

Article

Influence of Potting Radius on the Structural Performance and Failure Mechanism of Inserts in Sandwich Structures

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Abstract: In this study, the mechanical performance and failure modes of cold-potted inserts within sandwich structures were examined, focusing on the influence of the potting radius, while maintaining constant insert radius and specimen characteristics. In this research, destructive testing was used to evaluate the pull out, load-carrying capacity, and failure mechanisms of the inserts. The methods of stiffness degradation and acoustic emissions (AE) were employed for structural health monitoring to capture real-time data on failure progression, including core buckling, core rupture, and skin delamination. The results indicated that increasing the potting radius significantly altered the failure modes and critical failure load of the insert system. A critical potting radius was identified where maximum stiffness was achieved. Beyond this point, insert fracture became the dominant failure mode, with minimal damage to the surrounding core and CFRP skins. Larger potting radii also led to reduced displacement at failure, increased ultimate loads, and elevated stiffness, which were maintained until sudden structural failure. Through detailed isolation and observation of each failure event and with the use of AE data, precise identification of system damage in real time was allowed, offering insights into the progression and causes of failure.

Keywords: potting radius; cold potted insert; CFRP face sheet; honeycomb core; acoustic emissions



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1. Introduction

The increasing demand for lightweight, high-strength materials has positioned composite materials and sandwich structures at the forefront of material research in industries such as aerospace, high-performance automotive, marine, and wind energy [1–4]. These sectors prioritize materials that not only offer superior strength-to-weight ratios but also provide versatility, corrosion resistance, and the ability to withstand demanding environmental conditions.

In these applications, the connection of external components such as mounts, brackets, and fittings to composite structures is essential [2,5,6]; this is mainly achieved by adhesive bonding or mechanical inserts.

Unlike adhesive joints, which once cured cannot be disassembled without destruction, inserts provide a more flexible solution that allows for the disassembly and reassembly of attached components, facilitating easier maintenance, repair, and inspection throughout the lifecycle of the structure [7,8]. Furthermore, inserts provide superior resistance to environmental degradation, such as fluctuations in temperature, exposure to corrosive chemicals, and humidity, when compared to purely adhesive joints [9].

Their role as load-bearing elements within composite structures is paramount to the overall system reliability and safety, as a result, the failure of an insert can have cascading effects, potentially leading to the partial or complete failure of the attached components or even the entire system. Simulation of the mechanical performance of inserts is therefore critical and modelling approaches aiming at prediction of mechanical strength [10] rely mostly on numerical or mathematical methods to imitate the behavior of insert connections and optimize the topology, sizing, and geometry [6,11,12], as well as insert material type and core characteristics [13–16]. Furthermore, reliability analysis methods are efficient for calculating the probability of structural failure; however, these require a large number of simulations [17].

Structural health monitoring remains the most reliable method for acquiring reliable outcomes regarding the failure prognostics of the inserts. Modern engineering systems employ prognostics and health management (PHM) techniques to continuously monitor for signs of wear, damage, or impending failure [18,19]. Critical for the insert mechanical performance are the connections, structural design, and geometrical characteristics. The system elements such as aluminum inserts, epoxy adhesive, honeycomb core, and composite face sheets, as well as the geometrical characteristics (fully or partial potting, potting radius, insert diameter, etc.) are integral parts in the design of the bonded structure, defining a multi-parameter system, where complex modes of failure may occur [20,21]. Previous studies have highlighted the importance of geometrical features, such as potting radius, in influencing the pull-out performance of inserts [6,15], but no systematic investigation has focused on the impact of varying potting radius while keeping the insert radius constant.

In this study, the effect of different potting radii on the pull-out strength of cold-potted inserts is specifically explored, an approach not previously addressed in the literature. Unlike standard recommendations found in handbooks, which often suggest fixed potting radii relative to insert sizes, in this study, the potting radius is isolated as an independent variable, providing new insights into its role in enhancing the mechanical performance of bonded inserts in sandwich structures. The investigation is conducted with a fixed insert radius and uniform specimen characteristics, designed in accordance with typical potting radius specifications [12]. Destructive testing has been used to provide crucial insights into the failure behavior of the inserts under pullout loading conditions, offering a direct means to assess their load-carrying capacity and failure modes [5–12,21–23]. During the static testing process, continuous displacement measurements are performed to monitor stiffness degradation and acoustic emission signals are recorded using a microphone system. The latter provides an additional layer of diagnostics by capturing the microfractures and delamination events that occur as the material deforms or fails.

By analyzing the data, in this study, the aim is to provide experimental evidence on the failure initiation and propagation sequence of the insert system, taking into account the potting radius variation, and diagnose the damage evolution in the insert system by identifying the specific subsystems that are prone to failure. The testing process enables the identification of failure modes, such as core buckling and rupture, pull-out, or delamination [1,24–26], which are critical for understanding the limits of insert performance and how it varies for each different potting radius. Note that the potting radius (R_p) is defined as the average distance from the center of the insert to the nearest individual cell walls surrounding the potting, as seen in Figure 1.

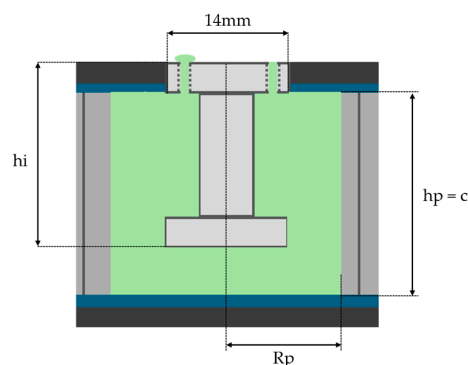


Figure 1. Potting geometry.

Based on the experimental findings, a dataset from the destructive tests that captures recurring patterns and signatures associated with specific failure modes is generated. By identifying these patterns, predictive models can be developed and refined for prognostic purposes. Such models can significantly improve the accuracy of failure prognosis in future applications involving cold-potted inserts, leading to enhanced safety, reduced maintenance costs, and extended component lifespan. Furthermore, any change in the system's mechanical response will be commented on and explained. This research contributes to the growing body of knowledge in composite structures, offering valuable insights into the durability and failure mechanisms of cold-potted inserts in critical structural applications.

2. Materials and Methods

2.1. Manufacturing Procedure

The manufacturing of composite sandwich structures and aluminum inserts is a critical step in ensuring the mechanical performance, durability, and reliability of the final assembly. In this section, the fabrication of the sandwich structure, the selection and design of aluminum inserts, the characteristics of the potting adhesive, and the overall assembly process are outlined.

2.1.1. Sandwich Structure Specimens

In the present study, the sandwich structure was fabricated using a perforated aluminum honeycomb core (I.MA.TEC. Al CR11 4.5-1/8-001N 5052), with a height of 20 mm, chosen for its favorable strength-to-weight ratio and resistance to corrosion. The face sheets were constructed using a 2×2 twill pre-preg carbon fiber fabric. A three-layer lamination was employed with fibers oriented at $0^\circ/90^\circ$, $\pm 45^\circ$, and $0^\circ/90^\circ$. This lamination approach imparts pseudo-isotropic mechanical properties to the face sheets, ensuring uniform strength in multiple directions, thereby reducing the risk of localized weaknesses in the structure. The core and face sheets were bonded together using an adhesive film designed to provide optimal load transfer between the materials. For the assembly of the panel, all components including the aluminum core, carbon fiber skins, and adhesive film were pre-cut to the final dimensions of the sandwich panel [21,27]. Careful alignment and assembly were essential for ensuring the correct lamination sequence and fiber orientation, as deviations could compromise the mechanical properties of the final structure. Given that pre-preg carbon fiber was used, the curing followed a standard autoclave curing cycle following the manufacturers' guidelines.

2.1.2. Aluminum Inserts

Aluminum alloy 6082 was chosen for its favorable manufacturing properties, including ease of machining and low mass, while fulfilling the necessary volumetric and structural

requirements of the inserts. The insert diameter was chosen to be 14 mm, incorporating a blind thread compatible with an M4 bolt. A height of 15 mm was selected for the inserts, following the guidelines provided in the European Cooperation for Space Standardization (ECSS) Handbook [28], which classifies the assembly as full potted, as per $h_p = c$ and $c \geq h_i \geq c - 7 \text{ mm}$, where h_p is the potting adhesive height, c is core height and h_i is insert height. This design enables a secure connection while allowing for sufficient flow of the potting compound around the insert, as seen in Figure 1. The fully potted configuration provides a balance between structural integrity, ease of installation, and lower weight compared to hot-potted inserts, which are often heavier [23,29].

2.1.3. Potting Adhesive Requirements

For potting adhesive, Loctite EA 9466 was selected due to its ability to meet all the critical requirements such as ambient temperature cure (cold potting) and minimal shrinkage during the curing process, as shrinkage could result in incomplete bonding between the core and insert, compromising the overall structural integrity. The adhesive also possesses low density to minimize additional weight while maintaining sufficient strength to prevent adhesive failure before aluminum core buckling occurs.

2.1.4. Assembly

The final stage of the manufacturing process involved the assembly of all subcomponents, focusing on the integration of the aluminum insert into the sandwich structure [20,30]. Initially, a hole matching the diameter of the insert was precisely cut into the sandwich panel (Figure 2a). This cutting process required careful control of the cutting speed to avoid generating excessive heat, which could degrade the epoxy matrix or weaken the surrounding composite material [7]. Once the hole was cut, the perimeter was undercut using a Dremel bur to match the final diameter required for the potting compound (Figure 2b). This step was crucial to ensure that the potting compound flowed evenly around the insert and formed a strong bond with the core material. After cutting, the area was cleaned to remove any remaining debris or contaminants, which could interfere with the bonding process. To further ensure a strong bond between the core and the insert, the insert itself was cleaned and degreased using Loctite SF 7840 cleaner–degreaser, which removes oils and residues that might compromise the adhesion. Once the potting cavity was prepared and the insert was cleaned, the potting adhesive was injected through the holes in the upper insert flange using a mixing gun equipped with a mixing nozzle (Figure 2c). One hole served as the injection point, while the other acted as a vent to allow air to escape during the adhesive filling process.

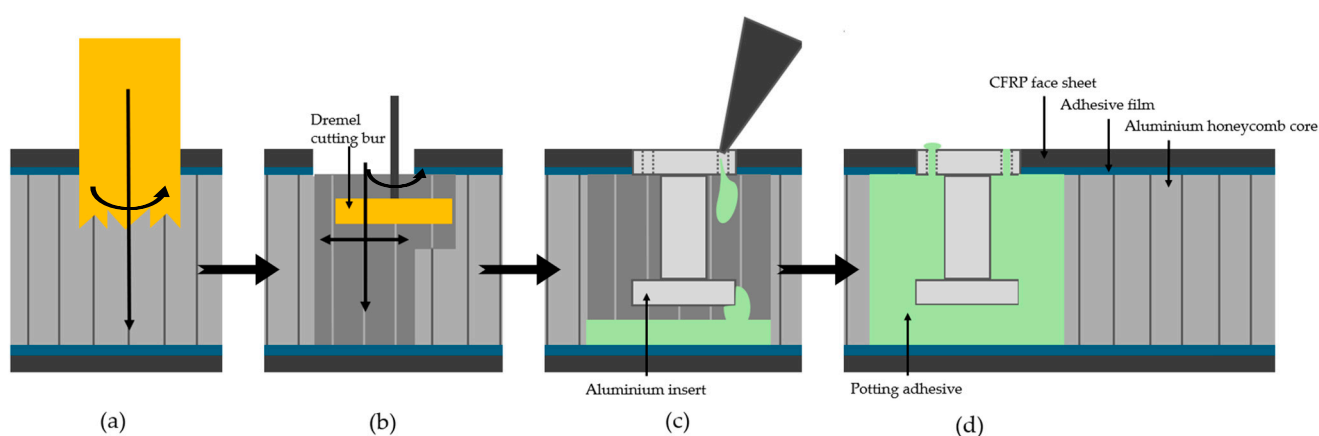


Figure 2. (a) Initial cut; (b) Undercutting; (c) Potting of the insert; (d) Final potted insert system.

2.2. Experimental Procedure

A total of 15 experiments were conducted, with three tests performed for each potting radius ($R_p = 8.6, 9.3, 10, 11, 12.5$ mm). These experiments involved out-of-plane tensile static strength tests, resulting in pull-out mode of the insert, and were conducted using the Instron 8801 machine of the Laboratory of Mechanics and Strength of Materials of the University of Thessaly. The test setup for out-of-plane loading, as illustrated in Figure 3, adheres to the guidelines outlined in the ECSS Space Engineering Insert Design Handbook [28]. The load was applied to the insert through a bolt secured within the insert's threads. Subsequently, the bolt was displaced via a connecting fixture with the Instron clamps, thereby transferring the load to the specimen. A constant displacement rate of 0.8 mm/min was employed, allowing for easier detection of amplitude changes via Acoustic Emissions (AE) monitoring.

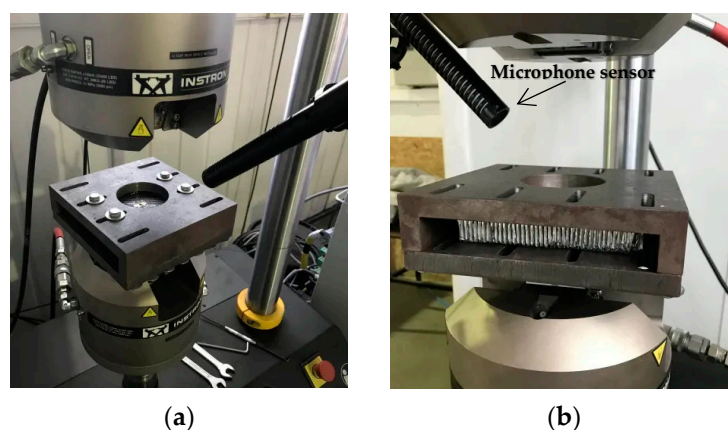


Figure 3. (a) Insert tensile out-of-plane loading fixture front view; (b) Test fixture side view.

The dimensions of the sandwich panels were selected based on the corresponding potting radius as per $e > 5R_p$, where e is the distance between insert center and sandwich panel edge, Figure 4, ensuring an appropriate edge distance to eliminate edge effects on the load-bearing capacity of the inserts, in accordance with ECSS standard.

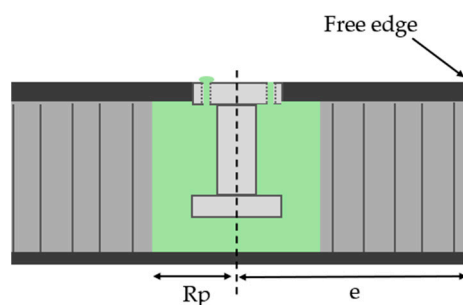


Figure 4. Distance (e) from free edge.

Acoustic emissions [31–34] were monitored using a microphone system which consisted of the Tascam DR-05 and RODE N3594. For this type of test, where failure (e.g., buckling or cracking) occurs within the audible frequency range, microphones can be used as an effective tool for failure detection [18]. It is important to note, however, that piezoelectric sensors typically offer higher sensitivity as they detect oscillations directly from the specimen, bypassing air transmission, and thus provide more accurate measurements of small changes [19].

For optical observation, photographs of the specimens were taken using a Canon EOS 750D camera. The specimens were sectioned using a Struers Labotom-5 machine, and stereoscopic observations of the cut sections were performed with a Leica Wild M3Z.

3. Experimental Results

In this section, the force–displacement behavior and acoustic emissions (AE) data are analyzed (Figure 5, Figures 8 and 11) and the stiffness evolution is evaluated based on displacement monitoring. Focus was paid to identifying, isolating and explaining failure mechanisms with respect to the acquired data. Confirmation of the findings is achieved through optical inspection of the specimens, including cross-sectional analysis after testing.

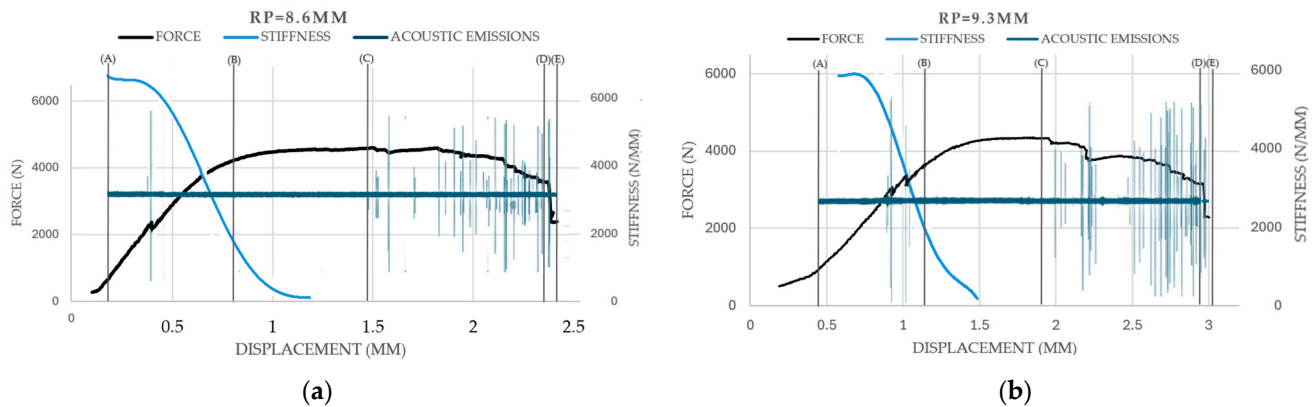


Figure 5. (a,b) Characteristic force, displacement and acoustic data diagrams for $R_p = 8.6$ mm and $R_p = 9.3$ mm, respectively.

From the acoustic emission data, changes on the force–displacement diagrams were observed and associated with specific failure modes that took place.

The potting radii of **8.6 mm** and **9.3 mm** are the minimum and typical recommended values, respectively, according to the ECSS handbook for the given composite sandwich structure and chosen insert radius. Both exhibit comparable behavior and will therefore be analyzed collectively. The force, displacement and acoustic data diagrams have been divided into distinct regions.

From the force displacement diagrams, it is distinguished in Figure 5a,b that in the **A–B** region, the behavior is linear in the first stages of loading followed by one or two force peaks indicated by local unloading, before non-linearity occurs in the force–displacement behavior. This is the stage where the honeycomb core undergoes loading and failure. Initially, stiffness has a constant value until the first force peak, after which it starts to decline due to core failure mechanisms caused initially by cell wall buckling and followed by membrane rupture. Beyond this point, the core can no longer sustain the applied loads, leading to a reduction in stiffness and the skins take over the primary load-bearing role. In the **B–C** region, the load is stabilized at a constant value, representing the residual load-bearing capacity of the skins, which are continuously loaded but have not yet failed. In the **C–D** region, a wave-like pattern emerges in the force data, signifying the initiation and propagation of microcracks within the skins. Finally, in the **D–E** region, complete structural failure occurs, marked by extensive cracking in the skins and resulting delamination.

From the acoustic emissions data shown in Figure 5a,b, the following observations can be made. In the **A–B** phase, only minor amplitude fluctuations are observed, except at points where the core undergoes buckling and rupture, as no other components of the system have yet incurred damage. The **B–C** phase shows no significant recordings, as this stage involves only the loading of the skins without failure. In the **C–D** phase, the majority of emissions are captured, corresponding to the propagation of microcracks within the CFRP skins, as described previously. Finally, in the **D–E** phase, a sharp rise in amplitude indicates the onset of complete specimen failure.

Optical examination of the failed specimens after completion of the tests revealed that the upper skin exhibited extrusion-type failure as shown in Figure 6c, while the lower skin showed signs of indentation, indicative of damage caused by local out-of-plane deformation that led to final delamination, depicted in Figure 6a,b. These observations are an indication of face sheet damage.

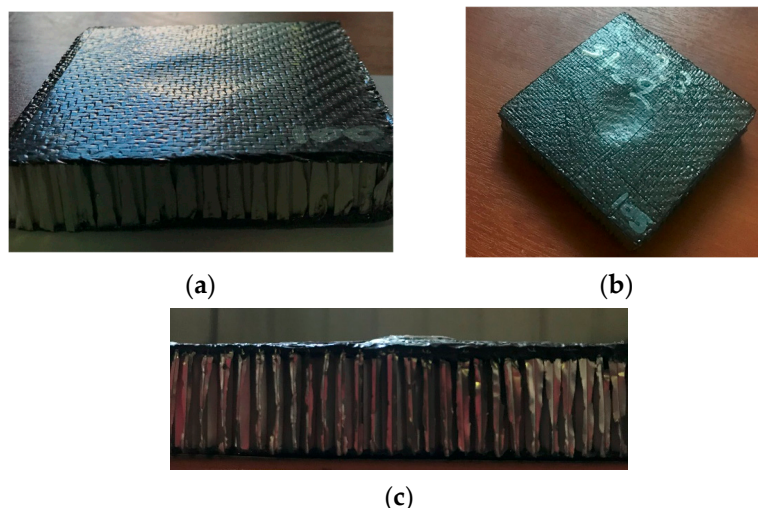


Figure 6. (a,b) Characteristic lower skin indentation for $R_p = 8.6$ mm and $R_p = 9.3$ mm, respectively; (c) Upper skin extrusion.

From Figure 7a–c, core buckling and core rupture (Figure 7d) are clearly visible, along with delamination of the lower skin, results that are in agreement with the signals obtained by the force, displacement and acoustic emissions data, confirming the above failure mechanisms.

For the potting radii of **10 mm** and higher, the values exceed those recommended by the ECSS Handbook standard, while the insert geometry remains constant.

From the force displacement diagrams in Figure 8, it can be distinguished that the **A–B** region exhibits similar characteristics to those observed with the smaller potting radii, including one or two force peaks followed by a decrease in stiffness. In the **B–C** region, the force stabilizes as the core's inability to bear loads shifts the load transfer to the skin. The **C–D** region represents the final failure phase. Notably, unlike in the smaller potting radius, the **B–C** region does not exhibit a load wave pattern but instead shows a sharp force drop to approximately 0 N. This distinct behavior beyond Point C suggests an alternative failure mechanism with regard to the previous cases, other than skin delamination.

The acoustic emissions data shown in Figure 8, display in the **A–B** region, similar activity to the smaller potting radii, with signals correlating to core buckling and core rupture at force peaks. In the **B–C** phase, there is initially no significant change in amplitude; however, toward the end of this phase, minor emissions are detected. This prompts the assumption that some minimal microcracking occurred before failure but not propagation and connection of them. The **C–D** phase shows a sudden increase in amplitude, indicating the onset of failure.

Optical investigation of the samples after failure showed minimal skin extrusion on the upper skin, as observed in Figure 9b,c. Also, on the lower skin, indentation took place confirming face sheet damage, seen in Figure 9a.

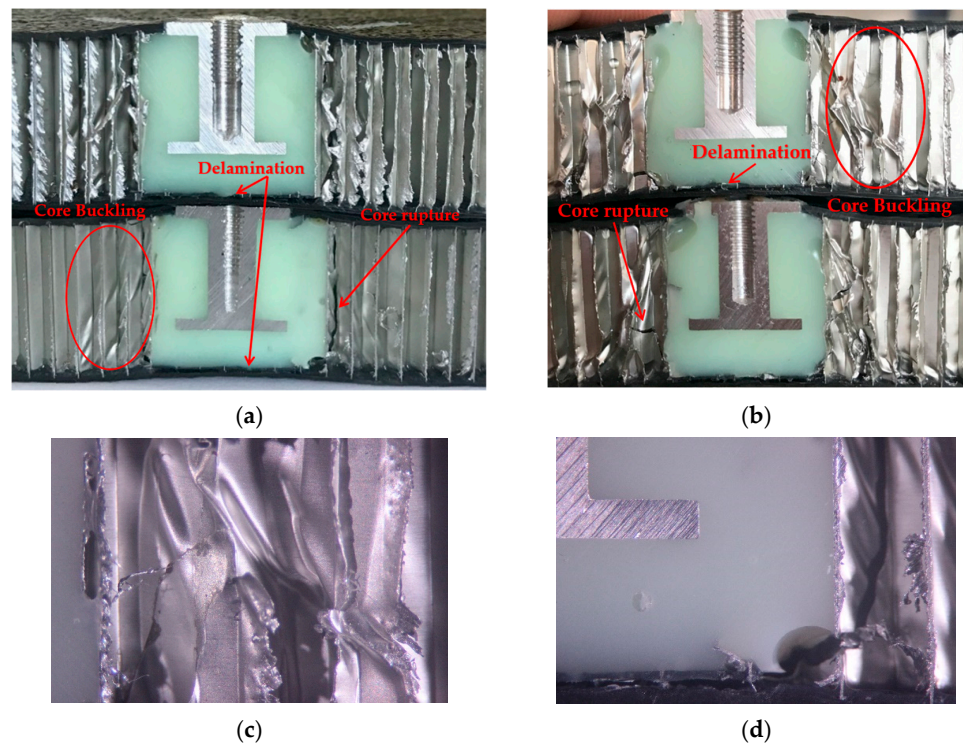


Figure 7. (a,b) Characteristic cross-sections for $R_p = 8.6$ mm and $R_p = 9.3$ mm, respectively; (c) Stereoscopic observation of core buckling; (d) Stereoscopic observation of shear rupture and skin delamination.

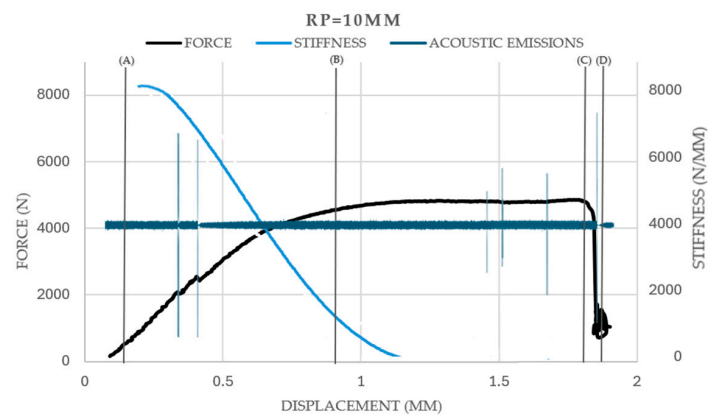


Figure 8. Characteristic force, displacement and acoustic data diagram for $R_p = 10$ mm.

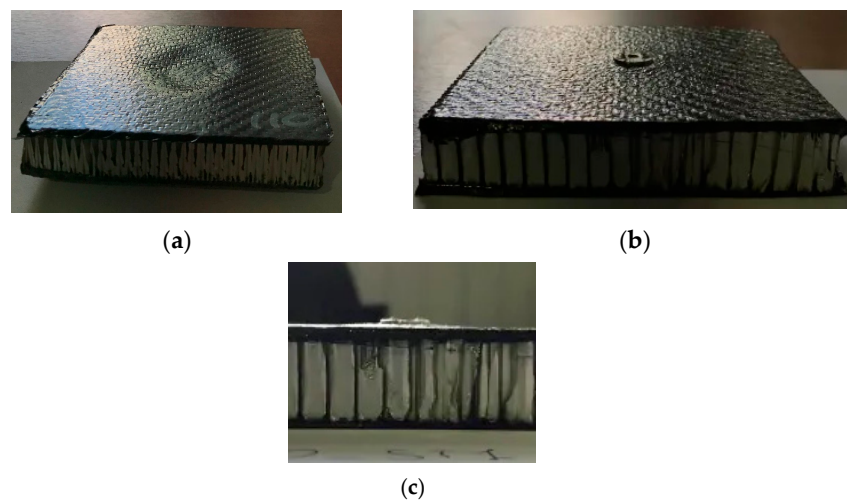


Figure 9. (a) Lower skin indentation; (b,c) Insert extrusion.

Core buckling and core rupture are seen in Figure 10a–c. The important observation that differentiates the failure mechanism compared to the smaller potting radius is that the insert's lower flange is cracked, allowing the insert to detach from the potting compound, as seen in Figure 10a,e. Finally, some lower skin curvature is observed but there are no signs of delamination, as seen in Figure 10d.

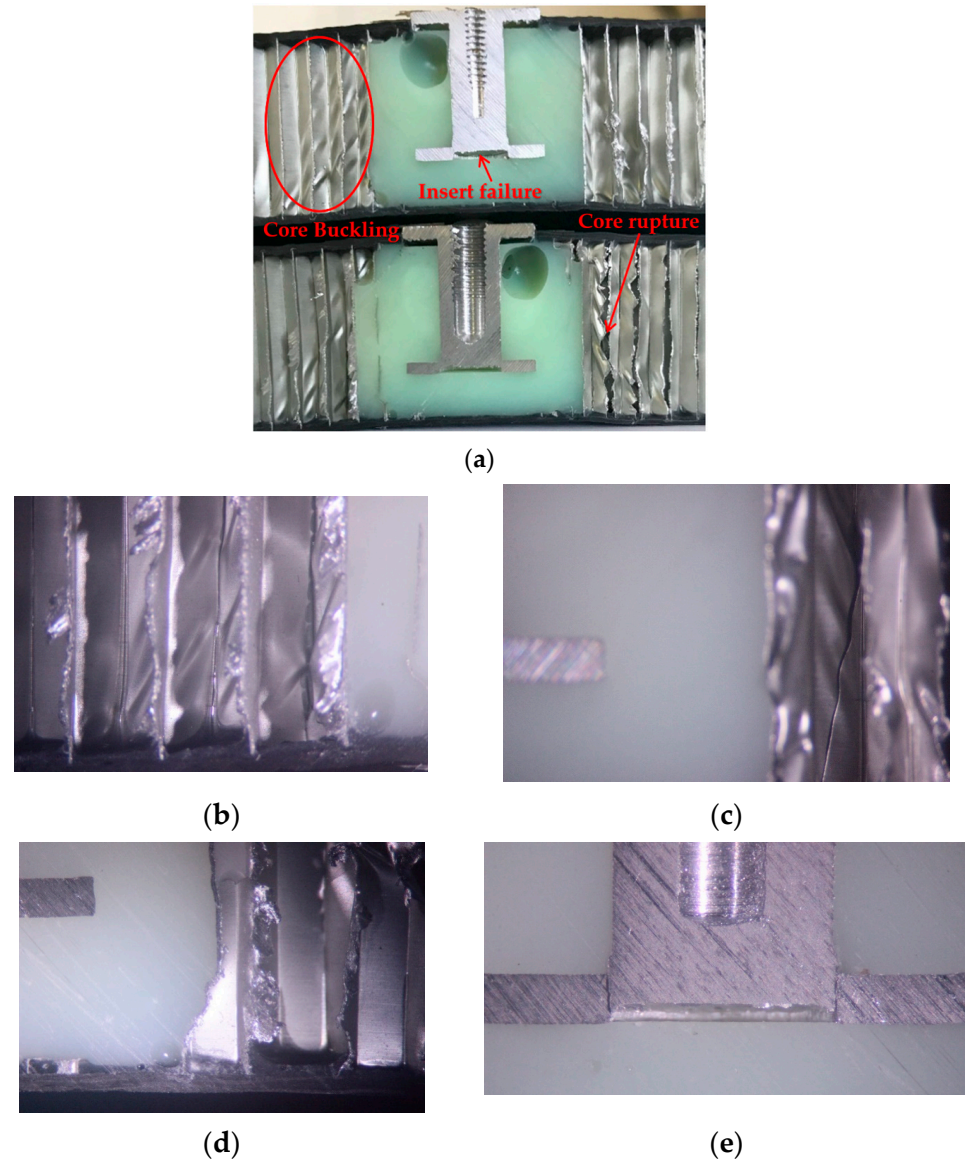


Figure 10. (a) Characteristic cross-sections of $R_p = 10$ mm; (b) Stereoscopic observation of core buckling; (c) Stereoscopic observation of shear rupture; (d) Absence of delamination; (e) Stereoscopic observation of insert lower flange failure.

From the experimental results, it is observed that with the increasing potting radius from 8.6 mm–9.3 mm to 10 mm, the failure mode has changed from skin microcracking and delamination to insert failure. Specifically, the cracking of the insert's lower flange becomes the dominant failure mechanism, occurring at approximately 5000 N.

For the larger radii of 11 mm and 12.5 mm, the mechanical response of the joint is illustrated in Figure 11a,b.

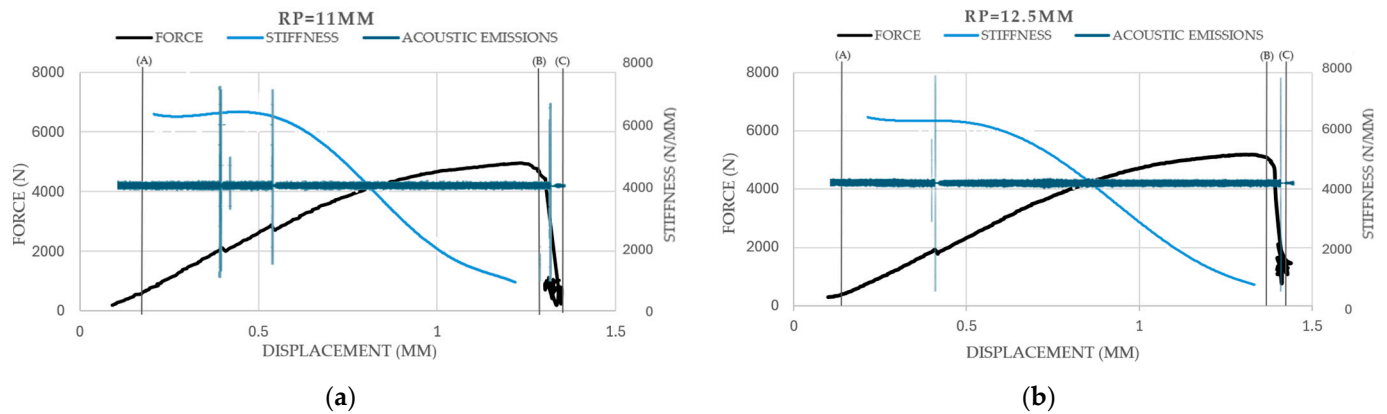


Figure 11. (a,b) Characteristic force, displacement and acoustic data diagrams for $R_p = 11$ mm and $R_p = 12.5$ mm, respectively.

From the force displacement diagrams, it can be distinguished in Figure 11a,b that in the A—B region, the behavior is predominantly linear until approximately 3000 N load, where onset non-linearity follows until the maximum load of 5200 N. The region B—C is the displacement range, where failure of the base flange occurs. Neither force wave pattern nor constant force was recorded, and this behavior which differs beyond Point B, suggests that the skins sustained minimal to no damage. Insert failure at the flange area seems the most favorable failure cause.

By evaluating the sensor data in Figure 11a,b, at the A—B area, acoustic emission spikes are detected only at force peaks; meanwhile, in the B—C region, no amplitude changes were recorded except at the instance of the insert's lower flange rupture. No indication of skin microcracking was detected by acoustic emission signals as in the case of the smaller diameter potting radii in Figure 5a,b and Figure 8.

On the upper skin of the specimen, no extrusion type failure was evident, while the insert protrudes from the skin surface in both radii, as seen in Figure 12c,d. Furthermore, on the lower skin, no indentation appeared, as seen in Figure 12a,b.

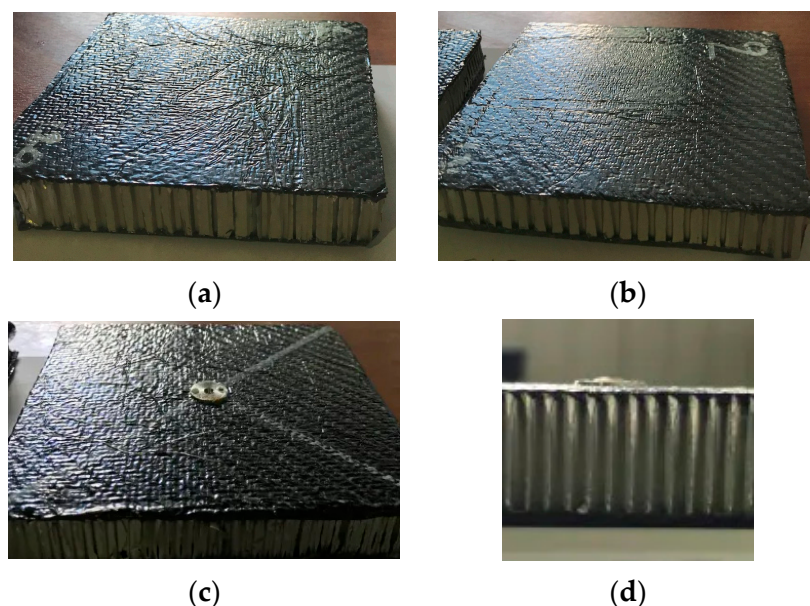


Figure 12. (a,b) Characteristic lower skin indentation absence for $R_p = 11$ mm and $R_p = 12.5$ mm, respectively; (c,d) Insert extrusion.

Buckling of cell walls is observed, but to a lesser extent compared to the joints with smaller potting radius and no rupture of honeycomb walls is visible. In addition, no appearance of skin delamination was experienced. Fracture of the insert at the base flange location is the main cause of catastrophic failure, as seen in Figure 13.

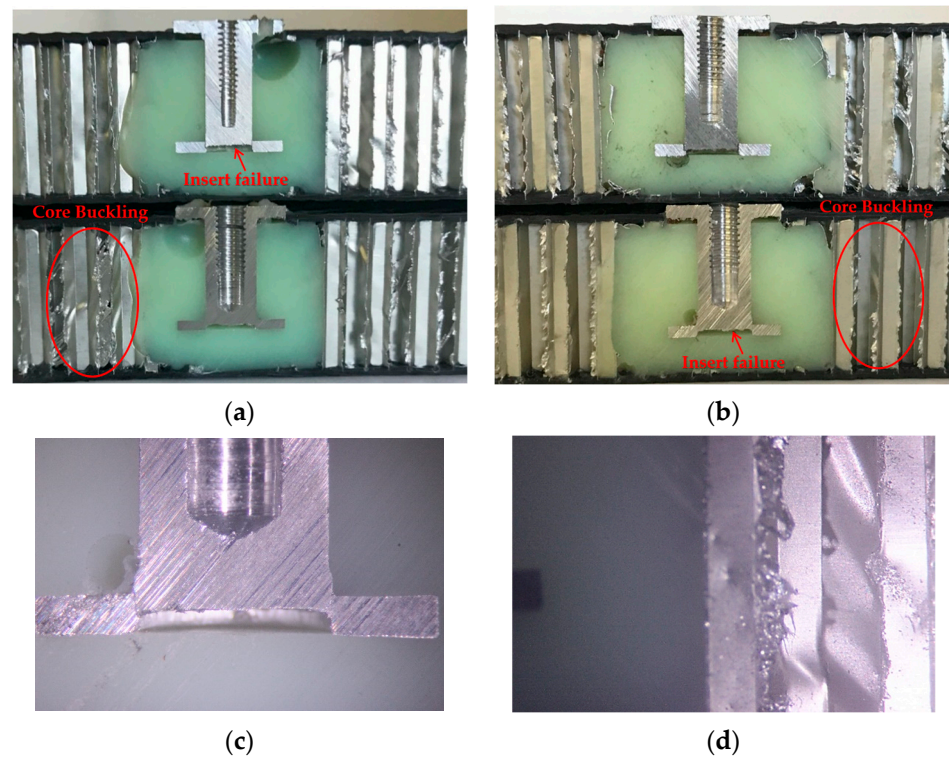


Figure 13. (a,b) Characteristic cross-sections for $R_p = 11$ mm and $R_p = 12.5$ mm, respectively; (c) Stereoscopic observation of insert lower flange failure; (d) Stereoscopic observation of core buckling.

The data acquisition and experimental observations suggest that honeycomb core damage was contained and no skin microcracking took place. Instead, specimens failed because of cracking of the insert's lower flange at a load level of 5000N.

4. Discussion

The measurements for the different potting radii taken as average value from three experiments are summarized in Table 1 and Figure 14.

Table 1. Average values of each potting radius.

R_p ¹ (mm)	F_p ² (N)	F_{max} ³ (N)	F_f ⁴ (N)	K_{max} ⁵ (N/mm)	D_p ⁶ (mm)	D_f ⁷ (mm)
8.6	2199.3	4626.5	3816.3	6398.9	0.584	2.94
9.3	2879.4	4381.6	3849.4	5987.9	0.737	2.895
10	2524.3	4955.7	4955.7	8337.2	0.439	2.16
11	2427.0	4914.5	4914.5	6683.3	0.474	1.33
12.5	2292.3	5104.7	5104.7	6411.6	0.442	1.33

¹ R_p = potting radius; ² F_p = peak load (core buckling, rupture); ³ F_{max} = maximum load reached; ⁴ F_f = failure load; ⁵ K_{max} = maximum stiffness (linear behavior); ⁶ D_p = displacement at peak load; ⁷ D_f = displacement at failure.

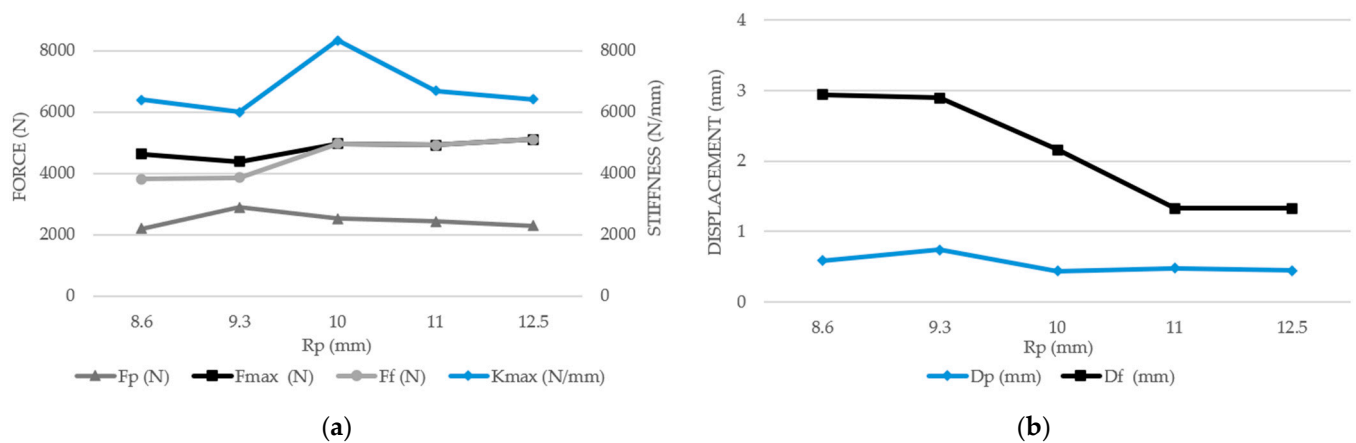


Figure 14. (a,b) Average experimental results for each potting radius (R_p).

The findings demonstrate that variation in the potting radius had no significant impact on the load at which core failure occurs (F_p) (via inelastic buckling and core final rupture), notably, the typical potting radius ($R_p = 9.3$ mm) achieves a higher peak load (F_p) compared to the other radii (see Figure 14a).

In contrast, it is evident that the maximum load (F_{max}) is influenced when examining larger radius values compared to the typical ones. The biggest difference from the typical potting radius ($R_p = 9.3$ mm) is obtained for the radius $R_p = 12.5$ mm, with a **16.5% increase** in maximum load.

Additionally, the failure load (F_f) of the joint is increasing with increasing potting radius. For small potting radii (8.6 mm–9.3 mm), the average failure load is in the range of 3840 N. With increasing radius, F_f increases, resulting in a failure mode which is characterized by shear rupture of the insert's lower flange. A comparison indicates a **32.6% increase** in F_f for $R_p = 12.5$ mm (Figure 14a) with regard to the typical radius ($R_p = 9.3$ mm). Note that for the radii of 10, 11, and 12.5 (mm), the failure load is equivalent to the maximum load.

Maximum stiffness (K_{max}) exhibits a slight decrease from $R_p = 8.6$ mm to $R_p = 9.3$ mm, followed by a marked **increase of 39.2%** at the intermediate value of potting radius ($R_p = 10$ mm) where K_{max} attains its peak value. For $R_p = 11$ mm and $R_p = 12.5$ mm, the stiffness slightly decreases but K_{max} remains higher compared to the first two radii (Figure 14a). The marked increase in stiffness at $R_p = 10$ mm can be attributed to the transition in failure mode, where both core-skin damage and insert flange rupture occur. It is plausible to assume that this enhances load redistribution and increases overall structural resistance before catastrophic failure. This transition is linked to changes in stress distribution around the insert. In comparison, smaller radii (8.6 mm and 9.3 mm) primarily experience core and skin failure, while larger radii (11 mm and 12.5 mm) result in insert flange rupture. The increased K_{max} at $R_p = 10$ mm reflects this shift in failure mechanisms.

The peak displacement (D_p) values, on the other hand, become smaller with increasing potting radius. The highest D_p is observed at a radius of $R_p = 9.3$ mm, while the lowest occurs at $R_p = 10$ mm. Between these two cases, a displacement **decrease of 40.4%** is observed. For the larger radii, D_p has a relatively stable value (Figure 14b). It is interesting to highlight that while the peak load (F_p) remains relatively constant across cases, the peak displacement (D_p) exhibits noticeable variations, leading to the associated variations in the system's stiffness. The increase in stiffness is beneficial in applications where system connection requires specific rigidity as a critical requirement. Additionally, increasing the potting radius alone may serve as an effective method for locally enhancing the stiffness of the assembly.

Displacement at failure (D_f) provides valuable data for structural health monitoring before total failure. The D_f parameter exhibits large differences across cases, the first two radii show comparable displacement values, followed by a decrease at $R_p = 10$ mm marking a transitional failure mechanism as outlined earlier in this study. The value then reaches its lowest values for the radii of $R_p = 11$ mm and $R_p = 12.5$ mm. Specifically, at $R_p = 10$ mm D_f shows a **25.4% decrease**, and at $R_p = 12.5$ mm, a **54.1% decrease**, relative to the typical potting radius ($R_p = 9.3$ mm). This behavior is attributed to differences in failure mechanisms, the specimens exhibiting extensive honeycomb core damage yield a higher D_f . Conversely, for radii where the predominant failure mode is that of insert lower flange rupture, accompanied by minimal core damage, a lower D_f is observed.

The optical observations, conducted after fracture of the specimens by mechanical testing, demonstrated that the potting diameter has a distinct effect on the failure mechanism of the joint. Failure in joints with smaller diameter (8.6 mm and 9.3 mm) is triggered initially by core inelastic buckling and followed by rupture of cell walls located at the vicinity of the adhesive interface. As previously mentioned, these phenomena occur at the peak load forces in the linear region. Additionally, acoustic sensing data indicate that core failure is evident from the first sharp and isolated emissions recorded at the start of the loading. Subsequent skin cracking is observed superseded by skin delamination in the final stages of damage, due to the load redistribution after core failure resulting in increased stresses of the outer skin. Initially, the residual loading of the skin is associated with the constant load reached, with no acoustic emissions detected. However, as microcracking in the skin propagates, it is reflected in the force wave pattern which is subsequently accompanied by continuous acoustic emissions. With the increasing diameter, a shift in final failure mode from the composite skin to the insert flange is observed, while the honeycomb core material suffers less pronounced damage with regard to the samples with a smaller potting area. As the bending stresses of the base insert flange are minimal due to the cohesion and rigidity of adhesion material, fracture occurs due to excessive shear stresses at the basis of the insert and the fracture location is the stress concentration area defined by the corner where the flange (basis) of the insert meets the insert column. Provided that there is an adequate fillet radius of the lower flange connection, the ultimate bearing loads for the larger potting radii could reach larger values. Consequently, more extensive damage to both the core and face sheets may occur at higher applied forces, prior to insert fracture. Future research could investigate the impact of incorporating such fillet radii, focusing on the mechanical properties and failure mechanisms associated with larger potting radii.

5. Conclusions

The design characteristics of insert joints in sandwich composite structure connections are of primary importance to meet the requirements for superior joint strength. In the present study, the findings indicate that with a constant insert radius and consistent specimen characteristics, increasing the potting radius has significant effect on the strength, failure mode, and mechanism of the joint. Specifically, increasing the potting diameter led to an overall increase in the maximum loads reached by the insert system, while decreasing the displacement at maximum pull-out load. Also, for larger radii, the system experienced a marked increase in maximum stiffness. Furthermore, the failure mechanism changes from core failure under inelastic buckling due to the shear deformation of honeycomb cell walls and subsequent skin failure to insert flange fracture. This is the result of different load distributions due to the larger adhesion area (radius) resulting in shear stress relief of the honeycomb core adjacent to the potted adhesive and an increase in the shear stresses at the insert flange. Throughout the static testing, each failure event was systematically isolated, analyzed, and observed using diagnostic methods such as acoustic emissions (AE) and stiff-

ness degradation monitoring. The monitored data could reliably identify the specific failure events and mechanisms as they occurred. Changes in AE amplitude provided insight into the nature and sequence of failures, allowing for the prediction of failure modes and causes. As a result, each variation in the distinct areas of the force-displacement (F-D), stiffness, and acoustic emission (AE) diagrams have been elucidated, correlating them with the corresponding failure modes and load conditions. This analysis enhances our understanding of the system's behavior, facilitating improved health monitoring and prognostics.

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