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Modeling a Combined Calcium-Copper System for Integrated Carbon Dioxide Capture and Clean Energy Production

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Abstract

The current increase in greenhouse gas emissions has a significant impact on the global warming rate. This impact requires mitigation and, where possible, a reversal of this trend. A feasible solution to this phenomenon is the development of multiple industrial methods aimed at capturing CO₂ from flue gas emissions that result from chemical processes. Chemical looping is an ideal method for performing carbon capture during blue hydrogen production; however, multiple technological systems are needed for the process to remain profitable. In this paper, the initial steps in model development methodology are presented by the validation of the kinetic parameters. In addition, a short case study was performed to investigate the economic impact of the industrial process for a single operating cycle.

Keywords: *clean energy production, chemical looping, Ca-looping, mathematical modeling and validation.*

1. Introduction

The key challenge of modern times, besides researching and treating serious global health issues, is to mitigate and reverse the effects of global warming. The significance of the latter issue, in terms of social impact, and how it affects us all cannot be overstated. The effects of global warming are becoming increasingly more evident primarily due to the rapid increase in greenhouse gas emissions (GHG). Recognizing this danger, humanity is striving to find a solution through various climate agreements, involving governments and economies around the world. [1, 2]. According to the latest data in the literature, there are three main gases in the atmosphere with large amounts that have global warming effects: carbon dioxide (CO₂), methane (CH₄) and nitrous oxide (N₂O). The composition of these gases in the atmosphere is illustrated in Fig. 1. CO₂ constitutes the largest percentage of greenhouse gases in the atmosphere.

In order to reduce the level of CO₂ in the atmosphere, the solution is either to separate these gases from the atmosphere through filtration, or to capture them chemically from flue gas emissions of industrial processes, after which they can be stored or reused. These methods are called Carbon Capture, Utilization, and Storage (CCUS).

However, in small quantities, its role in global warming is not as significant as other gases such as, for example CH₄ or CF₄. [3, 4]. can be utilized in important industrial processes, specifically in the production of methanol and formaldehyde. The most cost-effective way to capture CO₂ in situ during chemical processes is through chemical looping (CL), thereby minimizing carbon dioxide emissions from the synthesis process. CL is a viable industrial technology that uses metal oxides as oxygen carrier (OC) materials to deliver oxygen from the air to the reaction chamber and perform a combustion or reforming process without direct O₂ usage. There are two known operating types for these reactor systems: tubular reactors

equipped with fluidized bed or with fixed bed. In the case of Ca-Looping, the tubular reactor is filled with a Ca-based material which can form chemical bonds with CO_2 . In most cases, calcium oxide (CaO) is used for this purpose as it is cheap and the reaction proceeds with high efficiency [3, 4].

A chemical looping process must contain the following stages:

- *Carbonation process (A)*, where a steam enhanced reforming process can be performed and large amount of blue H_2 can be produced, while the in situ generated CO_2 gas can be captured by CaO;
- *Oxidation process (B)*, where the oxidation process of the OC material is carried out;
- *Calcination/Reduction process (C_1 , C_2 , C_3)*, where the active OC material reacts with the fuel gases to produce a large amount of energy to induce the calcination process.

The goal of this study was to develop a complex mathematical model to simulate the Chemical Looping process with Integrated Combustion and CO_2 Capture (CL-ICCC) and investigate the behaviour of the model under dynamic conditions. The innovation of our research is the structure of the mathematical model. Besides model development, a case study was also performed to investigate the economic impact of a single cycle of the process [6].

2. Validation of the kinetic model by experimental data from literature

Model development commenced with identifying and verifying the kinetics of chemical reac-

tions. A wealth of literature data were available for analysing the processes. Multiple literature sources were reviewed in order to identify the most appropriate and accurate kinetic parameters describing the reaction rates of these processes, and to create a precise and reliable mathematical model using the resulting equations.

2.1. Oxidation process (B)

In the case of the oxidation stage, only one chemical reaction takes place in the system. There are various kinetic equations presented in the literature, and they differ significantly from one another. In most cases, the rate equation is greatly influenced by the equilibrium concentration of O_2 in the gas phase or by its equilibrium partial pressure [7].

In contrast, if the system uses particles coated with a thin layer of OH material, the mathematical equation changes significantly. In this case, the reaction time must be calculated using the physical parameters and the rate constant, and the rate coefficient of the process—also known as the rate-determining step—must be determined from this. The mathematical equations used to develop the kinetic model are shown in Equations (1)–(3). Key factors in determining the residence time include the thickness and molar density of the OC material on the granule surface, while the concentration of the active component in the gas phase is also essential. However, several sources indicate that the kinetics of this chemical process are not linear but rather follow a curve resembling a square-root function [8]. Therefore, the kinetic parameters were examined for both types of equations. The results of the linear model fit well, as expected, but sufficiently good results were also obtained in the case of the square-root function, as can be seen in Fig.2. and 3. Accordingly, an upward trend can be observed in the figures, from which it can be concluded that the curves will rise more steeply, increasingly matching the corresponding practical results [8].

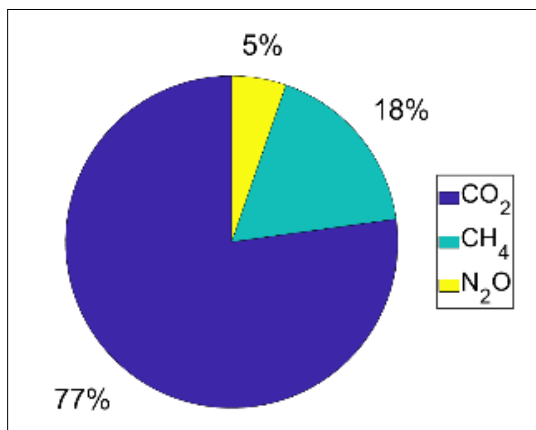


Fig. 1. The composition of greenhouse gas emissions by mass composition. [3]

$$\tau_{ox} = \frac{L_{CH} \rho_{m,CH}}{bk_{ox} C_{O_2}^n} \quad (1)$$

$$\frac{dX_{ox}}{dt} = \frac{1}{\tau_{ox}} \quad (2)$$

$$\frac{dX_{ox}}{dt} = \frac{2}{\tau_{ox}} (1 - X_{ox})^{\frac{1}{2}} \quad (3)$$

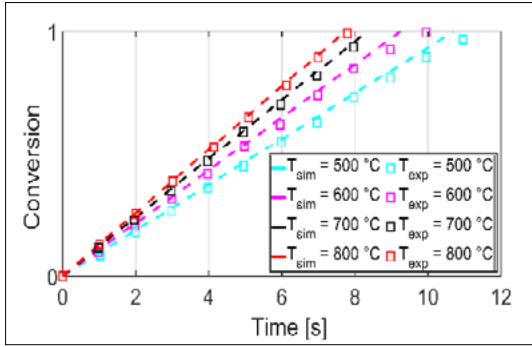


Fig. 2. Validation of the oxidation process: Cu conversion by time in the case of equation (2).

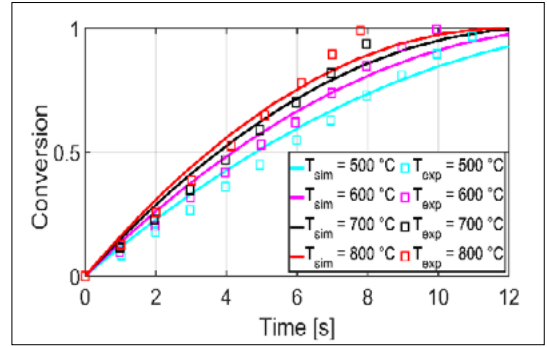


Fig. 3. Validation of the oxidation process: Cu conversion by time in the case of equation (3).

2.2. Calcination process

The following industrial stage is the reduction process, where the captured CO_2 gas is released due to the excess energy. The kinetics of these processes are almost identical to those of the oxidation step and to the carbonation process. To evaluate the literature data, the behaviour of the kinetic equations was also examined and compared to the experimental data during the reduction step. In the validation process, three different reduction fuels were investigated: H_2 , CO , and CH_4 . The mathematical models' behaviour was examined in the case of Equations (4) – (6). [8]

$$\tau_{\text{red},i} = \frac{L_{\text{CuO}} \rho_{\text{m,CuO}}}{bk_{\text{red},i} C_i^n} \quad (4)$$

$$\frac{dX_{\text{red},i}}{dt} = \frac{1}{\tau_{\text{red},i}} \quad (5)$$

$$\frac{dX_{\text{red},i}}{dt} = \frac{2}{\tau_{\text{red},i}} (1 - X_{\text{red}})^{\frac{1}{2}} \quad (6)$$

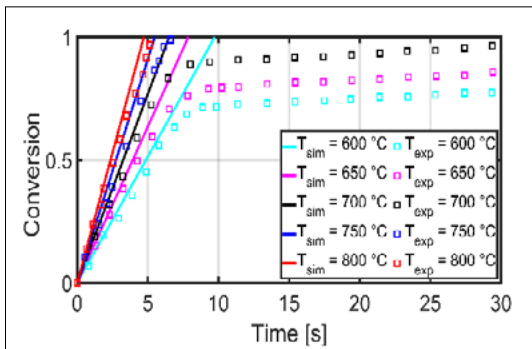


Fig. 4. Validation of the calcination process: CuO conversion by time in the case of equation (5) with H_2 gas as reduction fuel.

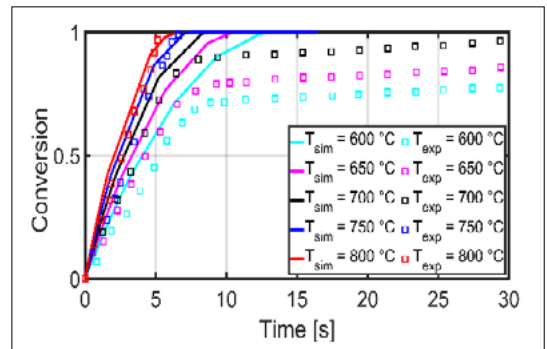


Fig. 5. Validation of the calcination process: CuO conversion by time in the case of equation (6) with H_2 gas as reduction fuel.

2.2.1. Reduction with H_2 gas (C_1)

Regarding the kinetics of the process, the phenomenon observed in the oxidation process appears to be accurate: the kinetic parameters were determined using a linear equation, but they only match the experimental results up to a certain point. During the process, the conversion of the solid material increases, then after 6 seconds this value reaches a breakpoint, followed by a very slow increase, as indicated by the well-illustrated break in the experimental results shown in Fig. 4. Given this, the chosen mathematical equation type does not fully correspond to reality; therefore, fitting a square-root function to the experimental data is a better option.

Fig. 5 was created by fitting the graphical representation corresponding to Equation (6). In this case, the simulated results fit the process much more naturally, with the significant difference that the mathematical model exceeds the ceiling value in some places relative to the experimental results. The explanation for this phenomenon is

that the experimental results show a trend in temperature change from which it can be inferred that, at higher temperatures, the maximum conversion of the solid component approaches 100%, as shown in Fig. 5. During the process, it is essential that the reactor reaches the solid phase's maximum conversion, as the remaining solid components can have a negative impact on subsequent processes, thereby contaminating the resulting products in a way that would make the purification process extremely costly.

2.2.2. Reduction with CO gas (C₂)

Through the reduction process with CO gas, a simple chemical reaction takes place, in which the fuel is converted into CO₂, and the separation can be performed in an efficient way. As previously mentioned, CO₂ gas is released in parallel during the process, so this larger volume of gas can be easily handled.

The process proceeds much more slowly with the same amount of solid phase than with H₂ gas. The linear equation can only adequately track the graph drawn from experimental data up to the breakpoint (Fig. 6).

The experimental data fits much more naturally in the case of Equation (6), as shown in Fig. 7. If the reduction step is carried out with CO gas, the process must be considered slower compared to the case of H₂ gas. In practice, this phenomenon can be attributed to diffusion effects and the size of the gas molecules. In the case of CO gas, the peak occurs at approximately 45 seconds and is exactly 7.5 times slower than in the case of H₂ gas.

2.2.3. Reduction with CH₄ gas (C₃)

For CH₄ gas, the analysis proceeded in a similar manner; for both types of equations, the graphical representations of equation (5) and (6) were fitted to the experimental data. As in previous cases, the linear equation (5) fits the experimental data with high accuracy; however, a peak point also appears here, where the curves break, and a much slower growth phenomenon occurs (Fig. 8). In this case as well, the square-root function provides us with more reliable results, since the graphical representation of the mathematical model begins to curve in a manner consistent with the breakpoint (Fig. 9). Based on its kinetics, this type of reduction falls between the other two fuels: the detectable breakpoint occurs around 15 seconds, which is exactly 2.5 times slower than in the case of H₂ gas.

3. Case study: Investigation of the profitability of a single cycle of the presented process

After validating the mathematical models, a simple case study was conducted to determine how much valuable gas and how much electricity could be produced from the process over a complete cycle. The simulations were performed using the MATLAB/Simulink with „ode15s” solver, and they were performed based on literature data for operating parameters [6, 7, 8]. Table 1 shows the quantities of useful products obtained during the various processes. The abbreviations of the processes in Table 1 were defined in the introduction. Several factors can occur during the process that may reduce its efficiency. One

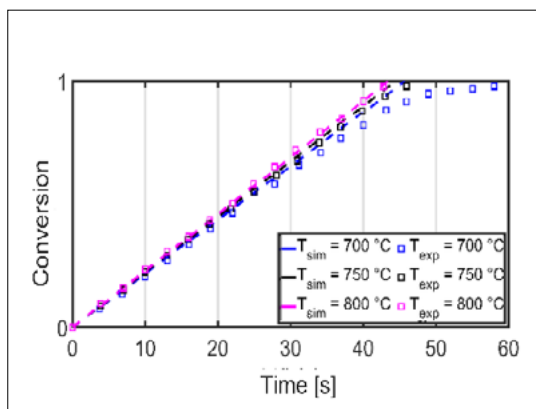


Fig. 6. Validation of the calcination process: CuO conversion by time in the case of equation (5) with CO gas as reduction fuel.

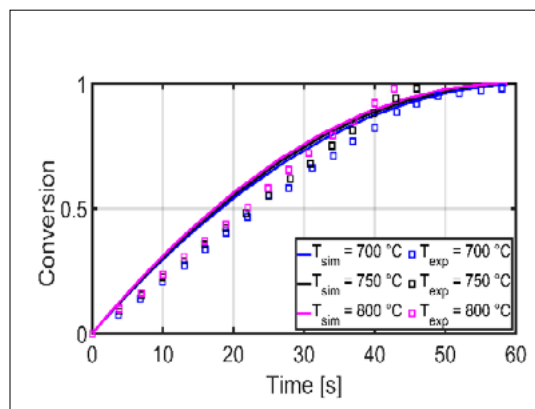


Fig. 7. Validation of the calcination process: CuO conversion by time in the case of equation (6) with CO gas as reduction fuel.

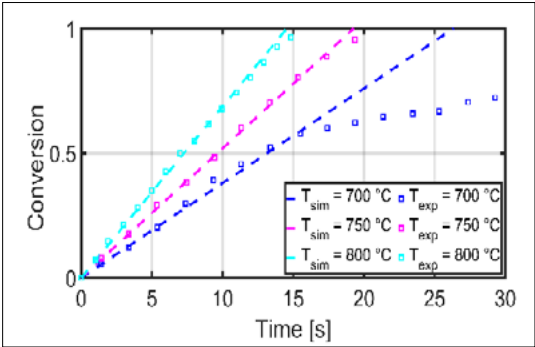


Fig. 8. Validation of the calcination process: CuO conversion by time in the case of equation (5) with CH₄ gas as reduction fuel.

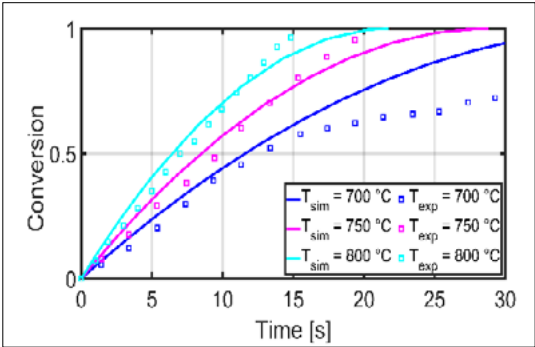


Fig. 9. Validation of the calcination process: CuO conversion by time in the case of equation (6) with CH₄ gas as reduction fuel.

such situation is the onset of the calcination process during the oxidation step, which causes the amount of CaCO₃ to decrease; more precisely, 7% of the CaCO₃ is lost in the process. At the same time, H₂ is consumed during the process to carry out the reduction step, so there is no need to use an external source; in other words, the production line also produces the raw materials it needs.

Table 2 lists the energy requirements of the processes and the maximum recoverable energy amounts for the compression step and the initial temperature control step of the process. The energy balance does not include the energy required for the preheating process, the energy consumed during the chemical process, nor does it account for the amount of energy that can be recovered using heat exchangers. In most cases, the product leaves the reactor at high temperature and passes through condensers to separate the generated water vapour.

Table 1. The amount of the produced components by the industrial process

Amount of gas by process (kmol/cycle)						
	H ₂	CO	CH ₄	H ₂ O	CO ₂	O ₂
A	6654	0	-1700	-3256	1557	0
B	0	0	0	0	0	-982
C1	-1963	0	0	1963	0	0
C2	0	-1963	0	0	1963	0
C3	0	0	-491	982	491	0
Total amount by reduction process (kmol/cycle)						
C1	4691	0	-1700	-1293	1557	-982
C2	6654	-1963	-1700	-3256	3520	-982
C3	6654	0	-2191	-2274	2048	-982

Table 2. The invested and recovered energy from the industrial process (considering the compression and preheating steps).

	Electric power IN	Electric power OUT
Carbonation process	64.5 MW	53.2 MW
Oxidation process	145 MW	62 MW
Reduction process	0	0
Total	209 MW	115 MW

The energy recovered in this way can cover a large portion of the invested energy. For the quantities shown in Table 2, 82.6% of the energy invested in the carbonation process and 42.9% of the energy invested in the oxidation process can be recovered. Energy loss from the chemical reactor was not considered, and the operating parameters were obtained from a literature source [9, 10]. Based on that, the efficiency of the compressor was 90%, while in the case of the gas turbine, efficiency was 40%. No energy can be recovered during the reduction process via the aforementioned method, because the process takes place at atmospheric pressure; energy can only be recovered using heat exchangers.

4. Conclusions

In this work, the examination and validation of the kinetic parameters was performed to confirm the reliability of the results found in the litera-

ture. The analysis was done across all chemical reactions in the system. The simulation results were correlated with experimental data obtained from the literature, thereby confirming that our models accurately reflect the natural behavior of the process. Following the simulation, the amount of H_2 that could be produced and, how much CO_2 is generated was determined.

Alongside component monitoring, the amount of recovered energy was defined, thereby demonstrating the economic viability of the processes. In the best-case scenario, 13.31 tons of H_2 gas can be produced alongside 84.79 tons of CO_2 gas in a single cycle. Considering the current market prices, the process can yield a profit of \$54,033.38 in the best-case scenario per cycle, provided that all generated products can be successfully sold [11]. A full cycle of the industrial process takes approximately 1 hour, depending on additional processes, which can be also activated.

The results obtained are encouraging, and there is great potential to make this process much greener and more economical.

Further research objectives can include preparing comprehensive and detailed energy and financial studies of the process under both batch and continuous operation.

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Nomenclature

X_y	conversion of the process y , y = oxidation, reduction, dimensionless
k_y	rate constant of reaction y , y = oxidation, reduction, m/s
C_i	concentration of component i (O_2 , H_2 , CO , CH_4), kmol/m ³
P	total pressure, Pa
P_0	initial value of total pressure, Pa
L_{Cu}	thickness of Cu layer, m
$\rho_{m,Cu}$	molar density of Cu layer, kmol/m ³
b	stoichiometric component of the solid raw material, dimensionless
n	reaction order, dimensionless
τ_j	operating time of the process j , j = oxidation, reduction; s.

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Examination of the Mechanical Properties of Composite Sandwich Panels

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Abstract

The present study examines the effect of layer composition on the mechanical properties of lightweight composite sandwich panels by evaluating six distinct configurations. The cores of the test specimens measured 600×100×50 mm, with reinforcement layer thicknesses ranging from ~ 0.5–1 mm on the side surfaces and ~ 3.3–5.5 mm on the top and bottom surfaces, depending on the specific layering. All specimens were fabricated using the hand lay-up method for matrix application and vacuum bagging for lamination. Mechanical performance was evaluated through three-point bending tests conducted at a constant speed of 10 mm/min. The results indicate that the specimens with an additional carbon fiber reinforcement layer (C200T) exhibited a 69.57% higher load-bearing capacity than the base specimen (U165P), with only a 23.83% increase in weight. Variations in the areal weight of glass fabric did not result in significant differences. Specimens with 5 mm-thick PVC foam (PVC5) also outperformed the base specimens (U165P) considerably, achieving a 27.17% higher load-bearing capacity with only a 13.90% increase in weight.

Keywords: sandwich panel, composite, three-point bending, foam core, fiber reinforced composite.

1. Introduction

Composite materials are fundamental pillars of 21st-century technology and industry. A composite material is a structure made from at least two different materials with different physical characteristics that, when combined, exceed the physical properties of both the original materials.

Composite materials consist of two main components:

The matrix material (bonding agent), which can be polymer, metal, ceramic, etc. The matrix material maintains the material's form and bonds the components together.

The reinforcement ensures the material's high strength and rigidity. The reinforcement most commonly consists of fibers or particles. These can be formed from glass, carbon aramid, etc. It is important to note that orientation and arrangement have a decisive influence on the material's final properties. To achieve proper adhesion, the

materials must be prepared; this can be done in different ways. For example, mechanical roughening or chemical treatment. In a fiber-reinforced epoxy resin system, chemical treatment is usually applied. The chemical treatment ensures proper development of the interface region between the composite material's components. The interface region, located between the matrix material and the reinforcing fiber, exhibits the combined physical properties of both materials. Furthermore, the chemical pretreatment of the fibers facilitates improved penetration of the matrix material into the reinforcement.

Sandwich panels are composite structures consisting of a lightweight core material and thin face sheets on either side.

The top and bottom sheets are typically composed of polymeric composites reinforced with glass or carbon fibers, providing the panel with high strength and rigidity.

The core is typically made from low-density materials, most commonly polyurethane or polymer foam.

2. Material selection

The final structure is composed of the following layers: an expanded polystyrene (EPS) core material; a glass fiber composite; a stiffer foam layer (PVC); and outermost load-bearing layers consisting of carbon fiber with different weaves, fiber directions, and fiber counts.

2.1. Comparison of carbon fiber and glass fiber

In fiber-reinforced polymers, glass fiber and carbon fiber are the two primary reinforcement materials. Glass fiber is less expensive and offers greater flexibility in composite systems; however, it is denser and exhibits inferior mechanical properties compared to carbon fiber. Additionally, glass fiber reinforcement typically results in a weaker interface zone between the matrix material and the reinforcing fibers than carbon fiber. In addition to cost-effectiveness, glass fiber's higher hardness makes it more suitable for protecting underlying layers, thus reducing the risk of delamination. In contrast, carbon fiber offers superior mechanical properties and is primarily used as a load-bearing layer [1, 2, 3].

2.2. Weave types

The tests utilized twill and plain weave fabrics, as well as biaxial and unidirectional fiber materials. Twill and plain weave fabrics exhibit lower load-bearing capabilities than unidirectional or biaxial weaves because a significant portion of the fibers are not aligned with the applied force. However, the weakening of the material is less detrimental than if a load perpendicular to the fiber were to be applied [4, 5].

During material selection, consideration was also given to the fact that composite materials made with twill-woven fabrics have higher bending strength than those made with plain-woven fabrics, but lower tensile strength [6].

2.3. Core material

In the tests, a medium density expanded polystyrene (EPS 100) foam was used as the core material. This foam has a density of 20 kg/m³ and a compressive strength of 30 kPa at 2% compression. The use of EPS 100 maintained low specimen weight and facilitated comparison among the different configurations.

2.4. Hard foam material (PVC)

The selection of the hard foam material involved comparing the physical properties of three materials: XPS, PVC, and Corecell. Among these, PVC foam demonstrated the most favourable properties.

Among the various PVC foam densities, 80 kg/m³ was selected. Although higher-density PVC foams generally exhibit superior physical properties, the 80 kg/m³ foam offers an optimal balance between weight and mechanical performance [7, 8]. The specific physical properties of PVC foam increase more rapidly than density up to 80 kg/m³, but this trend reverses at higher values. Additionally, lower-density PVC foams typically demonstrate higher relative fatigue strength when normalized by static strength [9].

2.5. Chosen Materials

- X-C-305 g/m²-1270 mm (C305B)
- 200 GSM T CARBON FABRIC (C200T)
- SIGRATEx® C U290-PL1/1 (C290UD)
- E-glass fabric UNIGLASS 80P (U80P)
- E-glass fabric UNIGLASS 163P (U163P)
- E-glass fabric UNIGLASS 165T (U165P)
- E-glass fabric UNIGLASS 220P (U220P)
- EPS 100
- PVC Foam 80kg/m³ (PVC3/PVC5)
- SR 1280 SD/ SD 4773 Epoxy Resin

3. Manufacturing process

The manufacturing process began with cutting the EPS 100 core material to size using a hot-wire cutting machine. The core dimensions were 600 × 100 × 50 mm, with a tolerance of ±1–5 mm for length and width, and negligible initial deviation in thickness.

Subsequently, the PVC foam was cut to match the exact dimensions of the EPS core for each test specimen. In the following step, the fabric materials were cut to size.

3.1. Layering order

The bottom layer consists of fiberglass, which provides maximum protection for the core. The PVC foam is located directly above the fiberglass layer. Various types of carbon fabrics are placed on top of the hard foam layer, functioning as reinforcement and load-bearing layers.

Six test specimen variations with different layering configurations were prepared for the study. Each variation included two test samples.

All test specimens utilized identical outermost layers and the same matrix material. On one side,

the outer layer consists of the 305 g/m² biaxial carbon fabric (C305B), while the opposite side features the 290 g/m² unidirectional fabric (C290UD). To ensure continuity, the fabrics were overlapped on the side of the test specimens. The samples were all made with the same 3 mm-thick PVC hard foam, except one, which had a 5mm-thick foam layer. A separate designation (PVC5) was used to distinguish its different composition. 165g/m² plain woven glass fabric (U165P) was used in cases where the test specimens' names do not include the glass fabric classification, e.g., U80P, U220P.

Table 1. Layering of different types

	Variants					
	C200T	U80P	U220P	U165P	U163T	PVC5
1	C305B	C305B	C305B	C305B	C305B	C305B
2	C200T	PVC3	PVC3	PVC3	PVC3	PVC5
3	PVC3	U80P	U220P	U165P	U163T	U165P
4	U165P	EPS	EPS	EPS	EPS	EPS
5	EPS	U80P	U220P	U165P	U163T	U165P
6	U165P	PVC3	PVC3	PVC3	PVC3	PVC5
7	PVC3	C290U	C290U	C290U	C290U	C290U
8	C200T					
9	C290U					



Figure 1. Intact cross section

3.2. Lamination process and methodology

The matrix material was applied using hand-lay-up method, with reinforcement layers positioned concurrently. Following this process, the test specimens underwent vacuum bagging. This process is limited by inconsistent resin distribution and variable reproducibility. The vacuum pressure was set to 20-25 kPa, which results in approximately 1-2% compressive deformation in EPS 100 foams. This level is below the lower limit of plastic deformation for EPS ma-

terial of this density, which is ~4%. [10, 11].

4. Test methodology

The mechanical tests were performed using a Hegewald & Peschke - EU-40 Inspekt Retrofit – 400 kN machine. Mechanical characterization was performed by using three-point bending tests. Supports were positioned symmetrically, 250 mm from the centre point on each side. The support cylinders had a diameter of 50 mm, while the pressure cylinder had a diameter of 30 mm. Testing was conducted at a constant displacement rate of 10 mm/min.

4.1. Test data and results

The test data for the samples are shown in Table 2.

Table 2. Type properties

Type	Mass [g]	Max Force [N]	Mass dev. [%]	Force dev. [%]
C200T	446	3229.6	4.35	17.92
C200T	427	2698.4		
U80P	303	1505	3.25	7.97
U80P	313	1389.6		
U165P	344	1667.2	4.82	9.24
U165P	361	1828.8		
U160T	342	1456.8	na	na
U220P	344	1352	5.37	3.84
U220P	363	1301		
PVC5	384	2054	8.72	15.12
PVC5	419	2390		

The results support the initial hypothesis that the addition of carbon-fabric reinforcements results in a significant increase in load-bearing capacity compared to the base test specimen (U165P), in both relative and specific strength. Overall, the measured values aligned with preliminary expectations. The load values of the samples were consistent with predicted behaviour and material properties. One exception was observed: sample U220 exhibited results that differed significantly from the expected values. This deviation is likely attributable to the hand-lay-up application of the matrix material. The force-displacement diagram revealed results that deviated from initial expectations. The test specimens failed after 4 mm of displacement, indicating lower stiffness than anticipated. The observed failure mode likely resulted from a combination of bending and shear stresses.

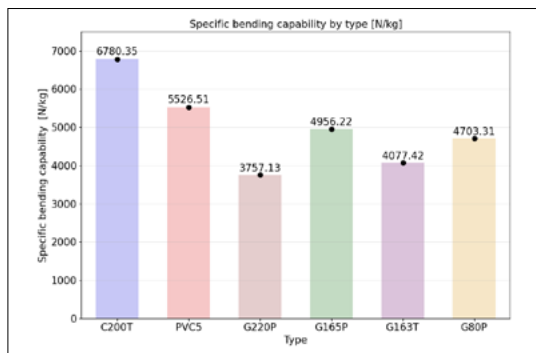


Fig. 2. Specific Bending Force Capability Diagram

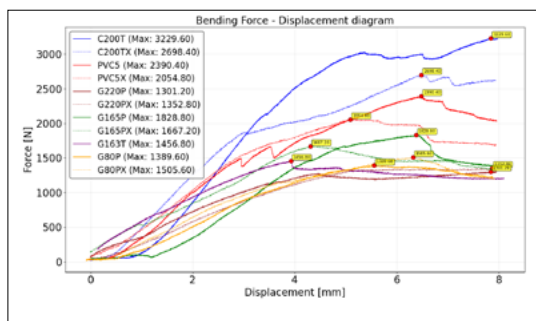


Fig. 3. Bending force-displacement graph



Fig. 4. Surface damage morphology



Fig. 5. Core failure morphology

4. Conclusion

4. Conclusion

In accordance with the hypothesis, specimens reinforced with additional carbon fabric (C200T) demonstrated the highest relative and specific strength.

The C200T variant exhibited a 23.83% increase in mass and a 69.57% increase in maximum bending load capacity compared to the base composition U165P.

The PVC5 variation resulted in a 13.9% increase in mass and a 27.17% increase in maximum bending load capacity relative to the base composition U165P.

Based on the results obtained, the PVC5 material composition may be suitable as a replacement for the C200T composition, under specific conditions, with potential cost advantages. A direct comparison between the two specimen types indicates that C200T has an 8.72% higher weight and a 33.39% greater maximum bending load capacity than PVC5.

No significant differences were observed among the glass fiber variations. Their influence on the sandwich system will be further assessed through impact testing.

The unexpectedly low results observed for the U220P variation require confirmation through larger sample sizes and repeated testing.

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Material Testing of the Mixing Tank of Equipment Operating on the Principle of Thermal Drying for Use on Sea-Ship

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Abstract

This article describes waste treatment options and the operating principle of thermal drying, a process in which moisture is removed using thermal energy. The equipment has a modular design and utilizes a pressure-retaining steel tank, whose welded joints are critical to safety. The authors examined various welding processes (submerged arc welding and TIG welding) and verified the quality of the welds using mechanical and non-destructive tests. The results showed that all tested joints met, and in many cases exceeded, the specified strength and quality requirements. Radiographic testing confirmed defect-free welds, so the chosen technology can be considered reliable and safe.

Keywords: welded structure; pressure vessel; welding technology; safety, reliability; quality control, material testing.

1. Waste management options

The waste hierarchy system helps to establish a priority order for waste management activities, based on which prevention is the best option, with some exceptions. However, if this is not possible in certain circumstances, recycling should be applied to as much waste as possible, and incineration or landfilling should be used as a last resort [1].

Landfilling is the most harmful solution for the environment and our health, but it is also the cheapest [2]. Utilization therefore means the use of waste in a way that results in the exchange of raw materials and resources. There are two main types of utilization, material recovery and energy recovery. The utilization, recycling and regeneration of components should be considered, among other things, as utilization in its material. The given device is a wet waste drying device.

This article presents the material testing of one element (mixing tank) of such a waste treatment device.

2. Principle of thermal drying

Drying is basically the phenomenon of removing liquid from solids by evaporation. Mechanical methods of separating liquid and solids are not considered drying. The principles and phenomena of drying processes are generally independent of the type of energy used. A significant portion of the energy consumption during drying is for the evaporation of liquid water (2258 kJ/kg at 101.3 kPa). [3].

The dewatering option is called thermal drying, where an external additional energy source allows the waste to be heated [4].

The drying process is generally used to evaporate water from residual solids in wastewater. In direct heat dryers, hot air/gas flows through a vessel and comes into direct contact with wet solid particles. The contact between the hot air and the cold wet concentrate allows the transfer of thermal energy, which causes the temperature of the wet material to increase and the water to evaporate. The hot air/gas can be produced by al-

most any heat source but is most commonly produced by a gas or oil-fired furnace [5, 6, 7].

The dried material is separated from the hot exhaust gas, screened and processed, or returned to the dryer or to storage silos. The exhaust air/gas is cooled and a portion is returned to the dryer. The remaining air/gas is treated in air pollution control equipment and then discharged to the atmosphere. The recirculation of the dryer exhaust gas serves three important functions. First, it increases the overall thermal efficiency of the drying system, second, it minimizes the volume of exhaust gas, which requires air pollution control, and third, it provides a safety role by limiting the oxygen concentration in the system, which reduces the risk of explosions [5, 8, 9, 10].

The heat supply is usually a fuel-burning furnace, but unlike the rotary drum system, the system exchanges its heat with a thermal fluid, hot water, or flue gas to air heat exchanger, rather than the furnace being directly immersed in the drying cabinet [7].

3. Equipment designed in the modular system

The theoretical structure of the completed device is shown in Fig. 1.

The lower unit, called Frame I, is the same as an extended height 20' HC container (L:6060 × W:2440 × H:2900mm), while the upper unit, Frame II, is the same as a normal height 20'DC (L:6060 × W:2440 × H:2600mm) sea container. The units can be lifted using corner elements designed and manufactured to the same design as the factory.

The completed equipment is shown in Fig. 2. The tested tank is shown by the arrow.

4. Tank presentation and purpose of material tests

4.1. Inspection and technological evaluation of welded joints of pressure equipment

The design and execution of welded joints in pressure equipment is one of the most critical areas of industrial production, as the operational safety, service life and reliability of the structure depend crucially on the quality of the welds. In the case of the welds CW1, CW12, CW9, L5 and P2, which were the subject of the investigation, several factors arose that justified the careful choice of welding technology and the complex inspection

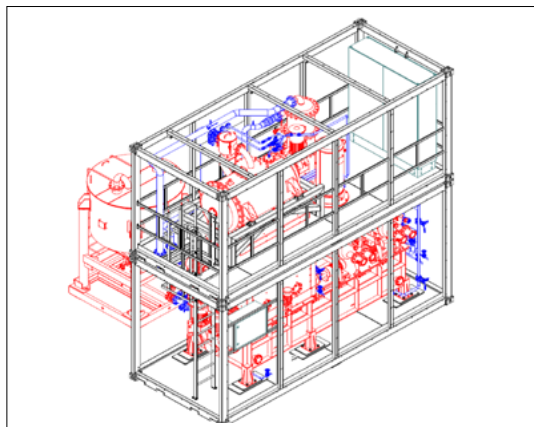


Fig. 1. Schematic diagram of the frame structure.

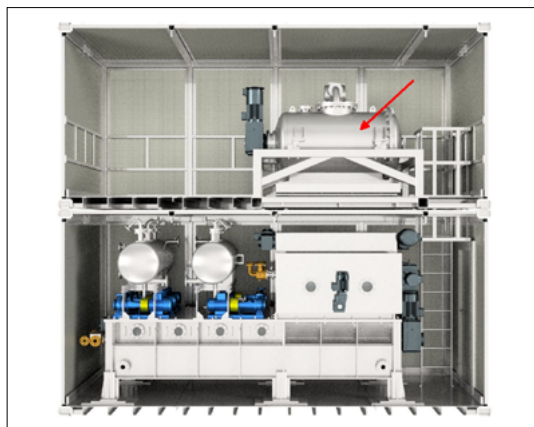


Fig. 2. The assembled drying equipment.

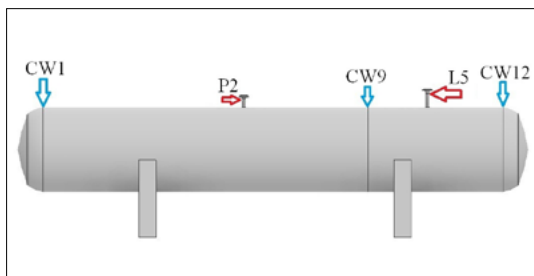


Fig. 3. Weld map on 3D model of the tested tank.

of the joints. The weld map is shown in Fig. 3. .

The tested tank body was made of P355GH pressure steel, while the connecting stubs were made of S355J2 structural steel. The most important design issue was the selection of the appropriate welding process. In the case of CW1, CW12 and

CW9 joints with large material thickness and exposed to significant stress, submerged arc welding (SAW) proved to be optimal, as it ensures a high penetration depth, high productivity and good weld cleanliness. However, when applying the technology, special attention had to be paid to the accuracy of the fit, the selection of the parameters of the electrode wire and the covering powder, and the minimization of residual stresses and deformations caused by heat input.

In the case of joints marked L5 and P2, with smaller material thicknesses and higher aesthetic and quality requirements, the tungsten electrode argon-shielded arc welding (TIG/TIG) process proved to be appropriate. The advantages of the process are precise seam formation and excellent joint quality with low heat input. Despite its disadvantages – lower welding speed and high operator skill requirements – the quality requirements justified the choice of TIG technology.

4.2. Requirements for joints

The following main requirements were applied in the design of the welded joints:

- Strength requirements: The mechanical properties of the joints (tensile strength, yield strength) had to meet the parameters of the base material, in accordance with the relevant standards.
- Material quality requirements: When welding S355J2 and P355GH materials together, special attention had to be paid to the structural stability of the heat-affected zone, grain refinement, and the prevention of crack formation.
- Handling varying material thicknesses: The differing wall thicknesses required appropriate heat input and weld geometry design to minimize residual stresses and distortion. Each of these factors directly influenced the determination of the process parameters as well as the selection of the necessary inspection and testing procedures.

4.3. Test methods and their justification

To verify technological compliance, the joints underwent mechanical and non-destructive testing.

- Mechanical tests: tensile, flexural and Charpy impact tests to evaluate strength and toughness.
- Hardness measurement: Vickers method to determine the mechanical properties of the heat affected zone (HAZ).
- Non-destructive testing: X-ray examination to detect internal seam defects.

5. Material tests

During the tests, all measurements were repeated seven times according to MSZ EN ISO 9015-1:2011. When evaluating the data sets obtained in this way, the two extreme values were omitted, so the average of the middle five measurement results served as the basis for the evaluation. This method minimizes the effect of measurement errors and random deviations, thus allowing us to obtain a more accurate picture of the actual mechanical properties of the tested welds. The primary goal of the measurements was to check the quality of the welds. To this end, the tests were performed on those welds that can be considered critical for the structure. A total of five welds – CW1, CW12, CW9, P2 and L5 – were evaluated. The basis for the selection was that these welds are located in the parts of the designed equipment that are exposed to the greatest stress, so their quality has a direct impact on operational safety and long-term reliability. Welds CW1, CW12 and CW9 were made using the submerged arc welding (SAW) process, with a material thickness of 30 mm, on P355GH base material. These welds are located in the thick, heavily loaded parts of the pressure vessel, therefore it was of utmost importance to check their quality in detail. The other two tested welds, L5 and P2, were made using S355J2 steel, with a material thickness of 15 mm. These welds are also located in critical locations, so their inclusion in the test was essential.

5.1. Vickers hardness measurement

Fig. 4 shows the position of the measured points, which shows the results of the measurements, the locations of the measurement points and the hardness values associated with them. These data graphically illustrate the hardness distribution of the welds in different areas, thus enabling a more accurate understanding of the spatial changes in the material properties.

The equipment used was a Digital Vickers Hardness Tester 8~2900HV – In size measuring machine. The measurement was carried out in an environment of 20 °C and 65% humidity, MSZ EN ISO 6507-1 or MSZ EN ISO 9015-1 standards, which ensured that the environmental conditions were optimal for accurate and reliable hardness testing. Among the welds involved in the test, welds P2 and L5 were made using the TIG process, while the other welds were made using the submerged arc welding process.

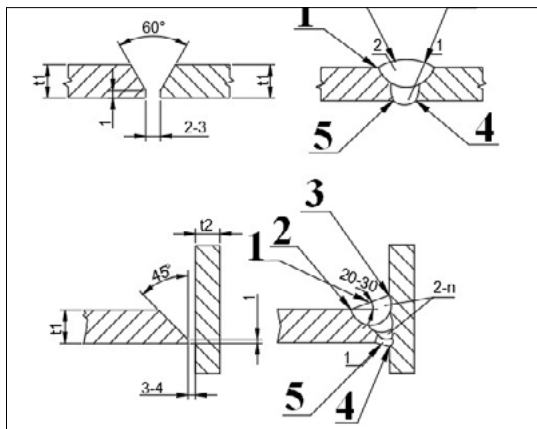


Fig. 4. General location of hardness measurement points.

Fig. 5 shows the trend of hardness measurements, which clearly illustrates the development of hardness values measured on different welds. It can be clearly observed from the graph that the individual welds differ to a certain extent, which can be attributed to several factors. These include, on the one hand, the type of welding process used (submerged arc or TIG), on the other hand, the different material thicknesses and the resulting heat input, which directly affect the mechanical properties of the weld and the heat affected zone. Comparing the measurement points allows us to determine which welds have a greater standard deviation or deviation from the average value.

The results of the hardness measurements exceeded the expected value of 150 HV for all tested welds.

This shows that the applied welding process is suitable for the task. The higher variation in the P2 and CW1 welds is probably due to the secondary alloying elements introduced during weld formation, but this does not affect the quality. The other welds showed a more balanced hardness distribution, which further strengthens the reliability of the technology.

5.2. Tensile test

During the tensile test (MSZ EN ISO 6892-1:2020), a welded specimen made of steel of material grades P355GH and S355J2 (Fig. 6) was tested.

The test was carried out at room temperature, usually between 20-23°C. During the test, the maximum tensile strength (R_m), which indicates the force that the specimen could withstand until it broke, was recorded. In addition, the yield strength (R_{eH} vagy R_{eL}), and the contraction

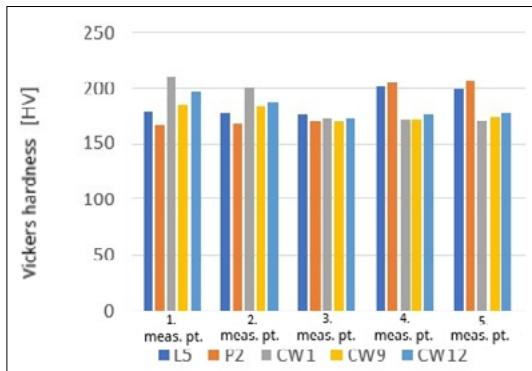


Fig. 5. Hardness measurement results.



Fig. 6. The tensile test specimen.

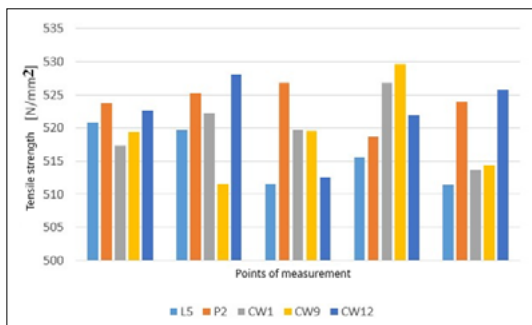


Fig. 7. Tensile strength measurement results.

strength (R_u), which is the point at which the material underwent permanent deformation, were measured, as well as the elongation after fracture (A %), which indicates the degree of deformation of the material before breaking.

Machine used for measurement: aServo-Plus automatic controlled, universal four-column shearing and crushing machine.

5.2.1. Tensile strength

Fig. 7 clearly presents the results of the tensile strength tests in graphical form.

According to the tensile test results, the average tensile strength of the five tested welds exceeds the 500 N/mm² requirement in all cases. The aver-

age tensile strength of the L5 weld is 515.8 N/mm², while the P2 weld shows 523.7 N/mm². The CW1 weld has an average value of 519.9 N/mm², while the CW9 and CW12 welds have an average value of 518.9 N/mm² and 522.2 N/mm², respectively.

The standard deviation for weld L5 is 4.41 N/mm², while for weld P2 it is 3.09 N/mm², which in both cases is 1% standard deviation. Welds CW1, CW9 and CW12 also have a standard deviation of 1%, which is also a good indicator. So not only do all welds meet the minimum requirements, but the standard deviation analysis also shows that the results are consistent and reliable.

5.2.2. Upper yield point

Fig. 8 clearly presents the upper yield strength measurement results in graphical form.

Based on the yield strength test results, the average values of the five tested welds all exceed the 490 N/mm² requirement. In the case of weld L5, the average yield strength is 506.1 N/mm², while weld P2 shows 502.4 N/mm². For weld CW1, the average is 505.4 N/mm², which also meets the requirements, while weld CW9 shows 500.4 N/mm², and weld CW12 shows 499.3 N/mm², which is also favorable.

The standard deviation for the L5 weld is 3.29 N/mm², for the P2 weld 5.55 N/mm², and for the CW1 weld 5.29 N/mm², resulting in a standard deviation of 1% in each case. The standard deviation for the CW9 and CW12 welds is also 1%, with values of 3.53 N/mm² and 5.11 N/mm².

Overall, it can be stated that the measured values exceed the minimum expectations in all cases, and the low standard deviations reflect the consistency and reliability of the test procedure.

5.2.3. Lower yield point

Fig. 9 early presents the lower yield strength measurement results in graphical form.

Based on the lower yield strength values measured during the test, the average lower yield strength values of the five tested welds also meet the expectation of 480 N/mm². The average lower yield strength of the L5 weld is 484.9 N/mm², while the P2 weld shows 485.5 N/mm². In the case of the CW1 weld, the average value is 485.7 N/mm², for the CW9 weld it is 487.5 N/mm², while the CW12 weld shows 489.8 N/mm². The standard deviation of the measurements is low, indicating the stability and reliability of the measurements. For the L5 weld, the standard deviation is 4.08 N/mm², for the P2 weld it is 2.01 N/mm², while for the CW1 weld it is 4.80 N/mm². The standard deviations of

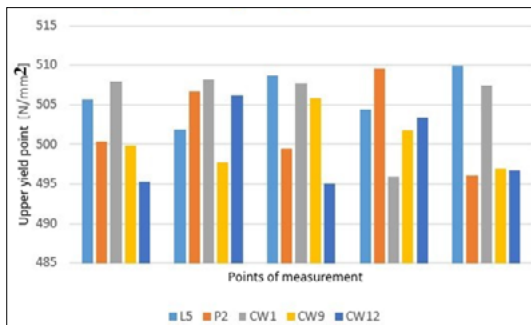


Fig. 8. Upper yield strength measurement result.

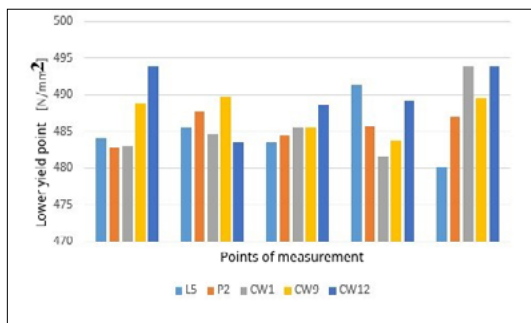


Fig. 9. Lower yield strength measurement results.

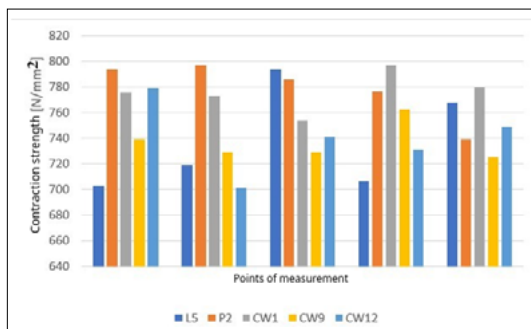


Fig. 10. Contraction strength measurement results.

the CW9 and CW12 welds are 2.71 N/mm² and 4.39 N/mm², which is also favorable. Overall, it can be stated that the lower yield strength values in all cases exceed the minimum requirements.

5.2.4. Contraction strength

Fig. 10 clearly presents the results of the contraction strength measurements in graphical form.

The average contraction strength of the CW1 seam is 775.78 N/mm², the CW9 seam is 736.80 N/mm², and the CW12 seam is 740.08 N/mm².

The standard deviation for the L5 weld is

40.48 N/mm², while for the P2 weld it is 23.39 N/mm², which shows favorable stability. The standard deviation for the CW1 weld is 15.53 N/mm², for the CW9 weld it is 15.14 N/mm², while the standard deviation for the CW12 weld is 28.21 N/mm², which is also acceptable. Overall, it can be stated that the contraction strength values in all cases significantly exceed the minimum expectations, and the low standard deviation values reflect the consistency and reliability of the test procedure. Based on the results, the mechanical performance of the welds can be considered excellent.

5.3. Charpy impact test

The purpose of the impact test (MSZ EN ISO 148-1:2022) was to measure the fracture toughness of welds and the energy absorption capacity of the material, on samples of different material thicknesses, which were tested under standard conditions using a Hoytom Charpy pendulum impact machine. The test specimens were prepared in a predetermined manner, and the impact test was carried out in accordance with the standards. The results obtained are shown in Fig. 11.

The standard deviation of the measurements varies for each weld, with the largest standard deviation being 27.4 J for the L5 weld and the smallest being 10.8 J for the CW12 weld. The percentage standard deviation ranges between 5% and 12%, which is relatively low, indicating that the measurements are stable and provide consistent results.

5.4. Radiographic examination

The purpose of the test was to check the quality of the welded seams, with particular attention to the detection of internal defects such as inclusions and cracks. One such test result is shown in Fig. 12. The X-ray image shown in Fig. 12. clearly demonstrates that all the examined welds are in perfect condition. No defects such as cracks, air pockets or deformations are found on the image. The fit of the welds and the quality of the welds fully meet the requirements. These results can be observed not only in the case of a single weld, but also in all the welds examined, which are all flawless. The lack of defects that arise during the X-ray examination is extremely important, since possible defects such as structural cracks or material deficiencies could cause significant problems in the stability and reliability of the structure in the long term.

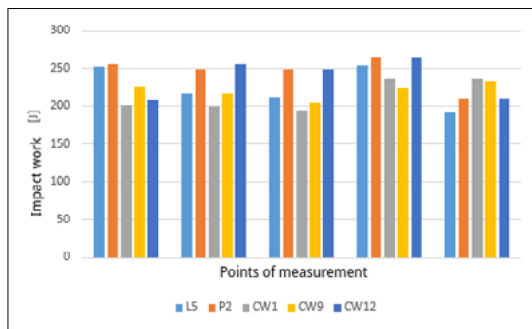


Fig. 11. Measurement results of the Charpy impact test.



Fig. 12. Taking the X-ray

6. Summarizing

The drying process is usually used to evaporate water from the residual solids in wastewater. In addition to the dryer, the drying system usually consists of material handling and storage equipment, heat generation and heat transfer equipment, air handling and distribution equipment, emission reduction equipment and auxiliary systems. Our equipment is a drying equipment composed of several parts, which has a so-called pre-drying part and a so-called main drying part. The task of the former is to preheat and pre-dry the material to be treated by applying it in a thin layer (~ 80% moisture content). The task of the latter is to dry the material to the desired ~ 20% moisture content.

6.1. Technology concept and evaluation

The aim of the tests was to verify that both submerged arc welding (SAW) and tungsten inert gas welding (TIG) meet the structural requirements

and material quality requirements.

When developing the technological concept, the first step was to select the welding process appropriate to the wall thickness for the different material qualities (P355GH and S355J2). Optimizing the heat input was crucial for preserving the HAZ properties and reducing residual stresses. The appropriate selection of the weld geometry and the covering materials (shielding gas, covering powder) further improved the strength and crack resistance of the joint.

The results of the laboratory tests confirmed that the selected technological parameters were suitable from both a production and operational perspective. The mechanical properties of the joints reached and, in several cases, exceeded the required values, and the destructive thread tests confirmed a defect-free seam structure.

7. Conclusions

Based on the tests, it can be concluded that submerged arc welding (SAW) ensured optimal quality for thick-walled elements, while the AWI/TIG process ensured optimal quality for thinner, precision joints. The application of the complex testing methodology enabled the full validation of the technology, which is a fundamental condition for the safe operation of pressure equipment.

Acknowledgement

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Materials Science Issues in Explosive Cladding

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Abstract

Cladding is a widespread technology with several well-known process types. During the explosive cladding process, the specification requires knowing the parameters of the selected baseplate, cladding plate and the expected joint. For the technological parameter specification, it is necessary to understand the material structure modification and its causes under the explosive cladding process. The plastic deformation even causes macro- and microstructural changes in the case of static but more in the case of dynamic, high-rate deformation. Microstructural changes are the lattice transformation and the cohesion joint established between the metallic atoms, and macrostructural changes are the grain deformation. The explosive cladding process affected structural results, depending on the used material crystal structure, chemical composition and the applied explosive material properties.

Keywords: *cladding, work hardening, explosion welding.*

1. Introduction

Explosive cladding is a welding technology that uses the shock waves from an explosion to bond a cladding plate or flying plate to a base plate [1]. Unlike roll-forming, explosive forming does not require large-scale rolling mills or other manufacturing equipment. For this reason, it offers a wide range of options in terms of different material grades and sheet thicknesses, but of course there are limits to the applicability of this technology. The literature generally focuses on the parameters used in material pairings and the resulting mechanical properties. Numerous case studies are available, typically concerning material combinations such as steel-copper, steel-aluminium, etc. It is of paramount importance to understand the range of explosives used in this technology and their properties [2]. One limitation of the technology is the detonation speed, which cannot exceed the speed of sound in the material [1, 3]. The shock wave from an explosion caused by excessive velocity can cause damage. Based on the information available in the literature, it is often difficult to reproduce experiments due to the wide variety of explosives and their differing characteristics. Explosives are generally classified based on their chemical composition, density, and

detonation velocity. When designing explosive blasting technology, determining the amount of explosive to be used is of paramount importance. Since industrial explosives are designed for safe handling, this means that they can only be detonated using a high-velocity detonator. However, the explosion will not propagate across the entire surface of the workpiece if the amount of explosive is too small. Therefore, this limitation must also be taken into account during the sizing process [4]. Plating technology is a coating process: a layer of a different material is applied to the surface of the substrate. During the bonding process, a direct atomic bond – a cohesive bond – is formed between the metals. Several methods for achieving this are known and used in industrial practice. Material combinations created through cladding can have numerous advantageous properties, making their use is beneficial. Such properties may include increased corrosion resistance and wear resistance of the surface, as well as an increase in the strength of a low-strength base plate due to the coating layer.

In this study, we analyse the properties of the plattír layer – formed by creating a cohesive bond on the surface of metal sheets – and the base material.

1.1. Roll cladding

Roll cladding is one of the most widely used cladding processes. Depending on the temperature used in the process, it can be classified as cold cladding or hot cladding. The advantages of cold cladding include excellent repeatability, precision, scalability, and a wide range of material pairing options in mass production. Typical material combinations: copper-steel, aluminium-steel, copper-aluminium. In the hot-cladding process, the material pairs are assembled, heated to the rolling temperature in a vacuum, and then rolled together (Fig. 1).

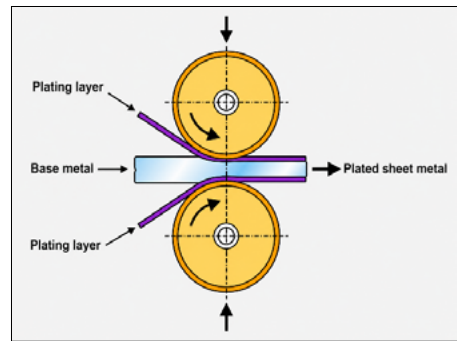


Fig. 1. Roll cladding.

1.2. Cladding with build-up welding

Build-up welding can be performed using various bulk welding processes. Due to the nature of bulk welding, the base plate and the layer welded to it become intermixed during the process. To achieve the desired coating properties, multiple layers are applied. Fig. 2 illustrates the build-up weld using tungsten-electrode gas-shielded arc welding.

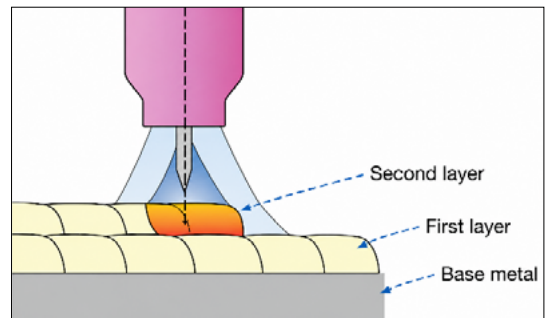


Fig. 2. Plating by Build-Up Welding.

1.3. Explosive cladding

Explosive cladding can also be considered a form of welding, but in this case, we are referring to a pressure-welding process, since joint formation is not primarily driven by heat. The shock wave generated by the explosion bonds the cover plate to the base plate at high speed Fig. 3 illustrates the parallel arrangement of explosive cladding. The gap between the base plate and the cladding plate allows the cladding plate to snap into place on the base plate. The velocity of the explosive used in explosive cladding is 3,000–5,000 m/s, since higher velocities can damage the materials. The explosion is initiated using a detonator.

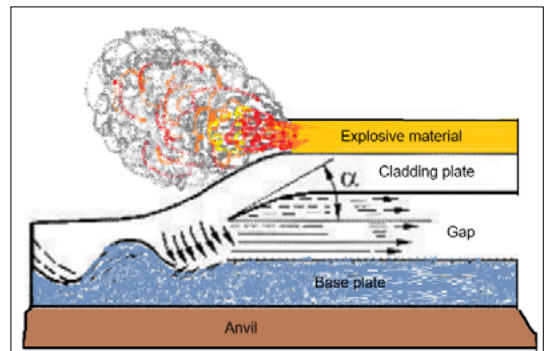


Fig. 3. Explosive cladding (illustration).

2. Analysis of explosive cladding

2.1. Explosive cladding process

During explosive cladding, the cladding plate strikes the base plate at high speed. The shock wave from the explosion shapes the cladding plate onto the surface of the base plate, creating a characteristic wavy bond geometry (Fig. 4), where the arrow indicates the direction of the explosion. The collision causes plastic deformation and, consequently, growth. It can be observed that, although the degree of deformation is small, a significant increase in hardness occurs within the bond. The deformation rate is much higher than that of conventional plastic forming technologies.

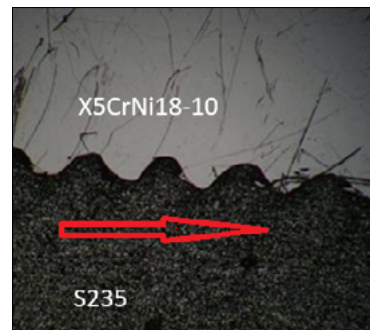


Fig. 4. Cross section of the explosive joint.

Heat is generated during explosive cladding, but this does not cause the metals to melt. The gas pressure resulting from the explosion creates a stress above the yield point, thereby joining the metals through plastic deformation. Since the explosive pressure acts directly on the cover plate, it is advisable to use a cover plate with a low yield strength and good ductility. If the explosive has a high detonation velocity, a damping plate is placed between the explosive and the casing. Determining the gap between the cover plate and the base plate is an important step in the engineering design process. There are numerous research findings available for determining the gap thickness. According to some re-searchers, the gap size is half the thickness of the cover plate, while other researchers' findings indicate that it is 1.0–1.5 times that thickness [5–8]. In any case, it must be noted that the work hardening that occurs in the joint depends significantly on this parameter [5, 6].

To determine the thickness (l_e) of the explosive, one must know the density (ρ_b), and thickness (l_b), of the casing, as well as the density (ρ_e) of the explosive.

The basis for sizing is that the specific mass ratio of the explosive (M_e) to the casing material (M_b) should be $0.5 < Q < 1.5$ [7]. The mass ratio can be calculated from the densities and volumes of the explosive and the casing (1).

$$Q = \frac{M_b}{M_e} \quad (1)$$

where M_b is the mass of the cladding plate, and M_e is the mass of the explosive.

Since the explosive occupies the same surface area as the cladding plate, only its thickness needs to be determined using Equation (2) below.

$$l_e = \frac{\rho_b \cdot l_b}{\rho_e} \quad (2)$$

Another necessary parameter is the gap size, which in practice is selected based on the thickness of the covering sheet. No precise formula for calculating this can be found in the literature.

2.2. The effects of explosive cladding on material structure

The gap used in parallel arrangements—even though no precise correlation has been established for it—is of paramount importance in terms of the properties of the resulting bond. In practice, numerous experimental results have

been published for gap sizes ranging from half the thickness of the cladding plate to one and a half times that thickness [1, 3, 5, 8].

The gap size is particularly important because, during an explosion, this is the distance the cover plate travels until impact. Measurement results show that the increase in hardness within the joint increases as the gap distance increases [5, 6]. By increasing the gap distance to 10 mm, the hardness of the X5CrNi18-10 steel base plate reaches 348 HV30 in the joint, whereas its original hardness was 200 HV30 [5, 6]. The explosive used (PERMON 10T) had a velocity of 3200 m/s and a density of 850 kg/m³ [5].

It is well known that the face-centered cubic crystal structure of austenitic steels can be transformed into a tetragonal structure under the influence of forming, which constitutes an austenite-martensite phase transformation. Martensite exhibits high hardness and magnetic properties. The phenomenon of lattice distortion has also been observed in alloys with a cubic crystal lattice (Cu2Be) on other surfaces [7, 9, 10]. In the case of Hadfield steels, which also have a cubic lattice structure on the surface, the process used in practice to harden the surface of railroad switches is shot peening, a process based on the same principle as cladding. Hadfield steel undergoes a phase transformation during forming, which results in an increase in hardness [6]. The increase in forming hardness is caused, on the one hand, by phase transformation and, on the other hand, by an increase in dislocation density [11, 12, 13, 14]. During plastic deformation, a significant portion of the energy is converted into heat, while the remainder is stored in the metal, primarily in the form of dislocations [9].

Macro- and microstructural changes occur in the cladding joint as a result of high-energy forming [12, 13, 14, 15]. The quality of the joint is primarily determined by the detonation velocity, the impact velocity and angle, the amount of explosive, the thickness of the flying plate, and the initial distance between the plates [8, 9].

During this process, extremely high pressure, high strain rates, and a brief rise in temperature occur at the bonding interface. When the impact pressure exceeds the yield strength of the materials to be joined, intense plastic deformation occurs. One characteristic result of this is the wavy joint interface, which is one of the most important microstructural features of explosive cladding. Near the interface, the grains may elongate, rotate, and fragment, while the degree of defor-

mation gradually decreases with distance from the bonding zone [8, 9].

As a result of significant plastic deformation, the original grain structure may gradually transform into a cellular, subgranular, or lamellar deformation structure. According to studies on the deformation of body-centered cubic metals, boundaries consisting of dislocation cells, dense dislocation walls, micro-loops, and geometrically necessary dislocations form within the grains. As deformation progresses, these internal boundaries separate increasingly large differences in orientation, leading to further subdivision and refinement of the grain structure [10, 13].

One of the most significant changes in the bonding zone is the substantial increase in dislocation density. Atomic scale examination of aluminium subjected to a shock wave have shown that the formation of Shockley-type partial dislocations and the breakdown of full dislocations play a decisive role in dynamic plastic deformation. The rate of dislocation formation is influenced by grain size, shock pressure, and the wave propagation distance [4]. High dislocation density, dislocation interactions, and dislocation wall formation increase resistance to deformation, which can lead to work hardening and a local increase in hardness near the bond interface [10, 13, 14].

Explosive cladding is generally considered a solid-state process; therefore, under appropriate technological parameters, the original phase structure of the base materials is largely preserved. However, in wave crests, vortex regions, and at local points of high-energy impact, short-term melting, material mixing, and rapid solidification may occur. New phases, including intermetallic compounds, may form in these areas, particularly in the case of material pairs that differ significantly from one another, such as aluminium–copper, aluminium–steel, or titanium–steel systems [8, 9]. A thin and discontinuous reaction zone may contribute to the formation of a metallurgical bond, whereas a thick, continuous, and brittle intermetallic layer may reduce the ductility of the bond and promote crack formation. Mechanical properties are determined by a combination of grain refinement, an increase in dislocation density, work hardening, residual stresses, the geometry of the yield stress, and any new phases that may form. In the explosive bonding of TA2 titanium and Q235 steel, the wavy interface resulted in pronounced microstructural and mechanical anisotropy. In the study, quasi-static shear strengths of 380–410 MPa and dynamic

shear strengths of 450–600 MPa were measured; with the highest values obtained when the load was applied at a 90° angle relative to the interface waves. The joint exhibited positive strain sensitivity, and fracture occurred due to the combined presence of ductile and brittle mechanisms [15].

3. Conclusions

Explosive cladding is a high-speed, solid-phase joining technology in which the energy of an explosive propels a cladding plate to strike the base plate at high speed. The jet of material generated during the impact, which occurs at the correct angle, removes the oxide layer and surface contaminants, thereby bringing clean metal surfaces into direct, atomic-scale contact. A suitable theoretical basis for interpreting the deformed zones in explosive cladding joints is provided not only by research directly analysing explosive cladding, but also by findings describing the general microstructural evolution of intensively formed metals.

Overall, the optimal explosive-plated bond is characterised by a refined and highly deformed grain structure, high dislocation density, localised increases in hardness, and the formation of strong cohesive bonds. However, excessive impact energy can lead to extensive melt zones, brittle intermetallic phases, high residual stresses, and localised cracks. The goal of selecting the appropriate technological parameters is therefore to create a bonding zone in which the favourable strength-enhancing effects of deformation and grain refinement prevail, while the formation of continuous brittle phases remains limited [5, 6, 7, 10, 13, 14, 15].

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Three Point Bending of Hybrid-Reinforced Compound Bows Limbs

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Abstract

Glass fibre-reinforced composite limbs for compound bows were developed to improve their mechanical performance through different stacking sequences of glass fibre reinforcements. Three composite bow limb configurations with different lay-up sequences were investigated using three-point bending tests, microscopic analysis, and finite element simulations. The results showed that incorporating woven glass fibre reinforcement into the central region of the laminate significantly increased both the maximum flexural load and the corresponding deflection. The highest bending load-carrying capacity (8,835 N), together with favourable flexural deflection, was achieved by the composite limb reinforced with five layers of unidirectional glass fibre and four layers of glass woven reinforcement. This lay-up also exhibited more favourable damage behaviour than the other stacking sequences examined. The developed composite bow limbs are expected to provide greater resistance to dry-fire loading, thereby improving the operational safety and service life of compound bows.

Keywords: compound bow limb; glass woven, glass fibre reinforcement; layer order, three-point bending.

1. Introduction

Modern compound bows are high-performance mechanical structures that are subjected to significant dynamic loading during operation. Their reliable performance depends on appropriate structural design, the use of advanced materials, and proper operation. Contemporary compound bows are typically manufactured from glass fiber-reinforced composites, which provide an excellent strength-to-weight ratio, high specific stiffness, and superior fatigue resistance, making them well suited to withstand the repeated dynamic loads encountered during service. Despite substantial advances in materials technology and manufacturing processes, structural failures continue to occur in practical applications, often as a consequence of improper use. One of the most critical failure scenarios affecting compound bows is the so-called dry-fire event, which occurs when the bowstring is released without an arrow. Under these conditions, the elastic energy stored

in the limbs cannot be transferred to the arrow. Consequently, the kinetic energy that would normally propel the arrow is redirected back into the bow structure within an extremely short time, generating transient overloads and severe dynamic stresses that considerably exceed those experienced during normal shooting. Such extreme loading conditions may cause permanent damage or even catastrophic failure of the bow limbs despite the application of appropriate safety factors during the design process. Practical observations indicate that damage most frequently initiates near the cam at the tip of the limb, where cracks are formed and subsequently propagate along the fiber reinforcement direction. Crack propagation not only renders the bow unsuitable for further operation but also significantly increases the risk of injury to the user.

Fig. 1 illustrates the structural configuration of a compound bow and the composite limbs investigated in the present study [1].

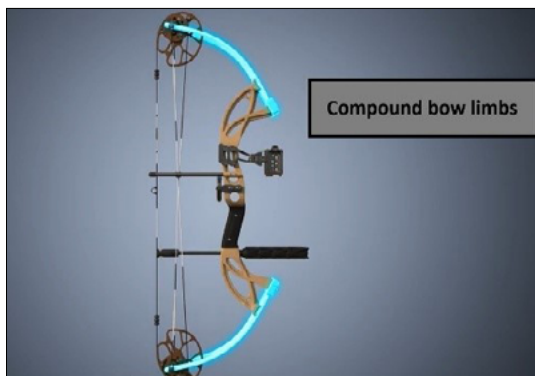


Fig. 1. Structural configuration of a compound bow and the investigated composite bow limbs.

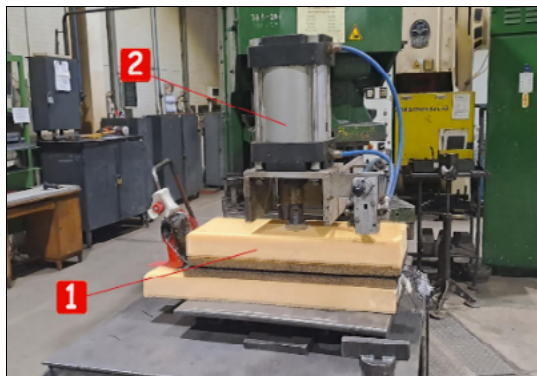


Fig. 2. Pressing tool (1) and hydraulic press (2)

2. Materials and Methods

2.1. Experimental Materials and Testing Procedures

To manufacture the test specimens, a dedicated pressing tool was designed and fabricated. The adhesive curing process of the epoxy matrix-based glass fiber-reinforced composite laminates was carried out under constant pressure and controlled temperature conditions. Following the curing process, machining operations were performed to produce the final test specimens. These procedures ensured excellent bonding quality and high structural integrity of the manufactured composite bow limbs. The pressing tool and the hydraulic press used during specimen fabrication are presented in [Fig. 2](#). [2, 3].

Before mechanical testing, the specimens were subjected to intensive machining to obtain the required geometry. Owing to the high strength of the composite material, machining was particularly demanding. Subsequently, three-point bending tests were performed at the KERMI Testing Laboratory on specimens with different laminate stacking sequences. The three-point bending experiments were conducted under the following test conditions:

- Crosshead speed: 2 mm/min;
- Specimen dimensions: 250×20×10 mm;
- Support span supports positioned at one-third of the specimen length, corresponding to 83 mm from each edge.

Initially, specimens reinforced exclusively with unidirectional glass fibers, representing the configuration commonly employed by major bow manufacturers, were investigated. Thereafter, composite laminates designated P1 and the sub-

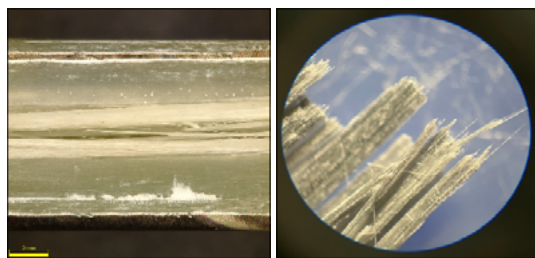


Fig. 3. Microscopic images of the P1 laminate configuration.

sequently developed lay-up configurations were evaluated according to the applied reinforcement architecture. The experimental results demonstrated significantly higher bending performance than typically reported for comparable composite materials. The highest measured maximum bending load reached $F_{\max 1} = 5790$ N, $F_{\max 2} = 5770$ N, while the corresponding maximum deflections were $dL_1 = 10,6$ mm, and $dL_2 = 9,6$ mm. [4]

Microscopic observations ([Fig. 3](#)) revealed that the specimens manufactured exclusively from unidirectional glass fiber laminates (P1 configuration) exhibited fiber fracture. Such damage is considered particularly critical in bow limbs because it may lead to structural failure during service. The experimental results clearly demonstrated that the P1 laminate exhibited insufficient resistance to bending loads. Consequently, the reinforcement architecture was modified by introducing a woven glass fiber fabric into the laminate. The additional woven reinforcement improved the adhesive bond between the four primary structural layers, resulting in the P2 laminate configuration. For all specimens, the epoxy matrix material was identical and consisted of Araldite AW 106. The structural layers constituting the composite bow limbs are shown in [Fig. 4](#).

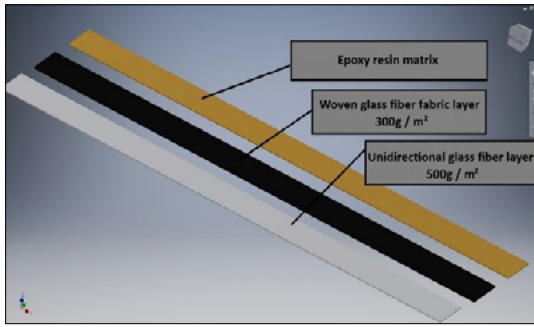
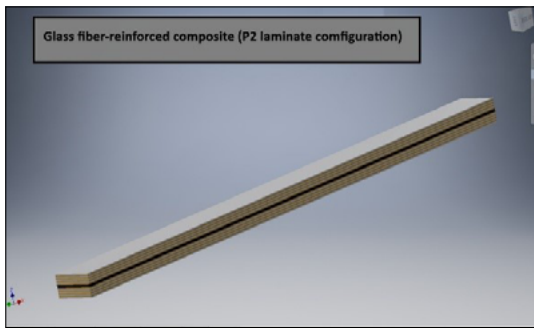


Fig. 4. Principal structural layers of the composite specimens.



5. ábra. A P2-es rétegrendű kompozit próbatest szerkezeti felépítése

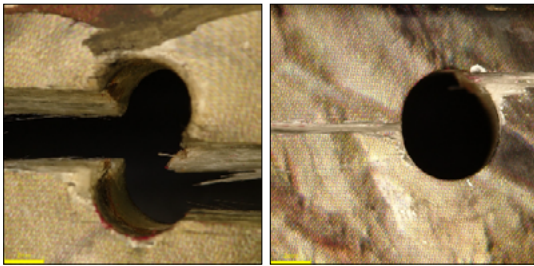


Fig. 6. Fracture surfaces of the P2-reinforced composite specimen in the vicinity of the mounting hole.

To improve flexural performance, composite specimens reinforced with woven glass fiber fabric (P2 configuration) were manufactured and subjected to three-point bending tests. Compared with the P1 configuration, the P2 laminate exhibited significantly improved mechanical performance, including higher maximum bending loads and greater flexural deflections. The structural arrangement of the P2 composite specimen is presented in Fig. 5:

- Eight layers of unidirectional glass fiber sheets (500 g/m²)
- One layer of woven glass fiber fabric (300 g/m²)
- Two-component Araldite AW 106 epoxy resin as the matrix material
- After rapid mixing, the epoxy resin was applied to the reinforcement layers within 15 min at room temperature.
- The adhesive curing process was carried out for 6 h under a pressure of 3 bar at 70 °C to ensure complete crosslinking of the epoxy resin.

Based on our previous finite element simulations of the bow limb using Euler–Bernoulli and Timoshenko beam theories, the expected failure load during a dry-fire event was estimated. A dedicated measuring device was developed to determine the magnitude of the force acting on the bow limb. According to the measurements, the applied load was calculated as:

$$F_{kar} = \frac{F_{húzó}}{R} = \frac{267\text{N}}{0,615} = 434\text{N} \quad (1)$$

Finite element analyses further indicated that the maximum bending stress developed in the bow limb reached 251 MPa, while the corresponding maximum deflection was 48.5 mm under the applied loading conditions [5].

$$\sigma_{max} = \frac{434\text{ N} \times 0,25\text{ m} \times 0,006\text{ m}}{2,592 \times 10^{-9}\text{ m}^4} = 251\text{ MPa} \quad (2)$$

$$\sigma_{max} = \frac{434\text{ N} \times 0,25\text{ m} \times 0,006\text{ m}}{2,592 \times 10^{-9}\text{ m}^4} = 251\text{ MPa} \quad (3)$$

Following the application of Timoshenko beam theory, the natural frequencies of the bow limb were determined. The first bending natural frequency was calculated to be 105 Hz [6].

$$f_1 = \frac{1,875^2}{2\pi \times 0,25^2} \sqrt{\frac{18 \times 10^9 \times 2,592 \times 10^{-9}}{1900 \times 2,16 \times 10^{-4}}} = 105\text{Hz} \quad (4)$$

At the mechanical interface between the bow limb and the cam, the limb is connected through a mounting hole (Fig. 6). This geometric discontinuity results in significant stress concentration. Depending on the notch geometry, the theoretical stress concentration factor ranges from $K_t = 3$ to 3.5 for relatively small radius [7].

Under these conditions, the local bending stress around the hole may increase from the nominal 251 MPa to approximately 850 MPa. The microscopic images presented below illustrate crack initiation and propagation around the mounting hole in the P2 laminate configuration.

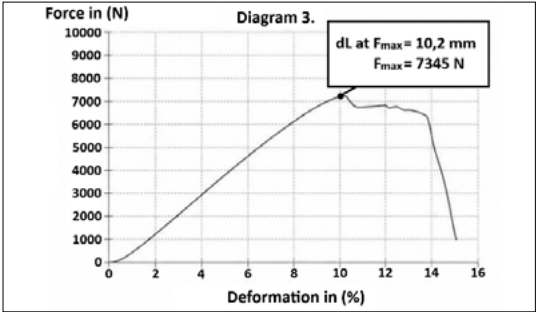


Fig. 7. Three-point bending test results of P2 laminate specimen No. 3

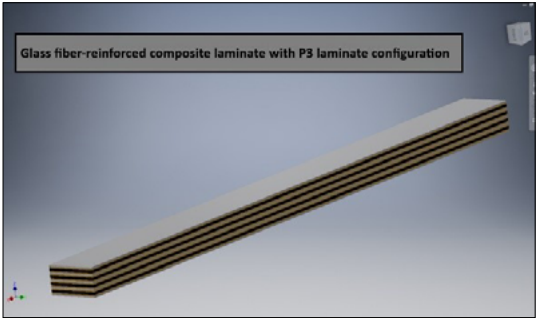


Fig. 8. Structural configuration of the P3 composite laminate

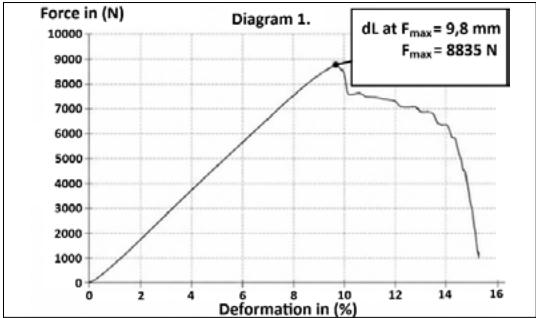


Fig. 9. Structural configuration of the P3 composite laminate.

Table 1. Comparison of the maximum deflections measured for the investigated laminate configurations

Specimen numbers	P1 laminate specimens (dL)	P2 laminate specimens (dL)	P3 laminate specimens (dL)
1.	7.1 mm	14.6 mm	9.8 mm
2.	7.2 mm	12.8 mm	10.6 mm
3.	6.8 mm	11.5 mm	12.3 mm
4.	6.9 mm	11.8 mm	12.2 mm
5.	6.8 mm	12.3 mm	13.3 mm

3. Experimental Results

The composite specimens containing woven glass fiber reinforcement (P2 laminate configuration) exhibited considerably higher bending resistance than specimens reinforced solely with unidirectional glass fibers. Based on these encouraging results, additional laminate configurations were developed to further improve the flexural strength of the composite bow limbs. Fig. 7 presents the results of the three-point bending test performed on specimen No. 3 of the P2 configuration.

Based on the structural modifications (Fig. 8) introduced in the tested laminates, a new configuration designated P3 was developed. The P3 laminate consisted of:

- five layers of unidirectional glass fiber sheets (500 g/m²);
- four layers of woven glass fiber fabric (300 g/m²)
- Araldite AW 106-05 two-component epoxy resin as the matrix material;
- the mixed epoxy resin was applied to the reinforcement layers within 15 min at room temperature;
- curing was performed for 6 h under a pressure of 3 bar at 70 °C, ensuring complete crosslinking of the epoxy matrix.

Fig. 7 shows the results of the three-point bending test on test specimen No. 3, which is reinforced with P2 fibers.

The three-point bending test results obtained for specimen No. 1 of the P3 laminate configuration are presented in Fig. 9.

3.1. Comparison of Laminate Configurations

Table 1 and 2 summarize the maximum bending loads and corresponding maximum deflections obtained during the three-point bending tests for the P1, P2, and P3 laminate configurations. The

Table 2. Comparison of the maximum bending loads measured for the investigated laminate configurations

Specimen numbers	P1 laminate specimens (F _{max})	P2 laminate specimens (F _{max})	P3 laminate specimens (F _{max})
1.	5480 N	7785 N	8835 N
2.	5770 N	7215 N	8625 N
3.	5760 N	7345 N	8505 N
4.	5790 N	6885 N	8455 N
5.	5760 N	7090 N	8460 N

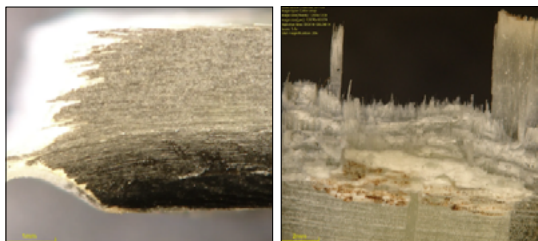


Fig. 10. Microscopic images of the fracture surfaces of the P3 composite specimens.

results clearly demonstrate the influence of the reinforcement architecture on the flexural performance of the composite bow limbs..

Microscopic examination of the fractured P3 composite specimens is presented in Fig. 10. The side-view image (left) reveals a pronounced tensile failure, whereas similar fractures were not observed in the earlier P1 laminate configuration. The front-view image (right) clearly illustrates the woven glass fiber layers integrated between the unidirectional glass fiber laminates, forming the structural basis of the P3 reinforcement architecture.

4. Conclusions

In this study, the reinforcement architecture of epoxy matrix-based glass fiber-reinforced composite bow limbs was systematically optimized. Three laminate configurations were investigated: the conventional unidirectional laminate (P1), a hybrid laminate containing a single woven glass fiber layer in the central region (P2), and a newly developed multilayer hybrid laminate incorporating four woven glass fiber layers (P3). All composite specimens were manufactured using a dedicated pressing tool to ensure consistent curing conditions and high-quality laminate consolidation. The experimental results demonstrated that the conventional P1 configuration, which is representative of the construction employed in commercially available compound bows, exhibited the lowest flexural strength. Introducing a woven glass fiber layer into the laminate (P2) significantly improved the mechanical performance. Based on finite element simulations and experimental observations, the reinforcement architecture was further optimized, resulting in the P3 laminate. The maximum bending load increased from 5790 N for the P1 laminate to 7785 N for the P2 configuration, corresponding to an

improvement of approximately 35%. The highest load-carrying capacity was achieved by the P3 laminate, reaching 8835 N, which represents an increase of approximately 53% compared with the reference P1 configuration. Microscopic observations of the fracture surfaces confirmed that the hybrid laminates containing woven glass fiber reinforcement exhibited a more favorable damage mechanism than specimens reinforced exclusively with unidirectional fibers. The woven reinforcement effectively delayed crack initiation and propagation, resulting in improved damage tolerance and greater structural integrity under severe bending loads. Overall, the developed P3 laminate demonstrated the highest flexural strength, improved damage resistance, and superior structural stability. These findings indicate that incorporating multiple woven glass fiber layers into the composite bow limb substantially enhances resistance to the extreme dynamic loading associated with dry-fire events, thereby improving both the operational safety and the service life of compound bows.

Acknowledgement

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Detection of Deformation-Induced Martensite in 1.4462 Type Duplex Stainless Steel

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Abstract

In our research, we formed test specimens made of 1.4462 type duplex stainless steel sheet to different degrees and then examined whether deformation induced martensite had formed in them. To detect deformation-induced martensite, we performed X-ray diffraction, Feritscope and electron channeling contrast imaging examinations, thus also examining the applicability of the methods. The results of the measurements showed that these methods are not suitable for detecting deformation induced martensite with the tools available to us.

Keywords: *deformation-induced martensite, duplex stainless steel, electron channeling contrast imaging, plastic forming.*

1. Introduction

Strain-induced martensite can be formed by cold plastic working, typically in austenitic stainless steels and in the group of duplex stainless steels, where austenite is metastable for plastic working due to the low nickel content. Duplex steels are stainless steels whose microstructure consists of ferrite and austenite. Due to their high strength and good corrosion resistance, they are widely used in power plants, chemical plants, shipbuilding, etc. Strain-induced martensite can form from the austenite phase in several ways due to plastic deformation. One possible route is that austenite with a face-centered cubic (fcc) crystal lattice on the surface first transforms into ϵ -martensite with a close-packed hexagonal lattice, and then, if the deformation is large enough, can further transform into α' -martensite with a face-centered cubic (FCC) lattice in space. The other possible route is that austenite transforms directly into α' -martensite. The detection of strain-induced martensite is made particularly difficult by the fact that the crystal lattice and properties of α' -martensite are similar to those of ferrite. Strain-induced martensite is more brittle and harder than ferrite and austenite, and its corrosion resistance poorer, which can cause cracks in parts manufactured by

plastic forming and increase the risk of corrosion damage [1]. Our current research aims to determine whether strain-induced martensite forms in factory-rolled 1 mm-thick LDX2205 duplex steel under stretching, and whether our chosen testing methods are suitable for its detection.

2. Experiments and test methods

2.1. Experiment

During the tests, tensile test specimens machined from 1 mm thick, 1.4462 duplex steel sheet in the rolling direction were pulled to various degrees using an MTS810 hydraulic universal material testing machine. The extent of stretching was determined by dividing the uniform plastic deformation stage into equal parts. Based on our previously performed tensile tests, the contraction stage begins at approximately 24% elongation, so the six test specimens were stretched to 4%, 8%, 12%, 15%, 18%, and 21%, respectively.

We took samples from the middle section of the elongated specimens, then prepared sections both in the direction of drawing and perpendicular to it, and then attempted to detect the strain-induced martensite in these sections using X-ray diffractometry (XRD), eddy current magnetic test-

ing, and a scanning electron microscope imaging technique, the electron channeling contrast imaging (ECCI) method - also examining the applicability of the testing methods.

2.2. X-Ray diffraction testing

The X-ray diffraction study was performed with a Bruker D6 Phaser X-ray diffractometer equipped with a θ - 2θ vertical goniometer, over 20 - 80° , with a current of 30A and an accelerating voltage of 40V , using $\text{K}\alpha$ radiation excited from a copper tube.

2.3. Eddy current testing

The eddy current test was performed with the Fischer Feritscope FMP30 instrument, which is easy to use and can easily determine the ferromagnetic phase content, thus indirectly detecting α' -martensite, since it is also ferromagnetic. We assumed that the ferrite content measured in the as-delivered state would increase with the degree of deformation, which indicates the presence of α' -martensite [2]. We cannot detect ε -martensite with this method because it is nonmagnetic; therefore, this test alone is insufficient to detect strain-induced martensite. Since the penetration depth of the exciting electromagnetic field emitted by the Feritscope is 3 mm , the specimens were measured while supported by insulating material, and the measurements were also performed by placing the cut specimens on top of each other to increase the tested volume, thereby increasing accuracy. This test was performed on the nonembedded part of the specimens because the probe diameter was approximately equal to the plate thickness; if it was not placed exactly in the middle of the plate, it did not yield an accurate result.

2.4. Electron channeling contrast imaging method (ECCI)

Electron channeling contrast imaging (ECCI) is an electron microscopy-based material-testing method. The technique uses high beam current and low accelerating voltage in backscattered electron mode. The principle of the method is as follows: a high beam current yields a high electron flux at a shallow penetration depth, due to the low accelerating voltage. During the measurement, differently oriented grains and local lattice defects/lattice distortions in the material can conduct/ground electrons accumulated at the surface at different rates, or, conversely, generate backscattered electrons at different rates [3].

3. Results

3.1. Results of X-Ray diffraction

The X-ray diffraction diffractogram is shown in Fig. 1. The figure does not show an intensity peak indicating strain-induced martensite, because it may appear as a layering defect on the diffractogram, so this method alone is not suitable for its detection, but it cannot be ruled out that martensite has not been induced. As shown in Figure 1, with increasing plastic deformation, the width of the intensity peaks increases, reflecting the increase in dislocation density. The greater the deformation of the specimen, the greater the dislocation density, and thus the more the peaks are broadened [4].

3.2. Results of Eddy current testing

The ferrite content of each sample, as a function of deformation, is plotted in Fig. 2 in which it can be seen that the ferrite content decreases

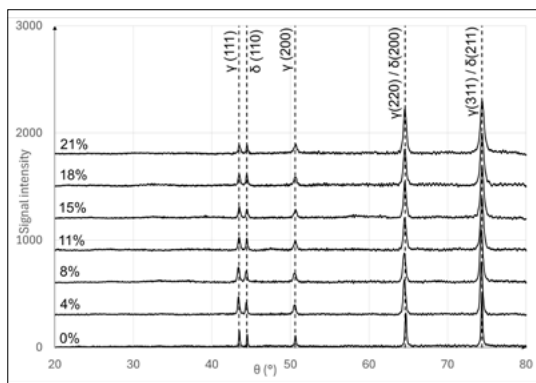


Fig. 1. X-ray diffractogram.

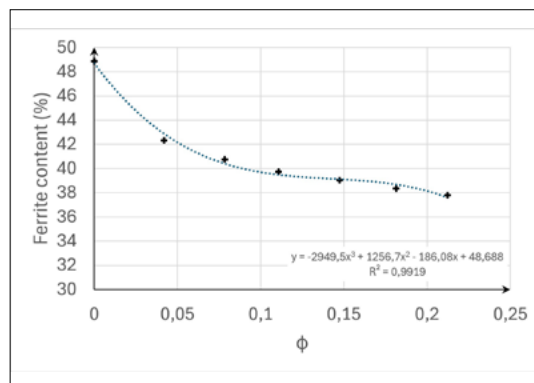


Fig. 2. Variation of ferrite content as a function of the degree of forming.

with increasing deformation. This seems to contradict the original assumption that it should increase if α' -martensite is formed, but if it is not formed, there should be no change. The apparent decrease in the ferrite content is related to the effects of cold plastic deformation on the microstructure. We know that as a result of deformation, the hardness increases with the increase in dislocation density, which in turn not only causes an increase in strength, but also changes the magnetic properties of the material in such a way that a larger magnetic field is required for full saturation induction, so the slope of the tangent drawn to the initial section of the magnetization hysteresis curve decreases with increasing deformation. The apparent decrease in ferrite content occurs because the Feritscope measures magnetic permeability at the beginning of the curve, which will decrease as the curve flattens. Based on this,

it can be concluded that the Feritscope is not suitable for detecting strain-induced martensite in duplex steels [5].

3.3. Results of ECCI testing

The ECCI test was performed using a ZEISS EVO MA 10 scanning electron microscope in backscattered electron mode with a beam current of 5 nA and an accelerating voltage of 15 kV. Images of the cross-sections, parallel and perpendicular to the stretching direction, of the specimens with 4% and 21% deformation are shown in Fig. 3 and 4 respectively in Fig. 5 and 6.

In these figures the test sheet is observed to have a fine-grained structure. In addition, the behavior characteristic of very fine-grained materials can be observed: in the less formed samples, the grain boundaries can still be distinguished, but they become increasingly blurred as the degree of forming increases. Furthermore, in Fig. 5 and 6.



Fig. 3. ECCI image of a specimen stretched by 4% in a section perpendicular to the stretching direction

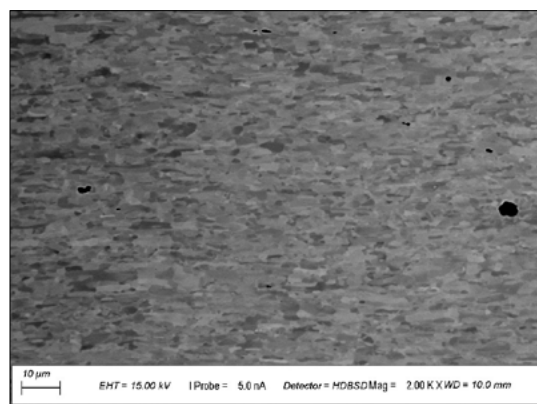


Fig. 5. ECCI image of a specimen stretched by 4% in a section parallel to the stretching direction.

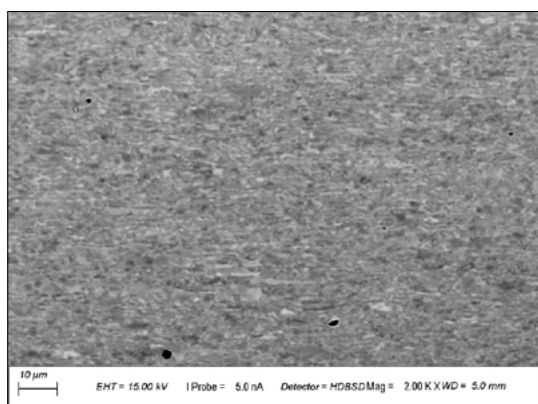


Fig. 4. ECCI image of a specimen stretched by 4% in a section perpendicular to the stretching direction

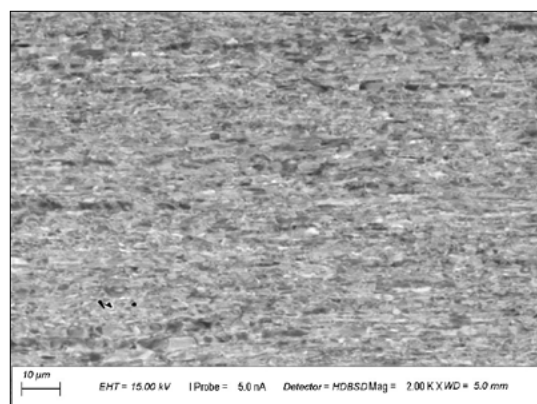


Fig. 6. ECCI image of a specimen stretched by 21% in a section parallel to the stretching direction

it can be seen in the section parallel to the rolling direction that the grain structure, which was already slightly stretched by rolling, has stretched even further. It can also be observed that the grain structure of the specimen that has undergone the greatest shape change appears to be finer than that of the least formed specimen. This may be due to the formation of twin boundaries, or even martensitic transformation, but it cannot be clearly determined at this magnification and resolution. Apart from this observation, no signs of strain-induced martensite are visible in the figures. This is partly because the microscope's resolving power is inversely proportional to the beam current, and the material under investigation has a very fine-grained structure, so it was not possible to achieve a high enough magnification to resolve the different phases. The color of the individual grains differs only due to their orientation. From this, we concluded that, with the available tools and under these test conditions, this method cannot be used to detect strain-induced martensite.

4. Conclusions

During our research, we stretched test specimens made of duplex steel plate 1.4462 to different degrees to create strain-induced martensite in them. On the stretched test specimens, we examined the detectability of strain-induced martensite formed during the forming process and the applicability of eddy-current and electron-conductivity contrast imaging methods for its detection. Based on the results of the tests, we drew the following conclusions:

- The Feritscope is not suitable for the detection of strain-induced martensite formed in plastically formed duplex steel.

- Our efforts to detect strain-induced martensite by analyzing images taken with the contrast imaging technique based on the channel effect were also unsuccessful due to the capabilities of the available electron microscope and the very fine-grained structure of the raw material.
- From the results of our tests, we cannot unequivocally determine whether deformation martensite was formed in duplex steel 1.4462, but it probably was not.
- We have realized that the detection of strain-induced martensite in duplex steel is extremely difficult and requires complex tests.

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Automotive Spare Parts Production Via Additive Manufacturing

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Abstract

The study presents the reconstruction and remanufacturing of a missing air filter cover for a German domestic-market motorcycle produced in the 1960s in a series of 80,000 units, using modern engineering methods. The research focuses on the methodology of part replacement in the absence of original documentation or physical samples, based on a reverse engineering approach as well as modern digitalization and additive manufacturing technologies. During this process, the connecting surfaces were captured using a high-precision industrial 3D scanner, which subsequently provided the reference dimensions. The missing dimensions were determined through photographic sources and approximate ratio estimation. Following the creation of the CAD model, the FDM process proved to be optimal among the manufacturing technologies due to its high precision and cost-effectiveness. The material selection was determined by the characteristic properties of the original polymer, leading to the choice of ABS material. Finally, an experimental post-treatment was performed on the prototype produced via additive manufacturing. The results confirmed that this method is suitable for the economical reproduction of components with complex geometries.

Keywords: *reverse engineering, 3D scanning, additive manufacturing, spare parts manufacturing.*

1. Introduction

The service life and availability of vehicle components continuously decrease during operation and with the passage of time, which poses a particularly significant problem in the case of older vehicles produced in small series or intended primarily for domestic markets. For these types, original factory spare parts are often unavailable or only accessible in limited quantities, while the procurement of aftermarket components is frequently difficult, uneconomical, or technically infeasible. At the same time, the design practices of the past characterized by oversizing and the application of considerable safety margins provide an opportunity for damaged components to remain serviceable even today if appropriate repair methods are applied by qualified professionals. In cases where a component is missing or the extent

of damage is too severe, the only viable solution is engineering redesign combined with the application of modern manufacturing technologies. It is important that the function of the reconstructed component remains the same as the original, while dimensional and material deviations are kept to a minimum [1].

The aim of the present study is to reproduce the missing air filter cover of a Simson Sperber (SR4-3) motorcycle manufactured in the 1960s [2, 3]. The first step of the work involves thorough measurement of the environment surrounding the missing component and the creation of a digital replica using 3D scanning. This process enables the accurate capture of geometric data, which serves as the basis for further analysis and the development of the CAD model [4]. Subsequently, a suitable printing material is selected that corresponds to the material of the original

component, ensuring that the replacement part possesses the required mechanical and physical properties. During the design phase, the available information, period photographs, and documentation assist in reconstructing the form and design as accurately as possible. After the CAD model is completed, the component is virtually fitted to the scanned environment in order to verify the fit, the contact surfaces, and dimensional accuracy. The next step involves selecting the appropriate manufacturing technology and producing a prototype, during which modifications and redesign may be necessary to achieve the desired level of accuracy. Finally, the process includes the post-processing of the finished component, the purpose of which is to reduce surface roughness and improve the external appearance of the part. The presented procedure offers a technically and economically advantageous solution for the replacement of unique or difficult-to-obtain vehicle components [5].

2. Materials and methods

2.1. 3D scanning

Due to the complex geometry of the component to be replaced, as well as the presence of non-fixed internal elements (e.g., the air filter insert), a fundamental prerequisite for precise reproduction was the high-accuracy digital representation of the surrounding assembly environment. The virtual capture of the assembled and properly fixed air filter unit was performed using a MetraSCAN 750 Elite industrial 3D scanner manufactured and distributed by the company Creaform. The main components of the system are shown in Fig. 1, including a handheld optical scanning unit, an optical tracking system consisting of two high-resolution, high-speed camera optics, and the associated data processing software environment. During the scanning process, the handheld device projects seven laser crosses and an additional laser line onto the examined surface. By optically detecting the reflected light beams, the system reconstructs the geometry of the object with high precision. A key advantage of the equipment is its safe operation and its accuracy of 0.028 mm within a working volume of 9.1 m², as specified in the calibration certificate. The continuous determination of the scanner's spatial position and orientation is ensured by white reflective targets placed on the handheld unit. If necessary, magnetic or adhesive reference markers can also be applied to increase measurement stabil-

ity. Another essential element of the technology is the optical tracking system, which monitors the reference markers using two high-resolution cameras and central infrared illumination. This enables the application of dynamic referencing, which guarantees the accurate positioning of the recorded points within the global coordinate system even in the presence of environmental vibrations. The measurement procedure is always preceded by calibration of the optical unit. Following the measurement of a dedicated calibration standard in predefined positions, the software confirms that the required level of accuracy has been achieved, in the present investigation this value was 0.032 mm. During the actual data acquisition process, the results of the handheld scanning appeared in real time on the computer. After successful acquisition, the software generated a polygon mesh model from the raw point cloud. Although the software environment provides extensive post-processing functions (e.g., geometry recognition and surface correction), the subsequent design operations were carried out on the exported intermediate mesh model due to the high practical demand for the software and its limited accessibility [6, 7].

2.2. Additive manufacturing

During the reproduction of the air filter cover, the primary objective was to preserve the original geometry and functional characteristics while ensuring rapid and cost-effective production. Due to the component's complex curved surfaces and small wall thickness, the application of conventional manufacturing or CNC machining technologies would have involved significant technologi-



Fig. 1. MetraSCAN 750 Elite scanner.

cal difficulties. Therefore, the chosen solution was the widely used modern FDM (Fused Deposition Modeling) 3D printing process, which offers favorable manufacturing parameters and enables the production of spatial geometries through the layer-by-layer deposition of molten filament [8]. The manufacturing process was carried out using a Bambu Lab P1S 3D printer (Fig. 2) with white ABS material. The preparation of the print was performed in the Bambu Studio slicing software environment. After importing the STL model, a printing orientation of 50° was selected in order to avoid “printing in mid-air” and to improve surface quality. The layer height was set to 0.08 mm, while the printing speed was reduced by 50% in critical curved sections to ensure proper adhesion and dimensional accuracy. To support the complex geometry, a “tree support” type support structure was generated, providing a material-efficient and easily removable solution. Following verification of the G-code generated during the slicing process, the actual manufacturing phase commenced.

The printing was performed with an extruder temperature of 270°C and a build plate temperature of 90°C , at an average printing speed of 150 mm/s.

2.3. ABS material and post-processing surface treatment

Among the filaments used in 3D printing, ABS (acrylonitrile–butadiene–styrene) was selected because its properties are similar to those of the original injection-molded component. It is one of the most widely used thermoplastic polymers in Fused Deposition Modeling technology, and due

to its favorable mechanical and physical characteristics it is suitable not only for prototype production but also for the manufacture of functional components. ABS is relatively lightweight, possesses good impact resistance, and demonstrates resistance to thermal loads (up to approximately 100°C) and moisture. In addition, it provides electrical insulation. Its slight flexibility and excellent post-processability enable its application in industrial environments, including mass production. However, its processing requires greater attention compared to materials such as Polylactic Acid (PLA). Due to its higher melting temperature (approximately $220\text{--}270^\circ\text{C}$), the use of a heated build plate is necessary during printing to ensure proper layer adhesion and to minimize warping. Furthermore, more intensive vapor emissions may occur during processing, which can pose potential health risks in the absence of adequate ventilation [9]. Parts produced using FDM technology typically exhibit visible layer lines resulting from the layer-by-layer manufacturing process. Since the objective of this work was to achieve a smooth and aesthetically pleasing surface, chemical post-processing was applied experimentally instead of mechanical material removal (sanding). In the case of ABS, a widely used method is vapor smoothing with Acetone, which relies on the solvent properties of the material. It is important to emphasize that this method cannot be applied to all polymers, as it may cause irreversible damage to certain materials. During acetone vapor treatment, the component was placed in a closed environment saturated with acetone vapor in such a way that the liquid acetone did not come into direct contact with the surface. The vapor induces partial dissolution of the outer layer of the material, resulting in surface swelling and the blurring of the layer lines. During the drying phase, the solvent evaporates, and the surface solidifies again, producing a smoother and glossier appearance. Determining the appropriate exposure time is critical, as excessive vapor treatment may penetrate deeper layers of the wall thickness, which can lead to deformation or a reduction in structural durability. In the case of commercially available ABS filaments, the optimal treatment time may vary. Therefore, the appropriate parameters are typically determined empirically [10, 11, 12].



Fig. 2. Bambu Lab P1S printer

3. Measurements and examinations

3.1. Determination of the dimensions of the replacement component

The accurate reconstruction of the missing air filter cover presented a significant challenge, as the mesh model obtained from the 3D scanning process captured only the connecting flanges and the contact surfaces of the air filter housing. Sufficient physical information was not available to precisely reconstruct the original form and the mounting points, therefore, additional research using online sources was required. Among the barely more than a dozen photographs identified, the majority depicted the component in an assembled state, and only two images showed the cover directly. In the absence of dimensioned drawings, a digital reconstruction method of photogrammetry was first applied to generate the model. For this purpose, the open-source Meshroom software was used. The software identifies characteristic points, detects edges and patterns, and matches these features between overlapping images. Subsequently, the program calculates the position and orientation of the cameras for each image and generates a polygon mesh model from the resulting point cloud using triangulation, in a process similar to that applied in 3D scanning. Although the method can theoretically provide an accurate approximation of proportions, the minimum dataset required for reliable operation—approximately 30–50 images taken from different angles with consistent quality—was not available [13, 14]. During the experiment, the software was able to utilize only two photographs, resulting in an incoherent and essentially random point cloud that could not be interpreted. Following the unsuccessful photogrammetric reconstruction, an alternative approach based on image-derived proportion estimation was applied. In this method, the geometric elements visible in the photographs were referenced to the dimensions obtained from the 3D scanning data, and approximate distances and proportions were determined by averaging measurements derived from multiple views. Although this method provides lower accuracy compared to software-based reconstruction, it offered a sufficient compromise for creating a functionally and aesthetically acceptable model. Based on these results, the final 3D model was created using the Solid Edge software environment, resulting in overall dimensions of $114 \times 94 \times 35.4$ mm.

3.2. Dimensional accuracy and surface quality of the printed part

Dimensional verification prior to manufacturing was performed through the virtual assembly of the completed model with the previously scanned air filter unit. This digital integration enabled the early identification of potential fitting errors and allowed fine-tuning of the model before the printing process. The dimensional accuracy of the printed component proved to be excellent; deviations relative to the CAD model ranged only between 0.05 and 0.1 mm. The cover fitted properly to both the air filter housing and the air filter insert. The overall printing quality of the air filter cover produced using Fused Deposition Modeling technology was generally accurate. However, after the removal of the support structures, residual surface irregularities remained, which required further finishing operations. The more pronounced defects were first corrected by mechanical sanding with P200 sandpaper to achieve a uniform surface quality matching the surrounding areas.

3.3. Effects of post-processing on the component

Since excessive exposure time may lead to structural deformation or softening of the wall thickness, the technology was first tested on a trial specimen. During the experiment, the component was treated in acetone vapor for one hour at room temperature (20 °C), corresponding to the maximum duration recommended by the online sources, with inspection intervals of 5–10 minutes. This was followed by a 24-hour resting and ventilation period. The result was a glossier surface with only a minor aesthetic improvement. However, measurements revealed that the chemical surface treatment caused an average linear shrinkage of approximately 0.5 mm compared to the original printed dimensions. Taking these observations into account, the final component was further fine-tuned accordingly. Since the improvement in surface quality did not reach the expected level, the acetone vapor post-treatment was repeated on another test specimen, this time increasing the exposure time to two hours. As a result, the component became more intensely saturated with acetone vapor, which made it softer and more susceptible to deformation. After drying, a significant improvement in surface quality was clearly observed. However, a larger dimensional change was also detected, including an average shrinkage of approximately 3 mm

and minor deformation at certain points of the component. Based on these results, the final print optimization of the component was adjusted to correspond to the one-hour surface treatment process.

4. Conclusions

The results of the study confirm that the combined application of Reverse Engineering and additive manufacturing technologies provides an effective and economical solution for the replacement of vehicle components lacking documentation or original reference samples. The high-precision industrial 3D scanning process, which achieved a calibration accuracy of 0.032 mm, ensured the reference dimensions required for precise fitting. These dimensions proved to be critical during the positioning of complex internal elements and the alignment of connecting flanges. It can be concluded that photogrammetric reconstruction does not provide evaluable results when only a limited number of source images are available. However, image-based proportion estimation, when combined with the environment captured by 3D scanning, provides sufficient accuracy for the creation of a CAD model that is both aesthetically and functionally appropriate. The manufacturing process demonstrated that the application of Fused Deposition Modeling technology together with the use of ABS material proved to be an optimal choice. The physical and mechanical properties of ABS show a high degree of similarity to those of the original injection-molded polymer component. The dimensional accuracy of the printed prototype remained within a tolerance range of 0.05–0.1 mm relative to the virtual model, confirming the suitability of the selected manufacturing technology. Investigations related to chemical post-processing revealed that vapor treatment with Acetone improves surface roughness, however, increasing exposure time leads to progressively greater linear shrinkage and structural deformation. In order to optimize the process, depending on the specific characteristics of the ABS material used, the application of a heated acetone vapor environment at approximately 40 °C may be recommended. This technological modification could significantly increase the efficiency and quality of the surface treatment, while the substantial reduction of exposure time may minimize dimensional distortion of the component and reduce the risk of unfavorable material-structural changes.

Overall, the complex methodology presented provides a reliable basis for the reproduction of unique vehicle components.

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Fracture Analysis of Grey Poplar for Structural Applications

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Abstract

To expand the application possibilities of grey poplar (*Populus × canescens*), it is important to know its strength values and behaviour. As an anisotropic material, the structural use of wood is significantly influenced by its failure modes. The analysis of fracture patterns resulting from tensile and compressive strength tests on test specimens prepared in the main anatomical directions is important for predictable design. Further classification within the group is the separation of sapwood and heartwood samples, which shows two groups regarding elastic and brittle characteristics.

Keywords: grey poplar, strength, surface, anisotropy, fracture analysis.

1. Introduction

One of the most pressing issues of our time is climate change, which has a significant impact on the living conditions of more sensitive tree species. Due to decreasing precipitation and rising temperatures, plants' water requirements are beginning to increase. Since they do not receive enough water, this can lead to desiccation and reduced resistance to insects [1, 2].

The grey poplar (*Populus × canescens*) is a tree species native to Hungary with a significant population, which can serve as a good alternative as a replacement for tree species whose numbers are declining due to climate change. The grey poplar is an easily cultivated, fast-growing tree species that currently occupies 1,5 million hectares of forest land in Hungary, with 1,3–1,5 million m³ of timber entering the market annually. This volume accounts for 23–25% of total annual timber harvest. Its primary uses include the manufacture of crates, pallets, underlays, particleboard, and fibreboard. [3]

Its structural use is theoretically possible; however, practical application requires predictability and the ability to plan, which is why it is important to understand its fracture behaviour in response to mechanical stresses in various anatomical directions.

2. Materials and methods

The fracture pattern of grey poplar depends significantly on the direction of grain growth, as well as on whether sapwood or heartwood is dominant in the test specimen. Sapwood, which is responsible for transporting nutrients and moisture, accounts for 30–40% of wood and has a higher moisture content than heartwood. The latter consists of dead tissue that stores tannins and lignin, resulting in a harder and drier structure. According to the literature, the different behaviour of these parts depends on the tree species. Some studies on oak have shown that the mechanical properties of the sapwood and heartwood did not differ significantly from one another, nor did the heartwood exhibit greater strength than the sapwood parts. [4] For this tree species, structural designability was not affected by differences between wood sections, whereas in the case of fast-growing tree species, significant differences were observed for most parameters, except for a few factors. [5] For the purpose of species-specific testing, the sapwood and heartwood sections were separated during sample preparation so that the strength of each section could be distinguished, and the fracture types could be identified, insofar as they differ from one another.

During the strength tests, the material's behav-

our was examined in the three main anatomical directions. In doing so, we distinguished between longitudinal (L), radial (R), and tangential (T) directions and their planes, where test specimens at 0°, 45°, and 90° were prepared depending on the direction. Askenazi's anisotropic strength theory and practical experience make it possible to experimentally determine shear strength for a 45° shear angle. In this case, the distribution of shear stress is uniform across the shear cross-section [6].

During the tests, compressive strength measurements were performed on 30 sapwood and 30 heartwood sections for each direction – LR0°, LR45°, LR90°, LT45°, LT90°, RT45° – for a total of approximately 400 test specimens, and tensile strength measurements were performed on the same number of specimens. An Instron 5985 universal testing machine was used for the tests. Compressive strength testing was conducted in accordance with ISO 13061-17, calibrated for durations between 1 and 5 minutes, until the specimen failed [7]. The tensile strength test was conducted in accordance with ISO 13061-06, also until the test specimens failed [8]. The moisture content of test specimens was optimized with different configurations by storing them in a climate-controlled room (temperature 20 °C, humidity 65%), thereby eliminating any differences in fracture patterns that might otherwise occur, which resulted in an air-dry (12%) state.

According to the literature, fracture patterns also depend heavily on anatomical orientations, and there may be variations regarding the sapwood and heartwood regions. In the case of oak, previous studies have shown that mechanical properties do not differ significantly [9], whereas for fast-growing tree species, it has been demonstrated that there are more substantial differences in some mechanical property parameters [5].

According to the literature, under pressure, several different basic types can be distinguished in the longitudinal direction, parallel to the fibres, such as crushing, wedge-shaped splitting, shearing, splitting, crushing and splitting, as well as slippage and opening of the ends. In the tangential and radial directions, typical fracture patterns under pressure perpendicular to the fibre include the crushing of a section off the early wood (shearing along an annual ring) and the buckling of the annual rings [10, 11].

When tension is applied parallel to the grain, the literature distinguishes between fissure fracture, combined tensile and shear fracture, and shearing; when tension is applied perpendicular to the grain, it distinguishes between fracture along the early sapwood, shearing along the growth rings, and fracture along the medullary rays [10, 11].

Photographic documentation of the fracture surfaces was prepared using a Hitachi S-3400N scanning electron microscope and a Nikon SMZ 800 stereoscopic zoom microscope.

3. Results and their evaluation

The most characteristic and frequently occurring fracture patterns were identified from the photographic documentation compiled during the investigation. The test specimens fractured in the ways shown below (Fig. 1).

To prepare the electron microscope specimens, we trimmed the fracture surfaces after the tests to reduce their size, as shown in Fig. 2. Each sample was marked with an identification code for traceability.

The electron microscope images are presented below by orientation. The following figure shows the fracture patterns of LR0° – that is, fractures formed under pressure parallel to the grain – for

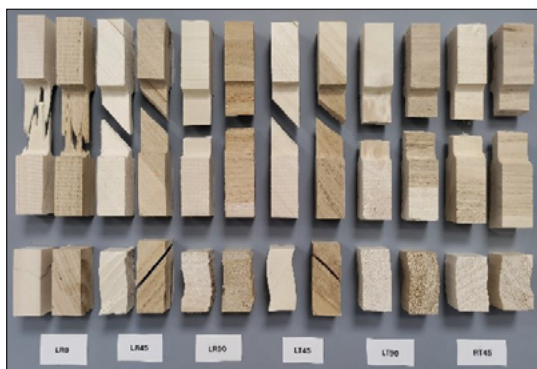


Fig. 1. Typical fracture patterns of the test specimens.



Fig. 2. The test specimens after fracture.

the sapwood and heartwood sections (Fig. 3). The crushing characteristic of the sapwood and the wedge-shaped fracture are clearly visible, while the heartwood is characterized by fracturing and tearing, with tissue separation.

The tensile fracture patterns are characterized by cleavage fracture, secondary cell wall exfoliation, and combined tensile and shear fracture in both cases. The sapwood shows localized material tears, while the heartwood reveals delamination, brittle shear fractures, and partial delamination (Fig. 4).

When pressure was applied in the LR45° direction, the sapwood samples underwent significant deformation, with crushing along the growth rings; however, they remained intact, a phenomenon characterized by the wrinkling and knotting of the fibre bundles. In the heartwood section, shearing occurred without permanent deformation; the cell walls peel off the fibres in layers, behaving rigidly (Fig. 5).

The specimens tested for tensile strength at 45° suffered shear failure. In the parenchyma regions, fibre tearing occurred, and the S2 layers of the secondary cell wall – which play a decisive role in cell wall reinforcement due to their fibril orientation and thickness ratio – were exfoliated. In the wood, the large cell cavities and other woody parts exhibit a brittle fracture surface

passing through the cell wall, which is also clearly visible in the electron microscope images (Fig. 6).

The LR90° compression fracture patterns (Fig. 7) typically showed crushing and shearing along the growth rings in the case of the heartwood. The test specimens remained intact, with significant permanent deformation. The wood behaved brittly in all cases. The fracture patterns were characterized by the collapse of cell rows, which resulted in the separation of the test specimens. The deformation of the large-diameter vessels is clearly observable; in the case of the sapwood, they flattened significantly, while in the case of the heartwood, they were completely compressed, and the cell walls cracked.

The fracture patterns of the LR90° – oriented tensile specimens are characterized by fracture along the early paste and shear failure along the growth rings. Based on the electron microscope images, there was no significant difference; the heartwood exhibited slightly coarser fracturing (Fig. 8).

In the LT45°, test – a compressive strength test conducted at a 45° angle to the grain – the sapwood samples failed by shearing, exhibiting an “S”-shaped permanent deformation and the separation of the compressed sides, while the heartwood fractured cleanly without deformation. In the photographs, the sapwood tissue units sepa-

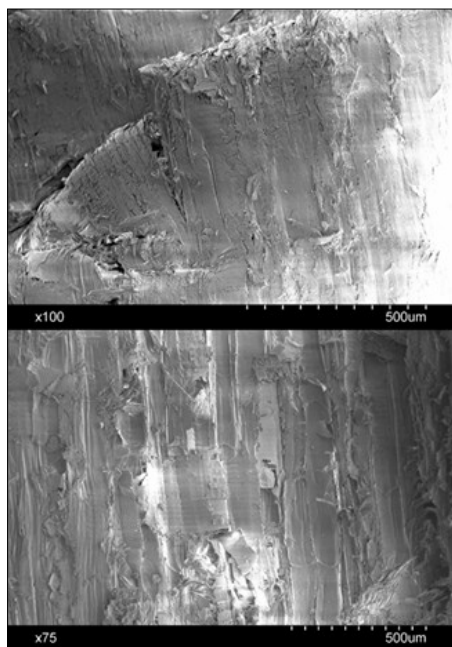


Fig. 3. LR0 compression fracture pattern – sapwood and heartwood.

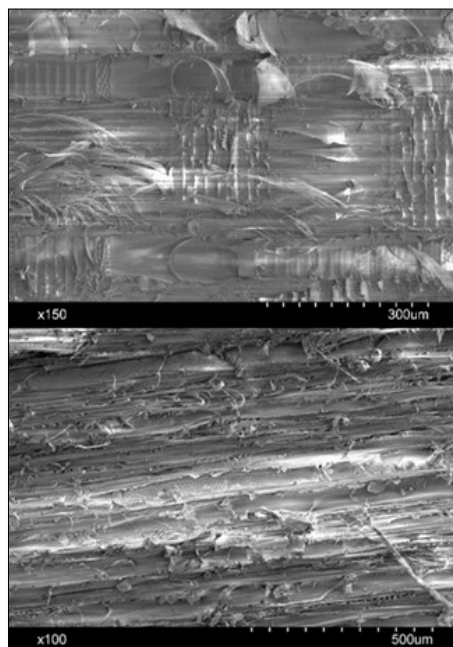


Fig. 4. LR0 tensile fracture pattern – sapwood and heartwood.

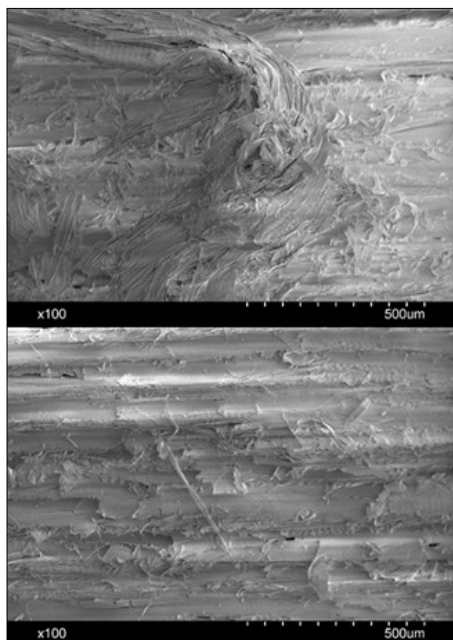


Fig. 5. LR45 compression fracture pattern – sapwood and heartwood.

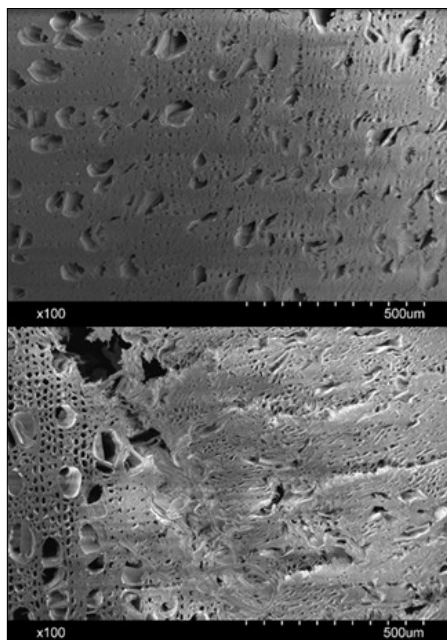


Fig. 7. LR90 compression fracture pattern – sapwood and heartwood.

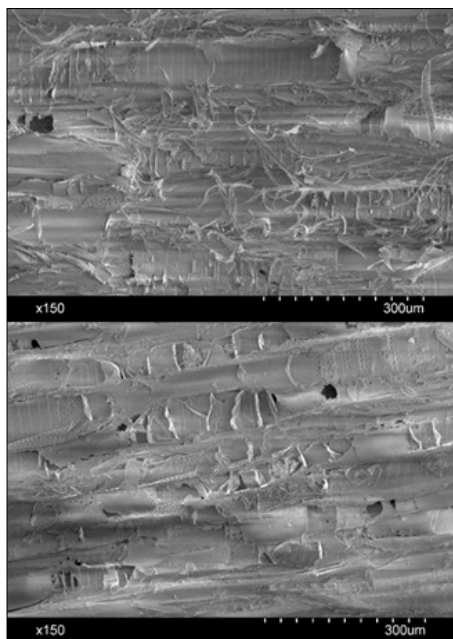


Fig. 6. LR45 tensile fracture pattern – sapwood and heartwood.

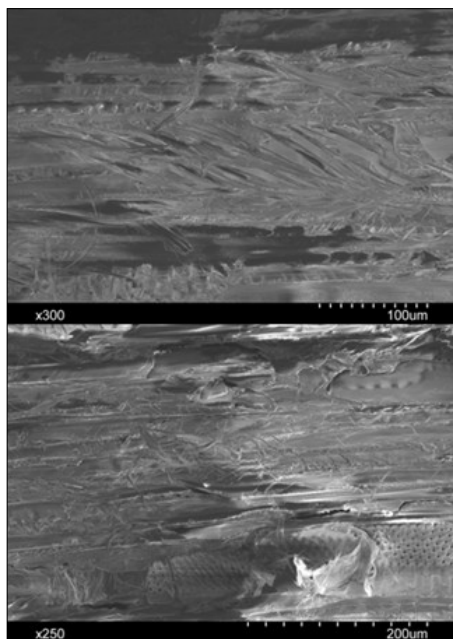


Fig. 8. LR90 tensile fracture pattern – sapwood and heartwood.

rated along longitudinal cracks, while the surface remained essentially smooth, revealing the fracture surface resulting from shear stress. In contrast, the heartwood exhibited stepped fractures in some places (Fig. 9).

When tested under tensile stress in the same direction, the fracture surface of the test specimens exhibits a brittle fracture with a distinct shear line. The sapwood shows longitudinal tears, in which separate tearing of the fibres is not typical.

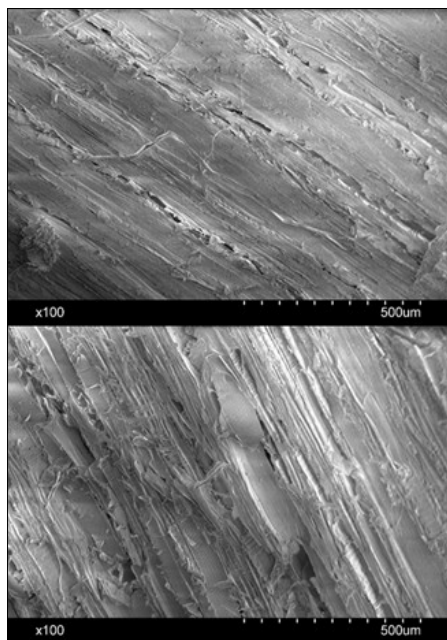


Fig. 9. LT45 icompression fracture pattern – sapwood and heartwood

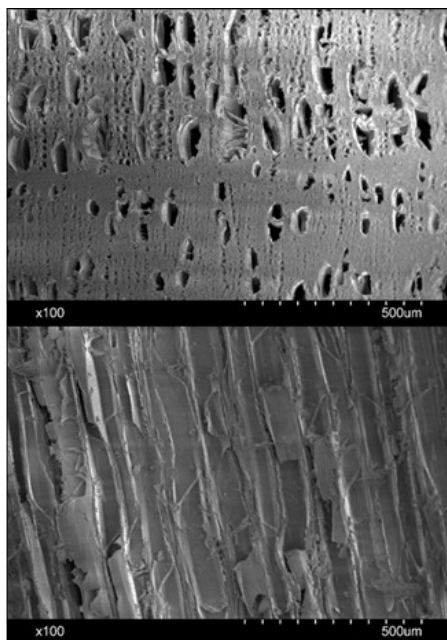


Fig. 11. LT90 compression fracture pattern – sapwood and heartwood.

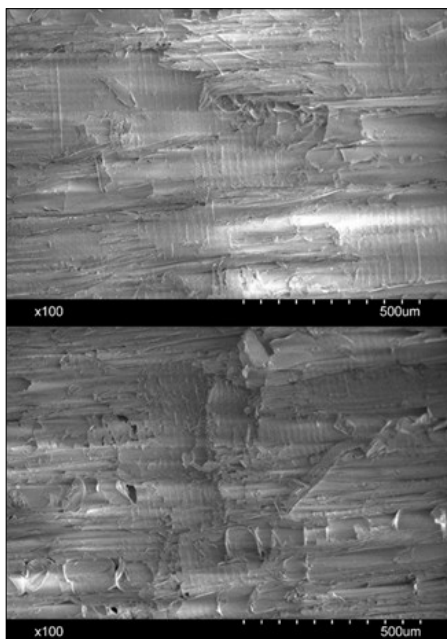


Fig. 10. LT45 tensile fracture pattern – sapwood and heartwood.

The heartwood specimens exhibit stepped fractures (Fig. 10).

The compression failure patterns of the LT90° test specimens, perpendicular to the grain, were characterized by the Buckling of growth rings

and deformation, which occurred more permanently in the heartwood than in the sapwood. In the sapwood, minor deformations and cracks appear around the vessels due to their compression, while the heartwood exhibits delamination along the grain and longitudinal cracks (Fig. 11).

The specimens for the tangential tensile strength test fractured along the radial direction. In the sapwood sections, the cell wall layers delaminated, thereby severing the connections between the fibres. In the heartwood section, cracks are prominent in places, accompanied by a significant section, cracks are prominent in places, accompanied by a significant amount of debris (Fig. 12).

The fracture patterns under RT45°, with pressure applied at a 45° angle to the grain, were characterized by the bending of the growth rings; in the sapwood, this resulted in visible fractures in nearly all cases, while in the heartwood, it caused permanent deformation. In the microscopic images, the cellular structure of the sapwood was completely flattened in several places, the vessels were deformed, and the surrounding ground tissue was also compacted. In the heartwood, the extent of damage was more severe; the cell walls had collapsed, resulting in a greater degree of destruction (Fig. 13).

In the fracture patterns of the 45° tensile test, brittle shear fractures occurred. In the sapwood,

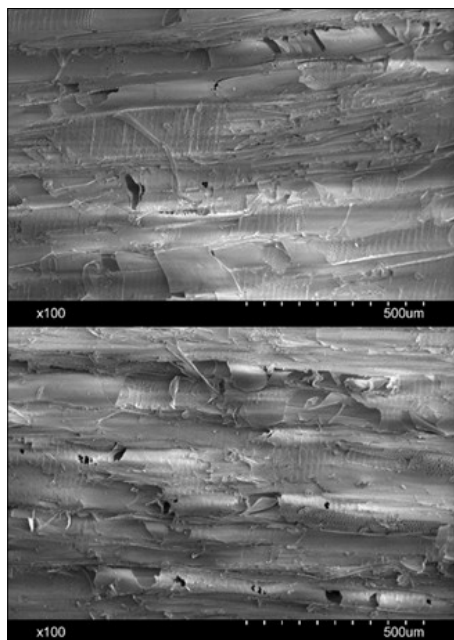


Fig. 12. *LT90 tensile fracture pattern – sapwood and heartwood.*

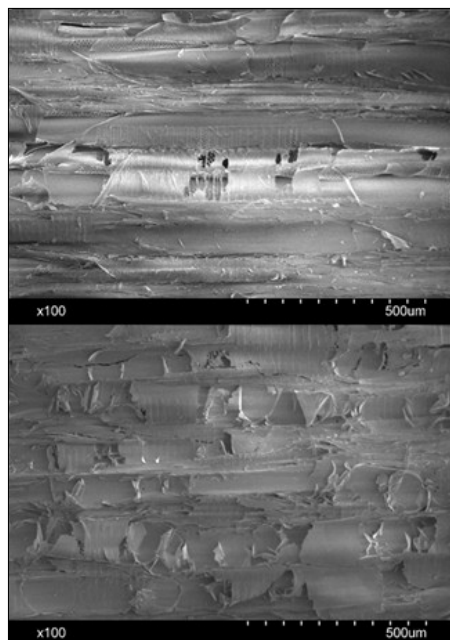


Fig. 14. *RT45 tensile fracture pattern – sapwood and heartwood.*

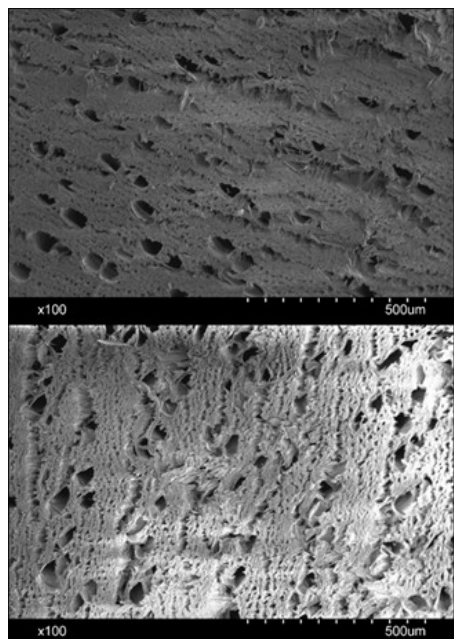


Fig. 13. *RT45 compression fracture pattern – sapwood and heartwood.*

the fibres detached over a large area in the longitudinal direction, a phenomenon also attributable to the membrane-like layers. A brittle shear fracture is also visible in the heartwood, where the layers of fibres tended to slip past one another (Fig. 14).

4. Conclusions

The results of the compressive and tensile strength tests clearly demonstrate the orthogonally anisotropic properties of wood. By covering all planes and directions, a comprehensive picture is obtained of the various types of failure, including shear, deformation, fibre breakage, splitting and other failure modes. The nature of the fracture patterns observed during this study can be identified from the stress-strain diagrams plotted during the tests – which will be detailed in a future publication [12, 13] –, referring to the behaviour of the test specimen and the type of failure.

Significant differences were observed between the sapwood and heartwood sections during the measurements. In some places, the sapwood exhibited elastic-tough behaviour; the fibres slipped and underwent deformation, but the test specimen remained intact under compressive loading. It also behaved in a more elastic and tough manner under tensile loading, which is why the more fibrous fracture patterns were observed. During compression failure of the heartwood, much more brittle fracture patterns occurred, mostly involving the splitting of the specimens, fibre tearing, and fracture. Brittle characteristics also dominated under tensile loading, with failure occurring sooner. Comparing the results with

the literature, it can be concluded that while the descriptions correspond to the fracture patterns observed in the measurement results, the specific behaviour of the sapwood and heartwood sections is not detailed separately.

During the compression and tensile strength tests on some test specimens, mixed specimens containing both sections were also produced. In these cases, failure generally occurred at the boundary between the two parts, which may also support the claim that there is a difference between the two parts and, moreover, may also support the claim that there is a difference between the two wood-resin parts, as the differing properties can lead to the accumulation of internal stresses in the boundary layer.

Based on data determined during strength measurements, despite its lower strength values, the sapwood exhibited more reliable and flexible behaviour with a small standard deviation. In contrast, despite its higher strength results, the heartwood, with its stiffer behaviour and larger standard deviation, behaved in a more unpredictable manner in terms of structural design.

Based on the results, it can be concluded that the sapwood and heartwood of grey poplar behave differently under load. From the perspective of practical applicability – as a timber intended for use in structural applications – all design possibilities must be considered, since it is not feasible to separate these wood components during harvesting and processing. Regarding strength classification, these properties fundamentally determine reliability, the assessment of which is crucial for safe timber construction design.

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Finite Element Modeling and Mechanical Analysis of Skull Implants

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Abstract

This study compares the biomechanical performance of a custom-made cranial implant made of titanium alloy (Ti6Al4V) and PEEK with that of natural cranial bone, using finite element modeling. For the simulation, we used a realistic skull and implant model generated from a patient's CT scan. During the modeling process, we performed surface modifications and automatic solid conversion using Meshmixer and Ansys software to ensure watertight geometry. We ran the simulation under critical loading conditions typical of skull implants, including a 50 N static load simulating forces during sleep and an intracranial pressure of 15 Hgmm. The analysis focused on total deformation and equivalent stress. The results showed that the titanium implant had the highest stiffness, while the PEEK implant proved to be the least stiff. The highest internal stress occurred in the titanium, but for all three materials tested, the stress remained well below the yield strength. This confirms that only elastic deformation occurs under the simulated load, meaning that both materials can be safely used under the tested 50 N load condition

Keywords: *finite element method (FEM), cranial implant, PEEK, titanium alloy.*

1. Literature review of skull bone implants

1.1. Implant geometry and materials

Designing the geometry of a cranial implant is a critical step in neurosurgery and biomaterials science. The primary goal is to restore the structural integrity of the skull and achieve an excellent aesthetic outcome. Modern geometry relies heavily on patient-specific design, moving beyond standard plates to custom-fit implants.

1.1.1. Introduction of Cranial Implant Geometry

Cranial implant geometry refers to the physical shape, thickness, and boundary features of the implant used to repair a skull defect. Unlike traditional standardized implants used in joints, cranioplasty demands a high degree of customization because every cranial defect is unique.

Designing skull implants requires careful con-

sideration of both geometry and material properties to achieve optimal biomechanical and biological outcomes.

The geometry for a cranial implant is usually generated using a Computer-Aided Design approach, geometry for production can be created with the following steps:

Image creation and Segmentation

- CT-scans of the patient's skull are acquired in DICOM format.
- The images are processed using specialized software (e.g., 3D Slicer, Mimics) to perform segmentation, isolating the compact bone and the cranial defect.
- This segmented geometry is converted into an STL-file, which is a triangulated mesh representation of the surface.

Implant Design

- Using CAD or reverse engineering software (Autodesk Meshmixer, Geomagic, Materialise

Mimics), the implant geometry is designed to restore the skull's natural contour. Common methods include:

- Mirroring: The unaffected, contralateral side of the skull is mirrored to reconstruct the missing portion, assuming skull symmetry.
- Spline Algorithms: Using interpolation to reconstruct the defect based on the surrounding healthy bone edges.
- The final implant shape is obtained by Boolean subtraction of the original defective skull model from the complete, reconstructed model [1].

1.1.2. Material Properties of the Skull Bone and the Cranial Implant

Skull bone characteristics

Skull bone is a layered structure consisting of a compact inner layer and an equally compact outer layer separated by a porous diploe (spongy tissue) layer. The compact bone is a dense calcified tissue that forms the outer layer of all bones and surrounds the spongy bone. Overall, bones behave more like rigid material. The human skull consists of approximately twenty-eight bones, most of which are connected by sutures. The sutures are flexible joints that allow the bones to grow evenly as the brain grows, and the skull expands. There are thirteen sutures in the human skull, of which four are the main sutures: the coronal suture, the sagittal suture, the squamous suture, and the lambdoid suture. The ratio of cortical to spongy bone varies across the head, and this structure, which resembles patchwork, is difficult to trace. The skull bone varies in thickness in different parts, but on average, we can expect thicknesses between 5 and 7.8 mm in a skull, of which the thickness of the outer cortical and inner cortical bone ranges from 1 to 2.6 mm, based on which the [2].

Table 1. Skull bone characteristics [2]

Material Name	Density (kg/m³)	Young-modulus (MPa)	Poisson-ratio	Tensile strength (MPa)
Cortical bone	934	500–800	0.21	114
Cancellous bone	1500	1000	0.05	4.0

Materials used for cranial implant

For cranial implants, common usage is metal, for example, the Ti6Al4V titanium alloy (herein-

after referred to as “titanium”). But nowadays polymers (for example, PEEK, PMMA, UHMW-PE, HDPE) are also becoming popular, due to their controllable properties. Also, their mechanical modulus is closer to natural bone than metals, and no image artifacts occur during CT and MRI examinations.

Table 2. Major materials used for cranial implant

Material Name	Density (kg/m³)	Young-modulus (MPa)	Poisson-ratio	Tensile strength (MPa)
UHMW-PE [3, 4]	934	500–800	0.46	21–28
PEEK [5]	1360	4000	0.4	100
Titán (Ti6Al4V) [6, 7]	4430	114 000	0.33	1100

In conclusion, both implant geometry and material selection are critical determinants of skull bone implant success. Advances in imaging, CAD/CAM, and materials science are enabling the development of implants that more closely mimic the native skull in both form and function, but each approach has distinct advantages and limitations that must be considered in clinical decision-making [8].

1.2. Loading and bone-implant interactions

Mechanical loading is a critical factor influencing the stability and longevity of skull implants. The interaction between the implant and the surrounding bone is primarily governed by the geometrical configuration of the implant, the loading conditions, and the material properties of both the implant and the bone. When loads are applied, fixation devices and the bone-implant interface are responsible for absorbing these forces, which in turn affects the stress and strain experienced by both the implant assembly and the adjacent bone tissue.

The accuracy of biomechanical performance assessments depends heavily on the quality of data used in computational models. As the structural properties of bone in vivo are often uncertain, they can only be predicted with limited reliability, therefore, the stability and functionality of cranial implants are closely linked to the mechanical interaction between the bone, implant, and fixation screws, and a comprehensive understanding of these interactions is essential for optimizing implant design and clinical outcomes [9].

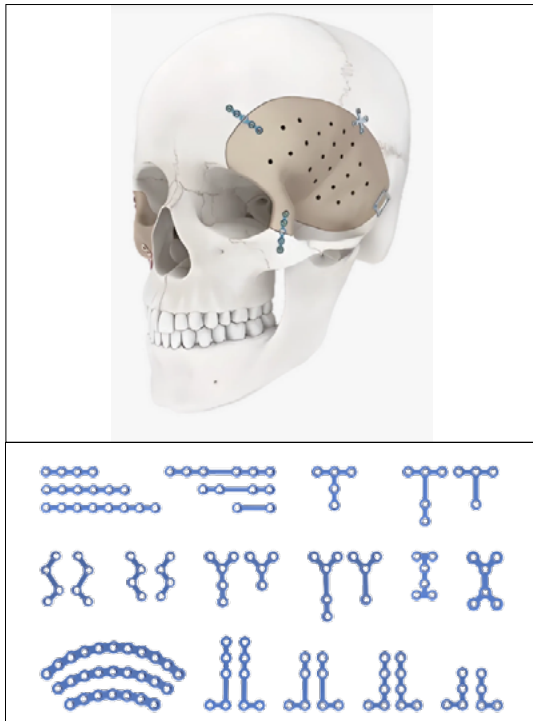


Fig. 1. Fixation of implant to skull and common fixation plates [10, 11]

1.2.1. Fixation

The whole geometry should be stable; movement of the skull in any direction must be prevented, which can be achieved with the use of fixed support in Ansys software. Also, during cranial surgery, the implant should be connected to the skull bone. For this purpose, doctors usually use fixation plates and stabilize them with fixation-screws made of titanium alloy [9]. The implant geometry dictates the placement and number of screw holes. The position of these fixation points significantly influences stress distribution across the implant and the surrounding bone when subjected to external forces (Fig. 1) [12].

1.2.2. Loading

Intracranial pressure

There is constant intracranial pressure in the skull, which is 15 mmHg. This can be simulated during finite element simulation using the pressure command applied to the inner surface of the skull [9].

Load due to external influence

Three different realistic and impacts can be mentioned:

1. Static impact force at the center of the implant during sleeping is 50 N. With applying this magnitude of force, software can simulate the impact of sleeping to the implant/skull part [13].

2. The 2000 N impact simulation shows the mechanical responses of a moderate to severe, but not extreme, impact to the skull and surrounding tissues [14].

3. Simulating a static load caused by a foreign object impact, 8000 N should be applied to the part of the skull [14].

2. Review of modeling possibilities

2.1. Creation of watertight 3D geometries

2.1.1. Reverse Engineering and surface fitting

The process of Reverse Engineering with Surface Fitting is the methodology for converting raw digital scan data (typically a mesh model, like an STL file) into an editable, mathematically precise CAD solid model (like a STEP or IGES file). The core of this process, surface fitting, involves creating analytical or free-form surfaces that accurately approximate the mesh geometry.

Steps of the Method

1. Importing the raw scan data (STL, OBJ, or PLY) into the software.

2. Removal of noise, holes, and nonmanifold geometry.

3. Feature and Region Segmentation: software analyzes the mesh geometry to automatically segment (separate) it into distinct geometric regions.

4. Surface Fitting (Extraction and Modeling): The software extracts exact mathematical entities (planes, cylinders, spheres, etc.) that best fit the selected region of the mesh.

5. Solid Model Generation with Trim and Knit function: individual extracted surfaces are trimmed (cut) and extended so they align and meet edge-to-edge. These trimmed surfaces are then knitted (or sewn) together to create a single, watertight closed surface body [15].

2.1.2. Surface preparation and automatic converting

First, the cranial implant and skull STL file should be imported into the Meshmixer software. This is followed by removal of a larger section of the skull bone with “Plane Cut” instruction (Fig. 2).

Then the cranial implant and part of the skull bone should have a rounded, less sharp, and fragmented surface. Therefore, in Meshmixer soft-

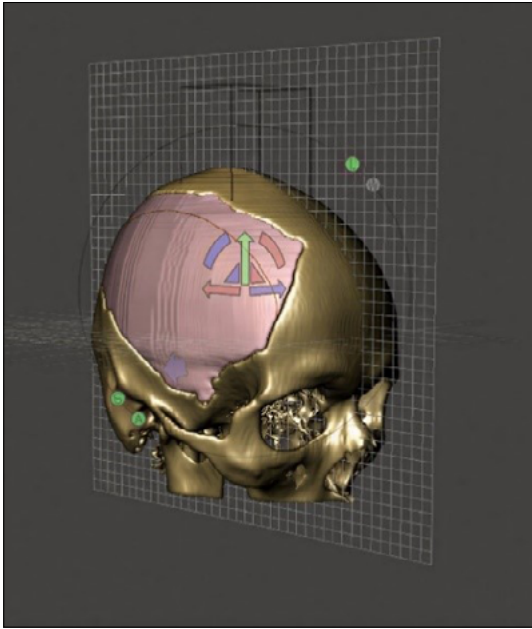


Fig. 2. „Plane Cut” in Meshmixer

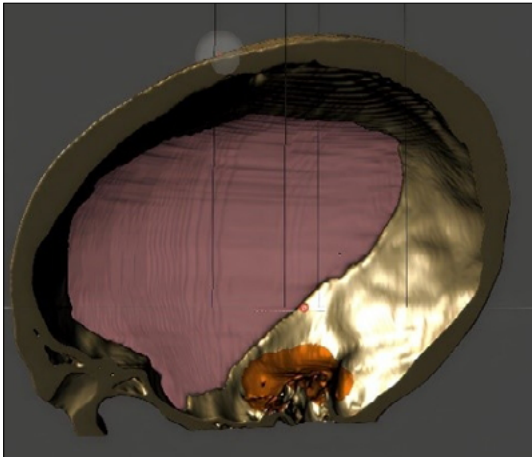


Fig. 3. „Erase + Fill” instructions in Meshmixer.

ware “Erase + Fill” and “Smooth” instructions can be used (Fig. 3).

After modification of the surface, the cranial implant and part of the skull bone should be fully closed, therefore in Meshmixer, the “Close Crack” command was created.

The final step involves applying the “Make Solid” function to the selected components. This guarantees a solid geometry, which is a prerequisite for successful FEM simulation.

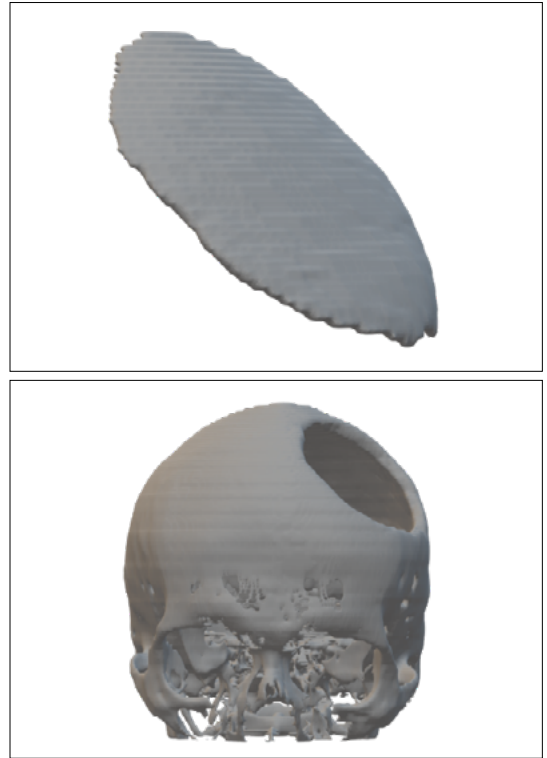


Fig. 4. Cranial implant and damaged skull used for FEM simulation

With the “Inspector” function, geometry can be analyzed, and filling the missing part of the file is possible.

Unfortunately, the Meshmixer automatic solid body creation was not enough to ensure we achieved a solid and watertight geometry, so additionally, in the Ansys software We applied an automatic command to the implant and the skull section to convert them to solid objects.

3. Define a case for finite element modeling, prepare, execute the simulation and analyze the results

This study was conducted to evaluate the performance of PEEK and titanium cranial implant in comparison to natural cranial bone under a 50 N static load, simulating effect during sleep.

3.1. Used model

The implant and skull model came from a real case, where the patient skull was scanned during CT. The CT created DICOM file was generated into an STL file, which was then used for the simulation (Fig. 4).

3.2. Used materials

3.2.1. Skull bone

The location of cranial sutures in the bone affects its mechanical properties. However, in the cranial bone model used in this study, we disregarded this factor due to the difficulties involved in modeling and tracking it.

A further simplification option is to use homogeneous skull material with a constant iron content, assuming linear elastic material behavior. [14]

Table 3. Material used for bone segments

Material Name	Density (kg/m ³)	Young-modulus (MPa)	Poisson-ratio	Tensile strength (MPa)
Simplified skull bone	1800	6000	0.21	77.6

Also important to highlight, for the skull part used in this simulation in every case “simplified skull bone material” was applied, only the implant material was changed in different cases.

3.2.2. Cranial implant

In accordance with Table 2, for the cranial implant, one polymer (PEEK) and one metal (Ti6Al4V) material were chosen; both materials are the most common used in cranioplasty.

3.3. Boundary Conditions

Boundary conditions in Ansys are constraints or applied values (such as forces, temperatures, velocities, or voltages) that control how a model behaves at its boundaries or key locations.

3.3.1. Fixation

In this simulation osseointegration of skull and cranial implant was already done. So, the implant and skull had a bonded interaction, which was already edited in Meshimer with the “Combine” command.

To prevent any displacement of the entire geometry, we applied constraints to the cut surfaces of the skull: the “Fixed Support” command ensured zero displacement and rotation (Fig. 5).

In this case, fixation plates were abandoned due to the complexity involved in importing them into this already huge cranial implant and skull bone simulation.

The bone-implant interface is modeled as a unified geometric assembly through the application of “Contacts” connection definition to simulate post-osseointegration structural continuity (Fig. 6).

3.3.2. Impact on the skull and implant

Impact on the skull and implant is evaluated under a simulated sleep scenario, where a 50 N

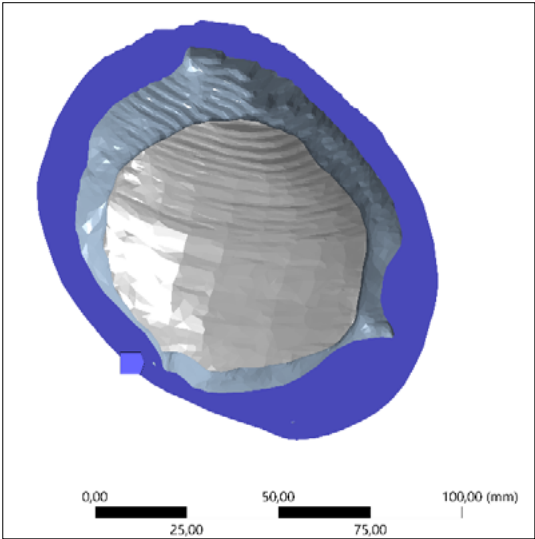


Fig. 5. Applying “Fixed Support” on skull part in Ansys

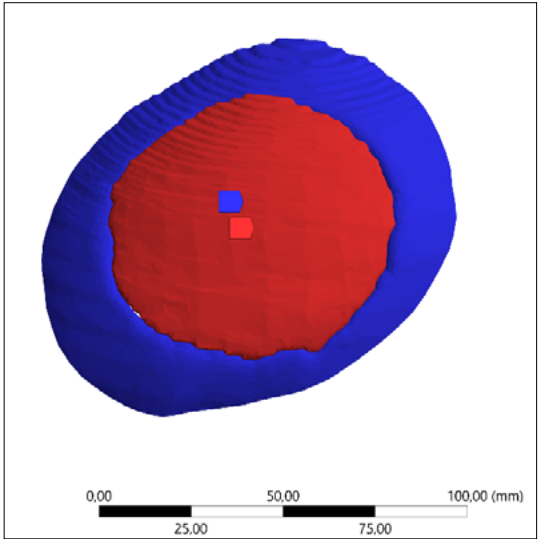


Fig. 6. Definition and visualization of the bonded contact condition in the Ansys software environment at the interlocking interface between the implant (highlighted in red) and the surrounding cranial section (highlighted in blue).

external force is applied perpendicular to the implant surface, directed inward toward the brain (Fig. 7).

3.3.3. Intracranial pressure

To simulate intracranial pressure (ICP) within the cranial cavity, a pressure load was applied to the interior surface of the implant. The selection was defined using the “Element Face” tool

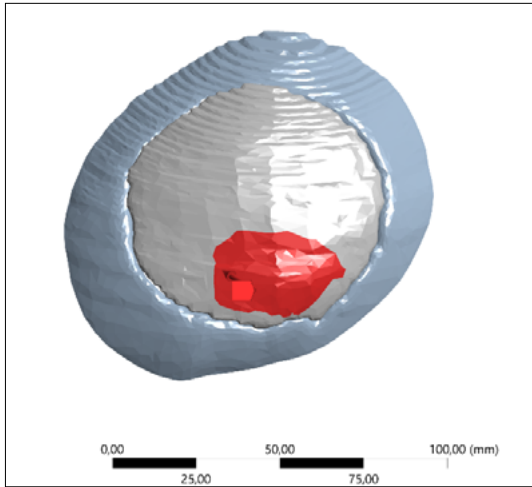


Fig. 7. The surface zone designated for the application of a 50 N load (highlighted in red) within the Ansys software environment, where the bottom metric scale (0.00–100.00mm) indicates the actual geometric extent of the loaded area.

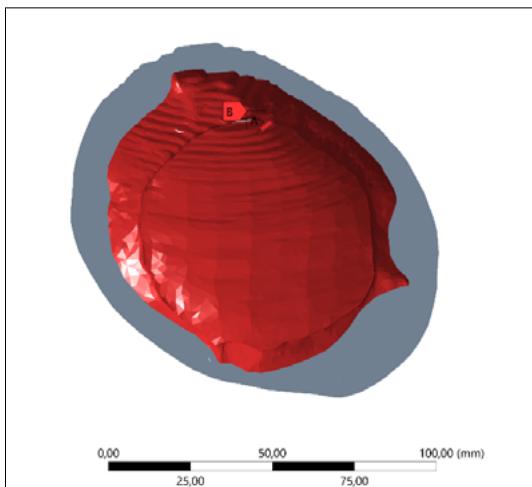


Fig. 8. Distribution of pressure on the internal surfaces of the skull and the implant, the regions highlighted in red indicate the loaded surfaces in Ansys.

and incrementally expanded using the “Extend” function until the specified “Limit” boundary was reached, ensuring the entire internal geometry was accurately loaded (Fig. 8).

3.3.4. Mesh

The Ansys student version has specific elements and cell limits depending on the physics type, with a 128k element/node limit for structural mechanics simulations [16].

Due to the importance of skull and implant geometry, instead of over-simplifying the model, the element size of the STL file was reduced before importing to Ansys and converting to a solid body (Fig. 9).

For meshing, “Program Controlled” mesh was used, with 12 mm size elements. Preliminary mesh sensitivity analysis indicated that tetrahedral elements provided the optimal configuration for this specific geometry. The resulting finite element model comprised of 46 460 elements and 82 942 nodes.

4. Results

During this simulation, Equivalent Stress (von Mises Stress) and Total Deformation were examined.

Equivalent Stress (von Mises Stress) tests whether the maximum stress developed in the implant and surrounding bone tissue exceeds the yield strength of the material.

The Equivalent Stress, most commonly calcu-

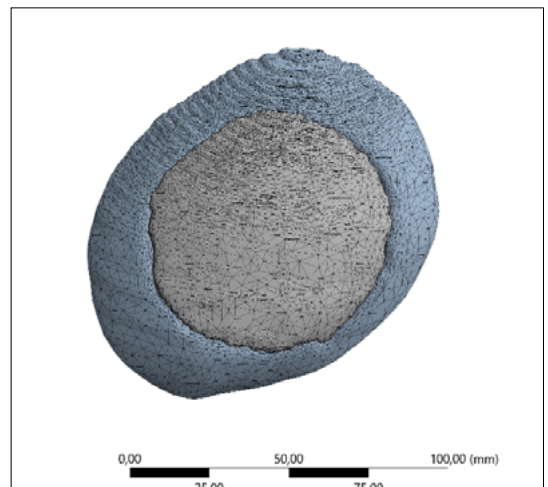


Fig. 9. Geometry after mesh generation.

lated as the von Mises Stress, is the single most critical scalar value used to describe the complex, multi-directional stress state in a skull implant and the surrounding bone tissue. It can predict the structural integrity and long-term stability of the implant during the design process.

If the calculated Von Mises stress at any point in the implant exceeds the material's Yield Strength, the implant is predicted to undergo permanent deformation or structural failure.

Total Deformation is the maximum movement or deformation of any point on the implant or the surrounding bone in all three directions (X, Y, and Z axes).

This value is crucial for assessing the implant's primary function: protecting the underlying brain tissue. A high total displacement means the implant is deflecting too much, which could lead to direct contact with or excessive pressure on the brain during an impact, risking neurological injury.

The interpretation of Total Deformation in Large Deflection (large deflection/geometric non-linearity) analysis in Ansys is radically different from the standard, linear (small deflection) analysis. Enabling the Large Deflection option (in Ansys Mechanical under Analysis Settings) initiates a nonlinear solution, which is crucial for the accuracy of the results when the magnitude of the deformations or the angle of rotations already change the stiffness of the structure and the path of the load [17]. During these simulations “Large Deflection” function was turned on.

Skull material loaded with 50 N is shown in Fig. 10 and 11. PEEK implant loaded with 50 N can be seen in Fig. 12 and 13, the titanium implant loaded with 50 N in Fig. 14 and 15. For every simulation and every material examined, the maximum value of the automatically determined equivalent stress resulted from a numerical error. Realistic maximum stress is marked manually with “Probe” marking.

The numerical results obtained under a static load of 50 N for the investigated models are summarized in Table 4.

Based on the distribution of total deformation, the titanium implant exhibited the highest stiffness, resulting in a maximum displacement of only 0.0017 mm. In contrast, the PEEK implant proved to be the most compliant, with a maximum deformation of 0.0218 mm, which is more than twelve times higher than that of titanium. The displacement of the cranial bone fell between the values of the two investigated materials,

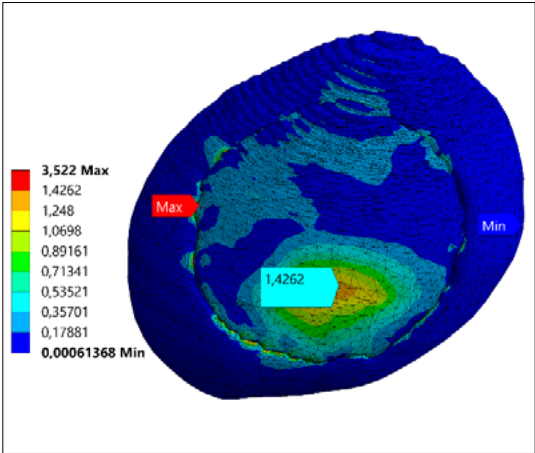


Fig. 10. Equivalent (von-Mises) Stress of skull material under 50 N force (MPa).

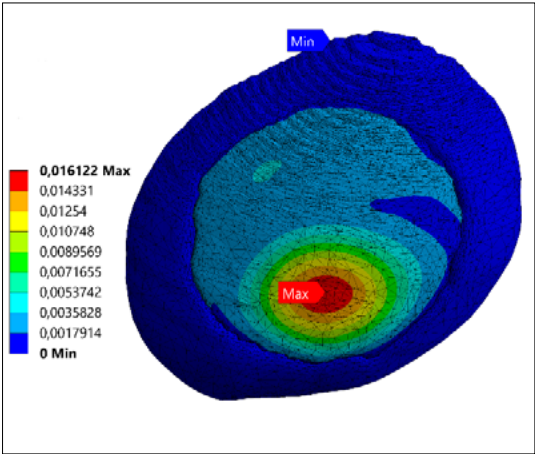


Fig. 11. Total Deformation of skull material under 50 N force (mm).

Table 4. Summary of simulation results for cranial implants made of different materials under 50 N static load

	Total Deformation (mm)			Equivalent Stress (MPa)		
	Skull	PEEK	Titanium	Skull	PEEK	Titanium
Minimum value	0	0	0	0.0006	0.0001	0.0002
Maximum value	0.0161	0.0218	0.0017	1.4262	1.5353	1.8394

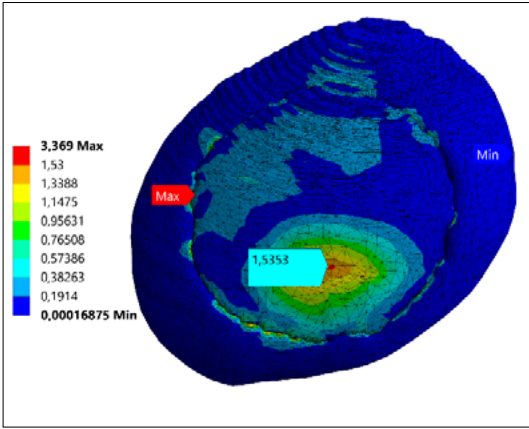


Fig. 12. Equivalent (von-Mises) Stress of PEEK implant under 50 N force (MPa).

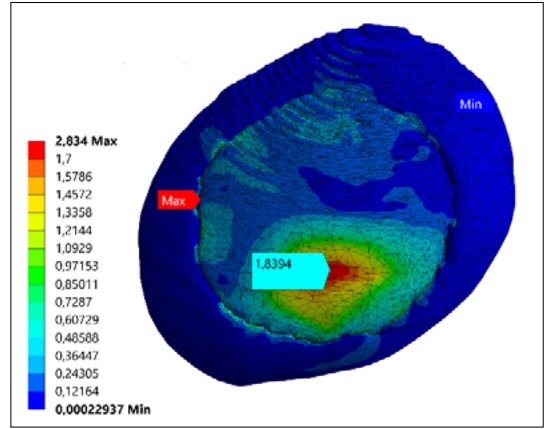


Fig. 14. Equivalent (von-Mises) Stress of Titanium implant under 50 N force (MPa).

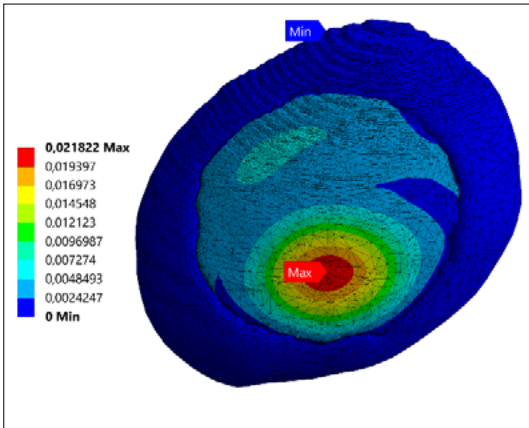


Fig. 13. Total Deformation of PEEK implant under 50 N force (mm).

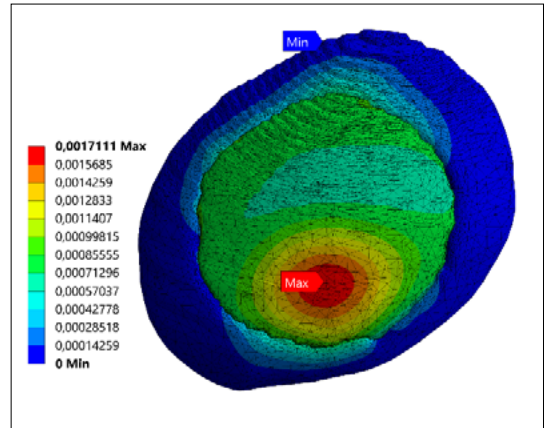


Fig. 15. Total Deformation of Titanium implant under 50 N force (mm).

showing a maximum deformation of 0.0161 mm. These results confirm that in clinical cases requiring minimal displacement and high-precision fitting, the application of titanium provides the highest structural stability.

Regarding the equivalent stress (von Mises stress) distribution in the Ansys environment, the highest mechanical stress developed within the titanium implant (1.8394 MPa), which is a direct consequence of the material's outstanding stiffness. Stiffer structures (undergoing smaller deformation) typically bear a higher share of the external load, potentially leading to elevated stress values. Conversely, the maximum stress levels in PEEK (1.5353 MPa) and the cranial bone (1.4262 MPa) were considerably lower. Since the induced mechanical stresses in all three structures inves-

tigated are remarkably low and remain far below the yield strength characteristic of the respective materials (e.g., the 100 MPa threshold for PEEK [5]), the risk of mechanical failure is practically negligible.

Consequently, the results indicate that all three materials operate well below their yield limits, therefore only elastic deformation occurs in the material, which disappears completely after the load is removed; it does not suffer permanent deformation.

5. Conclusions

The obtained numerical stress values clearly demonstrate that even in a sleeping position, the implant materials operate well within the safe

zone, far below their respective yield strengths. Since the applied load induces exclusively reversible, elastic deformation, this study confirms that under normal functional loading (simulated sleep), both materials are perfectly capable of safely substituting the missing cranial bone, as the resulting stresses remain significantly lower than the critical failure thresholds.

Based on the findings, the mechanical behaviour of the PEEK polymer is substantially closer to that of the natural cranial bone. The maximum displacement of 0.0218 mm exhibited by PEEK deviates only minimally from the 0.0161 mm value measured in the homogeneous bone model, whereas the highly rigid titanium implant behaves almost entirely as a rigid body with a displacement of 0.0017 mm. This indicates that the elasticity of PEEK is superior in mimicking the natural physiological movements of the human skull.

Considering the material properties and the simulation results, the clinical application of the titanium alloy is justified in cases where the primary objective is the absolute protection of the brain tissue combined with minimal implant displacement.

Conversely, PEEK offers a more optimal biomechanical alternative due to its bone-mimicking elasticity and more favorable load transfer characteristics. Future research should incorporate the simulation of fixation screws into the structural analysis and evaluate the geometry under higher load conditions to comprehensively map the progression of deformation and stress distribution across various mechanical demands.

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Extreme Loading Analysis of Subperiosteal Implants with Different Internal Structures

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Abstract

This study evaluates whether the applied fixation system used for testing subperiosteal implants provides reliable results under mechanical loading. The investigation considered the possibility that the fixation setup may fail earlier than the titanium implant itself. An idealized implant model with different internal structures was analyzed using finite element simulations and experimental tests combined with CT-based evaluation. Three loading cases were applied, including an extreme loading scenario introduced specifically to validate the fixation system. The results showed that no deformation occurred in either the implant or the fixation system under clinically relevant loading conditions. Failure was observed only under extreme loads significantly exceeding physiological bite forces. In such cases, the adhesive interface was identified as the weakest component, rather than the implant itself. These findings confirm that the applied fixation method is suitable for mechanical testing within the clinically relevant load range.

Keywords: custom implant, extreme loading, simulation, finite element analysis.

1. Introduction

Thanks to advances in modern 3D printing technologies and imaging techniques, patient-specific subperiosteal implant systems are experiencing a renaissance, and finite element analysis has become one of the most important tools for evaluating the mechanical behavior of these implants. For example, the study by Pellegrino et al. demonstrated that the placement and number of screws, as well as the geometric design of the implant, significantly influence the stress distribution in both the bone and the implant, particularly under forward loading conditions, where tensile stresses exceeding 50 MPa may occur in critical bone regions. The study also highlighted that the addition of posterior screws or the redesign of connections can reduce stress concentration and increase the long-term stability of the system [1].

Emerging material research directions, such as CFR-PEEK-based high-strength subperiosteal systems, are also gaining increasing attention. Etik and Yalcin investigated different implant

thicknesses and screw diameters using three-dimensional FEA modelling and found that a thicker implant (1.5 mm) reduces stress in the bone while simultaneously increasing the load on the implant and the metal structure. A larger screw diameter (2.0 mm) also reduced bone loading; however, it increased stress in the metal structural components, suggesting that careful balancing of implant geometry and material properties is necessary to achieve appropriate biomechanical performance [2].

Mechanical investigations of subperiosteal implants are not limited to virtual analyses; the work of Kulcsár and Zsoldos (2024) also presents a physical testing system that provides a comprehensive picture of the behavior of the implant–abutment system. In our research, a further developed version of this system was used. The combined application of experimental and numerical investigations is particularly valuable, as it enables the validation of numerical models, which is crucial from the perspective of clinical applicability [3].

In patients with healthy mandibular and maxillary bone structures, the replacement of damaged or completely missing teeth no longer represents a significant challenge. Owing to advances in modern medicine, it is now also possible to restore masticatory function in patients who have suffered severe bone loss due to trauma or disease, and for whom the use of endosseous implants is not feasible. In such cases, subperiosteal implants provide an alternative solution. Unlike endosseous implants, these devices are not inserted into the bone but are placed on its surface, beneath the periosteum.

Following implantation – provided that the surgical procedure is successful – the process of osseointegration begins, during which bone tissue gradually surrounds and stabilizes the titanium implant, ensuring its long-term mechanical and biological fixation.

In the present study, the mechanical behavior of subperiosteal implants is investigated under various loading conditions. The aim is to establish design guidelines that support the development of more reliable and efficient implant systems. Furthermore, while the ISO 14801 standard defines the dynamic fatigue testing methodology for endosseous dental implants, no such standardized procedure currently exists for subperiosteal systems. Therefore, another objective of this research is the development of a standardized testing method and experimental setup, which could improve the reliability and comparability of these implants in clinical applications.

These implants are designed based on CBCT scans using specialized 3D design software, such as exocad, by dental technicians and clinicians. Each implant is fully patient-specific, tailored to the individual anatomical and clinical requirements [4].

Fig. 1 presents the 3D CAD models of a maxillary and a mandibular subperiosteal implant, with the main structural components indicated.

Following the design phase, the manufacturing process consists of the following steps:

- Preparation of the model for powder bed fusion additive manufacturing
- Implant manufacturing by Laser Powder Bed Fusion (LPBF)
- Stress-relief heat treatment
- Aluminium oxide (Al_2O_3) grit blasting
- Post-processing of connection surfaces using a milling machine
- Removal of support structures used during 3D printing

- Manual post-processing
- Ultrasonic cleaning [6].

In the present study, custom-made subperiosteal implants produced using this workflow are investigated. In a previous publication, a reviewer raised the question of whether the applied fixation system – particularly the printed support structure and the adhesive interface – can withstand the applied loads, or whether it may fail earlier than the implant itself, thereby influencing the test results. This concern arises from the fact that the mechanical properties of the fixation materials are significantly lower than those of the titanium implant.

Our hypothesis is that the fixation system behaves adequately within the clinically relevant range of bite forces and can represent the mechanical role of the jawbone without failure.

2. Materials and methods

2.1. Idealized model and specimen mounting

Since subperiosteal implants are always patient-specific, the direct comparison of different implant designs is not always feasible. To overcome this limitation, an idealized model was developed to investigate the behavior of additively manufactured internal structures. During the design process, the professional experience of dental technicians from Dent-Art Technik Ltd. (Győr, Hungary), as well as dentists and oral surgeons, was considered.

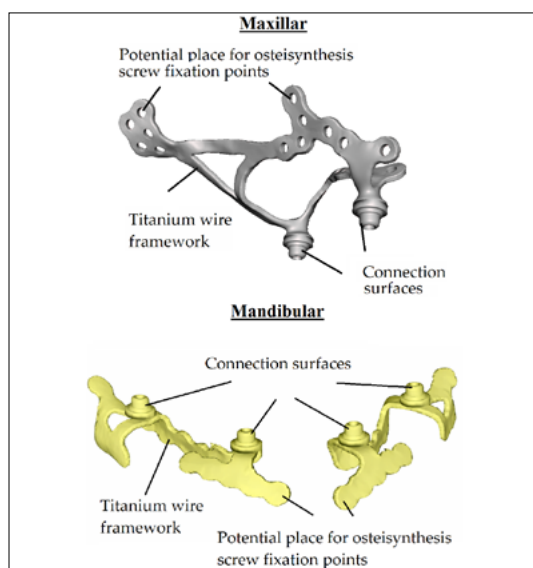


Fig. 1. Main components of maxillary and mandibular subperiosteal implant. [5]

Based on these considerations, the idealized model presented in Fig. 2 was created. The purpose of applying this model was to improve the reproducibility of the experiments and to ensure reliable comparability of the results.

The prepared implants were tested using a previously developed test rig capable of simulating the chewing process by means of pneumatic actuators and rotational motion [4].

To perform the experiments, a fixation specimen shown in Fig. 3. was designed; hereinafter, the term “specimen” refers to the implant mounted in the fixation structure. The figure clearly illustrates the fixation element as well as the location of the adhesive layer.

In the present study, four specimens with different internal structures were analyzed, ranging from a fully solid design to a significantly lightweight, lattice-like structure. The CAD models of the investigated specimens are shown in Fig. 4.

2.2. Materials and material models

The specimens were mounted into a mandibular model fabricated from Tough 2000 Resin V1 (Formlabs, Somerville, MA, USA). The implants were adhered to the mandibular model using a two-component Pattex Repair Universal epoxy adhesive (Henkel AG & Co. KGaA, Düsseldorf, Germany). After curing, the adhesive exhibited a tensile strength of approximately 130 kgf/cm² (≈ 13 MPa) at 24 °C, which was selected to approximate the mechanical constraint provided by osseointegration between the implant and the surrounding bone [7].

The main mechanical properties of the applied materials are summarized in Table 1.

Table 1. Material properties [8, 9, 10, 11]

Material properties		
Tulajdonság	Tough 2000	Ti6Al4V
Young's modulus	1.2 GPa	113 GPa
Poisson's ratio	0.35	0.342
Yield strength	Not modeled	790 MPa
Ultimate tensile strength	Not modeled	860 MPa
Elongation at break	Not modeled	15 %

2.3. Finite element analysis (FEA)

The increasing number of publications demonstrates that simulation methods are well suited for modelling intraoral processes and for the

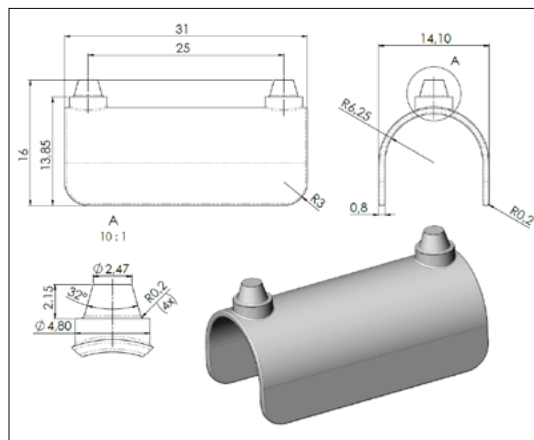


Fig. 2. 3D CAD model and technical drawing of the idealized subperiosteal implant. [5]

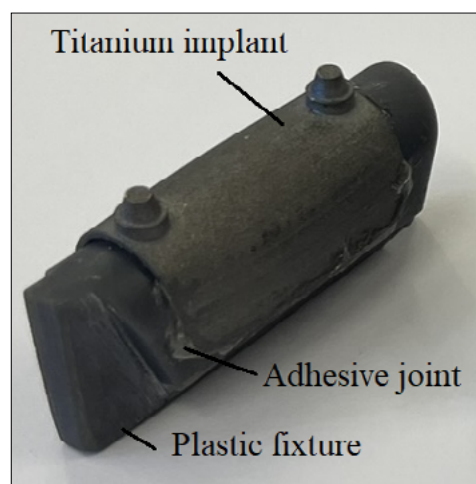


Fig. 3. Vest specimen before testing.

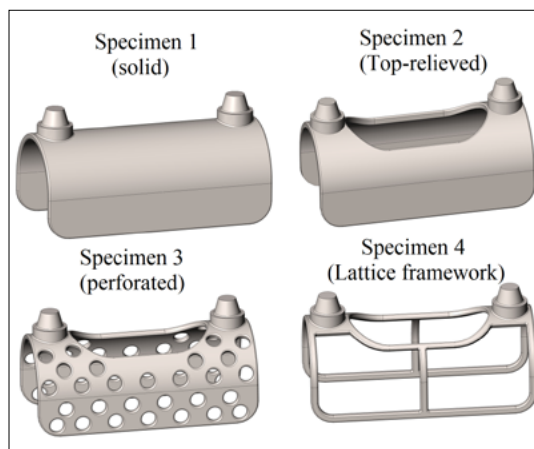


Fig. 4. CAD models of the investigated specimens.

design of dental implants. For the finite element analysis presented in this paper, a nonlinear static finite element model was developed in the Abaqus Standard software environment. The titanium implant material was modelled using a bilinear elastoplastic material model, while the mandible was modelled as a linear elastic material. The relationship between the elements was represented by a cohesive contact formulation with a normal stiffness of 15,000 N/mm and a tangential stiffness of 5,500 N/mm, corresponding to the properties of Pattex Repair Universal. Due to the lack of available material properties, damage of the adhesive was not included in the scope of the virtual investigation. The model setup is shown in Fig. 5.

The applied load was modeled as a uniformly distributed force acting on the fixation surface of the implant. As output parameters, displacement, stress distribution, and accumulated equivalent plastic strain were evaluated after the loading cycle(s). The finite element mesh consisted of second-order tetrahedral elements in all cases. The number of nodes and elements for each configuration were as follows: V1: 125,739 nodes and 74,496 elements; V2: 222,534 nodes and 138,465 elements; V3: 182,535 nodes and 108,671 elements; V4: 200,912 nodes and 120,909 elements.

2.4. Mechanical loading investigations

Three different loading tests were applied at different load levels during the investigations. The first two tests were conducted in our previous studies, and their detailed description can be found in our earlier publication [5].

In the third, extreme loading test introduced in the present study, the required high load could not be provided by our own testing apparatus; therefore, an INSTRON 5582 universal testing machine at Széchenyi István University was used. The experimental setup is shown in Fig. 6.

CT scans were acquired for each specimen before and after every test, and the datasets were compared to identify signs of permanent deformation in the titanium implant. The CT measurements were performed at Széchenyi István University (Győr, Hungary) using a Yxlon Modular CT system (YXLON International GmbH, Hamburg, Germany).

The acquired CT datasets were evaluated and compared using Volume Graphics StudioMax software (Volume Graphics GmbH, Heidelberg, Germany), enabling precise visualization and quantitative analysis of deformations.

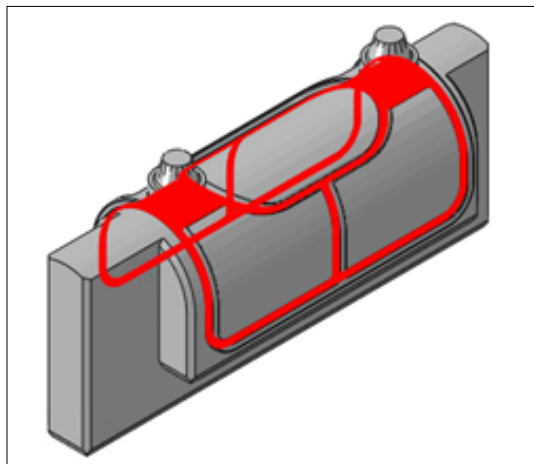


Fig. 5. Finite element model of Sample 4 highlighting the cohesive contact.

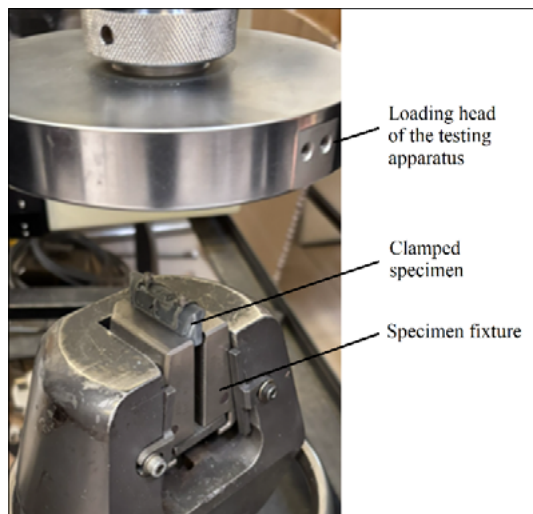


Fig. 6. Setup of the new test.

The first test was carried out on the previously developed test rig, applying 500 loading cycles with an approximate total load of 326 N and a loading duration of 2 s per cycle, thereby simulating normal mastication. The second test was also performed on the same test rig; however, a single loading event was applied with hydraulic assistance, where each specimen was subjected to a load of 2000 N for 10 s. The third, extreme loading test involved applying different load levels to each specimen – based on finite element predictions of plastic deformation – with a loading duration of 1 s.

The main parameters of the different tests are summarized in [Table 2](#), while the load values applied in the third test, derived from the finite element model, are presented in [Table 3](#).

3. Results

3.1. Simulation results

Finite element analysis is suitable for identifying critical cross-sections, highly stressed regions, and the occurrence of permanent deformation [Fig. 7](#) shows the stress distribution for the different structural configurations under the bite forces defined in [Table 3](#).

It can be observed that the critical regions are located at the base of the implant. It is important to note that the equivalent stress does not distinguish between compressive and tensile loading. In the present case, the stress is predominantly compressive, which is more favorable in terms of damage.

[Fig. 8](#) shows the accumulated plastic strain under a single loading condition.

Based on the simulation results, the presence of weight-reducing features decreases the bite force required to induce plastic deformation in the implant. It should be noted that the force level necessary to initiate plastic deformation is unrealistically high compared to physiological biting conditions. Furthermore, even at such elevated load levels, plastic material behaviour occurs only locally within the structure.

3.2 Mechanical and CT Investigation Results

The comparative results of the CBCT scans obtained before and after each test are presented in [Fig. 9-11](#).

[Fig. 9](#) shows the results of the first test, while [Fig. 10](#) presents the results of the second test, both of which were part of our previous study. [Fig. 11](#) illustrates the results of the third, extreme loading test introduced in the present work..

It is important to note that each state was compared to the preceding one; for example, the condition after the third test was compared to that after the second test, rather than to the initial geometry.

4. Discussion

Due to the characteristics of the experimental test setup, only loading configurations could be realized in which the applied force acted parallel to the axis of the implant connection cones.

Table 2. Testing parameters

Testing parameters			
No.	Number of bites (pcs)	Bite forces (N)	Bite duration (s)
1	500	326	2
2	1	2000	10
3	1	variable	1

Table 3. Bite forces in the third test

Testing parameters	
Specimen	Bite forces (N)
1	6000
2	5400
3	5200
4	4800

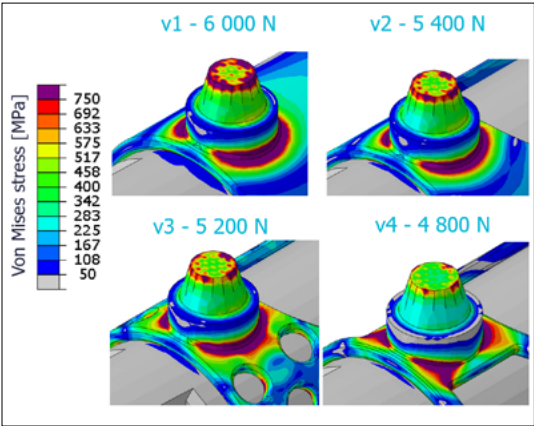


Fig. 7. Stress distribution.

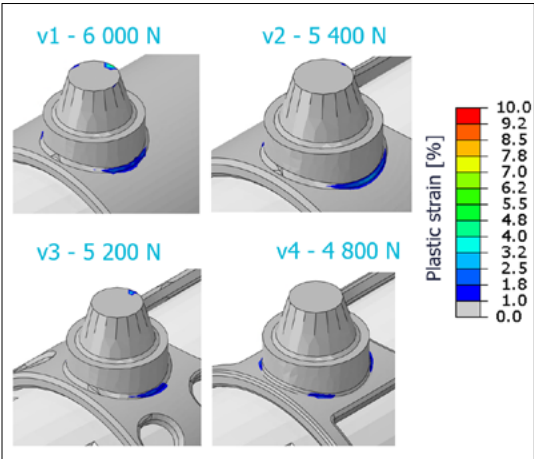


Fig. 8. Permanent deformation.

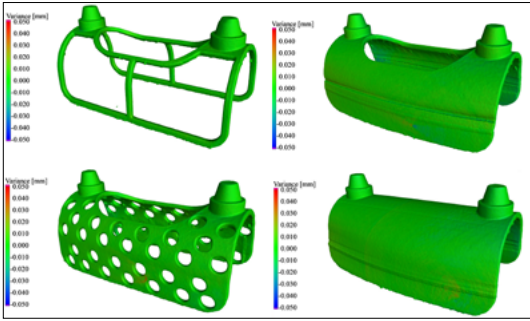


Fig. 9. Results of the comparative analysis of CT scans after the first test.

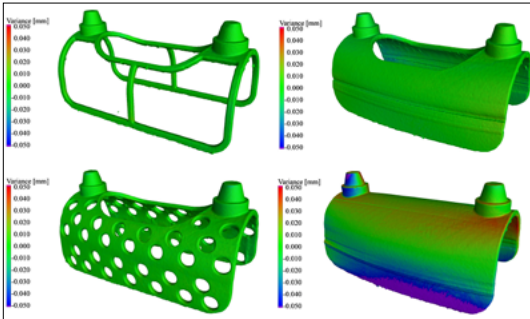


Fig. 10. Results of the comparative analysis of CT scans after the second test.

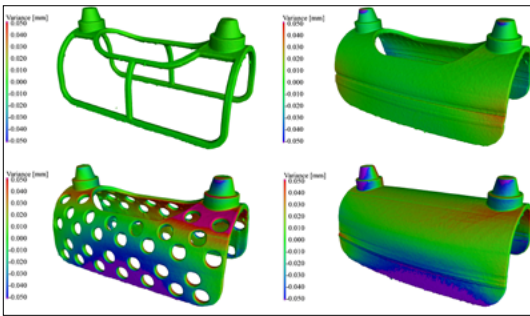


Fig. 11. Results of the comparative analysis of CT scans after the third test.

Based on the finite element analysis, the critical load levels were found to range between 4100 and 6000 N under vertical loading, and between 2000 and 3200 N under 30° loading conditions. The application of a 30° loading direction is a well-established approach in the investigation of endosseous implants; therefore, its consideration for subperiosteal implants is also justified [12].

The comparison of experimental and numerical results indicates that the implants exhibit damage

only under extreme loading conditions. All investigated specimens can be considered suitable for clinical application, as the internal structural design did not significantly influence the maximum load-bearing capacity.

The analyses revealed that the critical region consistently occurs at the junction between the mesh structure and the connection posts, which plays a key role in load transfer.

The CT-based investigations further refined the interpretation of the mechanical behavior. No detectable changes were observed in any specimen after the first test. Following the second test, the continuity of the adhesive interface was disrupted in specimen 4, allowing deformation of the titanium structure. During the third, extreme loading test, this phenomenon occurred in almost all specimens, except for specimen 1, which remained unchanged.

An important finding is that the weakest component of the experimental system was not the implant itself, but the adhesive interface. The Tough 2000 fixation structure and the adhesive joint failed only under significantly increased, clinically non-relevant overload conditions.

Overall, it can be concluded that the investigated subperiosteal implants are mechanically reliable within the clinically relevant loading range, and the presented methodology is suitable for the comparative evaluation of different structural designs.

5. Conclusion

Based on the results, it can be concluded that the mechanical behavior of subperiosteal implants is adequate within the clinically relevant loading range, and damage occurs only under extreme loading conditions. The different internal structures did not significantly influence the maximum load-bearing capacity; therefore, all investigated configurations can be considered suitable for clinical application.

The critical region was consistently located at the junction between the mesh structure and the connection posts. The experimental results showed that the weakest component of the system was not the implant itself, but the adhesive interface, which failed only under clinically non-relevant extreme loading conditions.

Overall, the presented methodology is suitable for the comparative investigation of different implant designs and can contribute to the development of more reliable subperiosteal implants.

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