the industry needs for TPS ablative materials. Norcoat HPK is a 2000 °C class ablative thermal protection used for aerodynamic thermal fluxes in the 1 to 5 MW/m² range.

Norcoat falls in the category of agglomerated composites, and is constituted by cork particles agglomerated with phenolic resin. It is manufactured in plates with customizable thicknesses ranging from 1 mm to 19 mm. The Norcoat Liege series is presented in two different materials: Norcoat Liege HPK FI and Norcoat Liege HPK FIH, whose characteristics we can see in Table 2.1.

Norcoat HPK is a flight-proven material and has been successfully used in several missions, as described in the following sections.

The Atmospheric Re-entry Demonstrator (ARD)

The ARD is the first guided sub-orbital re-entry vehicle to be developed, launched and recovered by ESA. This mission had two goals. Firstly, to demonstrate the ability of the European space industry to master all the phases involved in the
development and operation of low-cost sub-orbital re-entry vehicles. Secondly, to gather and analyze as much data as possible concerning the physical phenomena involved in all the re-entry phases, which constitute a valuable input for further developments in subsequent missions [7]. The ARD was launched by Ariane 5 V503 on October 21, 1998, and recovered in the Pacific Ocean.

This was the first ever space mission to include Norcoat-Liege as the prime TPS material for a re-entry vehicle [8]. 19 mm thick Norcoat 62250 FI cork tiles were used to protect the rear-cone and back-cover sections of the vehicle. Figure 2.1 illustrates the integration of Norcoat Liege rear-tiles integration before the flight, along with an aspect of the same tiles after re-entry and subsequent pyrolysis phenomenon. The rear-cone Norcoat Liege tiles performed nominally, having withstood a maximum heat flux of 37 kW/m$^2$ during re-entry.

The ExoMars Mission

ExoMars is a ESA mission to planet Mars, aiming at sending and bringing back to Earth both a rover and a Geophysical and Environmental Package. The ExoMars mission launch is due to happen in late 2013, arriving in Mars on October 2014 [9].

Norcoat Liege HPK is the reference material in consideration for design purposes, ahead of other materials such as EADS-ST Picsil, and AQ60. Norcoat Liege will be used in the form of panels and tiles for protecting both the frontshield and back-cover areas. Norcoat Liege was selected as a baseline solu-
CHAPTER 2. AEROSPACE APPLICATIONS

Figure 2.2: ExoMars Descent Module aeroshape (left) and Exo Mars Frontshield TPS arrangement (right) (Source: [8])

Figure 2.2 illustrates the Norcoat tiles and panels layout for the ExoMars TPS.

The Beagle 2 Mission

Norcoat Liege was also used as the TPS material for the frontshield and back cover of the Beagle 2 probe, designed to land on the Martian surface with a scientific payload that would study the composition of both the surface and the atmosphere of the planet.

Unfortunately, all contact with the probe was lost six days before the nominal date for orbital insertion date, on December 25, 2003. The Beagle 2 was declared lost on February 6, 2004 [10]. However, the ESA/UK Comission of Enquiry report stated that “the entry thermal protection is not a likely cause for the Beagle 2 loss” [11].

2.2.2 Thermal Protection Systems for Solid Rocket Boosters (SRB)

Ablative properties of cork agglomerates have been also used in providing thermal protection to exposed surfaces of propellant rocket boosters. Examples of such surfaces include the inner surface of the exhaust nozzle and the interface between the metal/kevlar case and the propellant, in the case of SRB.

An example of the former is that of the insulation of rocket solid propellant from high heat fluxes produced by propellant burning [13], in which a layer of
<table>
<thead>
<tr>
<th>Aspect</th>
<th>HPK FI</th>
<th>HPK FIH</th>
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<tbody>
<tr>
<td>Aspect colour sheets</td>
<td>Brown</td>
<td>Brown</td>
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<tr>
<td>Dimensions of sheets (mm)</td>
<td>1210 x 680</td>
<td>1210 x 680</td>
</tr>
<tr>
<td>Thickness tolerance (mm)</td>
<td>0.2 - 0.1</td>
<td>0.2 - 0.1</td>
</tr>
<tr>
<td>Hardness (Shore A)</td>
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<td>Density at 20°C (kg/m³)</td>
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<td>Tensile Stress (MPa)</td>
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<td>Elongation (%)</td>
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<td>$K$ at 150°C (Wm$^{-1}$C$^{-1}$)</td>
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<td>$C_v$ at 200°C (kJ.kg$^{-1}$C$^{-1}$)</td>
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<td>2.40</td>
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<td>Self-extinguishing (MO 10AQ321)</td>
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</tr>
<tr>
<td>Life cycle</td>
<td>20 years</td>
<td>20 years</td>
</tr>
<tr>
<td>Substrates</td>
<td>Composite and metallic</td>
<td>Composite and metallic</td>
</tr>
</tbody>
</table>

Table 2.1: Norcoat Liege HPK material properties (Source: [12])
EPDM is used as a base polymer, while various fillers used as compounding ingredients improve ablative efficiency. Such fillers include asbestos fibre, cork powder and iron oxide.

Experiments conducted with plasma arc jets indicate that ablative properties substantially increase with volume fraction of both asbestos and cork [13]. At the same time, both the rate of mass loss and the rate of corrosion substantially decrease. This performance contrasts with that of Fe$_2$O$_3$, in which this substance acts as catalyzer of the decomposition of vulcanites for low volume fractions, by decreasing ablative properties and increasing both the rate of erosion and the rate of mass loss. For volume fractions above 1.0, this trend is marginally reverted.

With the extensive demonstration of the carcinogenic properties of asbestos in recent years, it is strongly expected that cork will assume a more prominent role as an EPDM filler for insulating composites in launch vehicles.

An example of the latter includes the Launch Abort System (LAS) of the Orion Crew Exploration Vehicle [14]. Additionally, the aforementioned Norcoat-Liege has already been extensively used in launch vehicles. This material was developed in the 1970’s for the French deterrent force, then relying upon Ariane 4 [8]. Since then, Norcoat-Liege has been adapted to Ariane 5 launch vehicles as well.

### 2.2.3 Bolt Catchers for the Space Shuttle Solid Rocket Boosters

Cork agglomerate components have been recently incorporated within the bolt catchers of the Space Shuttle solid rocket boosters [15]. Each of these components catches a part of each of the two bolts that attach the boosters to the Shuttle’s External Tank. Approximately two minutes after lift-off, pyrotechnic devices break the two bolts that hold the booster to the External Tank, thus causing booster separation. It is vital that the ejected part of the bolt be retained so as not to cause impact damage to the Shuttle.

On the wake of the loss of Space Shuttle Columbia and its crew, several systems and components were redesigned before the remaining Shuttles could be put back into service. The bolt catchers were modified in order to become
a single machined piece. Previously, they consisted of two welded halves. The wall thickness of the catchers was increased from 0.125 inches to 0.25 inches. Finally, a stronger Aluminum alloy was selected, Al 7050.

A cork composite was selected as a thermal protection material for the catchers, substituting a super lightweight ablator. This thermal protection consists of machined cork covered with a moisture-protective Hypalon® paint finish. This cork composite was selected due to its heritage in other SRB areas, namely in terms of adhesive properties, which are essential in guaranteeing that no additional Shuttle-harmful debris is formed during separation.
Chapter 3

The Aerocork Project

3.1 Project Overview

During the course of 2008, the Portuguese-based lightweight aircraft manufacturer Dyn’Aero Ibérica (DAI) issued a request to industry and academia partners in order to assist in the substitution of several components in its lightweight airplane models that are made out of oil-derived materials, in favour of cork composites. A consortium was formed and an application was submitted to a QREN R&D grant within the European Union framework. In late 2008, this grant was approved and awarded to the consortium. The project has a time span of three years. The flight tests of the first re-engineered prototypes are expected to take place in late 2011.

The goal of this project is the phasing out of as many components made out of oil-derived materials as possible.\footnote{Components to be phased out are used in structural, safety and aesthetical applications. We shall focus only in structural components in the course of this study.} Such materials include PVC and Sphere.Tex, a type of synthetic micro-sandwiches. The phasing out of these components would result in substantial cost reduction of the whole manufacturing process. Oil-derived materials carry the risk of becoming more expensive by the year, as oil prices surge along with global demand.

Also, the integration of cork composites would benefit the social and environmental footprint of DAI, as cork is an entirely natural substance. Moreover, DAI’s manufacturing premises are located in Alentejo, a region in Southern Portugal.
Portugal which is strongly dependent on the cork extraction and processing industries.

The range of components comprises both critical and non-critical components. A range of estethical and comfort applications is also envisaged for cork-derived materials.

3.2 Sandwich Composites to be Re-Engineered

Two types of structural sandwich composites coexist in DAI’s aircrafts: those with a 6 mm thick PVC core (simply named sandwich), and those with a 0.8 mm thick Spere.Tex core (named as micro-sandwich). Both core materials are oil-derived. Also, both topologies feature carbon fiber facesheets.

ACC has proposed cork agglomerates from the CORECORK range to substitute currently used materials. This implied that the first development efforts will have to be directed towards validating the mechanical performance of these new cork core sandwich and micro-sandwich composites by means of standardized mechanical tests performed over specimens of both sandwiches and micro-sandwiches. These tests are being performed by Polo de Inovação em Engenharia de Polímeros (PIEP) and will result in the determination of equivalent properties such as tensile and compressive modulus and strength, and equivalent density.

A subsequent step will be that of determining the best computational modelling technique to predict the mechanical performance of re-engineered components, once they would be built in cork core sandwiches. The study described in this dissertation is part of this project step.

The shell components to be re-engineered included the upper and bottom fuselages (see Figures 3.2 and 3.3) as well as the front canopy (see Figure 3.1).

\footnote{By critical components we mean those whose malfunction or loss would result in the catastrophic loss of the aircraft.}
Figure 3.1: Two views of the canopy (Credit: DAI)

Figure 3.2: Bottom fuselage (in the left) and upper fuselage (in the right) (Credit: DAI)

Figure 3.3: Integrated fuselage (Credit: DAI)
CHAPTER 3. THE AEROCORK PROJECT

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<tr>
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<tr>
<td>$\rho$ (kg/m$^3$)</td>
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Table 3.1: Typical mechanical fiber properties for HexTow$^{TM}$ AS4C carbon fiber (Source: [16])

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<td>$D$ (GPa)</td>
<td>128</td>
</tr>
<tr>
<td>$D_u$ (GPa)</td>
<td>1.725</td>
</tr>
<tr>
<td>Short beam shear strength (MPa)</td>
<td>124</td>
</tr>
<tr>
<td>Fiber volume (%)</td>
<td>62</td>
</tr>
</tbody>
</table>

Table 3.2: Typical epoxy composite properties at room temperature for HexTow$^{TM}$ AS4C carbon fiber (Source: [16])

3.3 Sandwich Components

3.3.1 Facesheets

The facesheets for the current sandwich panels are made out of HexTow$^{TM}$ AS4C carbon fiber, with a 3000 (3K) filament count tow. Each facesheet is formed by two layers of fibers that are weaved together with and relative angle of 90° and has a total thickness $t_f$ of 0.2 mm. Typical fiber properties are provided by the manufacturer [16] and listed in Table 3.1.

However, facesheets will have their mechanical properties substantially altered by the epoxy resin that is used to bond it to the sandwich core. The HexTow$^{TM}$ datasheet [16] also provides the following representative properties at room temperature for the resulting epoxy composite (see Table 3.2)
3.3.2 Core

As previously mentioned in Section 3.2, two different cork agglomerates were considered for the sandwich core. These agglomerates are provided by ACC under the designations (removed due to confidentiality requirements) and (removed due to confidentiality requirements). Relevant material properties for these materials were determined in laboratory via mechanical testing at ACC’s premises. These properties comprise the tensile modulus $E_t$, the compressive modulus $E_c$, the shear elasticity modulus $G$, and the density $\rho$. The first three properties were numerically extracted from the experimental results, as a linear range was clearly observed for all tests performed.

The tensile modulus $E_t$ for (removed due to confidentiality requirements) was extracted after two tensile tests. Results are illustrated in Figures 3.4 and 3.5. The compressive modulus $E_c$ for was extracted after two compressive tests. Results are illustrated in Figures 3.6 and 3.7. The elasticity modulus $G$ was extracted after three tests. Results are illustrated in Figures 3.8, 3.9, and 3.10. The average magnitudes in the linear range for these (removed due to confidentiality requirements) properties are summarized in Figure 3.18.
Figure 3.5: Traction load as a function of strain for (removed due to confidentiality requirements) (curve 2) (Source: ACC)

Figure 3.6: Flatwise compressive load as a function of strain for (removed due to confidentiality requirements) (curve 1) (Source: ACC)
Figure 3.7: Flatwise compressive load as a function of strain for (removed due to confidentiality requirements) (curve 2) (Source: ACC)

Figure 3.8: Shear load as a function of strain for (removed due to confidentiality requirements) (curve 1) (Source: ACC)
Figure 3.9: Shear load as a function of strain for (removed due to confidentiality requirements) (curve 2) (Source: ACC)

Figure 3.10: Shear load as a function of strain for (removed due to confidentiality requirements) (curve 3) (Source: ACC)
Figure 3.11: Traction load as a function of strain for (removed due to confidentiality requirements) (curve 1) (Source: ACC)

Regarding (removed due to confidentiality requirements), the tensile modulus $E_t$ for was extracted after two tensile tests. Results are illustrated in Figures 3.11 and 3.12. The compressive modulus $E_c$ for was extracted after two tensile tests. Results are illustrated in Figures 3.13 and 3.14. The shear modulus $G$ was extracted after three tests. Results are illustrated in Figures 3.15, 3.16, and 3.17. The average magnitudes in the linear range for these (removed due to confidentiality requirements) properties are also summarized in Figure 3.18. The only exception is $G$, for which the modulus magnitude was determined linearization of one single curve.

### 3.3.3 Bonding Process

The sandwich manufacturing process is the same for both the sandwich and the micro-sandwich components. The process begins by the deposition of a HexTow facesheet upon a mould of the component. This facesheet is then impregnated with an epoxy resin, that provides the bonding effect. The core material is then deposed upon the facesheet, and an additional impregnation of epoxy resin is performed over the core material. Finally, the top facesheet is deposed over the core.
Figure 3.12: Traction load as a function of strain for (removed due to confidentiality requirements) (curve 2) (Source: ACC)

Figure 3.13: Flatwise compressive load as a function of strain for (removed due to confidentiality requirements) (curve 1) (Source: ACC)
Figure 3.14: Flatwise compressive load as a function of strain for (removed due to confidentiality requirements) (curve 2) (Source: ACC)

Figure 3.15: Shear load as a function of strain for (removed due to confidentiality requirements) (curve 1) (Source: ACC)
Figure 3.16: Shear load as a function of strain for (removed due to confidentiality requirements) (curve 2) (Source: ACC)

Figure 3.17: Shear load as a function of strain for (removed due to confidentiality requirements) (curve 3) (Source: ACC)
Figure 3.18: Structural properties for core candidate agglomerates (average magnitudes)

Generally speaking, a first cure treatment is then performed in an autoclave, in which the sandwich component is subject to a vacuum treatment during about 24 to 48 hours, depending on the type of resin. Additionally, a second cure treatment follows, consisting on one or more stages in a stove. The number of stages, their temperature and duration depend on the type of resin, and are specified by the resin manufacturer.

This manufacturing process is the same before and after the phasing out of synthetic materials. Before the transitioning toward cork agglomerate, Airex® C70 PVC would be used in sandwiches, and Sphere.Tex would be used in micro sandwiches. After the transitioning, cork agglomerates became used in both types of sandwiches.
Chapter 4

Theoretical Background

4.1 Overview

In this section the mathematical background for the plate-bending behavior and its implementation in the MSC Nastran\textsuperscript{TM} FEA software are developed. The classical plate-bending theory aspects are layed out firstly. This is followed by the classical lamination theory for isotropic and quasi-isotropic multi-layered plates, paving the way for the basic aspects of the implementation of composite shell elements in MSC Nastran\textsuperscript{TM} as they were performed in this study.

4.2 Classical Plate-Bending Theory

4.2.1 General Behavior of Plates

The plate-bending theory postulates the plate as a plane structure whose two largest characteristic dimensions $a$ and $b$ are of a much bigger magnitude than that of its thickness $t$. The plate behaviour is generally referred to that of the midplane, which bissects the plate thickness at each point. Plates are usually divided in three categories: thin plates with small deflections of the midplane, thin plates with large deflections of the midplane, and thick plates.

The structural analysis methods developed in this dissertation lay upon the theory of thin plates with small deflections. A thin plate is one whose thickness $t$ is small when compared with the plate’s smallest span, by a factor of at least...
Figure 4.1: Geometry for a classic plate

(a) A plate of constant thickness [17]  (b) Part of the plate before and after deflection [17]
20. By small displacements we mean those that do not exceed the magnitude of the plate’s thickness \((w < t)\).

Let us consider a load-free plate, shown in Figure 4.1, in which the \(x0y\) plane coincides with the midplane and whose \(w\) deflection is zero. The components of the deflection occurring in the \(x\), \(y\), and \(z\) direction are denoted by \(u\), \(v\), and \(w\), respectively. The fundamental assumptions upon which the small-deflection theory of bending lays postulate that:

1. The transversal deflection in the \(z\) direction is small when compared to the plate thickness \((w < t)\). Tangents to the deformed midplane are negligible when compared to the unit.

2. Strains in the midplane are insignificant when compared with bending strains elsewhere \((u = v = 0 \text{ in the midplane})\).

3. Deformations are such that any plane normal to the midplane will remain normal to the midplane following the deformation \((\gamma_{xz} = \gamma_{yz} = 0)\). The deflection of the plate is therefore associated with bending strains. Consequently, normal strains \(\varepsilon_z\) resulting from transverse loading are also considered negligible.

4. Stresses in the direction normal to the midplane are insignificant \((\sigma_{zz} = 0)\).

The above mentioned are known as the Kirchoff hypotheses and are valid both for isotropic and orthotropic materials.

From assumptions 3 and 4 it follows that \(\sigma_{xz} = \sigma_{yz} = \sigma_{zz} = 0\) and therefore a state of plane strain shall apply to the plate.

### 4.2.2 Strain-Curvature Relations

Departing from assumption 3, the strain-displacements are:
CHAPTER 4. THEORETICAL BACKGROUND

\[ \varepsilon_x = \frac{\partial u}{\partial x} \]  
(4.1a)

\[ \varepsilon_y = \frac{\partial v}{\partial y} \]  
(4.1b)

\[ \varepsilon_z = \frac{\partial w}{\partial z} = 0 \]  
(4.1c)

\[ \tau_{xy} = \frac{\partial u}{\partial y} + \frac{\partial v}{\partial x} \]  
(4.1d)

\[ \tau_{xz} = \frac{\partial u}{\partial z} + \frac{\partial w}{\partial x} = 0 \]  
(4.1e)

\[ \tau_{yz} = \frac{\partial v}{\partial z} + \frac{\partial w}{\partial y} = 0 \]  
(4.1f)

The strains at any point of the plate are given by

\[ \varepsilon_x = -z \frac{\partial^2 w}{\partial x^2} \]  
(4.2a)

\[ \varepsilon_y = -z \frac{\partial^2 w}{\partial y^2} \]  
(4.2b)

\[ \gamma_{xy} = -2z \frac{\partial^2 w}{\partial x \partial y} \]  
(4.2c)

These formulas provide the strains at any point in the plate.

The curvatures of a plain curve are defined as the rate of change of the slope angle of the curve with respect to the distance along the curve.

\[ \frac{1}{\tau_x} = \frac{\partial}{\partial x} \left( \frac{\partial w}{\partial x} \right) = \kappa_x \]  
(4.3a)

\[ \frac{1}{\tau_y} = \frac{\partial}{\partial y} \left( \frac{\partial w}{\partial y} \right) = \kappa_y \]  
(4.3b)

\[ \frac{1}{\tau_{xy}} = \frac{\partial}{\partial x} \left( \frac{\partial w}{\partial y} \right) = \kappa_{xy} = \kappa_{yx} \]  
(4.3c)

The strain-curvature relations can expressed derived from (4.2) and (4.3) in the form

\[ \varepsilon_x = -z \kappa_x \]  
(4.4a)

\[ \varepsilon_y = -z \kappa_y \]  
(4.4b)

\[ \gamma_{xy} = -2z \kappa_{xy} \]  
(4.4c)
No mechanical properties are involved in the derivation of equations (4.1) to (4.4), therefore these relations can be applied to both elastic and inelastic problems. Equations (4.2) to (4.4) state that the strains in the plate vary linearly with distance \( z \) from the midplane.

### 4.2.3 Stresses and Stress Resultants

For the case of a three-dimensional state of stress, the stress-strain relations for an isotropic homogenous material are

\[
\varepsilon_x = \frac{1}{E} \left[ \sigma_x - \nu (\sigma_y + \sigma_z) \right] \quad (4.5a)
\]
\[
\varepsilon_y = \frac{1}{E} \left[ \sigma_y - \nu (\sigma_x + \sigma_z) \right] \quad (4.5b)
\]
\[
\varepsilon_z = \frac{1}{E} \left[ \sigma_z - \nu (\sigma_x + \sigma_y) \right] \quad (4.5c)
\]
\[
\gamma_{xy} = \frac{\tau_{xy}}{G} \quad (4.5d)
\]
\[
\gamma_{xz} = \frac{\tau_{xz}}{G} \quad (4.5e)
\]
\[
\gamma_{yz} = \frac{\tau_{yz}}{G} \quad (4.5f)
\]

with

\[
G = \frac{E}{2(1+\nu)} \quad (4.6)
\]

Bearing in mind that for the small-deflection theory of bending we have \( \varepsilon_z = \gamma_{yz} = \gamma_{xz} = 0 \), equations (4.5) become

\[
\sigma_x = \frac{E}{(1-\nu^2)} (\varepsilon_x + \nu \varepsilon_y) \quad (4.7a)
\]
\[
\sigma_y = \frac{E}{(1-\nu^2)} (\varepsilon_y + \nu \varepsilon_x) \quad (4.7b)
\]
\[
\tau_{xy} = G \gamma_{xy} \quad (4.7c)
\]

Introducing the plate curvatures from equations (4.3) and (4.4) equations (4.7) become
\[
\sigma_x = -\frac{E_z}{1-\nu^2} (\kappa_x + \nu \kappa_y) = -\frac{E_z}{1-\nu^2} \left( \frac{\partial^2 w}{\partial x^2} + \nu \frac{\partial^2 w}{\partial y^2} \right)
\] (4.8a)

\[
\sigma_y = -\frac{E_z}{1-\nu^2} (\kappa_y + \nu \kappa_x) = -\frac{E_z}{1-\nu^2} \left( \frac{\partial^2 w}{\partial y^2} + \nu \frac{\partial^2 w}{\partial x^2} \right)
\] (4.8b)

\[
\tau_{xy} = -\frac{E_z}{1+\nu} \kappa_{xy} = -\frac{E_z}{1+\nu} \frac{\partial^2 w}{\partial x \partial y}
\] (4.8c)

For the moments and forces per unit length (also called stress resultants) we have

\[
\int_{-t/2}^{t/2} z \sigma_x dy dz = M_x dy
\] (4.9a)

\[
\int_{-t/2}^{t/2} z \sigma_y dx dz = M_y dx
\] (4.9b)

\[
\int_{-t/2}^{t/2} \tau_{xy} dz = M_{xy} = M_{yx}
\] (4.9c)

Similarly, for the other resultants we have

\[
\begin{cases}
M_x \\
M_y \\
M_z
\end{cases} = \int_{-t/2}^{t/2} \begin{cases}
\sigma_x \\
\sigma_y \\
\tau_{xy}
\end{cases} z dz
\] (4.10)

where \( M_{xy} = M_{yx} \), and

\[
\begin{cases}
Q_x \\
Q_y
\end{cases} = \int_{-t/2}^{t/2} \begin{cases}
\tau_{xz} \\
\tau_{yz}
\end{cases} dz
\] (4.11)

Substituting (4.8) into (4.10) the following formulas for the bending and twisting moments in terms of the curvatures and the deflection are derived

\[
M_x = -D (\kappa_x + \nu \kappa_y) = -D \left( \frac{\partial^2 w}{\partial x^2} + \nu \frac{\partial^2 w}{\partial y^2} \right)
\] (4.12a)

\[
M_y = -D (\kappa_y + \nu \kappa_x) = -D \left( \frac{\partial^2 w}{\partial y^2} + \nu \frac{\partial^2 w}{\partial x^2} \right)
\] (4.12b)

\[
M_{xy} = -D (1-\nu) \kappa_{xy} = -D (1-\nu) \frac{\partial^2 w}{\partial x \partial y}
\] (4.12c)

where

\[
D = \frac{Et^3}{12(1-\nu)}
\] (4.13)
is the flexural rigidity of the plate.

The static equilibrium equation for plates is derived from the equilibrium conditions for moments and transverse forces and is

\[
\frac{\partial^4 w}{\partial x^4} + 2 \frac{\partial^4 w}{\partial x^2 \partial y^2} + \frac{\partial^4 w}{\partial y^4} = \frac{p(x, y)}{D} \tag{4.14}
\]

and if no transverse loads are applied upon the plate we have

\[
\frac{\partial^4 w}{\partial x^4} + 2 \frac{\partial^4 w}{\partial x^2 \partial y^2} + \frac{\partial^4 w}{\partial y^4} = 0 \tag{4.15}
\]

### 4.3 Plates of Isotropic Multi-Layers

Regarding the bending problem of orthotropic plates, the generalized Hooke’s law is reformulated and becomes

\[
\sigma_x = E_x \epsilon_x + E_{xy} \epsilon_y \tag{4.16a}
\]
\[
\sigma_y = E_y \epsilon_y + E_{xy} \epsilon_x \tag{4.16b}
\]
\[
\tau_{xy} = G \gamma_{xy} \tag{4.16c}
\]

An alternate representation of (4.16) is

\[
\sigma_x = \frac{E'_x}{1 - \nu_x \nu_y} (\epsilon_x + \nu_y \epsilon_y) \tag{4.17a}
\]
\[
\sigma_y = \frac{E'_y}{1 - \nu_x \nu_y} (\epsilon_y + \nu_x \epsilon_x) \tag{4.17b}
\]
\[
\tau_{xy} = G \gamma_{xy} \tag{4.17c}
\]

where \(\nu_x, \nu_y, E'_x,\) and \(E'_y\) are the coefficients of Poisson and effective moduli of elasticity, the latter being experimentally determined. The two sets of equations (4.16) and (4.17) are related by
\[ E_x = \frac{E_x'}{1 - \nu_x \nu_y} \] (4.18a)
\[ E_y = \frac{E_y'}{1 - \nu_x \nu_y} \] (4.18b)
\[ E_{xy} = \frac{E_x' \nu_x}{1 - \nu_x \nu_y} = \frac{E_y' \nu_y}{1 - \nu_x \nu_y} \] (4.18c)

Equations (4.2) remain unchanged, as they are based on geometrical considerations only. This assumption is based upon the classical lamination theory, in which the following assumptions are made regarding the behavior of the laminates of the composite [18]:

- The laminae are perfectly bonded together
- The bonds are infinitesimally thin and no lamina can slip relative to another
- The variation of strain through the laminate thickness is assumed to be linear

By introducing equations (4.2) into (4.16) we have

\[ \sigma_x = -z \left( E_x \frac{\partial^2 w}{\partial x^2} + E_{xy} \frac{\partial^2 w}{\partial y^2} \right) \] (4.19a)
\[ \sigma_y = -z \left( E_y \frac{\partial^2 w}{\partial y^2} + E_{xy} \frac{\partial^2 w}{\partial x^2} \right) \] (4.19b)
\[ \tau_{xy} = -2G_{xy} \frac{\partial^2 w}{\partial x \partial y} \] (4.19c)

The formulas for the moments, by inserting (4.19) into (4.10) and integrating, become

\[ M_x = - \left( D_x \frac{\partial^2 w}{\partial x^2} + D_{xy} \frac{\partial^2 w}{\partial y^2} \right) \] (4.20a)
\[ M_y = - \left( D_y \frac{\partial^2 w}{\partial y^2} + D_{xy} \frac{\partial^2 w}{\partial x^2} \right) \] (4.20b)
\[ M_{xy} = -2G_{xy} \frac{\partial^2 w}{\partial x \partial y} \] (4.20c)

where
The governing differential equation for the deflection of an orthotropic plate becomes

\[ D_x \frac{\partial^4 w}{\partial x^4} + 2H \frac{\partial^4 w}{\partial x^2 \partial y^2} + D_y \frac{\partial^4 w}{\partial y^4} = p \]  \hspace{1cm} (4.22)

where

\[ H = D_{xy} + 2G_{xy} \] \hspace{1cm} (4.23)

The orthotropic plate moduli and Poisson’s ratios

\[ E_x', E_y', \nu_x, \nu_y, G \] \hspace{1cm} (4.24)

are experimentally determined via standardized tension and shear tests. Expressions for flexural rigidities are

\[ D_x = \frac{t^3E_x'}{12(1-\nu_x\nu_y)} \] \hspace{1cm} (4.25a)
\[ D_y = \frac{t^3E_y'}{12(1-\nu_x\nu_y)} \] \hspace{1cm} (4.25b)
\[ D_{xy} = \frac{t^3E_x'\nu_x}{12(1-\nu_x\nu_y)} = \frac{t^3E_y'\nu_x}{12(1-\nu_x\nu_y)} \] \hspace{1cm} (4.25c)
\[ G_{xy} = \frac{t^3G}{12} \] \hspace{1cm} (4.25d)
\[ H = D_{xy} + 2G_{xy} \] \hspace{1cm} (4.25e)

The sandwich plates analyzed in this study are composed of a core assumed by the author to be isotropic and facesheets whose fibers are woven in a "taffeta" fabric with perpendicular 90° orientations. Considering the fact that the orientations of the fibers in both facesheets coincide, we can assume...
that the sandwich composite thus formed is 2-D orthotropic, meaning that it
features similar equivalent properties in perpendicular directions \( x \) and \( y \). Consequently, \( E'_x = E'_y = E \), and \( \nu_x = \nu_y = \nu \). This type of laminate is termed quasi-isotropic.

This means equations (4.17) will become

\[
E_x = E_y = \frac{E}{1 - \nu^2}
\]  

(4.26)

and equations (4.25) become

\[
D_x = D_y = D = \frac{E t^3}{12(1 - \nu^2)}
\]  

(4.27a)

\[
G_{xy} = \frac{E t^3}{24(1 + \nu)}
\]  

(4.27b)

\[
H = \frac{E t^3}{12(1 - \nu^2)} = D
\]  

(4.27c)

Therefore, and departing from experimentally determined orthotropic mod-
uli and coefficients of Poisson, the problem reduces to that of an isotropic plate.

### 4.4 Composite Properties for Shell Elements in MSC Nastran\(^{TM}\)

According to [18], MSC Nastran\(^{TM}\) uses the postulates of the classical lamina-
tion theory in formulating shell behavior for composite laminate element prop-
erties. MSC Nastran\(^{TM}\) allows for two different modelling strategies. Firstly,
by imputing directly the membrane, bending, membrane-bending coupling and
transverse shear constitutive relationships on the PSHELL input card. Secondly,
by defining the composite laminate ply-by-ply via the PCOMP card. The latter
option was followed in the course of this study, as described in Section 5.3.1.

The above mentioned relationships are written in the following form for
composite elements

\[
\begin{bmatrix}
F \\
M
\end{bmatrix} = \begin{bmatrix}
A & B \\
B & D
\end{bmatrix} \begin{bmatrix}
\varepsilon^0 \\
\chi
\end{bmatrix}
\]  

(4.28)

where:
are the membrane, membrane-coupling, and bending matrices respectively. In the MSC Nastran\textsuperscript{TM} software these relationships take the form:

\[
\begin{bmatrix}
F \\
M \\
Q
\end{bmatrix} =
\begin{bmatrix}
tG_1 & t^2G_4 & 0 \\
t^2G_4 & t^3/12G_2 & 0 \\
0 & 0 & t_sG_3
\end{bmatrix}
\] (4.30)

in which:

\[
[A] = tG_1 \quad (4.31a)
\]
\[
[B] = -t^2G_4 \quad (4.31b)
\]
\[
[D] = t^3/12G_2 \quad (4.31c)
\]

Also:

\[
\{Q\} = \begin{bmatrix}
Q_x \\
Q_y
\end{bmatrix} \quad (4.32)
\]
\[
\{\gamma\} = \begin{bmatrix}
\gamma_x \\
\gamma_y
\end{bmatrix} \quad (4.33)
\]

where (4.32) and (4.33) are the transverse shear resultants and the transverse shear strains, respectively. \( t \) is the nominal plate thickness, \( T_s \) is the effective transverse shear material matrix, and \( G_3 \) is the effective transverse shear material matrix.

The constants \( G_1, G_2, G_4, T, G_3, \) and \( T_s \) can either be input directly in the PSHELL card or to have the composite equivalent matrices (4.29) be derived internally from PCOMP card inputs.

The terms \( G_1, G_2, G_4 \) are computed as follows:
CHAPTER 4. THEORETICAL BACKGROUND

\[ G_1 = \frac{1}{I} \int [G_e] dz \quad \text{(4.34a)} \]
\[ G_2 = \frac{1}{I} \int z^2 [G_e] dz \quad \text{(4.34b)} \]
\[ G_4 = \frac{1}{I^2} \int (-z) [G_e] dz \quad \text{(4.34c)} \]

where \( I = \frac{t^3}{12} \) and the element matrix \([G_e]\) for orthotropic materials is calculated as:

\[
[G_e]_0 = \begin{bmatrix}
E_1 & \frac{\nu_1}{1-\nu_1\nu_2} E_2 & 0 \\
\frac{\nu_2}{1-\nu_1\nu_2} E_1 & E_2 & 0 \\
0 & 0 & G_{12}
\end{bmatrix}
\quad \text{(4.35)}
\]

The relationship \( \nu_1 E_2 = \nu_2 E_1 \) must hold. Also, an angle \( \theta \) must be provided in order to indicate the orientation of the material coordinate system relative to the \( G_1 = G_2 \) side of the element. The material elastic modulus matrix is then transformed by:

\[
[G_e] = [U]^T [G_m] [U]
\quad \text{(4.36)}
\]

where:

\[
[U] = \begin{bmatrix}
\cos^2 \theta & \sin^2 \theta & \cos \theta \sin \theta \\
\sin^2 \theta & \cos^2 \theta & -\cos \theta \sin \theta \\
-2\cos \theta \sin \theta & 2\cos \theta \sin \theta & \cos^2 \theta - \sin^2 \theta
\end{bmatrix}
\quad \text{(4.37)}
\]

Finally, the matrix for transverse shear \([G_3]_m\) is a 2 x 2 matrix for which:

\[
[G_3]_m = \begin{bmatrix}
G_{11} & G_{12} \\
G_{21} & G_{22}
\end{bmatrix}
\quad \text{(4.38)}
\]

and the transformed matrix in the coordinate system of the element is

\[
[G_3]_e = [W]^T [G_e]_m [W]
\quad \text{(4.39)}
\]

where:

\[
[W] = \begin{bmatrix}
\cos \theta & \sin \theta \\
-\sin \theta & \cos \theta
\end{bmatrix}
\quad \text{(4.40)}
\]
Chapter 5

Correlation Study

5.1 Correlation Study Overview

This chapter focuses on the correlation study performed to validate a specific computational method for predicting structural deformations on cork core sandwich plates.

The benchmark for this correlation study was a three-point bending test applied to specimens of the sandwich and micro-sandwich composites to be implemented in DAI aircrafts. The relevant output of this test was the mid-vane node deflection \( w \) and the extensions \( \{ \varepsilon_x, \varepsilon_y \} \) at that same point. It was deemed that a comparison between the \( \{ \varepsilon_x, \varepsilon_y \} \) strains obtained would provide more information on the appropriateness of this method than a simple comparison between \( w \) magnitudes.

This correlation study focuses on rectangular plates. This choice of geometry facilitates the implementation of both the computational solutions and laboratorial testing, as rectangular plates are easier to prepare and accommodate, as far as the three-point bending testing goes.

This correlation study focuses on specimens whose total thickness \( t \) is less than \( 1/20 \) of the smaller span, so that the classic plate-bending and lamination theories MSC Nastran\(^{TM}\) makes use of can be assumed to reproduce the behaviour of the plates with acceptable fidelity.

A FE composite model was developed for this case. The boundary and load-
CHAPTER 5. CORRELATION STUDY

...ing conditions reproduce those of the laboratorially performed tests. Likewise, the \( w \) magnitude and the \( \{ \varepsilon_x, \varepsilon_y \} \) strains were extracted for the mid-vane node in all three models. In this model, all properties for the three sandwich layers are explicitly introduced. All rigidity terms for the three layers are therefore developed by MSC Nastran\textsuperscript{TM}. This is made possible by independently introducing the mechanical properties of both the facesheets and the core. This method features the advantage of being parametrical, in the sense that parameters of the sandwich can be iteratively varied without need for testing again the overall mechanical performance of the sandwich.

This correlation study addressed sandwich and micro-sandwich specimens bearing the two different kinds of cork agglomerates in consideration for DAI aircrafts, \( \text{(removed due to confidentiality requirements)} \) and \( \text{(removed due to confidentiality requirements)} \).

The correlation results are compiled in Section 5.4.

5.2 Laboratory Testing

5.2.1 Scope

The mechanical tests herein described aim at providing results that serve as a benchmark for the computational approach.

5.2.2 Sandwich Specimens

Two types of sandwich specimens were provided by DAI: sandwiches and micro-sandwiches. The difference between the two types lies in the thickness of the core. The former have a core thickness \( t_c \) of 6 mm, while the latter have a core of 0.8 mm. Both types of specimens have a facesheet thickness \( t_f \) of 0.2 mm.

Both the sandwiches and the micro-sandwiches were manufactured with two different types of cores: \( \text{(removed due to confidentiality requirements)} \) and \( \text{(removed due to confidentiality requirements)} \). All specimens have a 200 mm length \( a \) and a 100 mm width \( b \). A total of four specimens of each type was manufactured, yielding a total of 24 specimens. These specimens are seen in Figure 5.1.
Figure 5.1: Sandwich specimens provided by DAI, with sandwiches (below) and microsandwiches (above)

5.2.3 Three-Point Bending Machine

An Instron 4208 flexural testing machine was used, and with a three-point bending setup. This machine is seen in Figure 5.2. The three-point setup was prepared with two steel cylinders serving as loading fixtures, as seen in Figure 5.3. Another steel cylinder was placed between the loading pad and the specimen. These cylinders were used so as to mitigate any possible indentation effects.

Four load cells were available for this testing machine: 1 kN, 5 kN, 100 kN, and 300 kN. The 5 kN load cell was selected both for the sandwich and micro-sandwich specimens. This was because it was deemed as the one that would provide enough loading for rupture in all specimens, while providing enough resolution.

The load was applied at a constant rate, with the speed estimated as causing the maximum deflection after 5 minutes. A maximum deflection of 20 mm was estimated for the sandwiches, and a maximum deflection of 10 mm was estimated for the micro-sandwiches. This yielded a rate of 4 mm/min and 2 mm/min, respectively.
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Figure 5.2: Instron 4208 flexural testing machine used for the three-point testing

Figure 5.3: Steel cylinders used as loading fixtures (left) and sandwich specimen standing upon the fixtures (right)
5.2.4 Foil Strain Gauges

In order to extract the strain vector behaviour during the specimens bending tests, triaxial strain gauges by Showa Measuring Instruments Inc. were used. These sensors were customized by Showa according to the specific needs of this testing and are of the configuration type N32-FA5-5-120-11-VS3, where

- N32 is the designation of the triaxial strain gages series, with a triaxial $0^\circ/45^\circ/90^\circ$ stacked rosette
- F is the designation of the base material as polyester
- A is the designation of the foil material as a Cu-Ni alloy
- 5 the gage length in mm
- 120 is the nominal resistance in Ohm
- 11 is the reference of the compensated materials as common steel, SUS630, and SNCM
- VS3 is the designation of the optional specifications of the leadwire as parallel vinyl-coated with a 3 m length

The basic pattern of the triaxial strain gauges is illustrated in Figure 5.4. For each specimen, a sensor was placed at the mid-vane point of the bottom face, with direction 1 coinciding with the longitudinal $x$ axis, and direction 3 coinciding with transversal direction $y$. A photo of these sensors before integration is seen in Figure 5.5.

5.2.5 Experimental Procedure

The first step consisted in preparing the sandwich and microsandwich specimens for accommodating the strain gages. Instructions for strain gage use provided by Showa were followed. An area larger than the strain gage was smoothed with a very smooth sandpaper in order to provide a proper bonding surface. Acetone was then used to degrease the specimen surface. Finally, the strain gage was glued to the geometric center of the specimen surface with glue, with a firm
Figure 5.4: Basic pattern for the Showa triaxial strain gages, with directions labelled

Figure 5.5: Strain gages package (left) and strain gage with cables before integration (right)
pressure applied with the finger upon the gage for one minute, and left at room
temperature for about 3 hours for complete curing.

A three point bending testing was performed over the specimens, in which
a displacement was applied at a constant rate of 4 mm/min for sandwich speci-
mens and 2 mm/min for micro-sandwich specimens until specimen rupture was
reached. The load cell loading curve and the \( \{\varepsilon_1, \varepsilon_2, \varepsilon_3\} \) strain curves were
extracted for each specimen.

5.2.6 Experimental Results

A description of the experimental results obtained can be found in the two fol-
lowing sections. All strain values obtained are provided in \( \mu \text{m/m} \) units, having
been multiplied by a factor of \( 10^{-6} \). Furthermore, Showa prescribes a gage
factor \( K \) by which obtained strain values (apparent strains \( \varepsilon_0 \)) are corrected
in order to take into account additional strains due to the different coefficients
of thermal expansion between the material of the strain gage and that of the
specimen. The relation between apparent strains \( \varepsilon_0 \) and true strains \( \varepsilon \) is

\[
\varepsilon = \varepsilon_0 \frac{2}{K} \quad (5.1)
\]

For gages that measure strains in the \( 10^{-6} \text{ m} \) domain we have that

\[
\frac{\Delta L}{L} \approx \frac{\Delta R}{R} \quad (5.2)
\]

Temperature variations will cause an additional variation in the \( \frac{\Delta R}{R} \) ratio
given by

\[
\frac{\Delta R}{R} = \Delta T (\alpha + K (\beta_s - \beta_g)) \quad (5.3)
\]

Any additional dilations or contractions induced by different coefficients of
thermal expansion between the strain gage and the specimen shall be offset by
a proper value of \( K \) such that (5.3) is equal to zero.

For this application Showa reccomended a value of \( 2.05 \pm 1\% \) for the gage
factor \( K \). A nominal value of 2.05 was selected for strain gage values post-
processing.
Figure 5.6: Loading curves as function of mid-vane deflection for sandwich specimens with *(removed due to confidentiality requirements)* and *(removed due to confidentiality requirements)* cores

**Sandwich Specimens**

For sandwich specimens, loading curves as a function of the specimen mid-vane deflection are seen in Figure 5.6, while \{\varepsilon_1, \varepsilon_2, \varepsilon_3\} are seen in Figures 5.7 to 5.9.

All charts show a domain of linear behavior range for the sandwich specimens, both for load and strain curves.

In the case of the sandwich specimens, the load peak is associated with delamination occurring between the core and the upper facesheet, immediately followed by the buckling of the upper facesheet (as seen in Figure 5.10). Subsequent results have little significance as the specimen no longer structurally behaves as a sandwich.

**Micro-Sandwich Specimens**

For micro-sandwich specimens, loading curves as a function of the specimen mid-vane deflection are seen in Figure 5.11, while \{\varepsilon_1, \varepsilon_2, \varepsilon_3\} are seen in Figures 5.12 to 5.14.

As with sandwich specimens results, micro-sandwich specimens clearly display a linear behavior range both for load and strain curves. An unexpected result was obtained for the *(removed due to confidentiality requirements)* core.
Figure 5.7: Loading curves as function of mid-vane point $\varepsilon_1$ for sandwich specimens with (removed due to confidentiality requirements) and (removed due to confidentiality requirements) cores.

Figure 5.8: Loading curves as function of mid-vane point $\varepsilon_2$ for sandwich specimens with (removed due to confidentiality requirements) and (removed due to confidentiality requirements) cores.
Figure 5.9: Loading curves as function of mid-vane point $\varepsilon_3$ for sandwich specimens with (removed due to confidentiality requirements) and (removed due to confidentiality requirements) cores.

Figure 5.10: Peak load for the sandwich specimens, with delamination occurring between the core and the upper facesheet, followed by upper facesheet buckling.
Figure 5.11: Loading curves as function of mid-vane deflection for micro-sandwich specimens with (removed due to confidentiality requirements) and (removed due to confidentiality requirements) cores.

No delamination occurred and the load peak is associated with the rupture of the upper facesheet by compression (as seen in Figure 5.15).

5.3 Finite Element Model Results

5.3.1 Model Overview

A finite element model was developed in MSC Nastran\textsuperscript{TM} 2008 using four rectangular plates with cork cores under a loading equivalent to that of a 3-point bending test. Its purpose is to assess the validity of a FEA approach for the structural modelling of cork core sandwich and micro-sandwich plates. The four plate models differ in the core agglomerate thickness \( t_c \) (0.8 mm and 6 mm) and material (removed due to confidentiality requirements) and (removed due to confidentiality requirements). The FEA calculations were performed with MSC Nastran\textsuperscript{TM} 2008, while the pre-processing and the post-processing were performed with MSC Patran\textsuperscript{TM} 2008.

The mesh lies in the \( x0y \) plane, has unconstrained edges and is designed in such a way that element boundaries will coincide with the two lines of the
Figure 5.12: Loading curves as function of mid-vane point $\varepsilon_1$ for micro-sandwich specimens with (removed due to confidentiality requirements) and (removed due to confidentiality requirements) cores.

Figure 5.13: Loading curves as function of mid-vane point $\varepsilon_2$ for micro-sandwich specimens with (removed due to confidentiality requirements) and (removed due to confidentiality requirements) cores.
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Figure 5.14: Loading curves as function of mid-vane point \( \varepsilon_3 \) for micro-sandwich specimens with (removed due to confidentiality requirements) and (removed due to confidentiality requirements) cores.

Figure 5.15: Peak load for the micro-sandwich specimens, with upper facesheet rupture by compression.
plate supported by the loading fixtures, and also the line in which the load is
applied by the testing machine. The supporting effect of the loading fixtures is
simulated by constraining the three DOF along the supported lines.

According to the MSC Nastran\textsuperscript{TM} 2004 User’s Guide \cite{19}, the assumptions
used by this software to predict shell behavior for nonuniform and composite
laminates are those of classical lamination theory. This allows the modelling of
plates where the bending and membrane behaviors are coupled. Two alternative
approaches are provided: either by using the PSHELL card to be associated with
shell elements and inputing bending, membrane, membrane-bending coupling
and transverse shear constants; or by using the PCOMP card, in which relevant
properties are defined for each individual ply.

The latter option was chosen. The plate was explicitly modelled as a three-
ply composite material laminate, for which all stiffness terms are developed.
The mesh is constituted by the 30 x 60 CQUAD8 quadrilateral shell elements,
as discussed in Section 5.3.2. This mesh is depicted in Figure 5.16.

The lay-up sequence of the three plies is modelled via the MSC Nastran\textsuperscript{TM}
PCOMP card, which defines the relevant properties of an n-ply composite ma-
terial laminate \cite{20}, such as the thickness of each ply, its constitutive material,
and the overall stacking sequence.

The two outermost plies are modelled via the MAT8 card as a 2-D orthotropic material where elastic modulii $E_1$ (in the longitudinal direction) and
$E_2$ (in the transversal direction) are 143 MPa, which is the typical tensile modulus
for an epoxy composite for HexTow\textsuperscript{TM}, as seen in in Table 3.2. Since no
Poisson coefficient $\nu$ or in-plane shear modulus $G_{12}$ is provided by the manu-
facturer, a representative magnitude of 0.3 was assigned to $\nu$. The orientation
angle $\theta$ of the material coordinate system relative to the element coordinate
system is 45°.

The core is modelled via the MAT1 card as a linear isotropic homogenous
material for which the Young modulus $E$ coincides with linearized tensile modulus
$E_t$. The $E$, $G$ and $\rho$ magnitudes for (removed due to confidentiality re-
quirements) and (removed due to confidentiality requirements) are those listed
in Figure 3.18.

A diagram of the stacking sequence for both sandwich and micro-sandwich
Figure 5.16: FE mesh for the rectangular sandwich plate study

models is seen in Figure 5.17.

The loading case in consideration simulates the loading condition found in the three-point bending testing. The support provided by the loading fixtures was modelled by a constraintment of the three translational DOF $u$, $v$, and $w$. The loading along the mid-vane section was applied at the same constant rate of laboratory tests. A constant rate of 4 mm/min was applied to the sandwich models, and a constant rate of 2 mm/min was applied to the microsandwich models. These loadings were applied in the negative $z$ direction. Vertical displacements $w$ across the plate were extracted, along with the $\epsilon_x$ and $\epsilon_y$ magnitudes at $z = -t/2$ and the nodal constraint forces. For each analysis, the $z$ components nodal constraint forces along the mid-vane of the plate were summed to determine the equivalent mid-vane load.

The finite element analysis was performed over the deflection range in which all specimens featured a sandwich behavior (that is, before rupture). The maximum magnitudes of deflection correspond to the maximum load and are listed in Figure 5.18. The duration of each of the four FE analyses performed coincided with the duration of sandwich behavior.

5.3.2 Convergence Study

Before any FEA is undertaken, it should be confirmed that the selected outputs will have enough fidelity. For the case in consideration, in which a structural
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Figure 5.17: Stacking sequence of the sandwich and micro-sandwich specimens, with the orientation of the facesheet plies

Figure 5.18: Deflection magnitudes for the specimen behavior

Removed due to confidentiality requirements
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Table 5.1: Deflections and strains tensor at the mid-vane node of the sandwich mesh for increasingly refined meshes

<table>
<thead>
<tr>
<th>Mesh</th>
<th>$w_{\text{max}}$ (m)</th>
<th>$\varepsilon_x^1$</th>
<th>$\varepsilon_y^1$</th>
<th>$\gamma_{xy}^1$</th>
</tr>
</thead>
<tbody>
<tr>
<td>5 x 10</td>
<td>-2.099764E-04</td>
<td>9.6396E-06</td>
<td>9.6396E-06</td>
<td>1.59506E-05</td>
</tr>
<tr>
<td>10 x 20</td>
<td>-2.099777E-04</td>
<td>1.08628E-05</td>
<td>1.08628E-05</td>
<td>1.74880E-05</td>
</tr>
<tr>
<td>15 x 30</td>
<td>-2.099765E-04</td>
<td>1.12980E-05</td>
<td>1.12980E-05</td>
<td>1.79563E-05</td>
</tr>
<tr>
<td>20 x 40</td>
<td>-2.099769E-04</td>
<td>1.15056E-05</td>
<td>1.15056E-05</td>
<td>1.81893E-05</td>
</tr>
<tr>
<td>30 x 60</td>
<td>-2.099761E-04</td>
<td>1.17200E-05</td>
<td>1.17201E-05</td>
<td>1.84144E-05</td>
</tr>
</tbody>
</table>

model reproduces the three-point loading of a rectangular sandwich plate, this involved the assessment of the required mesh refinement. A convergence study was performed, in which the deflection $w$ and the $\varepsilon_x$ and $\varepsilon_y$ strains at the mid-vane node were determined for increasingly refined meshes.

For the case in consideration, five increasingly refined meshes were created for a (removed due to confidentiality requirements) core sandwich specimen. For each of these, a representative load of 200 N/m is applied along the plate’s midspan. All elements were assigned the composite property record PCOMP, with the material properties for the core being those of (removed due to confidentiality requirements), and the facesheets material properties being those of Hextow AS4C 3K. The facesheets thickness $t_f$ is 0.2 mm and the thickness of the core $t_c$ is 6 mm.

The resulting deflection $w$ at the mid-vane node was extracted. In all five meshes a node coincides with the mid-vane point. The output of these analyses is compiled in Table 5.1 and in the graph in Figure 5.19.

The deflection $w$ results compiled in Table 5.1 indicate that the selected mesh refinements produce a reliable enough output, as $w$ clearly remains stable all through the mesh refinement process, with variations of little significance. A clear convergence is also observed for the $\varepsilon_x$ and $\varepsilon_y$ strains.

Therefore, and because little computational intensity is always involved for each of the five refinements, the 30 x 60 mesh was selected to perform the mechanical behaviour prediction of the cork core sandwich plates.
Figure 5.19: Deflection at the mid-vane node of the sandwich mesh for increasingly refined meshes

Figure 5.20: \( \{ \varepsilon_x, \varepsilon_y \} \) strains at the mid-vane node of the sandwich mesh for increasingly refined meshes
5.4 Model Validation

5.4.1 Sandwich Specimens

A description of the correlation between the FEA composite ply model and the laboratory results is presented in this section for the sandwich specimens of (removed due to confidentiality requirements) and (removed due to confidentiality requirements) cores.

Regarding the results correlation between load curves (seen in Figures 5.21 and 5.22), it can be observed that FEA yields curves that indicate a considerable lower rigidity. This discrepancy is more obvious in the case of (removed due to confidentiality requirements) core. A possible explanation for this may lie in the fact that the FEM includes the mechanical properties of the core materials (removed due to confidentiality requirements) and (removed due to confidentiality requirements) in their pure form, not taking into account the effect the impregnation of epoxy resin in the core that takes place during lay-up and curing. The impregnation of the epoxy resin will provide additional rigidity to the core and therefore to the specimen.

An additional fact that seems to reinforce this explanation is that the discrepancy is less pronounced in the case of an (removed due to confidentiality requirements) core, which features a density which is nearly the double of that of (removed due to confidentiality requirements). This greater density implies less porosity and hence much less absorption of epoxy resin during the bonding and curing process. This will imply a less pronounced effect in the rigidity increase of the specimen.

Regarding the correlation between the $\varepsilon_x$ strain curves (seen in Figures 5.23 and 5.24), a significant discrepancy between FEA and laboratory results can be seen both for (removed due to confidentiality requirements) and (removed due to confidentiality requirements). It is observed that the FEA load curve for both (removed due to confidentiality requirements) and (removed due to confidentiality requirements) possesses a lesser elastic modulus than that obtained in the laboratory. This result is in line with the fact that the FEM does not take into account the contribution of the epoxy resin in impregnation of the core, thus providing results of a less rigid model.
Figure 5.21: Correlation between mid-vane load curves for sandwich specimens with (removed due to confidentiality requirements) core

Regarding the correlation between $\varepsilon_y$ (seen in Figures 5.25 and 5.26), a significant magnitude discrepancy between FEA and laboratory results is still observed. The load curves obtained via FEA feature higher absolute strain magnitudes than those observed in the laboratory tests for the same load. This discrepancy is consistent with that observed for $\varepsilon_x$, since higher than expected $\varepsilon_x$ absolute magnitudes necessarily imply higher $\varepsilon_y$ absolute magnitudes.

It is also observed that for both specimens $\varepsilon_x > 0$ and $\varepsilon_y < 0$, which is the expected outcome for a mid-vane point on the bottom face of the specimen under three-point bending.

Finally, it can be remarked that the sandwich behavior regarding deflection and strains clearly features a linear behavior. Since the MSC Nastran$^{TM}$ FEM for a sandwich composite is a linear one, a numeric correlation can be established between the composite FEA and the sandwich behavior.

5.4.2 Micro-Sandwich Specimens

A description of the correlation between the FEA composite ply model and the laboratory results is presented in this section for the micro-sandwich specimens of (removed due to confidentiality requirements) and (removed due to confidentiality requirements) cores.
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Figure 5.22: Correlation between mid-vane load curves for sandwich specimens with \( \text{removed due to confidentiality requirements} \) core

\[ \text{Removed due to confidentiality requirements} \]

Figure 5.23: Correlation between \( \varepsilon_x \) curves for sandwich specimens with \( \text{removed due to confidentiality requirements} \) core

\[ \text{Removed due to confidentiality requirements} \]
Figure 5.24: Correlation between $\varepsilon_x$ curves for sandwich specimens with (removed due to confidentiality requirements) core

Figure 5.25: Correlation between $\varepsilon_y$ curves for sandwich specimens with (removed due to confidentiality requirements) core
Regarding the results correlation between load curves (seen in Figures 5.27 and 5.28), it can be observed that FEA yields curves that indicate a smaller rigidity of the FEM specimen, but with a less pronounced discrepancy than that observed in the sandwich specimens.

As with the sandwich specimens, the discrepancy between load curves is less pronounced in the case of an (removed due to confidentiality requirements) core, which features a density which is nearly the double of that of (removed due to confidentiality requirements). This greater density will likewise imply less porosity and hence much less absorption of epoxy resin during the bonding and curing process. This will result in less pronounced effect in the rigidity increase of the specimen.

Regarding the correlation between the $\varepsilon_y$ strain curves (seen in Figures 5.29 and 5.30), it can be observed a significant discrepancy between FEA and laboratory results, both for (removed due to confidentiality requirements) and (removed due to confidentiality requirements). Also, and unlike the sandwich correlation results, strain magnitudes are bigger than those obtained via FEA for the same load. This result is not in line with the fact that the FE composite model is more rigid.

Finally, regarding the correlation between $\varepsilon_y$ (seen in Figures 5.31 and 5.32),
a significant magnitude discrepancy between FEA and laboratory results is still observed. The load curves obtained via FEA feature a lesser rigidity modulus than those obtained in the testing. In addition, the \( \varepsilon_y \) strain curve obtained for the (removed due to confidentiality requirements) specimen features positive values. This is not consistent with previous results, as it would be expected that \( \varepsilon_y < 0 \) and \( \varepsilon_x > 0 \) for a mid-vane point on the bottom face of the specimen under three-point bending.
Figure 5.28: Correlation between mid-vane load curves for micro-sandwich specimens with (removed due to confidentiality requirements) core

Figure 5.29: Correlation between $\varepsilon_x$ curves for micro-sandwich specimens with (removed due to confidentiality requirements) core
Figure 5.30: Correlation between $\varepsilon_x$ curves for micro-sandwich specimens with (removed due to confidentiality requirements) core.

Figure 5.31: Correlation between $\varepsilon_y$ curves for sandwich specimens with (removed due to confidentiality requirements) core.
Figure 5.32: Correlation between $\varepsilon_y$ curves for sandwich specimens with (removed due to confidentiality requirements) core
Chapter 6

Conclusions and Future Prospects

An experimental and computational study was performed in which the mechanical behavior of cork core sandwich and micro-sandwich specimens and the results were compared to validate the finite element models.

The overall results for sandwich specimens (core thickness $t_c = 6$ mm) indicate substantial discrepancies between both curves, which are attributed to the effect of epoxy resin impregnation in the core cork agglomerate. However, the overall results for micro-sandwich specimens (core thickness $t_c = 0.8$ mm) indicate less pronounced discrepancies, which suggests a less pronounced effect of epoxy resin upon the core.

An additional factor that can improve FEA results is to take into account that cork agglomerates feature different magnitudes for the tensile modulus $E_t$ and compressive modulus $E_c$. Since $E_t$ was attributed to the tensile and compressive range and $E_t > E_c$, the FEM behavior is necessarily more rigid. A strain-stress curve defined over the compressive and tensile domains shall be input into the FE material core models in further research.

Also, the assumption that (removed due to confidentiality requirements) behavior can be approximated by a linear isotropic behavior must be revised, as its experimentally determined magnitudes for $E_t$ and $G$ yield $\nu > 1$ when applied to Equation (4.6). This makes the linear isotropic behavior assump-
tion inconsistent, and therefore FEA results for (removed due to confidentiality requirements) core specimens need to be revised and further validated.

Regarding the laboratorial results obtained for the sandwich and micro-sandwich specimens, additional tests should be performed over each of the four types of specimens for further consistency. This is particularly true regarding the results obtained for $\varepsilon_y$ in the (removed due to confidentiality requirements) core sandwich specimen (see Figures 5.9 and 5.25). However, this was not possible due to the fact that only four triaxial strain gauges were available for this testing. This makes the linear isotropic behavior assumption inconsistent, and therefore FEA results for (removed due to confidentiality requirements) core specimens need to be revised and further validated.

Further work shall also include additional FEA analysis for validation, in order to gain more insight over the most appropriate models for predicting cork core sandwich components such as:

- The use of an isotropic material property definition for the FEM shell models, where equivalent isotropic mechanical properties are attributed to the shell elements using the MSC Nastran \textsuperscript{TM} PSHELL card instead of the ply composite card PCOMP. These equivalent properties will be determined by PIEP in the course of the Aerocork project (see Section 3.2) and comprise equivalent values for the plate modulii and coefficients of Poisson (4.24).

- The use of an alternative option for coupling the bending and membrane behavior (previously described in Section 5.3.1), in which PSHELL cards are assotiated with the shell elements. This simplified approach considers that only the facesheets carry in plane and bending loads, while the core carries all the shear loads. The constitutive relationships for bending, membrane, membrane-bending coupling and transverse shear are then inserted in the PSHELL card. A more detailed description of this technique (also termed honeycomb panel equivalent) can be found in [21].

- The use of a honeycomb panel equivalent FE models. This simplified approach considers that only the facesheets carry in plane and bending loads, while the core carries all the shear loads. This approach holds true
in cases where the bending stiffness of the core is of a considerably lesser magnitude than that of the facesheets. However, laboratorial results seem to indicate that agglomerate cork cores have significant bending stiffness when coupled with carbon/epoxy face sheets. This is because bending stiffness varies for different cork agglomerate cores, under the same bending loading conditions [22]. This is to assess the validity of this approach when applied to cork core sandwich plates similar to those that will be implemented in DAI aircrafts.
Bibliography


