

**THE EFFECT OF PROXIMITY BETWEEN
BARELY VISIBLE IMPACT DAMAGE SITES IN
METALLIC HONEYCOMB SANDWICH PANELS**

**L'EFFET DE LA PROXIMITÉ ENTRE LES
SITES DE DOMMAGES D'IMPACT À PEINE
VISIBLES DANS LES PANNEAUX SANDWICH
MÉTALLIQUES EN NID D'ABEILLE**

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by

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Dla Mamy i taty: Twoja miłość jest fundamentem wszystkiego, co osiągnąłem.

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Abstract

Aluminum honeycomb sandwich panels are commonplace in modern aerostructures, offering a high stiffness-to-weight ratio. However, they are susceptible to impact damage due to their poor out-of-plane deformation resistance. Current literature shows that low-velocity impacts, such as hail, tool drops during maintenance, or debris on the tarmac are difficult to detect and result in a reduction in in-plane residual panel strength. Impact sites can occur close to one another. Researchers have addressed the proximity of multiple impact sites in the panel's primary loading direction, but the effects of impact sites oriented perpendicular to this direction have not yet been investigated. This thesis presents a method for predicting the residual strength of a panel in compression using finite element modelling calibrated through experimental testing and digital image correlation. The numerical model is divided into two distinct phases. Initially, the dynamic impact generates two localized damage sites, which is followed by an in-plane compression of the panel until it structurally fails. The numerical model and the experimental results are compared based on dent characteristics, overall panel behaviour, full-field deformation and strain, and peak force. The panel's residual strength was found to be primarily dependent on the width of the damage region. The residual strength exhibited three characteristic behaviours governed by the separation distance between damage sites, with a maximum reduction of 9% in the peak force compared with a panel that experienced a single indentation. The results also show that once two damage sites are separated and create two individual dents, they act independently and do not influence one another.

Keywords: Honeycomb, Sandwich panel, Finite element modeling, Digital image correlation, Low-velocity impact

Résumé

Les panneaux sandwich en nid d'abeille en aluminium sont couramment utilisés dans les structures aéronautiques modernes, car ils offrent un rapport rigidité/poids élevé. Cependant, ils sont sensibles aux dommages causés par les chocs en raison de leur faible résistance à la déformation hors plan. La littérature actuelle montre que les chocs à faible vitesse, tels que la grêle, la chute d'outils pendant la maintenance ou les débris sur le tarmac, sont difficiles à détecter et entraînent une réduction de la résistance résiduelle des panneaux dans le plan. Les points d'impact peuvent être proches les uns des autres. Les chercheurs se sont intéressés à la proximité de plusieurs points d'impact dans la direction de charge principale du panneau, mais les effets des points d'impact orientés perpendiculairement à cette direction n'ont pas encore été étudiés. Les travaux actuels présentent une méthode de prédiction de la résistance résiduelle d'un panneau en compression à l'aide d'une modélisation par éléments finis calibrée par des essais expérimentaux et la corrélation d'images numériques. Le modèle numérique divise l'essai de compression post-impact en deux phases distinctes. Au départ, l'impact dynamique génère deux sites de dommages localisés, suivis d'une compression du panneau jusqu'à sa rupture structurelle. Le modèle numérique et les résultats des essais expérimentaux sont comparés sur la base des caractéristiques des bosses, du comportement global du panneau, de la déformation et de la contrainte sur l'ensemble du champ, ainsi que de la force maximale. Il a été constaté que la résistance résiduelle du panneau dépendait principalement de la largeur effective de la bosse. La résistance résiduelle présentait trois comportements caractéristiques, en fonction de la séparation des zones endommagées, avec une réduction maximale de 9% de la force maximale par rapport à un panneau ayant subi une seule indentation. Les résultats montrent également qu'une fois que deux zones endommagées sont séparées et créent deux bosses, elles agissent indépendamment l'une de l'autre.

Mots-clés : Nid d'abeille, Panneau sandwich, Modélisation par éléments finis, Corrélation d'images numériques, Impact à faible vitesse

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Nomenclature

A	Area	$[mm^2]$
A_{CS}	Cross Sectional Area	$[mm^2]$
d	Dent Spacing	$[mm]$
E	Young's Modulus	$[GPa]$
e	Energy	$[J]$
d	Center-to-center spacing	$[mm]$
L	Length	$[mm]$
m	Mass	$[kg]$
R	Fillet Radius	$[mm]$
T	Time	$[s]$
t	Thickness	$[mm]$
v	Velocity	$[m/s]$
w	Width	$[mm]$

Subscripts

a	Adhesive
E	Engineering
e	Equivalent
f	Fillet
ms	Multi-Indentation Site
ss	Single-Indentation Site
T	True
UCS	Ultimate Compression Strength
VM	von-Mises

w	Web
ys	Yield Strength
y, c	Yield Strength in Compression
y, t	Yield Strength in Tension

Greek Symbols

ϵ	Strain [mm/mm]
$\epsilon_1, \epsilon_2, \epsilon_3$	Principal Strains [mm/mm]
γ	Shear Strain [mm/mm]
ρ	Density [kg/m ³]
σ	Stress [MPa]
ε	Strength [kN]
ν	Poisson's Ratio []

Abbreviations

ADL	Allowable Damage Limit
BVID	Barely Visible Impact Damage
C-C	Center-to-Center
CAF	Canadian Armed Forces
CAI	Compression After Impact
CDT	Critical Damage Threshold
DAQ	Data Acquisition
DIC	Digital Image Coorelation
FE	Finite Element
FEM	Finite Element Method
NDI	Non-Destructive Inspection
OEM	Original Equipment Manufacturer
RMC	Royal Military College of Canada
SRM	Structural Repair Manual

1 Introduction

The composite sandwich panel emerged in the mid-20th century as a form of a structural composite designed to achieve a high stiffness-to-weight ratio through the use of two facesheets adhered to a low-density core material [1, 2]. The facesheets provide the high tensile and compression strength, while the core material maintains the facesheets' separation, preventing bending [3].

Aerostructural designers are challenged to design composite sandwich panels that meet the application's technical requirements. Aeronautical applications of sandwich panels typically have facesheets that are metallic or fibre-reinforced polymer, and core structures that are made of foams or bonded periodic structures, such as honeycomb structures. The components of metallic honeycomb panels are shown in Figure 1.1, where two metallic facesheets are bonded to the metallic honeycomb core through the use of an adhesive.

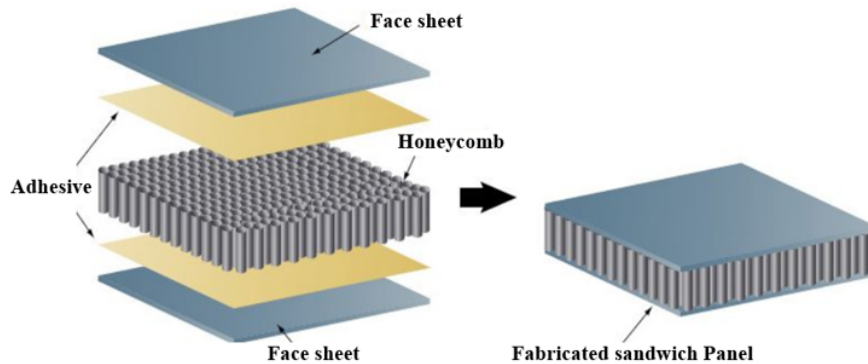


Figure 1.1: Typical composition of a metallic honeycomb panel containing a honeycomb core bonded to two thin facesheets [4].

Despite their high stiffness-to-weight ratio, aluminum honeycomb panels have poor out-of-plane impact resistance. Localized impacts cause the core to crumple, resulting in permanent damage to the core and facesheet in the form of a dent. Dents are often caused by tool drops, ground debris, or weather events such as hail. Low-velocity impacts can cause shallow damage called Barely Visible Impact Damage (BVID). Even barely detectable damage, such as BVID, may lead to a reduction in the residual strength of the panel [5].

Aluminum honeycomb panels are used throughout aircraft for components such as floor boards, bulkheads, and control surfaces, as shown in Figure 1.2. Figure 1.3 shows a CH-146 Griffon operated by the Canadian Armed Forces (CAF) that uses aluminum honeycomb panels in the tail boom. For each aircraft, Structural Repair Manuals (SRMs) are issued by

the Original Equipment Manufacturers (OEMs) to provide guidelines for repairing damage. SRMs first define the location of the damage, followed by the damage relative to its specified damage limits (size, depth, and type). A course of action is then recommended to correct the damage, and if necessary, detail the repair. Figure 1.4 shows an example of a panel from a Griffon helicopter that necessitated replacement due to the extensive BVID damage.

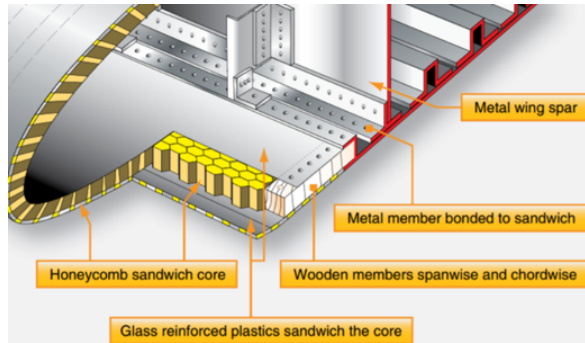


Figure 1.2: Structural application of a honeycomb panel in a control surface [6].



Figure 1.3: CH-146 Griffon helicopter operated by the Royal Canadian Air Force [7].

1.1 Motivation

SRMs define damage limits for impact damage using measurements of the dents, such as dent diameter, dent depth, dent location, and proximity of one damage site to another damage site. If the measured damage is below the Allowable Damage Limits (ADLs), the panel may remain in service. Beyond these limits, repairs or replacements are required. The data driving these limits is restricted as intellectual property of the OEMs. Organizations such as the CAF, which operate, among other aircraft, the CH-146 Griffon, seek a better understanding of the damage limits to evaluate the honeycomb sandwich panels' level of conservatism. Premature repair or replacement of these panels is both costly and can reduce aircraft readiness.



Figure 1.4: An aluminum honeycomb panel with multiple low-velocity impact damage sites that was retired from a CH-146 Griffon helicopter.

In-plane compression tests are often used to represent the critical load case for dented panels. With the support of the Department of National Defence, the Royal Military College of Canada (RMC) research group is working to understand and predict the in-plane Compression After Impact (CAI) residual strength of dented aluminum honeycomb panels. The dent characteristic of concern that will be explored in this thesis is the spacing between dents perpendicular to the loading direction.

1.2 Goal

The goal of this thesis is to determine the effect of two BVID dents at different distances apart, on the residual strength of aluminum honeycomb panels subject to in-plane compression. The dents are spaced perpendicular to the panel's loading direction. This thesis presents Finite Element (FE) model predictions and experimental results, including the overall panel stress-strain relationship, dent characteristics, full-field deformation, strain from 3D digital image correlation, and residual panel strength.

1.3 Approach

The experimental testing and numerical simulations followed the CAI test protocol outlined by ASTM standards. Experimentally, the multi-dent configurations are created using a drop tower system, then the panel is compressed in the in-plane direction until failure. Panel failure is defined as the point at which the load-carrying capacity of a panel begins to decrease. The panel failure and surface strains are captured using a 3D Digital Image Correlation (DIC) system. Figure 1.5 shows the center-to-center spacing (d) that is used throughout this thesis to define the proximity of the dents. Experimental results from CAI tests are highly variable and require multiple repetitions to yield trustworthy results. However, numerical simulations provide an avenue to identify trends, provided their accuracy is validated against experimental results. The numerical model and experiments consist of two stages. First, the panel is subjected to two low-velocity impacts from spherical impactors, spaced perpendicular to the in-plane loading direction, to produce BVID. Secondly, the panel is loaded in-plane and compressed to failure to obtain the peak force.

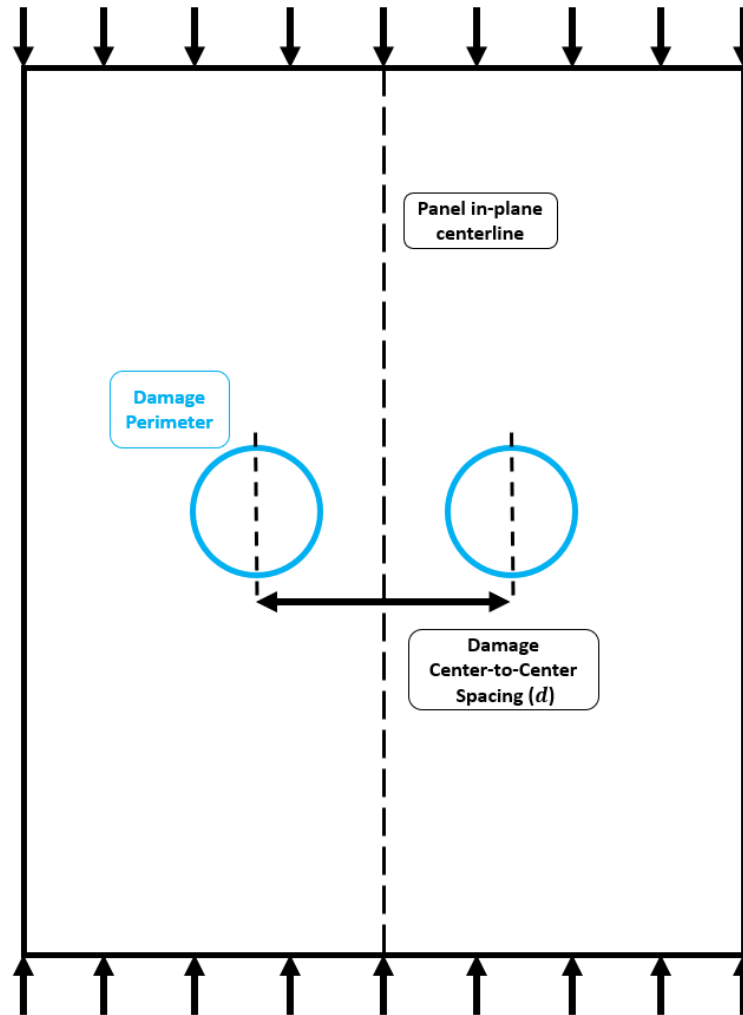


Figure 1.5: Illustration of proximity between damage sites. The center-to-center distance is equally spaced from the centerline of the panel.

1.4 Outline

The thesis is outlined as follows:

- **Chapter 1: Introduction**

An overview is provided of the structure and applications of honeycomb sandwich panels, along with the specific problems this thesis investigates.

- **Chapter 2: Background and Literature Review**

Outlines the background of aluminum honeycomb panels, related research on damage methods for CAI testing, multiple impact sites, FE modelling of dented honeycomb panels in research, and applications of digital image correlation in sandwich panel research.

- **Chapter 3: Methodology**

The general methodology for experimental and numerical testing is presented, including ASTM standards and geometric specifications. The methodology follows the FE model's approach to set up the CAI models. The cellular modelling methods, material model selections, contacts, loading, and boundary conditions are outlined. The experimental methodology used to test the panels is then described, covering the equipment, preparations, and testing procedures.

- **Chapter 4: Results**

Compares the results of both stages of the testing and compares the characterization data and the numerical CAI test data to validate the FE model against the experimental data.

- **Chapter 5: Discussion**

Covers the discussion and analysis of the results. With the FE model validated in the previous chapter, Chapter 5 examines the trends predicted from the FE model and explains why they occur. This chapter further examines the differences between the experimental set-up and the numerical models and discusses the effects of any differences in methodology.

- **Chapter 6: Conclusions**

States the conclusions from the work and identifies areas recommended for further research.

2 Background and Literature Review

2.1 Introduction

This chapter will provide background on composite sandwich panels and the characteristics of dented aluminum sandwich panels. It then navigates experimental test approaches and FE modelling approaches for CAI testing. Existing research on how dents affect panel strength and future investigation opportunities are identified.

2.2 Composite Sandwich Panels

The earliest significant use of the composite sandwich panel was in the De Havilland Mosquito, which used balsa wood as the core and plywood for the facesheets [8]. Since then, the fundamental construction of sandwich panels has remained largely unchanged. Two thin facesheets are bonded to a low-density core, forming a stressed-skin structure in which the facesheets carry most edgewise loads while the core provides nearly all bending rigidity [9].

Composite sandwich panels come in many material combinations. Facesheets are typically metallic (stainless steel, titanium, or aluminum) or fibre-reinforced polymer (glass, kevlar, carbon, or hybrid)[10]. The core is made from low-density non-metallic materials, such as foam, balsa, or reinforced plastic honeycombs, or from corrugated metals such as aluminum, titanium, and stainless steel [8, 9]. Bonding of the facesheet to the core is typically performed in high-heat environments by brazing or welding, or through the use of adhesive [9, 11].

2.2.1 Aluminum Honeycomb Sandwich Panels

Aluminum honeycomb sandwich panels are common in aircraft due to their high stiffness-to-weight ratio. The aluminum facesheets can have material properties that are isotropic or anisotropic. With anisotropic panels, the longitudinal direction is oriented in the primary loading direction [9]. Anisotropic properties are common in sheet metal due to the preferred grain orientation introduced during rolling, with the rolling direction also referred to as the longitudinal direction (L-direction) [12].

Structure and Manufacturing

The hexagonal core is primarily manufactured through two methods, expansion and corrugation. The expansion process stacks thin sheets of the material with specifically placed

adhesive. The stack or HOBE (Honeycomb Before Expansion) block is then cured in an oven before being mechanically expanded to form the honeycomb core as illustrated in Figure 2.1. This method is most popular with high-density honeycomb core [13]. The corrugation process shown in Figure 2.2 turns the rolled sheets into corrugated sheets, then bonds them with adhesive into corrugated blocks that can be trimmed to the required specifications. From both manufacturing processes, the aluminum honeycomb core is inherently directional. The manufacturing process, involves bonding thin ribbons or foils together to form the core, thereby imparting a ribbon or foil direction to the honeycomb structure. The ribbon direction has greater strength than that of the non-ribbon direction and thus is aligned with that of the primary loading direction of the panel [14]. Figure 2.3 shows the ribbon direction, and the thickness of the ribbon material. The cell size shown in Figure 2.4 is determined by the distance between the parallel cell faces. A cell in a honeycomb core refers to a single hexagonal unit formed by six walls, typically aligned in ribbon and non-ribbon directions. The web is the interconnected hexagonal-cell structure (core) that provides structural integrity to the sandwich panel.

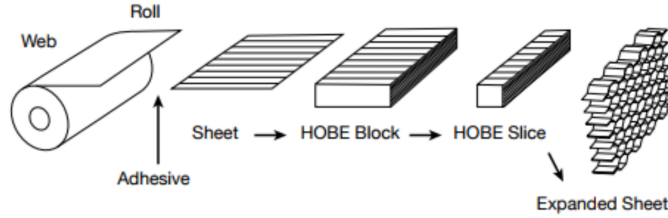


Figure 2.1: Expansion honeycomb core manufacturing method [15].

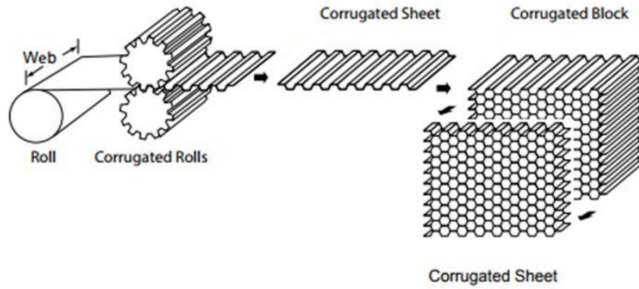


Figure 2.2: Corrugation honeycomb core manufacturing method adopted from [15].

For high-temperature applications of honeycomb sandwich panels, the aluminum honeycomb core is bonded to the facesheet by brazing or welding [11]. Otherwise, most structural applications of aluminum honeycomb panels are bonded using adhesive and cured in autoclaves. Regardless of the method, the resulting bond between the core and the facesheet is critical to transferring shear loads within the sandwich structure [16].

Figure 2.5 illustrates a typical aluminum honeycomb sandwich panel where L and W are the panel dimensions, t_f is the facesheet thickness, t_a is the adhesive thickness, and h is the height of the effective core [17].

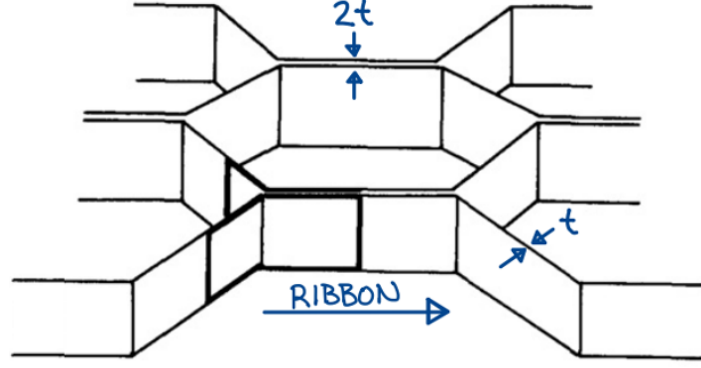


Figure 2.3: Illustration of the ribbon direction within a honeycomb sandwich panel. The cell-wall thickness is t , and on the bonded surface, the ribbon direction has a thickness of $2t$ [17].

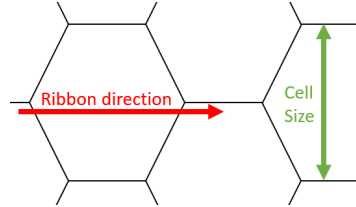


Figure 2.4: Ribbon direction of a honeycomb core and the cell size measurement.

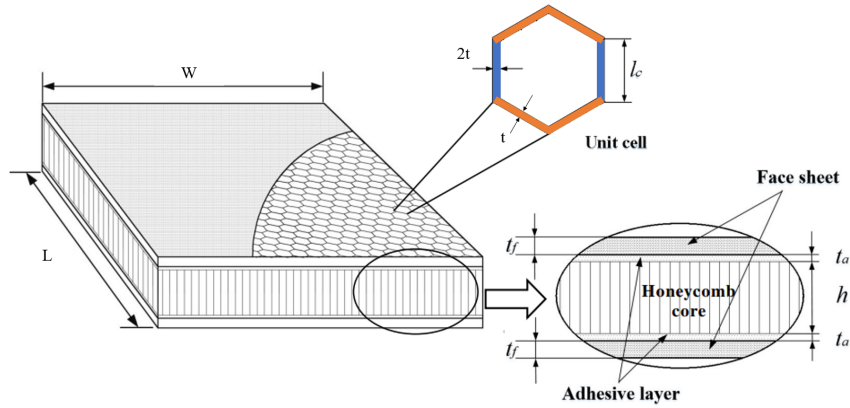


Figure 2.5: Schematic of a typical honeycomb panel where L is the panel dimension in the ribbon direction, W is the panel width, h is the height of the core, t_f is the thickness of the facesheet, t_a is the thickness of the adhesive, t is the cell wall thickness, and $2t$ is the ribbon direction thickness. [18].

Aluminum Alloys for Honeycomb Sandwich Panels

Aluminum honeycomb panels used in aerospace applications are made from a variety of aluminum alloy combinations, following the Metallic Materials Properties Development and Standardization [19]. For example, common facesheet materials are 2024-T3-Clad aluminum and 7075-T6 aluminum in addition to some glass-epoxy measuring 0.2032 to 2.032mm (0.008 to 0.08 inches) thick. Facesheet material is selected on a number of requirements such as the strength, fatigue, durability, and environmental requirements. Table 2.1 outlines material

properties for common facesheet materials [13]. The selection of aluminum cores is limited by cracking that occurs in different alloys when using ultra-thin sheets. Some literature references 2024-T81 as core material; most honeycomb core is either 3000 series or 5000 series aluminum [20–28]. The largest benefit of the 5000 series core is the higher compressive and shear strength than that of an equal-density 3000 series core [13, 29]. Common core properties for aircraft using 5052 aluminum have cell sizes of 3.175mm (1/8 inch) or 6.35mm (1/4 inch) with typical ribbon thicknesses of 0.01778mm (0.0007 inch), 0.0254mm (0.001 inch), or 0.0508mm (0.002 inch).

Table 2.1: Properties of typical facesheet materials reproduced from [13].

Facesheet Material	Yield Strength (MPa)	Ultimate Strength (MPa)	E (GPa)	Comments
Aluminum 1100-H14	117	NA	68	Low cost, weldable
Aluminum 2024-T4 - Clad	323 290	434	73	Good strength, reasonable cost
Aluminum 2219-T87	345	427	71	Best strength and weldability
Aluminum 6061-T4	131	207	69	Lowest cost heat treatable, weldable
Aluminum 7075-T6 - Clad	503 462	531	72	High strength

2.3 Low Velocity Impact Damage

Aluminum honeycomb sandwich panels have a high stiffness-to-weight ratio, however, the panels are susceptible to impact damage due to their poor out-of-plane resistance [9, 28]. High-energy impacts that can cause visible damage, such as facesheet cracking, punctures, or severe localized damage, necessitate component repair, regardless of the dent characteristics. Low-velocity impacts can result in shallow dents that are more difficult to identify. These impacts often occur during ground handling, maintenance, or service operations. Hail or debris on the tarmac can compromise structural integrity without obvious signs of failure in the facesheet [30]. Figure 2.6 shows a cross-section of an aluminum honeycomb panel following a low-velocity impact. Crushing of the cell walls has occurred in the core, however, the facesheet only shows a shallow dent. Additionally, adhesive failure can occur due to environmental degradation, and cracking in the crushed core can occur due to fatigue loading.

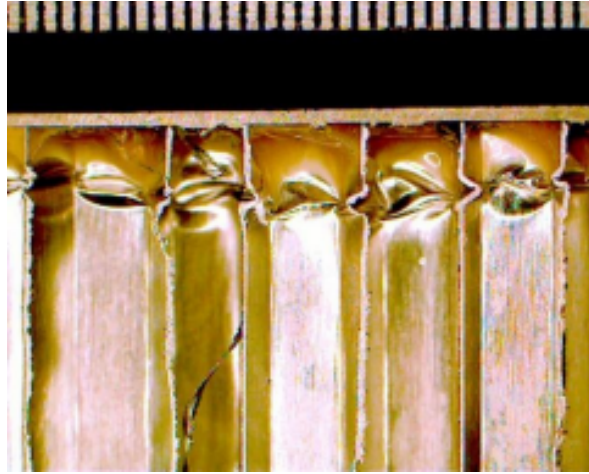


Figure 2.6: Core damage as a result of perpendicular, low-velocity impact to the honeycomb panel [24].

2.3.1 Damage Limits

SRMs outline damage limits for impact damage using a number of parameters, including dent characteristics such as dent diameter, dent depth, proximity to other damage sites, and proximity to other panel features. The aircraft structure may have undetectable (allowable) damage provided that the design ultimate loads can be sustained [31]. The critical damage threshold or CDT is the damage size or depth beyond which the state of damage must be detectable to ensure that the structure possesses adequate residual strength to sustain the design limit loads. For damage tolerance certification, Figure 2.7 outlines the philosophy used to maintain a reliable and safe operation of metallic structures [32].

- In the first region, the panel has nonvisible, or BVID, or defects that are not detectable during manufacturing or service inspections and may never be discovered, and the panel must still withstand the ultimate load designed for the panel.
- If the damage is greater than the ADL but less than the CDT, the damage must be repaired once it is found. This damage is not at risk of failing immediately.
- If the damage exceeds the CDT, the panel must be repaired and is at risk of failing immediately.

The implication of BVID with SRM guidelines ties back to the motivation of this thesis. The CAF are seeking a better understanding of the ADLs and their applications within the SRMs. If the damage limits are shown to be conservative, the CAF may revise the ADLs to allow for larger or deeper dents. Such adjustments to the ADLs could lead to reduced maintenance downtime and lower operational costs.

2.3.2 Barely Visible Impact Damage

BVID refers to low-energy impacts that result in dents that may be challenging to identify by visual inspection [33]. Even dents on the order of millimeters in depth may still be challenging to see in certain lighting conditions on curved panels or on rigidized panels. Resulting damage may include core crushing and facesheet plasticity with no cracks or punctures in the facesheet that would otherwise necessitate panel replacement. BVID refers to a form of

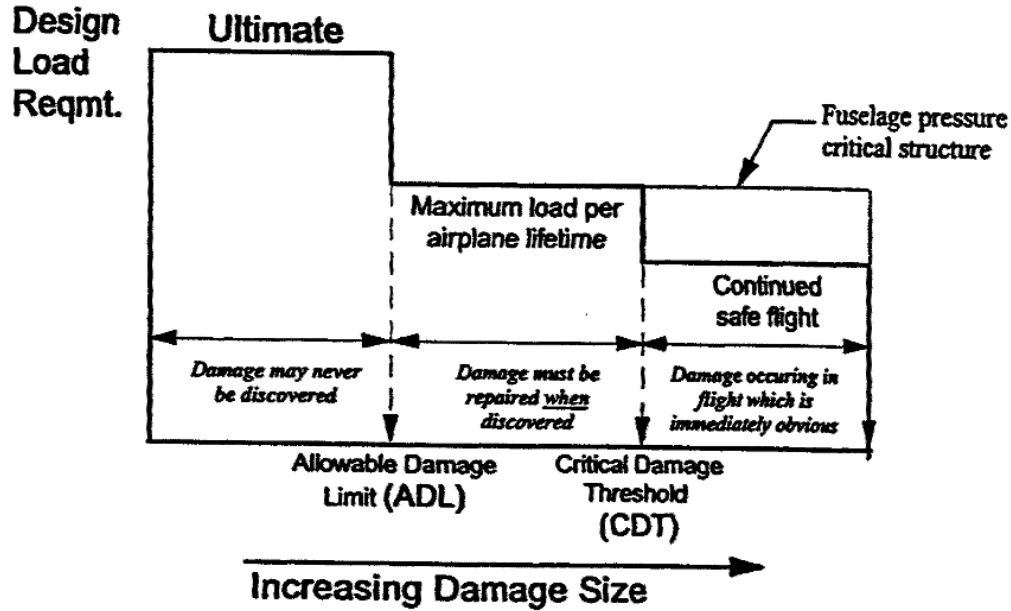


Figure 2.7: Damage-tolerant design philosophy [32]

low-velocity impact damage that typically falls within the allowable damage category. There is no set threshold that defines BVID, but rather it is generally based on dent depth being below the allowable damage limits outlined in the SRM [34]. Wowk et al. examined a retired aircraft panel and classified dents ranging from 0.15 to 1.13 mm as BVID, with the upper bound reflecting the deepest dent observed [35]. Similarly, Reyno et al. applied a BVID range of 0.14 to 1.01 mm [24]. In contrast, Fawcett et al. defined the threshold between 0.25 and 0.5 mm [36], McGowan et al. considered dents from 1.27 to 2.54 mm as BVID [37], and Gower et al. identified a fixed threshold of 0.5mm [38]. A residual depth of 1.27mm (0.05 inches) is generally accepted as the threshold for BVID [34], although, as previously discussed, some consider lower values. This thesis will consider dents with maximum residual dent depths of 2.0 mm.

Damage to an aluminum honeycomb panel from low-velocity impact damage typically consists of permanent deformation of the facesheet and corresponding bending/crushing of the cell walls, with the adhesive remaining intact unless it has been degraded or compromised. Damage to aluminum honeycomb sandwich panels is typically detected visually, or, if the dent is shallow, such as that of BVID, Non-Destructive Inspection (NDI) techniques, such as a tap tester, can be used to identify the crushed core. The tap tester seen in Figure 2.8 is used by lightly striking the panel to create a tone that is relative to the local stiffness of the panel under the strike. Experienced maintenance personnel with a trained ear can use this method to detect damage sites through the decreased stiffness of crushed core [39, 40]. Once the dent has been identified, the depth of the surface damage, or dent depth, is measured with a depth gauge, and the diameter is determined with a ruler. The dent is assumed to be circular, and its area is calculated from the measured diameter and subsequently applied in the SRM [24]. Various other NDI techniques are used to assess damage in honeycomb panels. Tomblin et al. showed that transmission ultrasonic C-scan measurements can detect core damage by finding changes in the propagation of the sound waves [33], and X-ray computed

tomography provides detailed imaging of the panel's internal structure [41]. Both transmission ultrasonic C-scan and X-ray computed tomography are used on aluminum and composite panels to detect surface damage and core damage. Laser scanning captures surface damage and dent profiles on all surfaces, though it cannot provide resolution of internal core damage. Eddy current testing identifies both surface and subsurface defects by varying the applied frequency [24], enabling analysis of surface damage in metallic-skinned honeycomb panels, though with limited subsurface detail. Thermography detects core damage by analyzing heat transfer properties [40, 41]. Thermography is primarily used with composite facesheets to detect delaminations and adhesive disbonds. Minor impacts can initiate damage mechanisms that propagate under cyclic loading, leading to panel failure [42]. Accurate characterization and periodic inspection of aluminum honeycomb sandwich panels are essential to ensure long-term performance and reliability.



Figure 2.8: A tap tester (right) and a depth gauge (left) are typically used in evaluating BVID damage in aluminum honeycomb sandwich panels [24].

2.3.3 Compressive Failure Modes for Honeycomb Panels with Impact Damage

Honeycomb panels are used in aviation as structural members and undergo a variety of loading conditions, such as in-plane compression, tension, bending, and shear, depending on their application. The presence of impact damage, such as BVID, may reduce the panel's peak carrying load and cause premature failure. A study by Nettles looked at the effect of impact energy and found the greater the impact energy, the deeper the dent and a greater the reduction in the CAI strength of the panel [43].

Figure 2.9 illustrates three distinct failure modes observed in undented honeycomb panels subjected to in-plane compression; general buckling, shear crumpling, and intracell buckling

(dimpling). General buckling typically occurs when the panel is excessively thin. Shear crimping arises when either the adhesive shear strength or the core's shear modulus is insufficient, and intracell buckling is commonly triggered when the core is not able to adequately support the facesheet [3, 9, 26, 44].

Damage such as BVID provides a location where intracell buckling can localize and grow outward to the edge of the panel transverse to the loading direction. This can be referred to as dent growth. Figure 2.10 shows an example of dent growth in a composite sandwich panel. A spherical impactor was used to create BVID in the center of a composite facesheet panel, and an in-plane compression load was then applied. The facesheet buckles and the dent becomes an ellipse and grows normal to the loading direction[45]. The dent continues to grow until the damage reaches the edge of the panel.

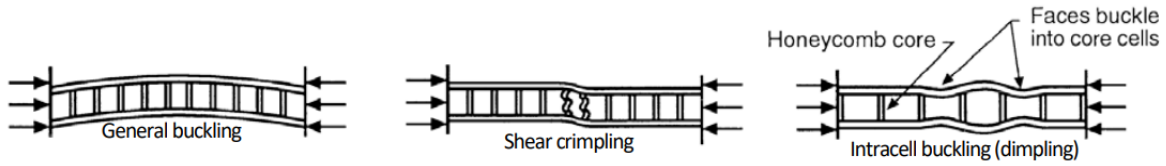


Figure 2.9: Failure modes of honeycomb panels under compression without dents [3, 26].



Figure 2.10: Front view of a panel dented with a spherical impactor (a), being compressed in-plane (vertically) causing dent growth across a panel (b - e)[45].

2.3.4 Residual Strength of Honeycomb Panels

Honeycomb panels can resist a variety of loading such as bending, in-plane tension and compression, and shear. When a dent is present, this can promote intracell buckling when the facesheet is in compression. This is why in-plane compression is considered to be the most critical loading for dented panels and why CAI tests are used.

The most common method of determining the residual strength of a honeycomb sandwich panel is through CAI testing, in which the damaged panel is subjected to an in-plane compressive load until failure. The maximum compressive load that the damaged panel can support is referred to as its residual strength. Staal et al. showed that the most common failure mode for CAI testing is intracell buckling [46], and Horrigan et al. demonstrated that the force required to initiate failure is related to the damage area and depth [47]. In-plane compression testing is considered the critical load case for damaged honeycomb sandwich panels because the damage such as BVID undermines the panels ability to resist buckling, significantly reduces the panel's carrying capacity, often leading to premature failure [48].

The loading is carried by the facesheets in in-plane compression and tension. Sandwich panel tensile testing is common in undamaged panels where ASTM C297 is used to measure the facesheet to core bond strength [49]. For a dented panel, tensile loading would not be the critical load case because the panel would retain most of its tensile capacity and the dent would become shallower in the process.

The residual strength of damaged honeycomb sandwich panels can also be measured through three- or four-point bend testing [46, 47], since these panels are primarily designed to carry flexural loads rather than compressive loads, making bending tests particularly relevant. However, such tests often result in premature failure at the support points, which does not reflect realistic loading conditions. In bending, failure typically occurs through compression of the dented facesheet while the opposite facesheet remains in tension; by contrast, in-plane compression places both facesheets into compression, ensuring that failure initiates at the dent site and thus providing a direct measure of the dent's effect. This thesis considered in-plane compression testing as the testing load case because it guarantees the failure will initiate from the dent.

2.4 Experimental Testing

Experimental testing in the literature examines CAI of dented panels and low-velocity impact characterization by comparing dynamic and quasi-static indentation, the coupon dimensions selected, the impact energies used, and the backing support used during indentation.

2.4.1 Low-Velocity Impact Characterization of Aluminum Honeycomb Sandwich Panels

Low-velocity impact testing of aluminum honeycomb sandwich panels has been widely investigated to understand the impact event and panel characteristics affect the resulting damage. The dent depth is influenced by the impact characteristics such as the impactor mass, velocity, size and shape. It is also influenced by the facesheet and panel thickness and the core properties such as cell wall thickness, and cell size.

To better understand how these damage features affect the structural response of the panel, Benotto characterized aluminum honeycomb sandwich panels through FE modelling [23]. His analysis showed that the severity of core damage has a larger effect on the stresses in the dented facesheet than the dent geometry when a panel is loaded in tension. Clarke characterized low-velocity impact damage in aluminum honeycomb sandwich panels using FE modelling [40]. He numerically analyzed 12 panels with varying core thicknesses and 7 indenter configurations of different diameters. The study showed that, for a given core density, the depth of core damage remained constant regardless of dent size. As shown in Figure 2.11, the width of the damaged core matched the width of the residual dent for all panel thicknesses, except in very thin panels (2–4 mm) where the core was completely crushed. Wowk et al. characterized low-velocity impact damage in aluminum honeycomb panels through a combination of experimental testing and finite element modelling [35]. By varying the adhesive fillet height and comparing numerical predictions with physical impact results, they identified

that the depth of core damage depended only on the adhesive fillets bonding the facesheets to the core. In this thesis, damage will be characterized by the dent depth, width, and the residual strength for different dent spacings.

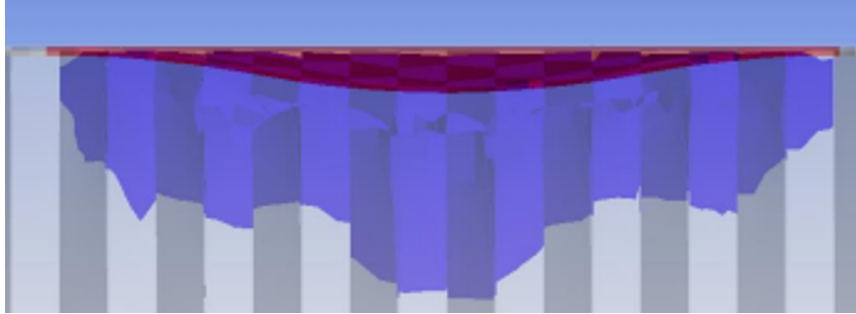


Figure 2.11: Cross section of a honeycomb panel impacted perpendicular to the facesheet in a post-impact state. Blue indicating sections of the honeycomb core which has yielded and red indicating sections of the face sheet with a downward deflection greater than 0.01mm [40].

Dynamic versus Quasi-Static Indentation

To induce damage in honeycomb sandwich panels in an experimental setting, the ASTM 8287 recommends denting the panel dynamically using a drop tower or quasi-statically using a testing machine [50].

Dynamic impact provides the most realistic damage where inertial effects are present. The largest drawback for dynamic impacts is the requirement for specialized drop tower equipment such as that featured in Prior et al., Ratcliffe et al., Davidson et al., McGowan et al., and Cvitkovich et al. [39, 48, 51–53]. Research by Zheng et al. used a drop-weight impact testing setup to create low-velocity impacts on carbon-fibre faced, aluminum honeycomb core sandwich panels that would be examined using Bragg grating sensors [54]. Ivañez et al. utilized a drop-tower setup to impact beams with fibre-epoxy facesheets with aluminum honeycomb core to compare impact energy, absorbed energy, and contact force from numerical and experimental results [20]. Wheeler utilized a drop-weight indenter to create the damage at different locations relative to the edge of the panel to study the interaction of the damage with the edge [27]. The study by Wowk et al. used a pendulum impactor rather than a drop tower and investigated how varying damage sizes [35], panel configurations, and impact energies influence the internal damage characteristics associated with BVID. Boctor applied the dynamic impact method to study the effect of dent spacing and impact energies on aluminum honeycomb sandwich panels [26]. Dobyns et al. used dynamic impacts to examine the effect of panel size on impact damage in composite honeycomb sandwich panels using an instrumented low-velocity impact test [55].

Quasi-static indentation is a common controlled method for creating consistent damage. By applying a preset displacement, a specific dent depth can be achieved in the honeycomb panels. Foo et al. used quasi-static indentation to examine the effects of honeycomb geometric factors, such as ribbon thickness and cell size, on the damage resistance of the core [22]. Foo's

application of quasi-static indentation was to control the displacement of the indenter and create dents of a certain depth and compare experimental and numerical results. Kendall et al. focused on the influence of different fillet sizes in aluminum honeycomb panels subjected to low-velocity impact damage [25], choosing to create the damage using quasi-static indentation to create different dents of different depths to compare the core crush depth and load-deflection curves. Ye et al. used quasi-static indentation on carbon-fibre laminate sheets with a Nomex core to examine residual deformation of the BVID dent depth [56], selecting this method to create specific dent depths that were subsequently examined using guided-wave signals.

Both damage methods are extensively used to create damage in honeycomb panels. A paper by McQuigg et al. examined CAI on composite facesheet, Nomex honeycomb panels and found that both methods are capable of creating similar dents, except for low-velocity damage, where quasi-static indentation created deeper residual dents, wider core damage, and reduced the residual strength of the panel than with dynamic impacts [45]. Schubel et al. investigated the differences between quasi-static indentation and dynamic drop-tower impacts on composite sandwich panels [57], showing that the quasi-static damage model exhibited earlier failure initiation and more severe strain and damage levels. This behavior was attributed to the fact that, under quasi-static loading, the energy is concentrated directly beneath the indenter without inertial effects to dissipate the load, leading to early delamination. In this thesis, dynamic impacts are used because they replicate real-world considerations and include inertial effects.

Coupon Dimensions

The size of samples or coupons used in published research differs depending on the application. Clarke selected a 50.8 mm $\times$ 50.8 mm coupon to ensure the entire damage region was captured, while conducting finite element simulations on panels with varying core thicknesses and indenter sizes [40]. Likewise, Foo et al. used a 100 mm $\times$ 100 mm numerical aluminum honeycomb sandwich panel to study low-velocity impact loading and to characterize how core thickness and facesheet properties influenced damage initiation and propagation [22]. Kendall et al. employed an 80 mm $\times$ 80 mm coupon to investigate the effects of adhesive quantity [25]. In summary, most coupons used to study impact characteristics are relatively small, since they are sufficient to capture localized damage.

CAI testing, however, typically requires larger coupons in experimental settings. For example, Wheeler selected a 215 mm $\times$ 265 mm aluminum honeycomb panel based on ASTM 8287 standards to study edge effects in CAI [27, 50]. McGowan and Ambur used a 127 mm $\times$ 254 mm coupon to examine CAI strength and damage types in composite sandwich panels [52], while in another study, they tested a forward fuselage keel panel measuring 1677 mm in length and tapering from 254 to 356 mm in width to investigate the effect of damage sites [37]. Zheng et al. employed a 300 mm $\times$ 300 mm carbon-fiber/aluminum honeycomb panel to replicate a wall or wing portion of a microsatellite, using piezoelectric transducers to locate low-velocity impacts [54].

Numerical impact studies, by contrast, often use smaller coupons for computational efficiency, ensuring that the dent damage remains contained within the coupon boundaries. Bactor selected 48 mm $\times$ 93 mm coupons [26], and McQuigg et al. selected 70 mm $\times$ 70 mm [45].

Benotto trialled three different coupon sizes through finite element modelling and determined that the largest coupon tested (93.5 mm $\times$ 95.2 mm) best represented an infinite panel in terms of equivalent stress on the facesheet [23].

In summary, impact studies that focus on the core damage from impacts typically employ small coupons to isolate localized damage, experimental CAI studies require larger coupons to capture global structural effects, and numerical CAI studies use smaller coupons constrained by computational limits.

Impact Energy

In honeycomb sandwich panels, BVID introduced through dynamic drop-tower impact is quantified in terms of impact energy. Impact energy is a function of the mass and velocity of the indenter, and different impact energies are used to study different panel geometry and construction parameters—such as core thickness, core density, facesheet thickness, adhesive height, and the material stiffness of the facesheet, core, and adhesive—as well as the impactor’s radius and stiffness. A range of impact energies from 0.070 J to 13 J was found in the research surveyed.

Foo et al. numerically studied impact damage ranging from 0.85 J to 13 J in a 0.75 mm-thick 1100-H-14 aluminum facesheet bonded to a 20 mm-thick 3003-H19 aluminum core and created dent depths in the range of 1.5mm to 7.5mm [22]. Since they focus was on different parameters of the core, four core densities were trialled with four ribbon thicknesses. Figure 2.12 shows the BVID dent depths created, ranging from 0.4 mm to 2.4 mm. Clarke used an impactor velocity of 1.5 to 4.0 m/s and an impactor mass of 0.3 to 2.15 kg to achieve a range of impact energy from 0.61 J to 4.31 J[40]. Bector numerically studied the impact energies from 0.070 to 1.06 J by varying the impactor velocity to achieve residual dent depths from 0.06 to 0.76mm [26], using panels consisting of 2024-T3 aluminum facesheets, 5052-H34 core, and a modelled adhesive fillet. Bector also studied the effects of impactor radius, cell size, facesheet thickness, and cell wall thickness at an impact energy of 0.27 J, and then examined the proximity of impacts spaced parallel to the panel’s loading direction at impact energies of 0.5, 1, and 1.5 J. Wowk et al. compared numerical and experimental results from four panel configurations containing two cell sizes (3.175mm and 6.35mm) [35], three cell wall thicknesses (0.01778mm to 0.0508mm), two facesheet thicknesses (0.508mm and 0.3048mm), and two facesheet materials (7075-T6 and 2024-T3), where each configuration received four adhesive heights. Wowk et al. tested impact energies ranging from 0.182 J to 2.079 J. In this thesis, the impact energy of 1.57 J was used to create a dent depth of 1.2mm.

Backing Support

In aircraft, sandwich panels are supported along their edges by the aircraft frame. During impact events, the panels are free to move or deform out-of-plane, causing overall bending or local deformation. With BVID, any out-of-plane deformation of the panel is typically localized near the dent site and could result in permanent deformation of the back facesheet.

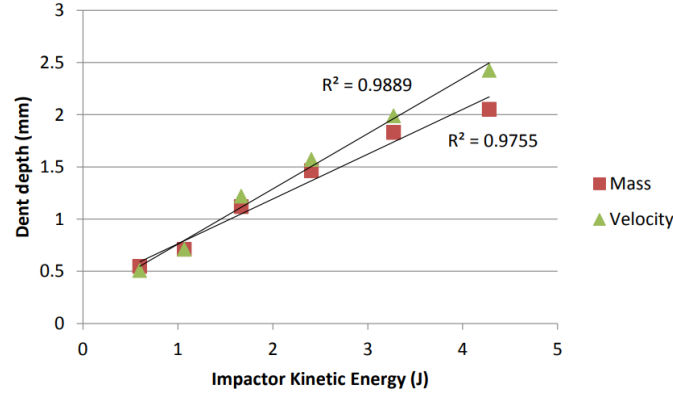


Figure 2.12: Effect of impact energy with various masses and impact velocities on dent depth[40].

ASTM 7766 [58], the standard practice for damage resistance testing of sandwich constructions, describes the use of rigid-backed panels to evaluate how material properties and geometry influence damage resistance. The standard also outlines testing with edge-supported panels, which capture the effects of flexural stiffness and support conditions on dent initiation and growth. Existing research, such as Clarke [40], Wowk et al. [35], and Foo et al. [22], examined the internal core damage caused by low-velocity impact and opted to use a solid backing to remove the effects of panel bending. Ivañez et al. also examined internal core damage during low-velocity impact on a beam panel [20]. However, Ivañez chose to support the panel using two cylinders spaced 430mm apart, similar to a three-point bend test. Since the panel contained fibre-epoxy facesheets with a honeycomb core, the use of the large span beam allowed Ivañez to study the impact and bending effects in the core and the facesheet at different impact energies. Wheeler selected to follow standardized test procedures outlined in the ASTM 7136 to create an impact site over a cut-out while supporting the remainder of the panel [27]. ASTM 7136 uses a cut-out to reflect the damage-resistance characteristics of an unstiffened monolithic skin or web rather than those of a skin attached to a substructure, which resists out-of-plane deformation [59]. In the CH-146 Griffon, sandwich panels are installed on the aircraft and are typically only supported around the edges of the panels. In this thesis, a cut-out was used to represent the backing found in aircraft structures.

2.4.2 CAI of Dented Panels

CAI is widely used in research to analyze the behaviour of dented sandwich panels under compression, including core crush, residual panel strength, and dent growth.

Compression After Single-Indentation Impact in Panels

Composite sandwich panels with a single indentation are commonly studied through CAI to understand the panel's behaviour and reaction under in-plane compressive loading.

McQuigg et al. used experimental results and numerical predictions of a single indentation to investigate dent characteristics such as dent depth [45], residual strength, and dent growth to

evaluate the new numerical model. McGowan and Ambur performed CAI testing on composite sandwich panels to evaluate impact damage under concurrent compressive load [52]. The results showed that impacts under compressive preload produced distinct damage characteristics and reduced the panels' residual compressive strength, demonstrating the sensitivity of sandwich structures to combined impact and compressive loading conditions. Tomblin et al. examined CAI testing using a single indenter and evaluated the load-displacement response and microstrain behaviour during compression [33]. Tomblin et al. measured the out-of-plane displacement in the center of the dent and found that it varied linearly with the applied load where a strain gauge at the center of the dent varied non-linearly. Wheeler extended the study of damage in aluminum honeycomb sandwich panels by examining the effects of edge distance on residual strength [27]. Wheelers' research showed an inverse correlation between residual strength and edge distance; damage positioned close to the panel edge significantly reduced strength, whereas damage located farther away behaved similarly to centrally placed dents. This highlighted edge distance as a critical parameter in defining allowable damage limits. However, Wheeler's study did not address overlapping dents or dent depth, leaving open questions about how multiple, interacting dents influence failure in aluminum honeycomb panels.

CAI of Panels with Multiple Indentations

Although CAI testing of individual dents is well represented in the literature, only a limited number of studies have investigated the influence of multiple dent proximity.

Tariq et al. examined the effects of multiple dent configurations with different indenter sizes on 6061-T6 aluminum facesheets adhered to a 5056 aluminum core using Redux-609 [60]. The focus was on experimentation with four dent configurations shown in Figure 2.13: single dent, two dent, three dent, and four dent. These configurations were designed to mimic a pattern observed in the field; the dents had a depth of approximately 50% of the panel thickness, representing damage far more severe than the BVID studied in this thesis. Their research trialled the four configurations with 5 mm, 10 mm, and 15 mm diameter indenters. Tariq et al. concluded that wider and deeper dents resulted in greater reductions in the panel's residual strength and found that the dents did not interact with each other in the respective configurations, except for bulging between them with the largest diameter indenter. Tariq et al. also showed that each panel failed perpendicular to the loading direction and through the region with the largest dent area.

Boctor had a more systematic approach in examining the effects of the proximity of indentations [26]. The research examined the effects of low-velocity dent spacing parallel to the panel's loading direction. Boctor incrementally separated the damage and predicted the panel's residual strength at each point. Figure 2.14 shows the relationship between the in-plane dent proximity and the peak reaction force. Boctor found that the minimum peak force occurred at a spacing of zero cells; thereafter, the residual strength of the panel increased, exceeding the single-indentation panel strength. Overall, the residual strength increased from a spacing of 0 cells to a plateau with the single indentation, except for a region between 4 and 7 cells where the residual strength exceeded that of a single dent. Boctor observed in this region of the graph that the dents merge, so the entire damaged area has the same maximum depth. Once merged, the dent acts as a single dent with the deepest point in the center.

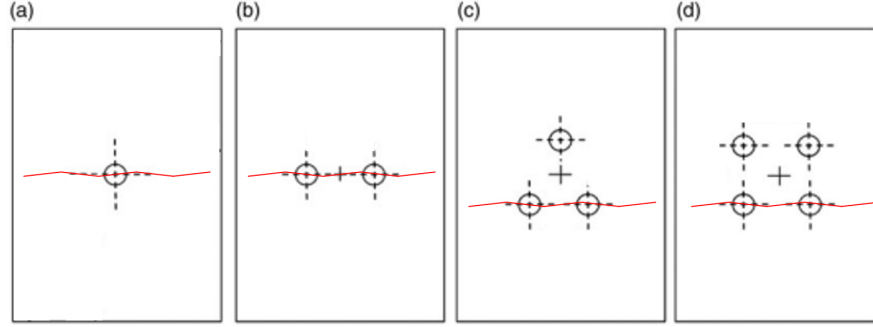


Figure 2.13: Tariq's tested configurations: (a) single dent, (b) two dents, (c) three dents, and (d) four dents, the red line across each configuration indicates where each dent configuration failed. Modified from [60].

However, to merge together a higher force than what is needed to localize a single impact site is required. Bocator observed that once two dents no longer overlapped, they did not interact. The study also showed that failure occurred perpendicular to the loading direction. In Bocator's finite element model, overlapping dents merged and triggered failure, whereas separate dents localized individually, but almost simultaneously due to the idealization of the model. Bocator's investigation was limited to numerical modeling and did not include experimental validation. In addition, the dent spacing was aligned parallel to the loading direction, and critical failure is expected to occur perpendicular to the load with overlapping dents. These constraints mean that while his work provided useful insights into dent proximity effects, it did not capture the dent spacing in the expected critical failure direction.

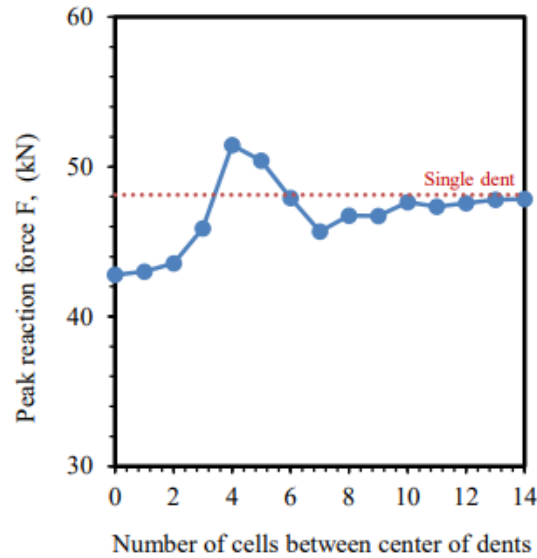


Figure 2.14: The effect of spacing between two impact sites on the CAI strength [26].

Walker et al. investigated damage assessment of composite sandwich structures, providing insight into multi-impact indentations oriented perpendicular to the loading direction [61]. For panels with glass fibre and carbon fibre reinforced composites and Nomex cores, Walker et al. demonstrated that panel strength depends on the individual dent width and the spacing

between dents. Using an infinite plane tension model, they showed that when the spacing between dents reached four times the dent diameter, the damage could be treated as two isolated sites. Their study did not address overlapping dents, but only cases with a minimum separation of half the dent diameter. Although dent depth was identified as a descriptor of damage severity, it was not incorporated into the analytical framework.

Current research on multiple dents in composite and sandwich panels shows that failure consistently develops perpendicular to the loading direction, even when several indentations are present. Numerical studies suggest that dents aligned with the loading direction do not interact once separated, and analytical models indicate that the strength of panels with two independent dents can be related back to single dent strength. Yet most of this work has focused on composite facesheets rather than aluminum panels, and there is little experimental evidence on overlapping dents—particularly those oriented perpendicular to the loading direction. Addressing this gap is critical to developing a realistic understanding of residual strength in aluminum honeycomb sandwich panels, the focus of the present study.

2.5 Finite Element Modelling of Honeycomb Sandwich Panels

Honeycomb sandwich panels have been numerically modelled to predict residual panel strength, deformation, and dent characteristics such as dent depth, dent diameter, and dent width, with different representations of the core, facesheet, and adhesive depending on goal of the research.

2.5.1 Finite Element Core Representation

Two primary modelling approaches for the honeycomb core are found in the literature: the homogeneous representation and the cellular representation, shown in Figure 2.15. In the homogeneous approach, the cells are not modelled and a uniform material property accounts for the cellular behaviour, offering computational efficiency and suitability for studying overall panel behaviour. In contrast, the cellular approach explicitly models each cell, enabling detailed investigation of local failure mechanisms but at significantly higher computational cost. Studies such as Benotto and Clarke employed cellular modelling to capture dent formation following impact [23, 40]. Sather and Krishnamurthy noted that cellular modelling is essential for analyzing failure propagation [62], while Lerch emphasized its necessity when core failure detail is required [21]. Zalewski et al. used a cellular core to study facesheet wrinkling using linear buckling and nonlinear static analyses [63]. Boctor modeled cellular core to study cell wall thickness, facesheet thickness, impactor radius, impactor velocity, cell size, and the proximity between dents [26]. Foo et al. examined the core crush diameter and the facesheet deformation diameter at different core densities [22], and Wowk et al. applied cellular core to examine cell wall thickness, cell size, and skin thickness effects on springback [28]. Li et al. compared nonlinear cellular and homogenized core models, showing that while the homogenized approach is efficient and suitable for linear elastic stretch–bending applications, it is limited in capturing nonlinear local failure mechanisms [64]. Cellular modelling of the core is used in this thesis to provide insight into the core behaviour during CAI.

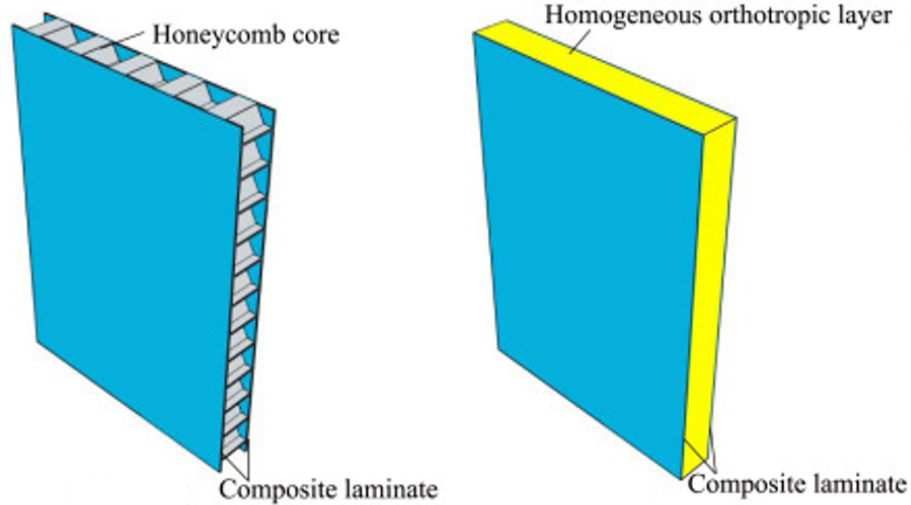


Figure 2.15: Honeycomb panel from a FE model using a cellular core (left) and with a homogeneous core (right) [65].

Finite Element Cellular Core Representation

Cellular core models can be represented as parallel springs or spring-dampers, or discrete cells to form the honeycomb core. The spring model is the simplest, capturing the elastic stiffness of the core with high computational efficiency, but it neglects damping and local failure mechanisms, which limits its use beyond preliminary analyses. Extending this to a spring-damper model enables simulation of energy dissipation and dynamic effects such as vibration and impact, making it more applicable to real-world scenarios, though it still requires calibration and cannot fully capture nonlinear collapse. Abrate proposed an energy balance model (Figure 2.16) to analyze the dynamics of impacts on honeycomb structures [66]. Similarly, Aminanda et al. examined dent growth of barely visible impact damage (BVID) in compression-after-impact (CAI) tests on a Brass-Nomex panel configuration, selecting nonlinear spring elements to represent the Nomex core's cyclic behaviour under compression [67]. Their results showed that at higher compression displacements the model failed to capture the nonlinear collapse evident in the residual force response. The most detailed approach is the discrete cell model, which explicitly represents each honeycomb cell and enables accurate prediction of local buckling, crushing, and nonlinear deformation, as demonstrated by Benotto, Boctor, Wowk et al., and Clarke [23, 26, 35, 40]. However, this resolution comes at the cost of high computational demand and complex meshing, limiting its practicality for large-scale system simulations. For this thesis, discrete cellular modelling was used.

2.5.2 Finite Element Facesheet Representation

The facesheet of an aircraft sandwich panel is a thin sheet bonded to the core material. Rose et al. outlined two primary methods for formulating the facesheets: the use of shell elements, or in the case of composites, layered shell elements [68]. Alternatively, solid element formulations can be used, though these are typically reserved for detailed local modeling due to their higher computational cost. This thesis focuses on modeling an entire panel, similar to

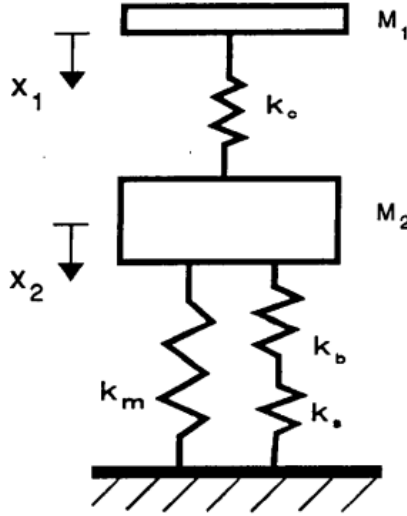


Figure 2.16: Spring-mass model where M_1 is the mass of the indenter, M_2 is the mass of the panel, k_c is the nonlinear contact stiffness, k_b is the bending rigidity of the panel, k_s is the shear rigidity of the panel, and k_m is to account for the membrane stiffness due to geometrical nonlinearities [66].

the approaches of Boctor, Wowk et al., Clarke, and Sather et al., and therefore adopts shell elements to represent the facesheet [26, 35, 40, 62].

2.5.3 Finite Element Adhesive Representation

The adhesive bonds the core to the facesheet, and its representation in FE models depends on whether adhesive failure is considered. When adhesive failure is not included, as in the work of Zalewski et al. [63], Clarke [40], Foo et al. [22], Dobyns et al. [55], and Wowk et al. [28], the bonding is typically represented through node-sharing or bonded contact definitions. When adhesive failure is modelled, the adhesive may be represented explicitly with solid elements which drives the core crushing deeper into the panel and reduces the unsupported length of the cell wall, as demonstrated by Benotto and Reyno et al. [23, 24], or, for improved computational efficiency, by an equivalent adhesive shell representation such as that used in Boctor's research [26]. Wowk et al. showed that the depth of core damage depends on the adhesive thickness [35], which, for peak force and the local buckling of composite sandwich panels in the dent region, is also dependent on core damage. Although the damage is expected to be in the core, an equivalent adhesive shell representation is used in this thesis to accurately model the peak forces and local buckling in the coupons.

2.6 DIC

DIC is a non-contact, full-field measurement technique used to capture displacement, strain, and deformation in materials and structures [69]. By tracking surface movement under load, DIC provides experimental data that can be directly compared with nodal predictions from numerical simulations [70, 71]. Because simulations often highlight potential strain concentrations, DIC is particularly valuable for verifying whether the numerical model accurately

represents strain gradients and localized effects. In addition, DIC can be used to validate boundary conditions and load transfer assumptions in finite element models and to visualize damage such as delaminations, wrinkling, and local buckling.

In sandwich panel testing, it has been used by Caminero et al. to detect damage on adhesively-bonded composite repairs [72], Dear et al. used it to measure displacement, out-of-plane deformation, shear strain, and principal strain in high-speed ballistic impacts [73], blasts, and quasi-static indentations on honeycomb panels, and Yang et al. applied it as a non-contact thermal deformation measurement device [74]. Although no ASTM-accepted calibration methods for DIC systems currently exist, a properly configured and calibrated system can still provide reasonably precise strain measurements and out-of-plane deformations [50, 69, 75]. Similarly, Davidson et al. show comparisons of honeycomb sandwich panels following indentation as von Mises strain and deflection compared against delamination patterns in simulations and ultrasonic scanning prior to CAI [51].

Wheeler outlined the adaptation of the ASTM 8287 standard and validated the use of DIC against a strain gauge fitted to an aluminum bar [27]. Using DIC, a virtual extensometer was placed in the software and directly compared with the known strain-gauge measurements. Wheeler’s application used ASTM 8287 panels to examine out-of-plane deformation and Lagrangian strain for different low-velocity damage edge spacings.

Wheeler’s validation method was derived from the work of Gardner et al. on the NASA SBKF program [75]. The SBKF program examined the full field deformation and strain of a launch-vehicle-like cylindrical shell structure. Gardner et al. also found that DIC could be used to provide more precise validation for high-fidelity structural simulations and to enhance the capability to detect anomalies.

The diverse applications of DIC in research provide valuable full-field understanding, ranging from large systems, as shown by Gardner et al., to small material samples, such as the 6mm by 3mm gauge area used by Harant et al. [12]. Zhu and Pan showed that mirror-assisted DIC can be applied to study both front and back sides of the facesheets during failure [76].

An earlier work by McQuigg et al. examined the evolution of damage tolerance in composite honeycomb sandwich panels under compression loads [45]. This study utilized FE modelling to replicate the CAI and compared the results with experimental data, including DIC. The primary application of DIC was to quantify changes in the shape and size of the damage as the panel was compressed. In the compression testing, McQuigg et al. found the dent widened perpendicular to the loading axis, whereas the dent height along the loading direction remained relatively unchanged.

Wheeler and Gardner et al. both demonstrate that calibration against a trusted and known source can provide valuable insight into out-of-plane deformation and strain using DIC [27, 45]. Further building on this information, McQuigg et al. and Gardner et al. demonstrate that DIC results can be compared with FE model results [45, 75]. DIC is used in this thesis to compare the panel facesheet full-field deformation and strain with the numerical simulation.

2.7 Opportunity for Further Research

This literature review indicates that extensive research has been conducted on honeycomb panels in CAI testing for both visible damage and BVID, with most studies focusing on composite-facesheet honeycomb panels and relatively few addressing aluminum facesheet and core configurations. Notably, no studies were found that examine the effects of low-velocity damage sites that overlap or are oriented transverse to the loading direction in aluminum honeycomb panels subjected to CAI. While damage limits for dent proximity are outlined in SRMs and certain technical papers, the supporting experimental and numerical literature has not been published, leaving a critical gap in understanding the interaction of multiple dents under compressive loading.

This research aims to determine the effect of spacing on the residual strength of aluminum honeycomb panels subject to CAI of two BVID sites at different distances apart, both overlapping and non-overlapping. This is achieved through CAI testing aligned perpendicular to the loading direction, including FE simulations and experimental testing.

3 Methodology

3.1 Introduction

To determine the effect of BVID proximity on the residual strength of aluminum honeycomb panels subject to in-plane compression, FE simulations and experimental testing of CAI loading on dented panels were performed. The FE simulation results were verified experimentally by impacting panels with two dents at different spacings, then compressing the panels until failure. The study examines two dents spaced at different distances, perpendicular to the loading direction of the panel. Residual panel strength is defined as the maximum force the panel can withstand under in-plane compressive loading.

This chapter first outlines the ASTM standards that define the indentation and CAI methods used in the simulations and physical testing. Section 3.4 describes the baseline finite element model that was used to determine the effect of dent spacing on residual panel strength. Section 3.5 summarizes the experimental test method used to validate the FE model.

3.2 General Methodology

The FE model and experimental tests followed ASTM standards for indentation and CAI for composite sandwich panels. Sections 3.2.1 to 3.2.3 outline the applicable details of the ASTM standards adopted in this thesis. Additional common practices and methodologies, such as panel reference axis, panel configuration, and indentation, are presented in Sections 3.2.4, 3.2.5, 3.2.6, respectively.

3.2.1 ASTM 7766

ASTM 7766 is the standard practice for damage resistance testing of sandwich constructions [58]. The standard covers both quasi-static and drop-weight impact test methods for both continuous (ie., foam) and discontinuous (ie., honeycomb) core material. The quasi-static indentation test method can be performed with either a rigidly backed specimen or an edge-supported specimen, whereas the drop-weight test uses an edge-supported specimen. ASTM D7766 recommends quasi-static indentation against a rigid backplate as the most suitable method for studying damage-resistance characteristics, such as material properties, geometry, and layer stacking, in composite facesheets. However, it cautions that damage resistance measured from rigidly backed specimens does not directly translate to edge-supported configurations, since the rigid backing suppresses out-of-plane deformation and thereby reduces the influence of flexural stiffness and support geometry on damage initiation and growth.

Consequently, the standard advises selecting the indentation and support method to match the intended application. On aircraft, dents are created dynamically by impacts (hail, tool drops, etc.), where backing is typically absent and panels are supported only around the edges. The experimental and numerical testing in this thesis utilized the edge-supported drop-weight method.

3.2.2 ASTM 8287

ASTM 8287 is the standard test method for compressive residual strength properties of damaged composite panels. This standard covers in-plane compressive testing of panels subjected to quasi-static or drop-weight impacts, as specified in ASTM 7766. Figure 3.1 shows that the panel size is recommended to be 215mm wide by 265mm in length, with the dent in the center of the panel. For a non-standard specimen configuration, such as multiple dents, the panel width shall not be less than three times the damage diameter.

The standard recommends the use of a support fixture shown in Figure 3.2 to provide a distributed compressive in-plane load across the specimen. Figure 3.2 shows support notches with the red arrows at the bottom and top of the fixture, keeping the panel ends inline during compression. The notches can be adjusted to accommodate different panel thicknesses in the fixture. The vertical knife edges indicated by the blue arrows in Figure 3.2 allow the panel to compress freely in-plane while preventing the edges of the panel and as a consequence, the overall panel from bending, but still allow local bending in the middle of the panel where the dents are present. The knife edges are also adjustable and are placed loosely in contact with the panel, so they do not preload or squeeze the panel while allowing it to expand laterally when compressed longitudinally. This setup promotes local failure at the dent rather than overall bending of the panel or shear crimping (recall Figure 2.9). The crosshead speed during compression is quasi-static at 1.25mm/min. ASTM 8287 recommends measuring the reaction force from a load cell and the crosshead displacement from the compression machine, as well as two strain readings [50]. These strain gauges were not used and were replaced with a non-contact alternative, digital image correlation. 3D digital image correlation was used instead of strain gauges, following the approach of Wheeler, to enable comparison of strain contours with FE simulations [27]. Additional details can be found in Section 3.5.

3.2.3 ASTM 7136

ASTM 7136 is the standard test method for measuring the damage resistance of fibre-reinforced polymer matrix composites to a drop-weight impact event [59]. Although ASTM 7136 is written for composites, the drop-weight impact procedure it defines is broadly applicable to sandwich constructions. ASTM 7766 extends the applicability of ASTM 7136 by specifying its use for sandwich panels across various material systems, including aluminum honeycomb cores and metallic facesheets [58]. Furthermore, ASTM 8287, which governs compression-after-impact testing of sandwich constructions, is tied directly to ASTM 7766 and thereby permits the use of the ASTM 7136 impact methodology for aluminum honeycomb sandwich panels. In practice, this linkage ensures consistency in damage creation across both composite and metallic sandwich structures.

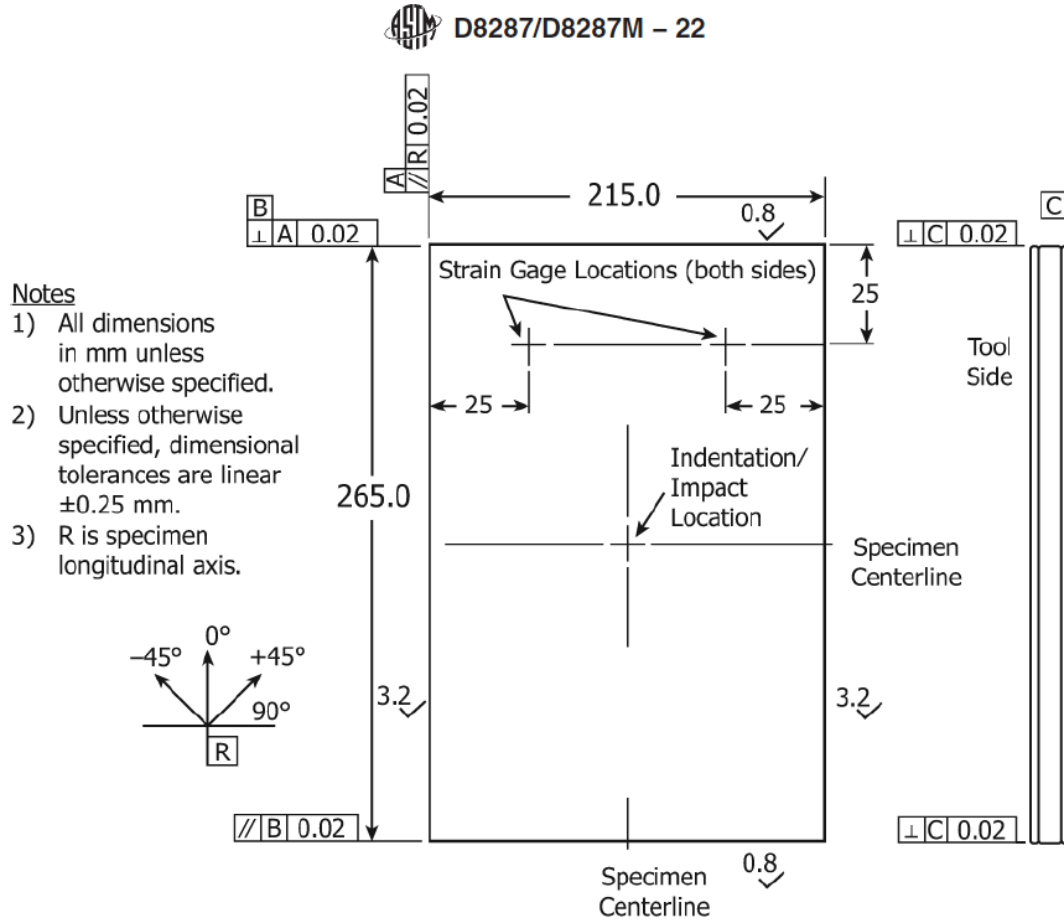


Figure 3.1: ASTM 8287 panel dimensions(mm) and tolerance[50].

The ASTM 7136 standard outlines the creation of out-of-plane impact damage on sandwich panels. The test apparatus shown in Figure 3.3 utilizes a 75mm by 125mm cut-out on a rigid fixture base that mounts a honeycomb panel using four rubber-tipped toggle clamps. This configuration creates an edge-supported specimen representative of unsupported backs in aircraft structures. The standard specifies that the drop tower should include a latching mechanism to release the crosshead, a catch mechanism to prevent secondary indentation during rebound, and guide rails. The indenter must have a smooth surface with a minimum diameter of 16 ± 0.1 mm.

3.2.4 Reference Axis System

The axis system follows the convention shown in Figure 3.4, where the X-axis is aligned with the width of the panel, the Y-axis is the in-plane loading direction of the panel, and the Z-axis is the out-of-plane direction.

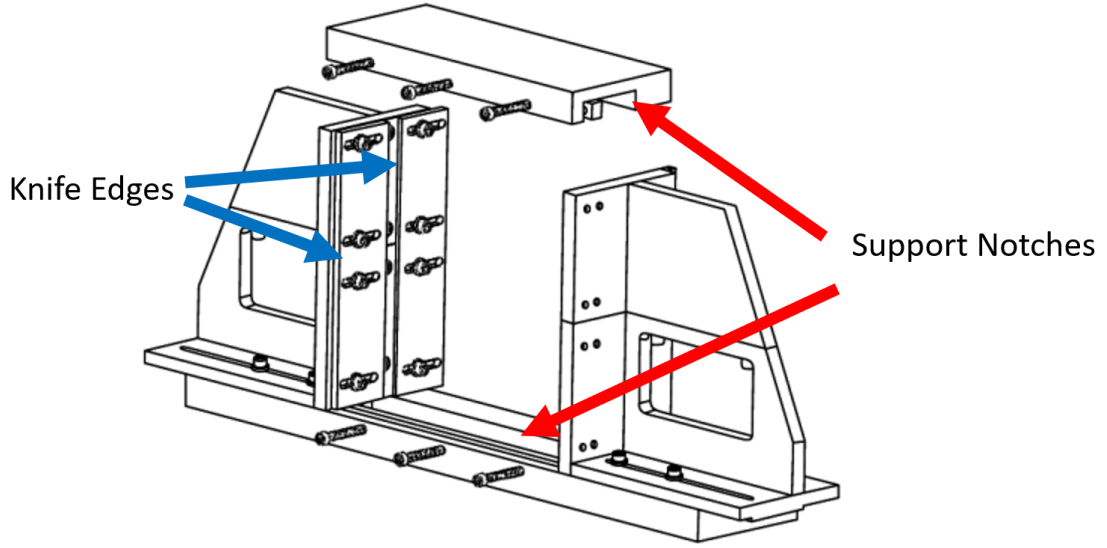


Figure 3.2: Example of ASTM 8287 compliant test fixture[50].

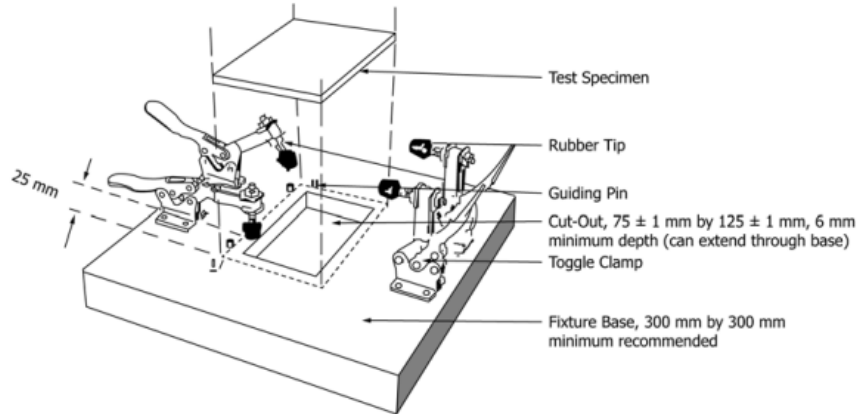


Figure 3.3: ASTM 7136 fixture base with cut out[59].

3.2.5 Panel configuration

A specimen size of 265mm $\times$ 215mm was selected to match the specifications outlined in ASTM D8287 and ASTM D7136 [50, 59]. The core was made of 5052-H32 aluminum with a cell wall thickness (t) of 0.0254mm and a 6.35mm cell size. The facesheets were made of 2024-T3-clad aluminum and were 0.508mm thick.

3.2.6 Indentation

A damage site is defined as the location where the indenter impacts the panel. For this thesis, a dent is defined as an out-of-plane deformation greater than 0.10mm in the facesheet, resulting in a shallow dent. This differentiation is essential when multiple damage sites are

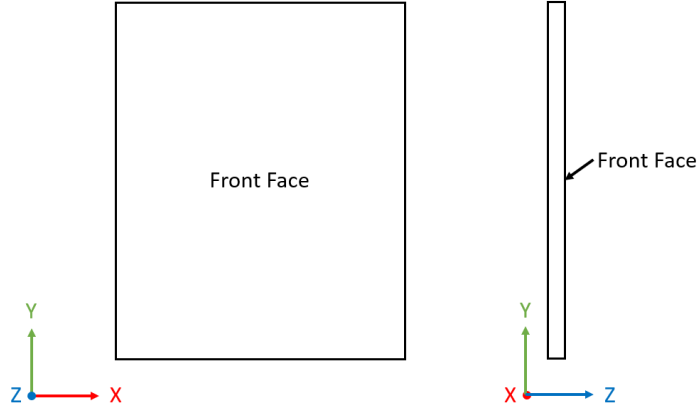


Figure 3.4: Reference axis system that is used to define the honeycomb panels. The direction of the arrows indicates the positive direction.

being studied, two impact sites may be present while forming only one continuous dent.

The ASTM standard recommends using a panel width at least three times greater than the diameter of the indentation, thereby promoting failure localization at the point of impact rather than at the panel edges [50]. Preliminary research by the RMC research group indicated that failure initiated prematurely at the free edges if the panel was not sufficiently wide.

Following the guideline of ASTM 8287, a 25.4mm (1 inch) diameter steel indenter was selected.

Indentation Spacing

The goal was to test a range of dent spacings, from a dent impacting directly on an existing dent to dents that overlap by different amounts, up to dents that do not overlap or interact with each other.

Preliminary testing showed that 30mm diameter dents could be produced by a 25.4mm indenter using a drop height of 152mm. This impact energy resulted in a dent depth of approximately 1.1mm, which can be considered to be BVID. With a panel width of 215mm, this allowed two 30mm-diameter dents to be spaced with a maximum gap of 12mm. The center-to-center distance between the indents was used to determine the spacing. This method provides an easily prescribed measurement in simulations that can be easily transferred to the experimental setup.

Twelve different spacings were tested with center-to-center distances (d) of 0, 5, 10, 15, 20, 25, 30, 35, 40, 45, 50, and 60mm. An example of the center-to-center distance of overlapping dents in the FE simulations can be seen in Figure 3.5.

A reference residual panel strength was determined by performing CAI on a panel with a single indentation. This baseline was used to compare the effect of a second dent at different spacings with the baseline single indentation. The study focuses on the effects of two impact sites as the distance between centers increases from 0mm. Thus, center-to-center distances

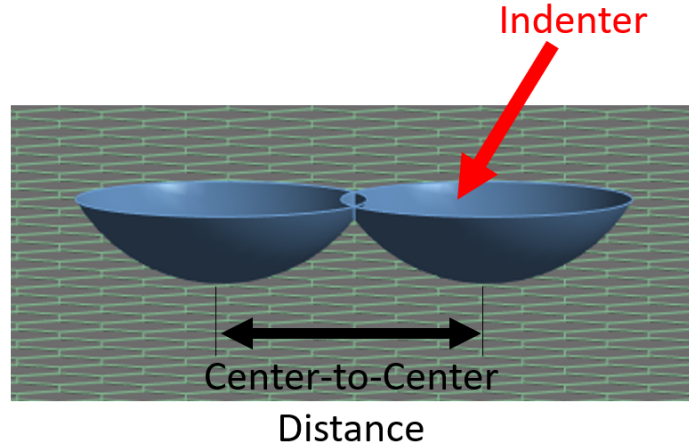


Figure 3.5: Center-to-center distance measurement of two 25.4mm diameter indenters at a 25mm center-to-center spacing.

from 0mm to 30mm result in overlapping damage sites.

3.3 Model Validation

The FE model was validated by comparison with experimental data. For both the FE simulations and the experimental specimens' dent characteristics were first quantified in terms of width, diameter, and depth. The FE and experimental results were then compared using the overall panel stress-strain response during in-plane compression, the peak force, and the facesheet strain contours.

3.3.1 Dent Characterization

The dent characteristics are retrieved from the panel following indentation, however, prior to the application of in-plane compression. Any 0.10mm (0.004 inches) out-of-plane deformation from the original panel surface is considered to be part of a dent. Figure 3.6 shows how the dent characteristics of diameter, width, and depth are measured. Figure 3.6a is specifically for a single indentation, Figure 3.6b is for overlapping damage sites, and Figure 3.6c is for two separate dents. The dent depth measurement shown in Figure 3.6d is common between the three possible spacings. These measurements were retrieved from the results through the use of ImageJ.

3.3.2 Stress - Strain

The true stress-strain curve for the dented panel in compression was determined from the force measured through the panel by the load cell in the experiment and the panel's end displacement controlled by the crosshead of the compression machine.

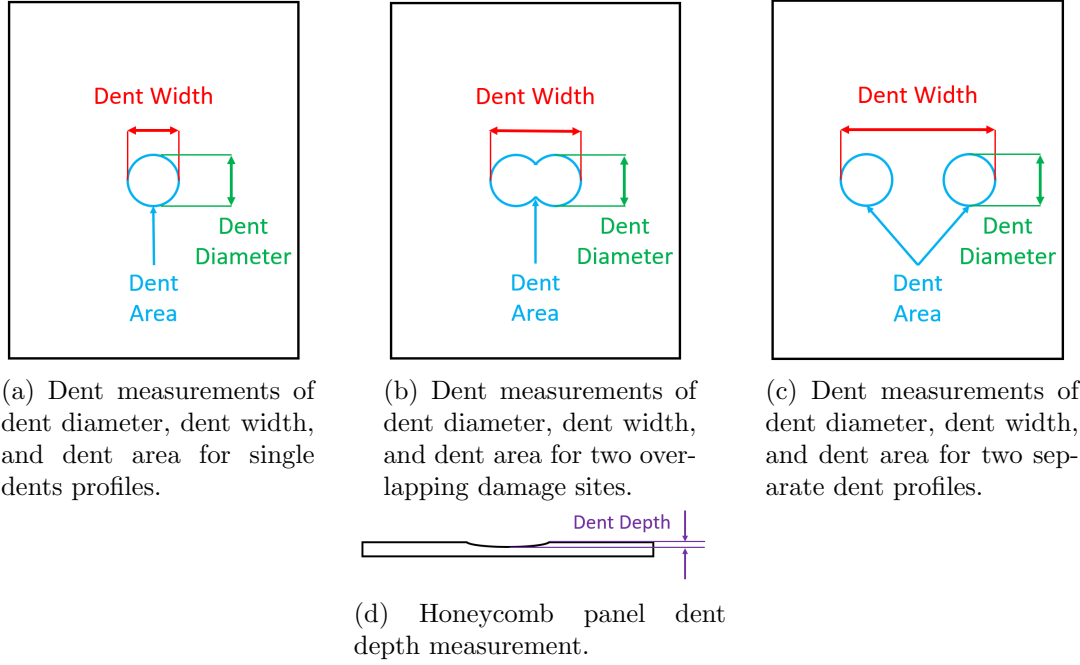


Figure 3.6: Dent measurements of dent depth, dent diameter, dent width, and dent area for all honeycomb panels subject to indentations

The engineering stress (σ_E) was calculated using Equation 3.1 where F is the force through the panel and A_{CS} is the cross sectional area of both facesheets.

$$\sigma_E = \frac{F}{A_{CS}} \quad (3.1)$$

Equation 3.2 uses the facesheet width of 215mm and thickness of 0.508mm to determine the cross-sectional area of the two facesheets. Stress is evaluated based solely on the facesheets, since the honeycomb core carries negligible in-plane load.

$$A_{CS} = 2wt = 2 \times (215 \text{ mm}) \times (0.508 \text{ mm}) = 218.44 \text{ mm}^2 \quad (3.2)$$

Engineering strain (ϵ_E) is calculated as:

$$\epsilon_E = \frac{\delta L}{L_0} \quad (3.3)$$

where δL is the change in length of the panel, and L_0 is the original length of 265mm. From this, the true stress (σ_T) is calculated using Equation 3.4 and true strain (ϵ_T) using Equation 3.5.

$$\sigma_T = \sigma_E(1 + \epsilon_E) \quad (3.4)$$

$$\epsilon_T = \ln(1 + \epsilon_E) \quad (3.5)$$

3.3.3 von Mises Strain

The von Mises strain was used to compare the experimental and FE model results. By default, the FE model calculates the von Mises (Equation 3.6) strain using the principle strains.

$$\epsilon_{vm} = \frac{1}{\sqrt{2}(1+v)} [(\epsilon_1 - \epsilon_2)^2 + (\epsilon_2 - \epsilon_3)^2 + (\epsilon_3 - \epsilon_1)^2]^{\frac{1}{2}} \quad (3.6)$$

Equation 3.6 incorporates through-thickness strain; however, this quantity cannot be measured experimentally. The digital image correlation software VIC-3D employs a modified von Mises strain formulation (Equation 3.7), which assumes zero through-thickness strain [70].

$$\epsilon_{vm_{DIC}} = \frac{2}{3} \sqrt{\epsilon_1^2 - \epsilon_1 \epsilon_2 + \epsilon_2^2} \quad (3.7)$$

For the purpose of comparison, Equation 3.7 was manually entered into the FE software to extract the equivalent strains as the DIC software.

3.4 Finite Element Model

3.4.1 Introduction

An FE model simulating an impact event followed by in-plane compression was created to represent the physical tests specified in ASTM 7136 and 8287 respectively. The FE model used LS-DYNA, a multi-physics explicit solver, and included inertial effects as well as contact between the panel and knife edges, panel and indenters, and core to the facesheets. The models incorporated material plasticity to replicate panel failure and determine their residual strength. Furthermore, dent spacing from 0mm to 60mm center-to-center distances was simulated to first compare the results of the FE model to the experimental data and to study the effects of the proximity of multiple impact sites.

Figure 3.7 outlines the two distinct stages of the FE model:

1. Figure 3.7a is the indentation of the panel. An initial velocity is applied to the indenters, allowing them to impact the facesheet, creating dents.
2. Figure 3.7b is the compression of the panel until failure. An in-plane displacement is applied to the panel, and the reaction force is measured to determine the residual strength of the panel.

3.4.2 Geometry

Figure 3.8 shows the five main components of the model: impactor(s), core, facesheets, adhesive and the knife edges. The model consisted of a 215mm-wide by 265mm-long panel with a 12.7mm thickness and a 6.35mm cell size. With the exception of the knife edges, all geometry was created as surfaces in SolidWorks and exported to ANSYS. The front and back facesheets were 0.508mm (0.020 inches) thick, the ribbon direction of the core is 0.0508mm (0.002 inches) thick, and the cell walls were 0.0254mm (0.001 inches). The facesheets and cell

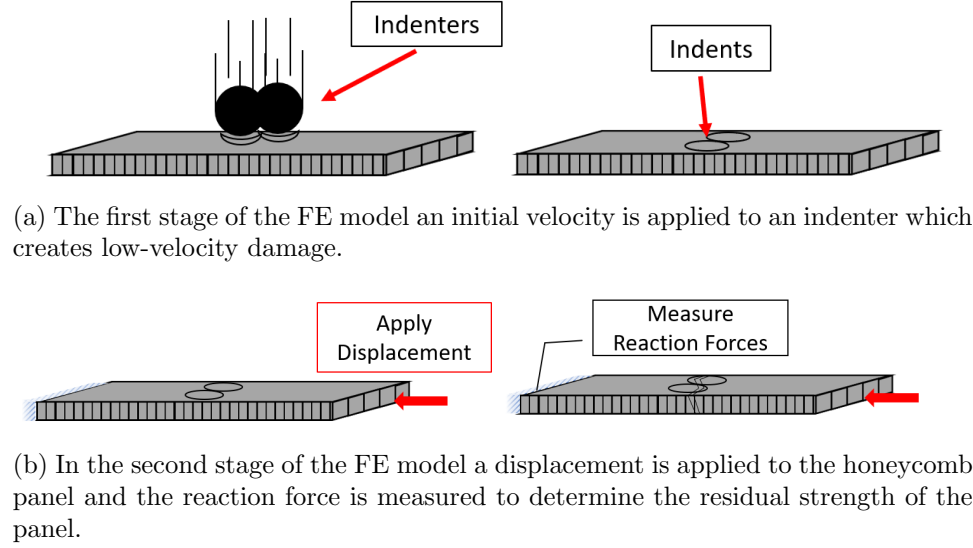


Figure 3.7: The two stages of the FE model.

walls have thicknesses much smaller than their in-plane dimensions, therefore, shell elements can be used instead of solid elements, which further reduces the simulations computational requirements.

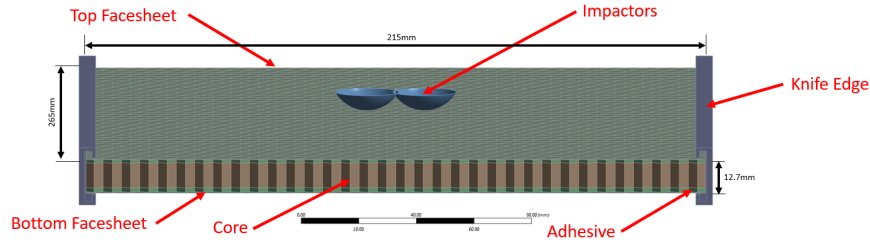


Figure 3.8: The primary components of the model: indenters, core, facesheets, adhesive and the knife edges.

The impactor (indenter) was a spherical profile set to be a 25.4mm (1 inch) diameter. To reduce computational requirements, the entire sphere was not modeled, only the lower surface shown in Figure 3.8. The largest indenter spacing was a center-to-center distance of 60mm as per ASTM 8287's recommendation to select a panel width of more than three times the damage diameter for non-standard specimen configurations [50]. The indenter was represented in the model as a rigid surface with a 0.5mm thickness. The indenter's comparable thickness to that of the facesheet ensured a robust contact definition and reduced the number of nodes required versus modelling a solid indenter.

The knife edges were formed as solid bodies that replicated the real knife-edge geometry and were spaced 0.05mm from the front and back facesheets, fitting tightly without causing contact issues in the model. The adhesive fillets were constructed of equivalent, representative shell elements. The adhesive joins the facesheet to the core and provides support to the core, preventing cell wall crushing at the connection, and causing the core to crush below

the adhesive as shown in Figure 2.6. While previous studies have modelled adhesive fillets as solid elements, this thesis adopted shell elements and calibrated their properties to achieve representative behaviour, as described later in Section 3.4.4. This modelling simplification reduced the simulation run time.

3.4.3 Element types

The geometry comprising the indenter(s), the facesheets, the core, and the adhesive fillets was considered thin-bodies and assigned higher-order quadrilateral shell elements (ELFORM = 23) to capture plasticity and deformation in the panel. The knife edges were created using lower-order rigid solid (ELFORM = 1) since the knife edges act as guides as the panel crushes, and do not experience deformation.

3.4.4 Material Models

Table 3.1 summarizes the material properties used to define the facesheet, core, indenter, knife edges, and the adhesive fillets in the FE model.

Table 3.1: Material properties of each component.

Component	Material	Density (ρ) [kg/m ³]	Modulus of Elasticity (E) [GPa]	Poisson's Ratio (ν)	Yield Strength (σ_y) [MPa]	References
Facesheet	2024-T3-clad Aluminum	2780	73.1	0.33	$270(\sigma_{y,c})$ $330(\sigma_{y,t})$	[19, 77]
Core	5052-H32 Aluminum	2680	70.3	0.33	165	[78]
Indenter	Steel, Adjusted ⁽¹⁾	7850 4155480 ⁽¹⁾	200.0	0.30	—	[26]
Adhesive Fillets	Hexcel® HexPly® EH25	1330	3.7	0.30	—	[79]
Knife Edges	Steel	7850	200.0	0.30	—	[26]

⁽¹⁾The adjusted density for the partial spherical shell indenter to achieve the mass of a solid spherical indenter.

Facesheets

The facesheets were made of 2024-T3-clad aluminum using the plasticity compression-tension material model *MAT_124. The damaged region was expected to undergo plastic deformation and work hardening, and since the 2024-T3-clad aluminum response is significantly different in compression than in tension, *MAT_124 allowed for the use of different material curves depending on the stress state of the element. Figure 3.9 shows the compressive and tensile *MAT_124 curves used in the FE model overlayed with typical tensile and compressive stress-strain for 2024-T3-clad aluminum alloy sheet at room temperature [19]. The compressive data was calibrated by performing a compressive test on an undamaged panel. As the compressive data matched that shown by Rice et al., the tensile data were retrieved from the MMPDS [19]. From testing performed by Wheeler, no cracking of the facesheet was experienced prior to the failure of the panel [27]; thus, a failure strain of 1mm/mm was selected.

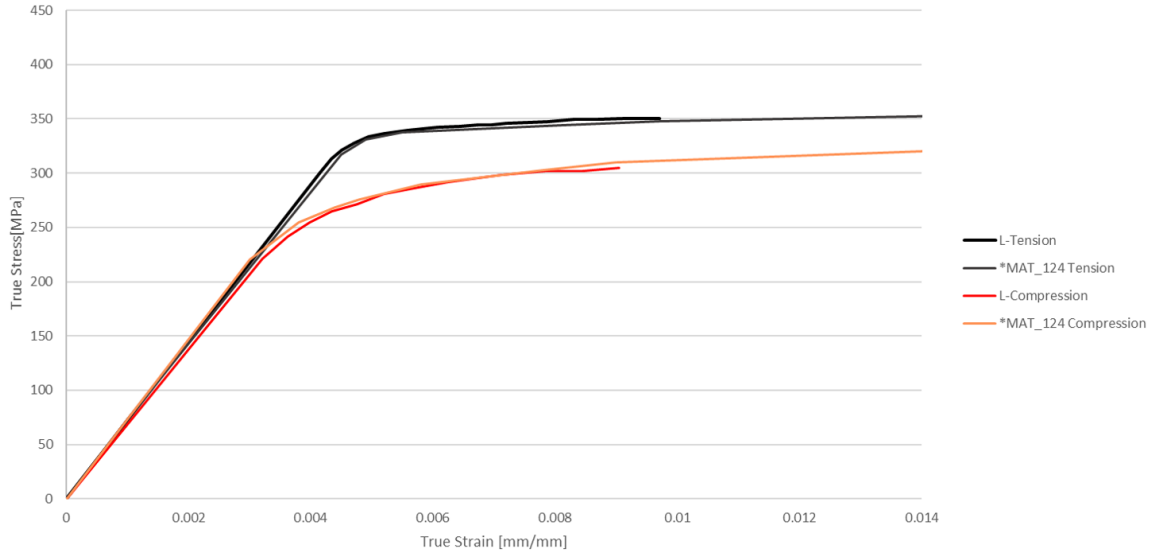


Figure 3.9: L-direction tension and compression stress-strain curves for 2024-T3-clad aluminum alloy sheet taken from Rice et al. overlaid with *MAT_124 curves used in the FE model [19].

Core

The core was made of 5052-H32 aluminum and used a piecewise linear plasticity model(*MAT_024). This material model utilizes the material curve outlined in Figure 3.10. During crushing, plastic deformation localizes in the cell walls, resulting in greater work hardening compared to the facesheets. *MAT_024 was selected because the core material exhibits similar stress-strain behavior in tension and compression. The material properties (Table 3.1) were retrieved from MatWeb [78], and the material curve was extracted from the Atlas of Stress-Strain Curves [80].

Knife Edges

The knife edges were made of steel. In the FE model they were defined as rigid bodies using the *MAT_020 material model. Since they served as guides and were significantly stiffer than the panel, deformations in the knife edges were neglected. Material properties for the steel were found on MatWeb and were necessary for determining sliding interface parameters [81, 82].

Indenters

The indenters were made of steel defined as a rigid body using *MAT_020 material model. Because the indenter was modeled as a partial spherical shell shown in Figure 3.8, the density was adjusted to achieve the same mass as a solid indenter. The physical indenter's mass ($m = 1053\text{g}$) was used to calculate a new density (ρ) of $4.15\text{e}6 \text{ kg/m}^3$ for the FE model indenters.

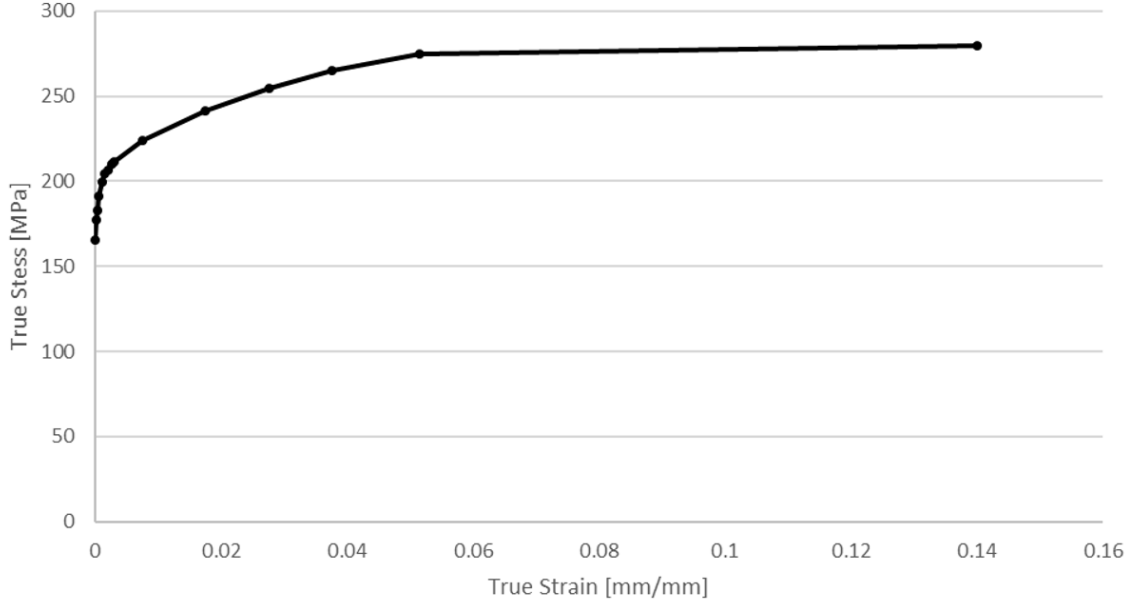


Figure 3.10: Material model *MAT_024 stress-strain curve for 5052-H32 aluminum [80].

3.4.5 Equivalent Representation of Adhesive

Adhesive is used to bond the facesheets to the honeycomb cell walls. Following the curing of the adhesive, the adhesive forms fillets that encapsulate the top and bottom of the cell walls, as shown in Figure 3.11a. The fillets and the encased honeycomb cell wall were represented by an equivalent shell with thickness (t_e) and modulus of elasticity (E_e), calibrated to match the stiffness of the adhesive fillet.

The adhesive fillet was modeled using an elastic material definition because it was not expected to yield or fail. Observations by Wowk et al. indicate that the cell walls within the adhesive do not buckle [35]. Instead, the unsupported core, such as that shown in Figure 2.6, undergoes buckling while the adhesive maintains contact and encapsulates the top and bottom of the core to prevent buckling.

The equivalent shell was created by first representing the real adhesive fillet geometry (Figure 3.11a) as a simplified triangular adhesive fillet (Figure 3.11b). Experimental panels were measured to have an average adhesive fillet height (t_a) of 1.5mm, and the adhesive cross-sectional area was calculated. Next, the area of the triangular cross-section (A_f) was calculated using Equation 3.8, where the width (t_f) of a single adhesive fillet is half the adhesive fillet height. The cross-sectional area of the realistic adhesive fillet (A_f) was then used in Equation 3.9 to calculate an equivalent thickness (t_f) rectangular cross-section adhesive fillet with the same cross-sectional area (A_f) (Figure 3.11c). To achieve the equivalent representation shown in Figure 3.11d, the thickness of the equivalent rectangular adhesive fillet (t_f) was added to the cell wall thickness (t_w) to determine the equivalent adhesive fillet thickness (t_e).

$$A_f = \frac{1}{2} t_a \frac{t_a}{2} = \frac{t_a^2}{4} \quad (3.8)$$

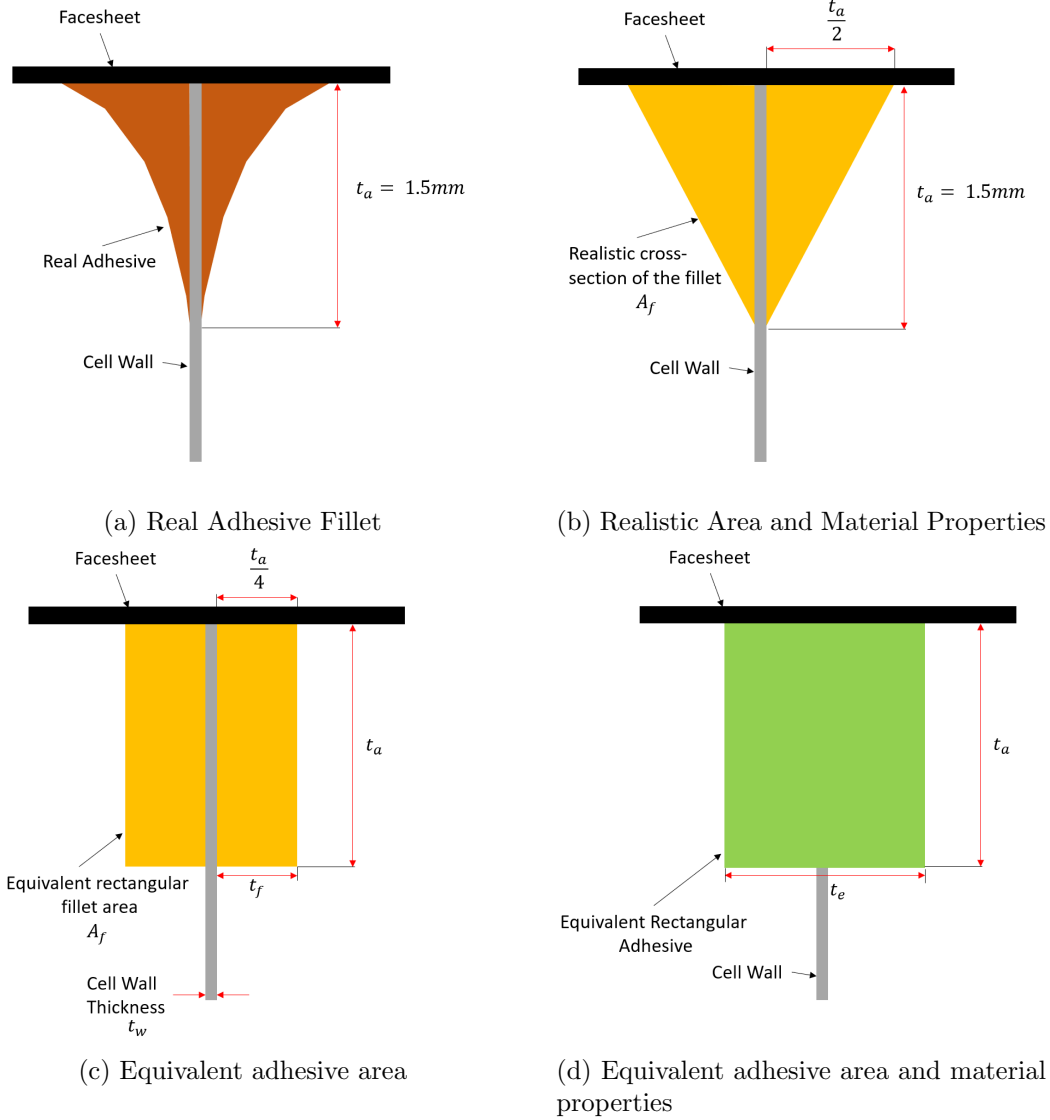


Figure 3.11: Development of equivalent adhesive area and material properties. Black represents the facesheet, grey represents the core material, red represents the real adhesive, yellow represents the equivalent area adhesive, and green represents the equivalent adhesive area and material properties.

$$t_f = \frac{A_f}{t_a} = \frac{t_a^2}{4t_a} = \frac{t_a}{4} \quad (3.9)$$

The cross-sectional area of the supported cell wall (A_w) was calculated in Equation 3.10 by multiplying the height of the adhesive fillet (t_a) by the thickness of the cell wall (t_w).

$$A_w = t_w t_a \quad (3.10)$$

Through Equation 3.11, the modulus of elasticity of the equivalent shell (E_e) was calculated using the modulus of elasticity of the cell wall (E_w), the cross sectional area of the supported

cell wall (A_w), the modulus of elasticity of the adhesive (E_a), and the cross-sectional area of the adhesive (A_f) shown on Figure 3.11.

$$E_e = \frac{E_w A_w + E_a A_f}{A_w + A_f} \quad (3.11)$$

The thickness and modulus of elasticity differ between the ribbon and non-ribbon directions due to differences in cell wall thickness. Table 3.2 outlines the modulus of elasticity and the adhesive fillet width of the equivalent adhesive calculated using adhesive material properties that were obtained from MatWeb and summarized in Table 3.1 [79].

Table 3.2: Equivalent adhesive fillet representation adhesive fillet dimensions and modulus of elasticity in the ribbon and non-ribbon directions shown in Figure 3.11d.

	Ribbon Direction	Non-Ribbon Direction
Adhesive Fillet Height[mm](t_a)	1.5	1.5
Equivalent Fillet Thickness[mm](t_e)	0.801	0.775
Equivalent Modulus of Elasticity[MPa](E_e)	7925	5882

3.4.6 Contacts

Bonded and frictionless contacts were implemented in the FE model. Contact was not needed between the core and the equivalent representation adhesive, as they shared nodes.

Facesheet to Adhesive Contact

Since the adhesive was modelled as an equivalent representation rather than explicitly, a bonded contact was defined between the core and the facesheet. This approach was chosen instead of node sharing so that the facesheet mesh could be refined independently of the core and adhesive.

Indenter to Facesheet Contact

The indenter was assigned an initial velocity and positioned 1.3mm above the front facesheet. A frictionless contact was used to simulate the interaction during impact. Because the indenter struck perpendicular to the facesheet, tangential forces were negligible, and a frictionless definition reduced computational cost without compromising accuracy.

Knife Edges to Facesheet Contact

The knife edges were positioned 0.05mm away from the facesheets. As the panel was compressed, the knife edges loosely constrained it and prevented global bending. A frictionless contact was defined between the knife edges and the facesheets, permitting deformation of the facesheets toward the core but restricting deformation away from it. Frictional effects were excluded in this model because the knife-edge interaction was dominated by normal forces rather than tangential forces, and including friction would have increased computational expense.

3.4.7 Loading and Boundary Conditions

This section outlines the loading and boundary conditions that were applied to represent the support from the drop tower during indentation, as well as the fixtures used in the CAI loading phase.

Indentation Velocity

The objective of the 25.4mm (1 inch) indenter was to achieve a shallow, small dent that allowed for large spacing between dents while maintaining edge distance as outlined in ASTM 8287 [50]. The FE model did not simulate the entire drop height of 152mm used in the physical test, rather, the indenter was placed 1.3mm above the facesheet to minimize the time before contact occurred, therefore reducing computation time. A target dent depth of 1.1mm was determined from preliminary experimental testing and an initial velocity (v) of 1200mm/s was selected, which provided a dent depth of approximately 1.1mm, and a dent diameter of approximately 30mm.

Indenter Boundary Conditions

The indenter was constrained only to move in the vertical direction ($UX = UY = 0$). These constraints allow the indenter to drop, impact the front facesheet, and bounce back in the +Z direction from the facesheet and the core.

Then, in the second stage as illustrated in Figure 3.7b, as the in-plane compression of the panel begins, a displacement of 8mm in the +Z is applied to move the indenter away from the panel to remove any chance of collision with the compressed panel ($UX = UY = 0$, $UZ = +8\text{mm}$).

Honeycomb Panel Boundary Conditions

In the indentation stage of the experiment, the panels are secured with toggle clamps retaining the panel against the base plate. In the FE model, the cut-out in the support plate shown in Figure 3.3, was represented by splitting the bottom facesheet into separate rectangular surfaces. This allowed the central section to remain unsupported and free from all constraints while the surrounding back facesheet regions were constrained in the out-of-plane direction ($UZ=0$) during the first stage of the simulation [59].

Following the research of Boctor et al., the impacts were modelled to occur simultaneously to reduce computational time [26]. Since the two indenters had the same boundary conditions outlined earlier in this Section, they did not interact with one another and only moved in the out-of-plane direction (UZ). The width of the unsupported region was increased to be the sum of the center-to-center distance of the indenters and the cut-out width of 75mm. This provides a similar unsupported region of the total unsupported region in the experiment [59].

A known issue in in-plane compression testing of honeycomb panels is edge buckling in the loading direction. This is prevented by filling the potting compound into the end cells in the

loading direction to support the panel, allowing the damage to localize around the damage in the panel [50]. The FE model replicated this through the use of edge constraints for all exposed in-plane edges as shown in Figure 3.12. The panel end edges were selected because the edges would be constrained by the potting compound in the experiment. In the first stage (indentation), these edges are constrained to prevent panel movement ($U_x=U_y=U_z=0$). Then, in the second stage (compression), the lower in-plane edge is displaced to represent the crosshead displacement, creating an in-plane compression ($U_y = +2.5\text{mm}$). The planar displacement of 2.5mm is sufficient to cause panel failure.

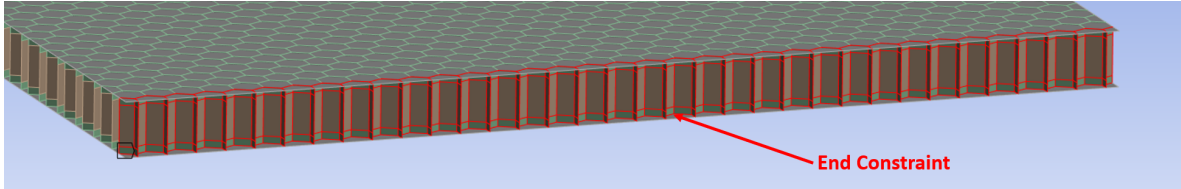


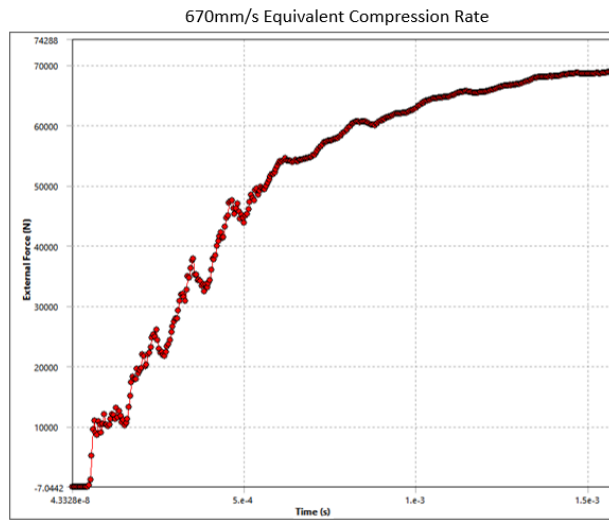
Figure 3.12: End constraint applied to edges of facesheet and core to represent adhesive-filled area preventing local buckling.

Knife Edge Boundary Conditions

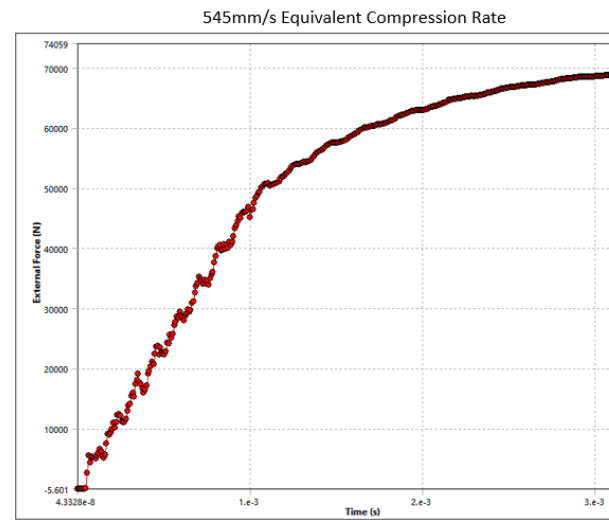
In the experimental fixture, the knife edges are fixed in the panel's in-plane direction and attached to a base plate mounted to the compression machine. The knife edges were adjusted to be snug against the panel and are tightened with eight screws, making the knife edges a rigid guide. For the FE model, the knife edges are constrained in position ($U_x=U_y=U_z=0$).

Compression Rate

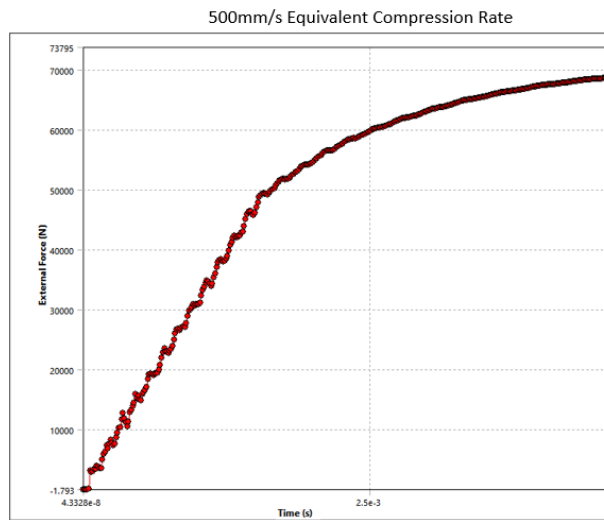
ASTM 8287 recommends a crosshead displacement rate of 1.25mm/min, allowing for a quasi-static loading of the panels [50]. Since the explicit solver requires small timesteps, to achieve the desired 2.5mm displacement at 1.25mm/min would create a simulation time in excess of 57 years with the available computational capabilities. Instead, the rate of applied displacement was increased to 500mm/s to achieve a second-stage simulation time of 21 hours. Increasing the rate of applied displacement causes oscillations in the reaction forces due to stress-wave propagation. Figure 3.13 shows the reaction force during in-plane compression plotted against the simulation time for 670mm/s (Figure 3.13a), 545mm/s (Figure 3.13b), and at the selected 500mm/s (Figure 3.13c). 500mm/s shows minimal oscillations and has a second-stage run time of 21 hours vs 4 hours for 670mm/s.



(a) Reaction force over time at a 670mm/s crosshead displacement rate.



(b) Reaction force over time at a 545mm/s crosshead displacement rate.



(c) Reaction force over time at a 500mm/s crosshead displacement rate.

Figure 3.13: The effects of oscillations in the residual force as a result of the stress wave propagation at different crosshead speeds.

Time

Figure 3.14 is the reaction force plotted over the simulation time in Stage 1 of the simulation. At 0s, the indenter starts travelling towards the panel at 500mm/s and impacts the panel. Between 0 and 0.0004s, the indenter is approaching the panel; however, it has not contacted the panel, thus there is zero residual force. At 0.0004s, the indenter contacts the panel. From 0.0004s to 0.0025s, the indentation is created by the contact, the indenter and the front facesheet, resulting in the maximum reaction force of -200N. Between 0.0025s and 0.004s, the panel springs back, reducing the reaction force from -200N to -75N. An additional 0.002s was then added to allow the dent to reach its final dent depth and allow any oscillations to stabilize. Thus, from 0.004s to 0.006s there is no significant change in reaction force. Stage 1 takes place within the time frame of 0s to 0.006s, following that, Stage 2 begins.

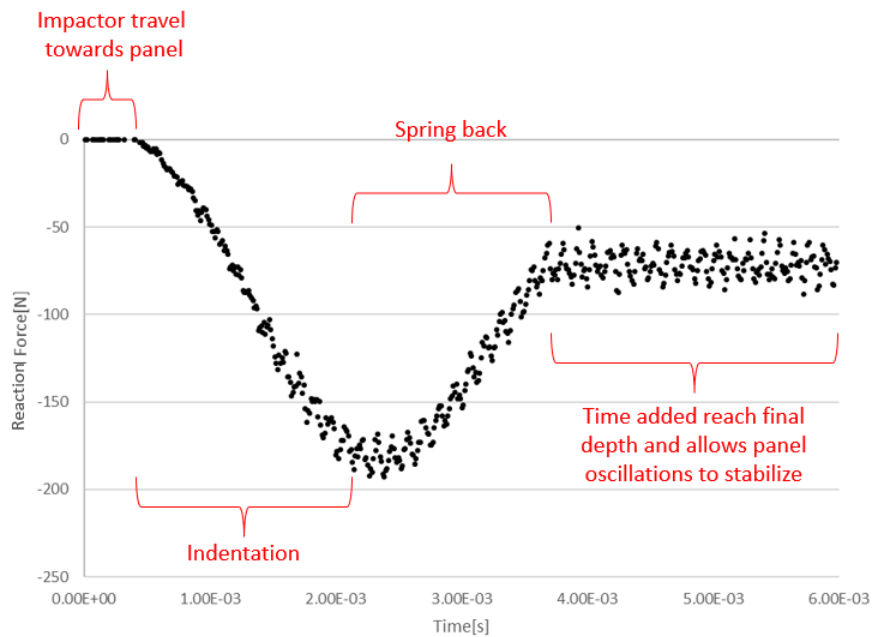


Figure 3.14: A typical plot of the reaction force over the time frame of Stage 1(0s to 0.006s).

During compression, the dent deepens as the facesheet is compressed. The crushed core beneath the dent provides reduced support to the facesheet, allowing further deformation to occur. As the dent depth increases, the facesheet becomes increasingly unstable under compression until it reaches a critical point where buckling occurs. This loss of stability results in a reduction in the load-carrying capacity of the panel and corresponds to the peak force observed during the compression stage.

Figure 3.15 shows the reaction force of a typical panel over the simulation time in Stage 2 of the simulation. Stage 2 starts at 0.006s, and a 2.5mm displacement is applied at 500mm/s in the loading direction of the panel. The panel experiences this displacement over 0.005s, during which it exhibits its peak force. The peak force, defined as the maximum reaction force experienced by the panel during compression, represents the residual strength of the panel, as this load corresponds to the onset of facesheet buckling. In Figure 3.15, the panel achieves a peak force of 67.5kN at 0.0095s. The panel then fails between 0.0095s and 0.011s.

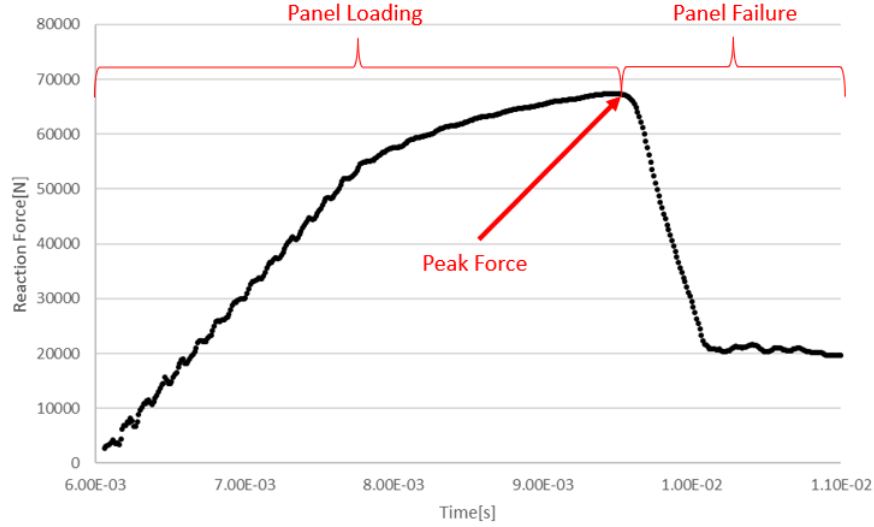


Figure 3.15: A typical plot of the reaction force over the time frame of Stage 2(0.006s to 0.011s).

3.4.8 Meshing

Higher-order shell elements were used to mesh the honeycomb panel and the indenter geometry. Lower-order elements were used to define the solid elements for the knife edges because higher-order was not available in LS-DYNA. The facesheets were meshed using a simple sweep mesh, and the core and adhesive were meshed using a sweep mesh and edge divisions, creating hexahedral shell elements for all planar geometries. The knife edges have a constant cross-section and were also swept, creating solid elements.

The peak force of the panel is highly dependent on the stiffness of the facesheet, adhesive and core. Therefore, a mesh convergence study was required to evaluate and reduce discretization error and determine an optimal mesh density for an FE model with a reasonable run time. The three primary components considered in the mesh studies are the facesheets, the core and the adhesive.

Mesh Refinement Methodology

The shell geometry and defined contacts pose a unique challenge for studying the effect of the mesh on the panel's stiffness. The facesheet mesh is generated using a simple sweep mesh, controlled by an element size parameter. This same size parameter controls the indenter and the knife-edge element size as they come in contact with the facesheets.

The core and the adhesive share nodes to achieve an optimal mesh. For this thesis, the mesh resolution for the adhesive and the core will be referred to by the number of elements across the horizontal edge, followed by the number of elements across the vertical edge. An example of a 2x2 adhesive and a 2x3 core can be seen in Figure 3.16.

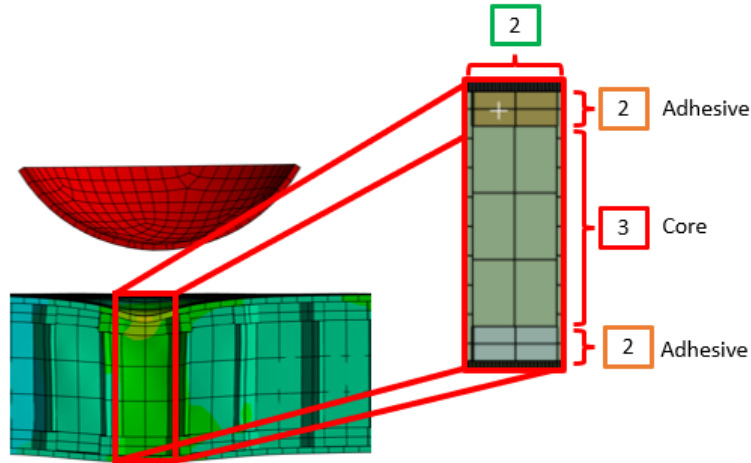


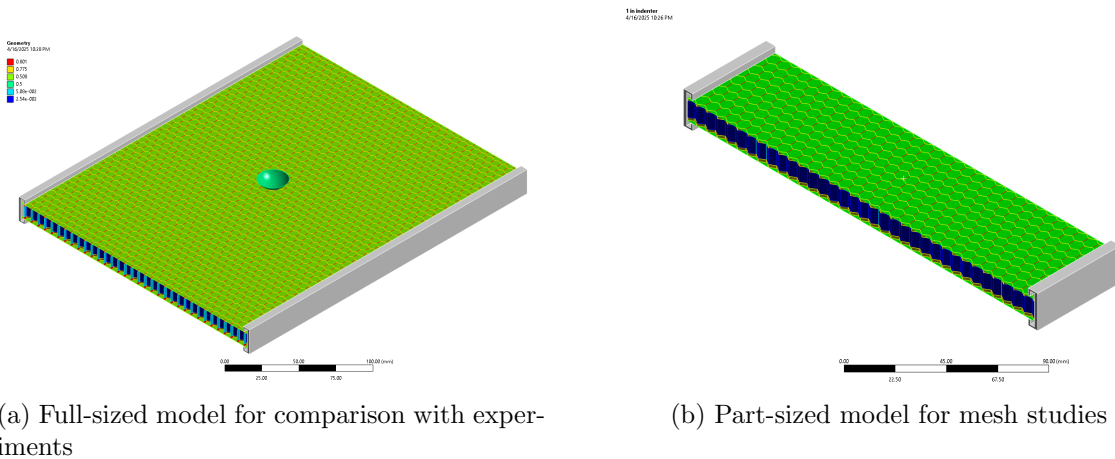
Figure 3.16: The core mesh was defined horizontally and vertically. Shared nodes are found between the core and adhesive creating a 2x2 adhesive and a 2x3 core.

Mesh Convergence Study

The overall approach was to first study refinement of the mesh in the facesheets, then the core, and the adhesive fillet. No independent study is required for the knife edge or indenter geometries because the knife edges and the indenter resolution were both set by the same element size parameter used on the facesheet to ensure robust, frictionless contacts.

Facesheet study

The facesheet study used a part-sized model (Figure 3.17b) that was $\frac{1}{5}$ the length of the full-sized panel (Figure 3.17a) with no indentation to reduce run times. Due to the simple geometry of the facesheet, an overall element size of 3x5 and 1x3 adhesive were defined. The bonded contact of the core to the facesheet mesh allowed the facesheet mesh to be changed independently of the adhesive and the core.



(a) Full-sized model for comparison with experiments

(b) Part-sized model for mesh studies

Figure 3.17: Full-sized FE model used for comparison with experiments and the part-sized model used for mesh studies.

The study focused on the peak force associated with the buckling of an undamaged panel at different resolutions of the facesheet mesh. Figure 3.18 shows the peak force plotted against the number of nodes with each of the data points labeled with the associated facesheet element sizes of 0.75, 1, 1.25, 1.5, 1.75, 2, 2.5 and 3mm. A facesheet element size of 1.25mm was selected as there was only a 0.1% difference between the last three points.

Core study

The core study focused on determining the appropriate number of elements along the horizontal edge. The number of elements along the vertical edges was chosen to maintain an aspect ratio as close to 1 as possible. Figure 3.19 shows the 7 core meshes used in this study, ranging from 1x1 to 7x13. The core study used the full-sized model as shown in Figure 3.17a, which includes the indentation stage.

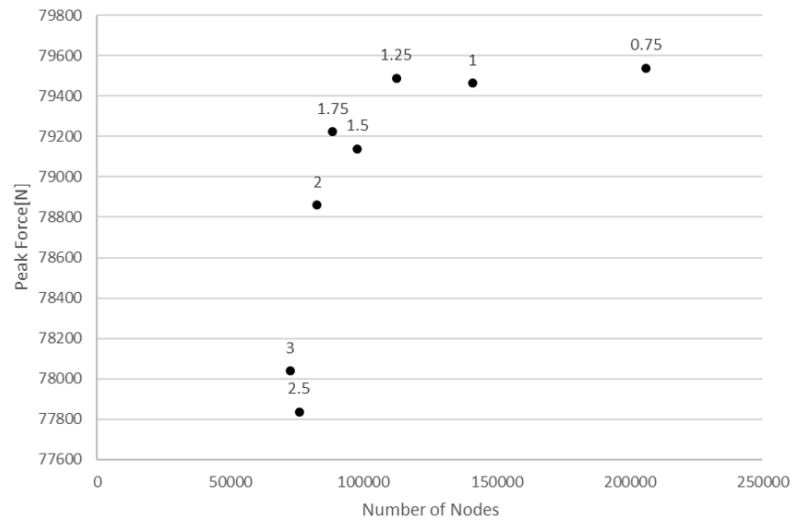


Figure 3.18: The peak force plotted against the number of nodes for an undamaged part-sized panel. The data points are labeled with the trialed facesheet element sizes in millimeters.

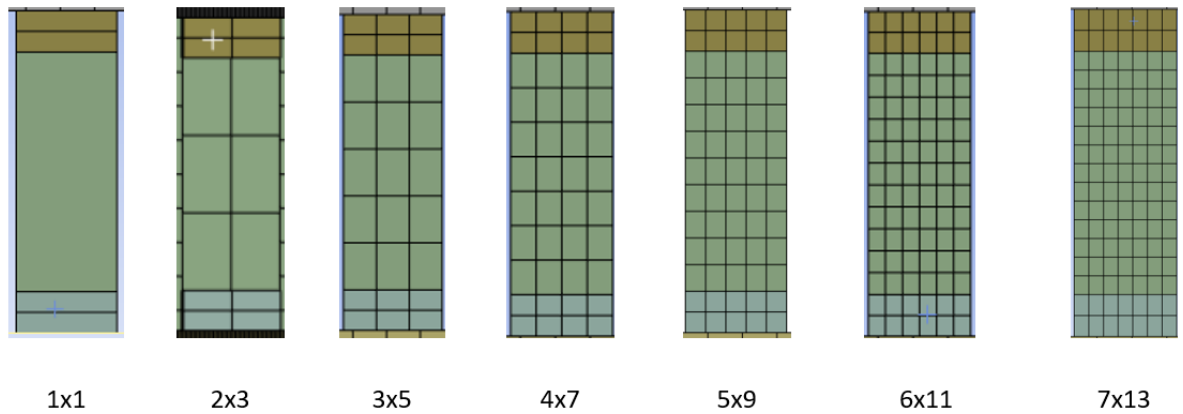
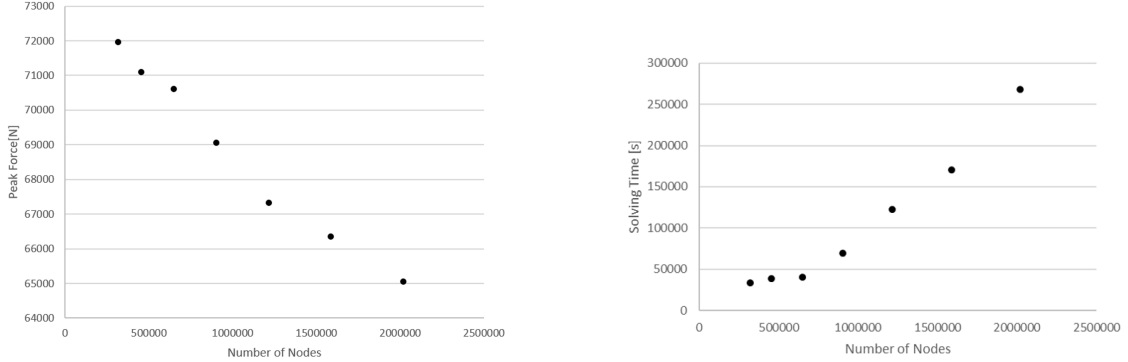


Figure 3.19: Core and adhesive fillet meshes trailed for core convergence. Note that the adhesive maintained 2 vertical divisions.

From the aforementioned core resolutions, Figure 3.20a does not show convergence in the peak force. The solving time of the most refined mesh (13x7) was in excess of 74 hours as per the run times shown in Figure 3.20b. Further mesh refinements would be needed to achieve convergence using peak force; however, were not possible with the computational resources available. Because of this, the dent depth, the modal response of the cell walls, and the stress-strain comparison were used to determine the element size.



(a) Peak Force plotted against the number of nodes. A significant deviation from the linear reduction in peak force (red) indicates convergence with a higher core density

(b) Plot of solving time relative to the number of nodes in the model. With increasing nodes, there is an exponential increase in solving time.

Figure 3.20: Peak force plotted against core resolution and the associated solving times.

Research by Wowk et al. found mesh size convergence in examining the dent depth [35]. The dent depth was plotted against the nodal count in Figure 3.21. Using the dent depth as the convergence criterion, a 5x9 core spacing was deemed suitable.

Second, the number of buckling lobes in the core at peak force is examined in Figure 3.22. The 1x1, 2x3, 3x5, and 4x7 core meshes have a bimodal (cell wall with a two folds) cross-sectional crumple profile, whereas the 5x9, 6x11, and 7x13 show a trimodal (cell wall with three folds) response. In the core resolutions studied there were no greater than three folds present. The 5x9 mesh is the coarsest mesh to show the cell wall crumpling with 3 folds and was used in this thesis.

Lastly, in plotting the true stress-strain curves as shown in Figure 3.23 for all mesh resolutions, the resolution affects the point at which facesheet buckling occurs. As expected, increasing the resolution increases the core's flexibility and thus caused the panel to fail sooner. A preliminary sample of an experimental test is shown in black in Figure 3.23. The 5x9 and 4x7 core resolutions both show similar stress-strain curves to that of the experimental test. From examining these results, the 5x9 core was selected as the mesh that would be used in the FE model.

Adhesive Study

The adhesive has been shown to stabilize the core, and all buckling occurs below the adhesive, within the core as per research by Wowk et al. [35]. A study was performed with 1-4 adhesive divisions and found only a 1.5% difference in peak force. Based on these results, the fillet was set to have 2 vertical divisions.

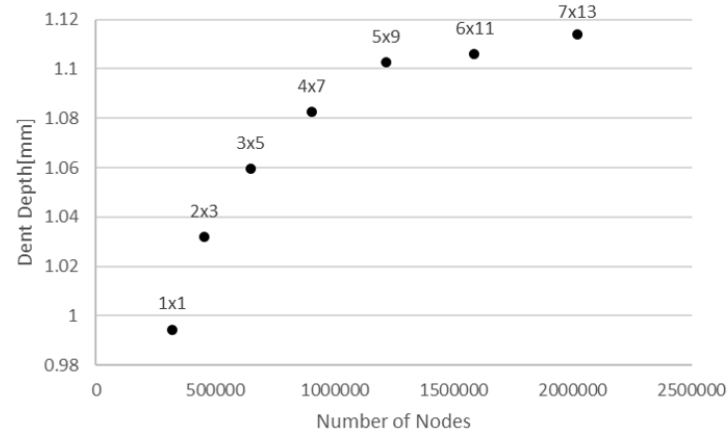


Figure 3.21: Dent depth plotted against the number of nodes. The core resolutions are present as the data labels.

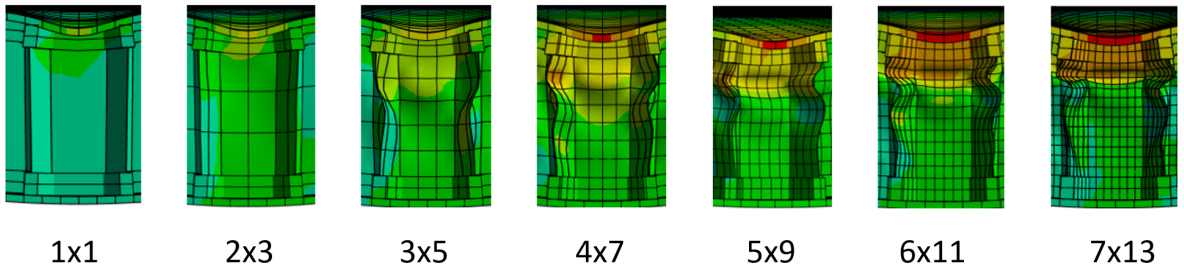


Figure 3.22: Cross section of core at peak force with different core resolution showing different modal responses of the core.

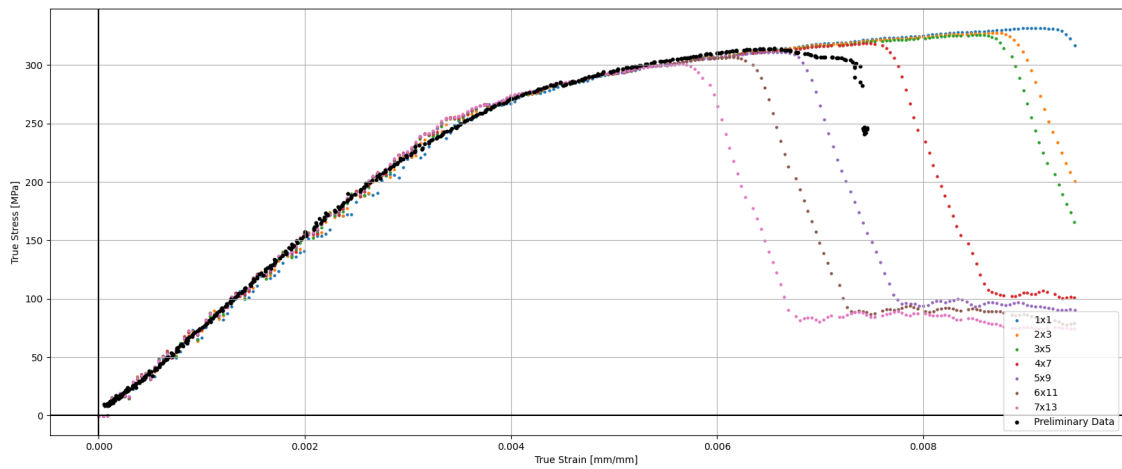


Figure 3.23: True stress-strain curves of all core mesh resolutions in addition to a preliminary sample experimentally tested.

3.5 Experimental Testing

3.5.1 Introduction

Experimental testing was performed according to a modified ASTM 8287 standardized testing procedure to validate the FE model. Test panels were created according to the ASTM 8287 parameters as shown in Figure 3.1, and 3D digital image correlation was employed to observe the displacement and strain of the panels under compression. This thesis follows the experimental methodology presented by Wheeler [27].

3.5.2 Digital Image Correlation

DIC was developed as a non-contact solution for measuring the shape, motion, and deformation of solid objects. This measurement method is achieved by capturing changes in the position of subsets of an isotropic non-repeating speckle pattern from the reference image. In the context of speckle patterns, isotropic is defined as random pattern with similar sized speckles. These changes in position can be used to determine shape changes, motion, and deformation. In a typical tensile or compressive test, a strain gauge is often used to measure the change between two points on a sample. While useful for measuring precision levels of strain, strain gauges are limited to the area they cover and only report one value. The application of DIC in experimental testing allows for the reporting of strain and displacement contours across the visible focus area of a sample.

In 2D DIC, a single camera is placed perpendicular to the test sample and all deformations, movement, and shape changes are assumed to be planar. 3D DIC or stereo DIC is an application of two or more cameras to capture the out-of-plane deformation, movement, and shape changes. 3D DIC achieves this by calibrating the two cameras in free space using standard calibration plates, allowing the system to determine their relative positions. The application of 3D DIC allows for a direct comparison of full-field measurements of deformation and strain with those of FE model methods.

3.5.3 Sample Preparation

All the test panels were created from a single raw-stock panel. The panel was rough-cut, precision-cut, potted, cleaned, and inspected. The panel then received the appropriate dent spacing configuration before the application of the speckle pattern. This procedure follows that outlined by Wheeler, meeting the criteria of ASTM 8287 [27, 50].

Panel Materials

The aluminum honeycomb panel used for the testing was Geneerco No. 3 Type 119C, provided by Upper Canada Steel Incorporated, lot number 101723. The data sheet is in Appendix A, Figure A.1. The data sheet in Figure A.2 lists the facesheets as 0.508mm (0.020 inch) thick aluminum 2023-T3-clad, and the core is composed of 6.35mm (0.25 inch) cells of 5052-H32 aluminum with a ribbon thickness of 0.0254mm (0.001 inches). The total panel thickness was 12.7mm.

Panel Preparation

As per Figure 3.1, the panels were prepared to 215 ± 0.25 mm width by 265 ± 0.25 mm length, ensuring the length was in the ribbon direction [50]. The specimens were first rough cut using a skill saw from a single 1.22 x 2.75 meter (4-foot width by 8-foot length) panel, then processed with a precision tile saw to bring the panel to the desired dimensions. The panels were placed in a dryer for 15 minutes at 60°C to remove any remaining water from the tile saw cut.

End Potting

ASTM 8287 recommends applying potting compound to the top and bottom (+Y and -Y) edges of the test panel to prevent curling of the facesheet during loading [50]. Following the research of Wheeler, painters' tape was applied to the facesheet along the edge to protect from spillover and assist with clean up [27].

Wheeler showed the LePage PL Premium Construction Adhesive was effective in providing sufficient strength and adhesion [27, 83]. The adhesive was applied to all exposed cells along the ends of the panel. The adhesive expanded slightly during curing, creating some spillover. Once the adhesive cured, a utility knife was used to remove excess material without damaging or removing the aluminum. An orbital sander was used to remove most of the excess adhesive, followed by hand sanding with a machinist's block and 180-grit sandpaper to provide a flat sanding surface [27].

The painter's tape was removed following the sanding with no spillover remaining on the facesheets. The panels were measured and documented as per Figure B.1 in Table B.1 in Appendix A to verify measurement conformity.

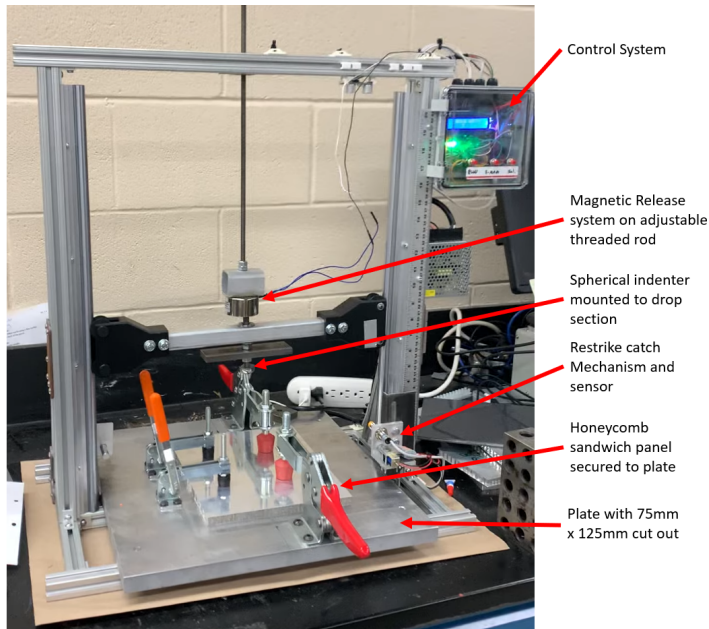
Specimen Cleaning and Inspection

With the panels sized, all facesheets were cleaned with methyl ethyl ketone and inspected for damage, such as scratches or impacts from shipping or handling. Any notable observations were recorded and minor surface scratches were noted. The back facesheet was marked with the sample number, and the center was located on the front facesheet using a ruler and fine-tipped marker. Using the center and edges, the assigned dent-impact locations were marked on the facesheet.

Damage Creation

The panels were dented using the drop tower in Figure 3.24a. The drop tower was modified to be compliant in accordance with ASTM 7136 [59]. The tower's frame was constructed of aluminum extrusion with two vertical rails to provide alignment to the drop section. The drop section contained a rail guide and a half-spherical indenter. Additional weight was added to achieve a total drop mass of 1053g. The base plate matched that in Figure 3.3 with a 75mm by 125mm cut-out. Rubber-tipped toggle clamps held the panel against the base plate. A control system was devised to release and then catch the drop section and prevent a re-strike of the panel through the use of a Hall effect sensor.

The system was operated by lifting the drop section as shown in Figure 3.24b to the engaged electromagnet to retain it at the pre-calibrated height of 152mm. The panel was then aligned in position over the cut-out and secured to the base plate using the toggle clamps. The control system was then activated. The system released the drop section, allowing it to impact the panel, which then activated the catch mechanism and caught the drop section before it re-struck the panel. The drop arm was then lifted and secured by the electromagnet, and the panel was repositioned for a second strike or removed from the base plate.



(a) Drop tower used to indent the panels.



(b) Drop tower section that is in free fall during indentation shown being weighted.

Figure 3.24: Drop tower system for indenting the panels and drop section mass indenting the panels in free-fall.

Following the impact, the panels were brought to a granite table like that in Figure 3.25, where the edges were placed on the machinist blocks. A vertical dial indicator was then zeroed to the front of the facesheet and run over the surface until a deformation greater than 0.10mm was observed. A marker point was placed to indicate this point, and the same was repeated on the other side of the dent. The distance between these points was used to preliminarily determine the dent width. The dent depth was measured to be the maximum dial indicator reading taken at the center of the dent. These preliminary measurements were taken to ensure consistency between panels. Each panel was then visually inspected to ensure no cracking or punctures of the facesheet. Due to cost and time constraints, only one sample was manufactured for each dent spacing. Figure 3.26 shows a typical example of a panel prior to paint that has been inspected.

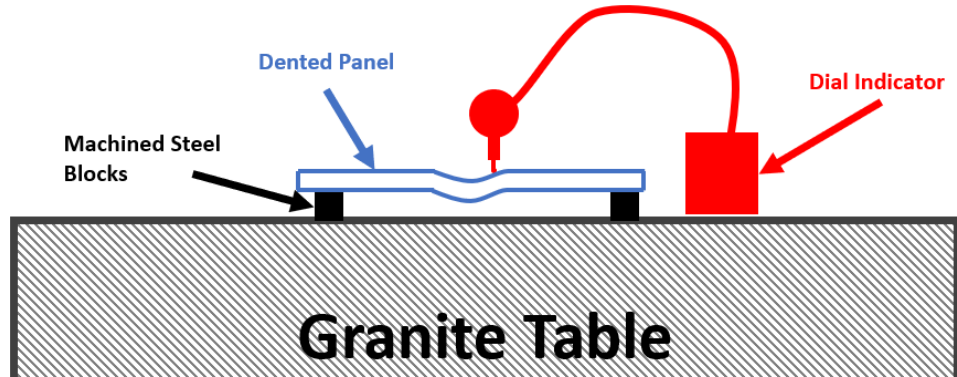


Figure 3.25: Preliminary dent measurements taken using machinist blocks and a dial indicator.

Panel Preparation

The front face was cleaned with methyl ethyl ketone, air-dried, and ensured to have no contact with any other surfaces or contaminants prior to painting. The objective of the speckle pattern is to make up 50% of both high contrast colours (black and white) [69].

The painting process consisted of three coats of Rust-Oleum Painter's Touch 2X Interior/Exterior Ultra Cover Multi-Purpose Paint and Primer in Flat White to ensure a solid base with no reflections. Then, a black speckled pattern was applied using Rust-Oleum Painter's Touch 2X Interior/Exterior Ultra Cover Multi-Purpose Paint and Primer in Flat Black [84]. The speckle pattern was applied to create an isotropic, non-repeating pattern on the observed facesheet. The speckle pattern density was matched with work performed by Wheeler to achieve the desired 3-5 pixels per speckle [27, 69]. Figure 3.27 shows an example of a panel painted with a flat white base followed by a flat black to create the speckle pattern.

Equipment

The experimental testing was performed on a MTS 204.71 tensile/compression machine running MTS Flex Test 40 Station Manager V5.9E as shown in Figure 3.28. The MTS provided outputs of force and displacement. Testing was performed using ASTM 8287-compliant support fixtures provided by Carleton University Department of Mechanical and Aerospace. Figure 3.29 shows the panel orientation loaded in the MTS, the load cell, and the fixture components and the actuated and fixed components of the MTS.

Strain gauges were used to validate the DIC. The strain gauges were fed into a HBM QuantumX 1615b Analog/Digital Digital Acquisition (DAQ) device running HBM V5.2.2.19 software package.

DIC Setup

The setup was identical to that performed by Wheeler [27]. Two Basler acA2440-77um 5 MP cameras, each mounted with Schneider-Kreuznach Citrine 1.9/35 C were mounted at approximately 25°. The cameras were pointed towards the sample on rigid tripods with

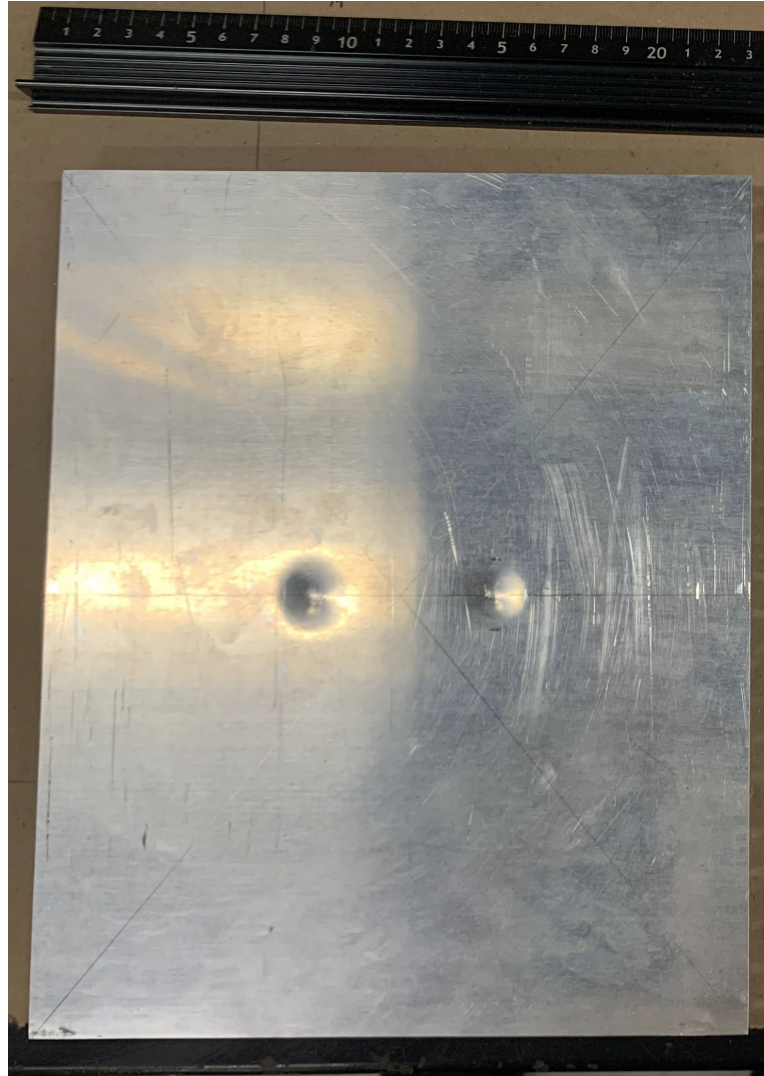


Figure 3.26: Image taken of the 50mm center-to-center configured panel before the panel has been cleaned and inspected.

lighting, and cabling to a desktop computer. The desktop computer captured the DIC imagery using VIC-Snap. The DIC system captured images of the sample used for post-processing in VIC-3D 9 to measure displacement and strain. The setup seen in Figure 3.28 shows the use of the two cameras and the lighting positioned.

Data collection

The MTS computer recorded data, including the machine's displacement and force at 10Hz. The DAQ system was configured to record data at a frequency of 2 Hz, and VIC-Snap also captured images at 2 Hz. The MTS, the strain gauges, and the DIC fed wired connections into the DAQ. The VIC-Snap software captured the images and triggered the DAQ to read both the MTS displacement and force, as well as read the strain gauge if connected [27]. This provided data that was directly attributed to each image taken, which could be used in post-processing.

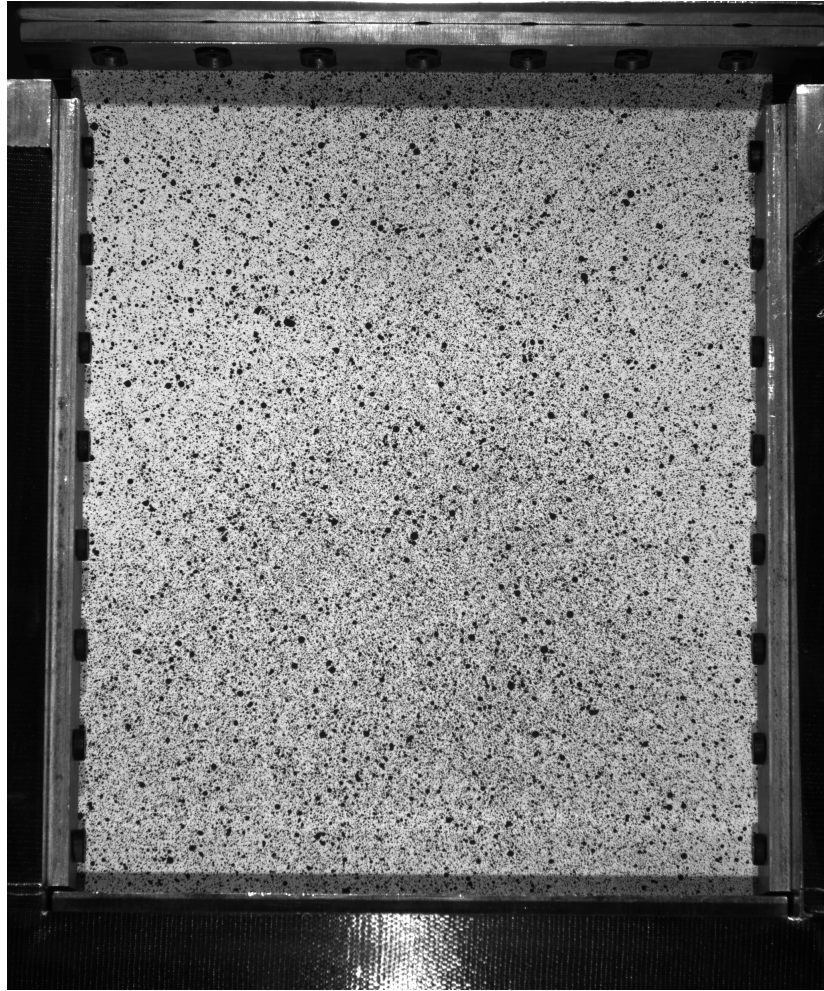


Figure 3.27: Image taken of the 50mm center-to-center configured panel after application of the speckle pattern.

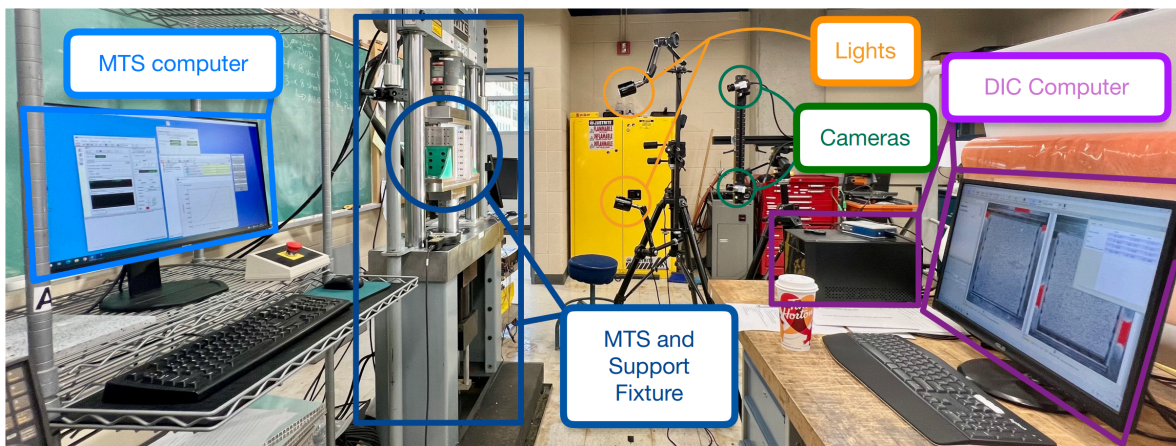


Figure 3.28: DIC setup outlining all the essential components [27].

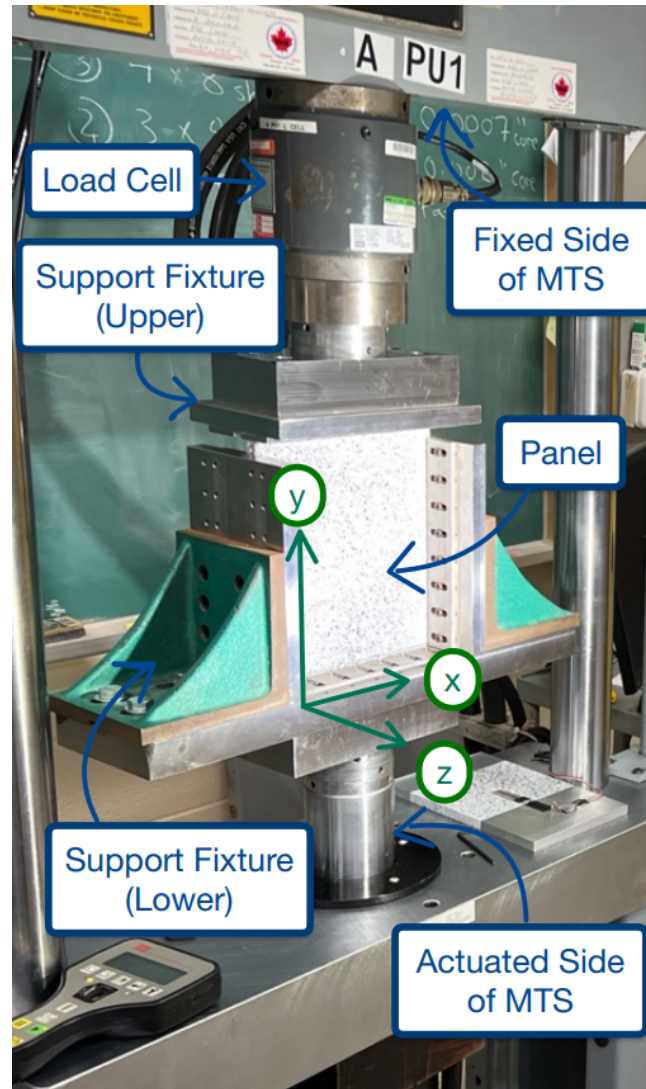


Figure 3.29: ASTM 8287-compliant support fixture installed in MTS test system [27].

DIC Calibration

3D DIC calibration was necessary to produce accurate deformations and strain data. Calibration uses a plate with a grid of high-contrast dots of known size and location for the software. The calibration images are used in the post-processing software VIC 3D. Before testing, calibration images of the panel are taken at different angles and run through VIC 3D. VIC 3D uses the image to determine the exact positions of each of the cameras relative to one another through its software.

VIC 3D provides a score that measures the average distance between the theoretical and actual positions of grid points in the calibration images with lower the score, the better. It is recommended to consider a score of 0.1 acceptable [85]; the experimental testing achieved a score of 0.058.

3.5.4 Strain Validation

The DIC data needed to be validated to ensure it was comparable to existing testing measurement methodologies. Wheeler devised a system comparison method shown in Figure 3.30 [27]. This method uses a 152mm wide by 265mm tall by 12.7mm thick milled aluminum bar mounted with a 30 mm strain gauge. Around the strain gauge, the aluminum bar is prepared with a speckle pattern so that the DIC virtual extensometer can be compared directly with the strain gauge mounted on the aluminum bar. Consistent with Wheeler's research, the panel was captured using a DIC system, and the strain gauge system was loaded in compression at 0.75mm/min and halted once a force of 150kN was reached. This ensured that the test specimen remained within the material's elastic deformation limits. In the DIC post-processing, two virtual extensometers were applied and compared with the strain gauge.

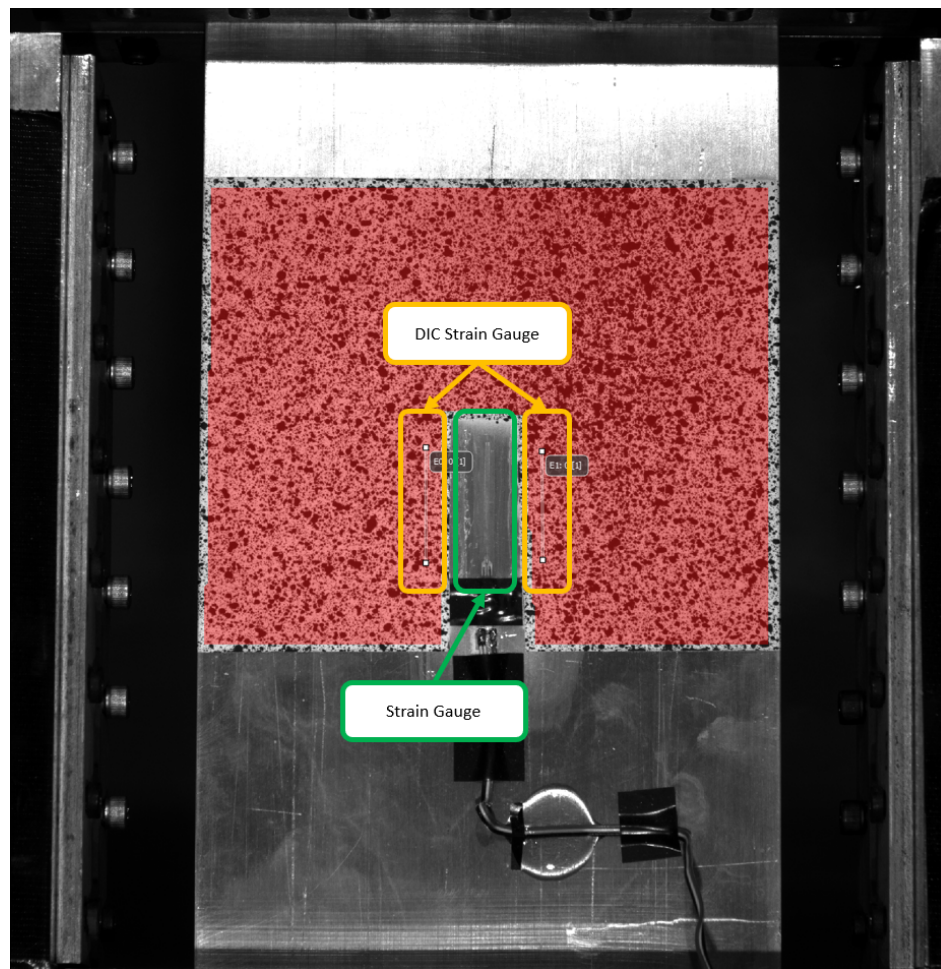


Figure 3.30: Solid aluminum bar painted with a speckle pattern showing the use of the strain gauge and the virtual extensometers created in the DIC software (VIC 3D) [27].

Figure 3.31 overlays the DIC extensometer strain, the average DIC strain and real strain gauge data over the index of the measurement. In this validation, 125 indexes/samples were taken at a rate of 2 images per second or 2Hz. This data was used to determine the average strain (Equation 3.12), and the difference in the measured strain (x_i) (Equation 3.13) at every

index between the Average DIC Strain and the Strain Gauge data. Using the average strain and difference in strain at each index, the average difference $\bar{x}$, standard deviation SD , and a 95% confidence interval was calculated using Equations 3.14, 3.15, and 3.16. The calculated data points are shown in a Bland-Altman plot in Figure 3.32.

$$\text{Average Strain} = \frac{\text{Average DIC Strain} + \text{Strain Gauge Data}}{2} \quad (3.12)$$

$$\text{Difference in Measured Strain} = \text{Average DIC Strain} - \text{Strain Gauge Data} \quad (3.13)$$

$$\bar{x} = \frac{1}{n} \sum_{i=1}^n x_i \quad (3.14)$$

$$SD = \sqrt{\frac{\sum |i - \mu|}{N}} \quad (3.15)$$

$$CI_{95} = 1.96 \times SD \quad (3.16)$$

The average difference was -0.00058% between the strain gauge and the average DIC strain. This yielded upper and lower confidence levels of 0.00347% and -0.00465%, respectively. The Bland-Altman plot in Figure 3.32 indicated very similar results in the strain data, validating the operation of the equipment.

Compression Testing Setup Procedure

A standard testing procedure was followed to ensure consistency between tests. The specimen was first loosely loaded into the MTS to ensure correct orientation. The fixture head was lowered so that it remained above the panel, then locked into position. The sample panel was then loaded into the fixture, and a pre-load of less than 300N was applied to ensure good contact [50]. ASTM 8287 suggests a compression rate that produced failure within 1 min to 10 mins [50]. The suggested compression rate is 1.25mm/min, although to best capture the panel failure with the DIC system, the compression rate was reduced and validated at 0.75mm/min. Once the initial displacement of the MTS was zeroed, the DIC software was started, and verified that it was capturing images. The MTS was then engaged, allowing it to compress the panel for a 2.5mm crosshead displacement.

Following the panel failure, image capture and data recording were halted, the data saved, and the machine reset for the next panel. This standard operating procedure ensured a streamlined approach that captured and ensured consistency between tests.

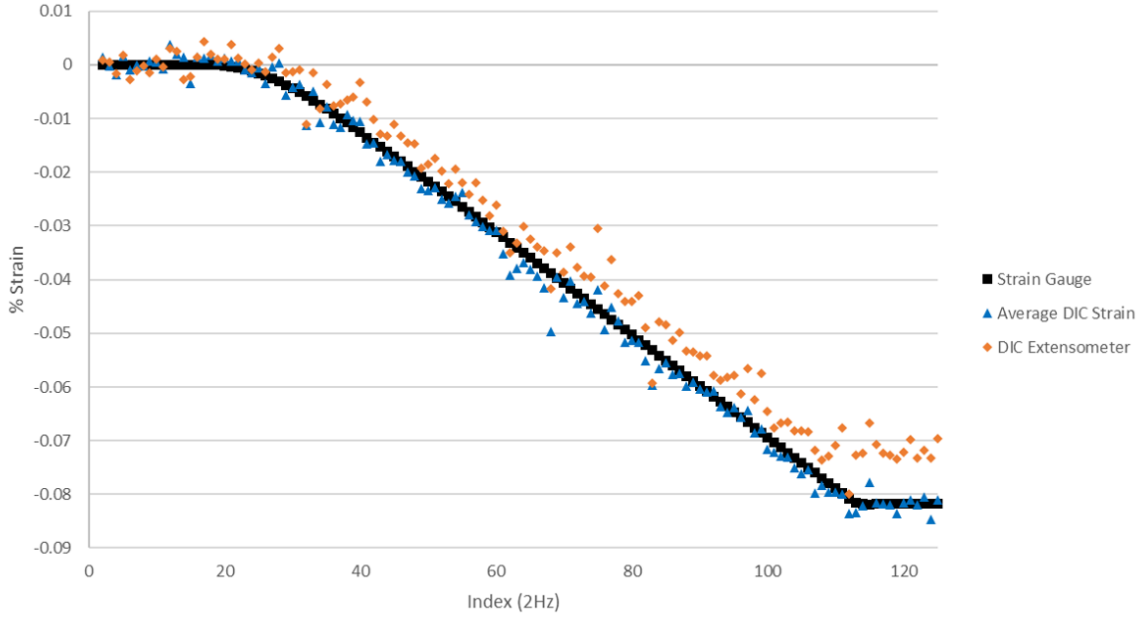


Figure 3.31: Results from the strain validation performed before the experimentation. The plot shows the output of the DIC extensometer, the strain gauge, and the average DIC strain [27].

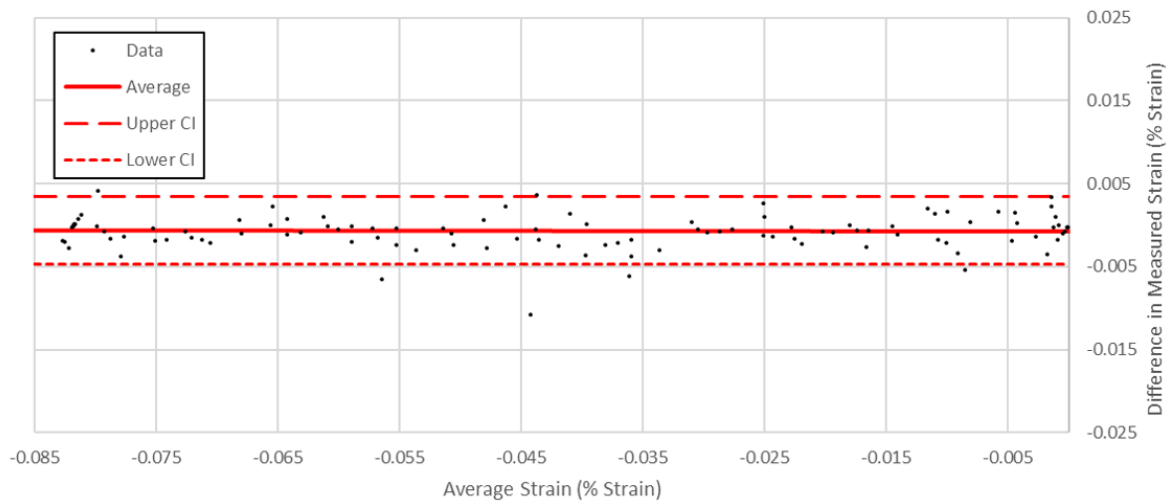


Figure 3.32: Bland-Altman plot comparing the 30mm strain gauge % strain reading with the average of the virtual strain gauge % strain measured using 3D DIC. Measurements were taken prior to the testing.

4 Results

4.1 Introduction

This thesis assumes that the peak force of the composite honeycomb panel defines the panel's residual strength. However, peak force is highly sensitive to imperfections and panel-to-panel variations, as it is driven by local buckling, which is an instability-driven and localized phenomenon.

Less imperfection-sensitive experimental results, such as the dent depth measured in Stage 1 and the stress-strain curves obtained in Stage 2, were selected for model validation. The trends in peak force are compared between the simulations and the experimental results rather than comparing individual values. Figure 4.1 outlines the structure of the chapter: following the introduction, Stage 1 of the experimental results and the simulations are compared in terms of residual facesheet deformation, and dent width. Next, Stage 2 experimental results and the simulations are used to validate the numerical model by comparing the overall behaviour of the panels, the stress-strain curves, and the von Mises Strain. Then the trends in the peak force are examined.

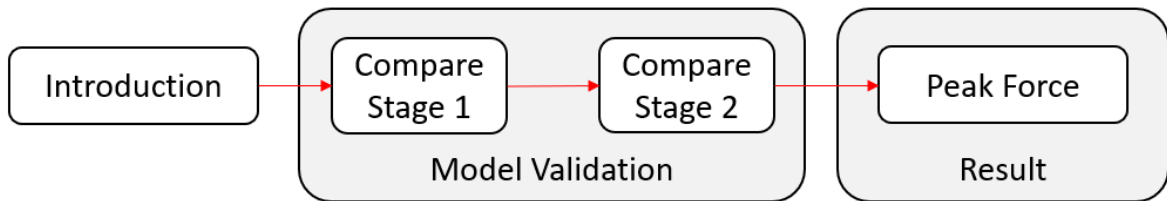


Figure 4.1: Overview of the Results chapter.

4.2 Model Validation

4.2.1 Stage 1 Results

The first stage consisted of indentation of the honeycomb panel. The key characteristics evaluated were the residual indentation depth, and the dent width for both the FE model and the experimental tests. Each dataset was plotted as a function of the center-to-center dent spacing.

The dent characteristics were calculated from out-of-plane displacements taken using inspection lines L0, L1, and L2 across the face of the panels shown in Figure 4.2. Using DIC, 1000 points were taken across the face of the panel along lines L0, L1 and L2. In the FE model, nodal displacements were retrieved from the nodes on the impacted facesheet.

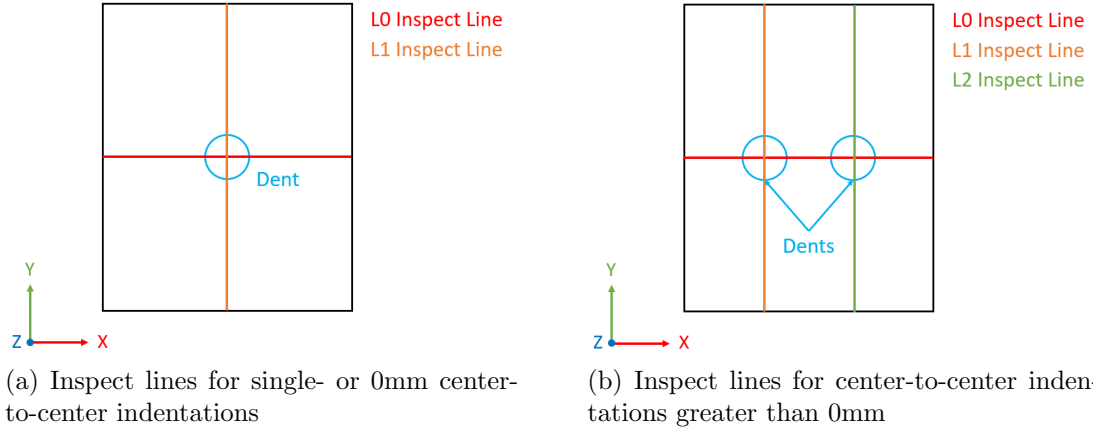


Figure 4.2: Inspection lines were made to measure the out-of-plane displacement along the lines L0, L1 and L2.

Residual Facesheet Deformation

In the FE model, both dents were nearly identical in depth and width, as the impacts were applied simultaneously, resulting in identical damage profiles at both locations. In contrast, the experimental dents were not identical because the damage sites were introduced sequentially. The second impact consistently produced an approximately 8% deeper dent than the first dent. For the experimental dataset, the deeper dent characteristics were used as the deeper dent represents a more severe damage condition and therefore a more critical case for panel failure.

The dent depth was defined as the maximum facesheet deformation measured relative to the original, undeformed panel surface. An example of the maximum facesheet deformation obtained from both the experimental data and the FE model for a single dent is presented in Figures 4.3a and 4.3b, respectively, where the L1 inspection line is plotted.

As defined in Section 3.2.6, a dent is any out-of-plane deformation greater than 0.10mm. This definition establishes a consistent measurement criterion and eliminates ambiguity in identifying the onset of the dent.

In the experiment, the panel rested on a plate with a cut-out outlined in Section 3.2.3 and shown in Figure 3.3 and was secured in place using four rubber-tipped toggle clamps shown in Figure 3.24a. This prevented the panel from moving on the plate; however, it did not prevent the edges of the panel from lifting off the plate when impacted, resulting in macro-scale bending. Figure 4.3a shows the macroscale bending where the yellow line is above a 0 mm residual facesheet deformation away from the dent. Since the panel is dented prior to loading into the test fixtures, the zero is defined by the virtual best-fit plane applied by VIC-3D to the dented

panel. VIC-3D defined the best-fit plane by which it represents the average orientation and position of the measured 3D point cloud of the dented panels [86].

In Figure 4.3b, the zero reference in the FE model corresponds to the undeformed front surface of the facesheet. In the numerical model, the bottom surface of the panel was split into a region representing the cut-out in the base plate and a region resting on the base plate. The region resting on the base plate was constrained from out-of-plane deformation as shown in Figure 4.3b, by the yellow line equaling 0mm away from the dent and does not show the same macro-scale bending that the experiment showed. The consequences of this difference will be discussed further in Section 5.4.

Figure 4.4 shows the residual facesheet deformation of the deepest dent from each test panel as a function of center-to-center dent spacing (0mm to 60mm). Data points in black are from experimental tests, and green data points are from numerical simulations. The black dashed line represents the residual facesheet deformation for a single impact experimental result, and the green dashed line represents the residual facesheet deformation for a single impact numerical simulation.

At a 0mm dent spacing, the numerical predictions and experimental results correspond to the condition in which the indenter impacts the same location. The experiment replicated this by impacting the same location twice, while the simulation modelled a single impact of a single indenter, however, with double the mass.

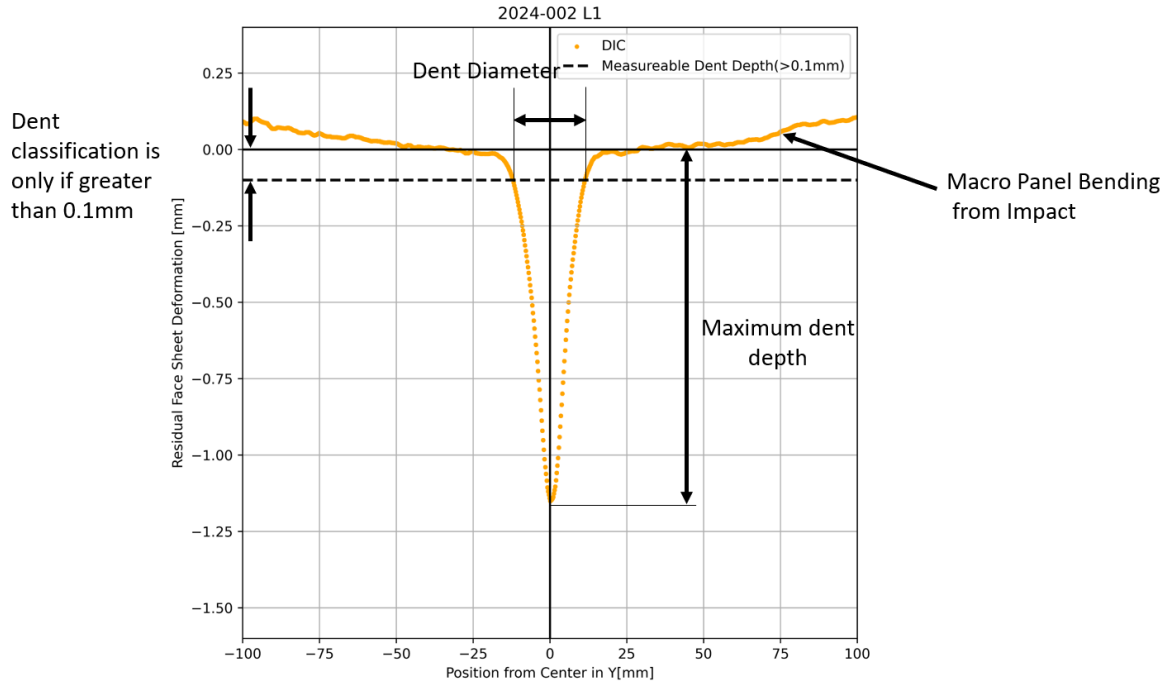
In all other simulated impacts, two indenters were used. Between 0mm and 20mm center-to-center dent spacing, the impact sites overlap. As the dent spacing increases, the residual facesheet deformation decreases until it is similar to that of a single indenter in both the experiment and the simulations.

For dent spacings greater than 20mm, the residual dent depth does not change significantly and remains similar to that of the experimental single-indenter result and the numerical single-indenter prediction. In the experimental results, the dents continue to overlap until a center-to-center dent spacing of 30mm, and in the simulation, a 45mm center-to-center spacing yields no overlap.

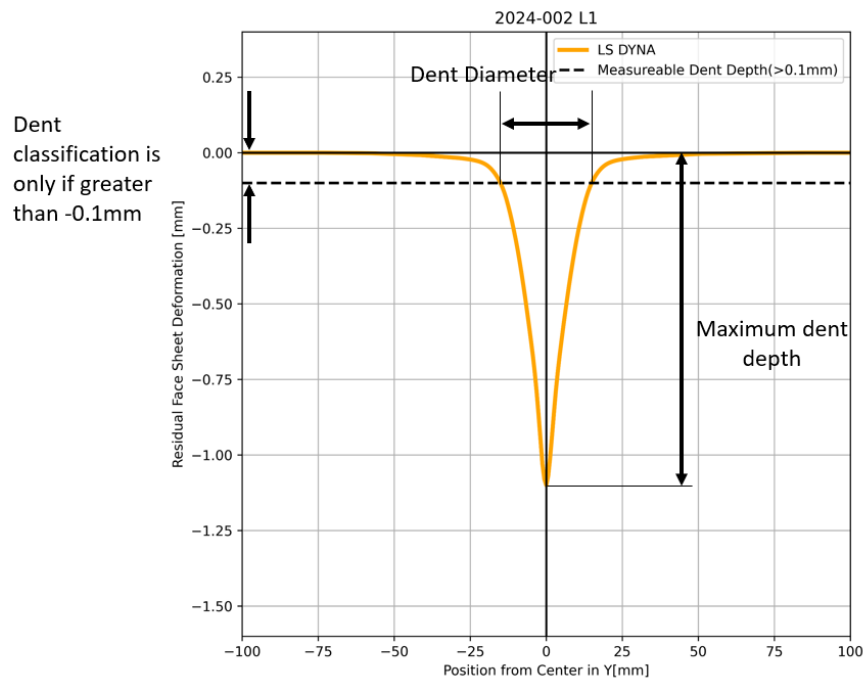
The results indicated that increasing dent spacing reduced dent depth between 0 and 20mm. Beyond 20mm, the dent depth matches that of a single dent, indicating that the second dent no longer affects the depth of the first dent.

Both numerical and experimental datasets exhibited the same overall trend: the residual facesheet deformation decreases with increasing dent spacing, then maintains a residual facesheet deformation similar to that of a single indent. The single indentation of the experimental result and the numerical simulation are similar, differing by 2.7%.

At 0mm, the simulation predicted a residual facesheet deformation 19% larger than the experimental result. At the 5mm center-to-center dent spacing, this difference is maintained, although as the dent spacing increases to 10mm, 15mm, and 20mm, the difference between the simulations and the test results reduces to 13%, 4%, and 3%, respectively. At each of these spacings, the numerical predictions are higher than the experimental results because



(a) Dent diameter and dent depth retrieved from the L1 inspection line from an experimental sample with a 0mm center-to-center indentation spacing.



(b) Dent Diameter and dent depth retrieved from the L1 inspection line from a FE model with a 0mm center-to-center indentation spacing.

Figure 4.3: Inspection line L1 from the experimental and numerical model showing dent depth and dent diameter.

of the simulated constraints on the panel during the indentation. These constraints were applied to the back facesheet, where the region surrounding the cut-out was restricted from out-of-plane deformation. This boundary condition concentrated the impact force within the dents and prevented macro-bending.

Beyond the 20mm center-to-center dent spacing, the residual dent deformation in both the numerical predictions and the experimental results remains similar reaching a minimum difference at a 25mm spacing of 0.2%. With the larger dent spacing, the simulated cut-out has sufficient width to deform freely for the same impact energy as at the smaller dent spacings, thereby reducing the influence of the boundary conditions.

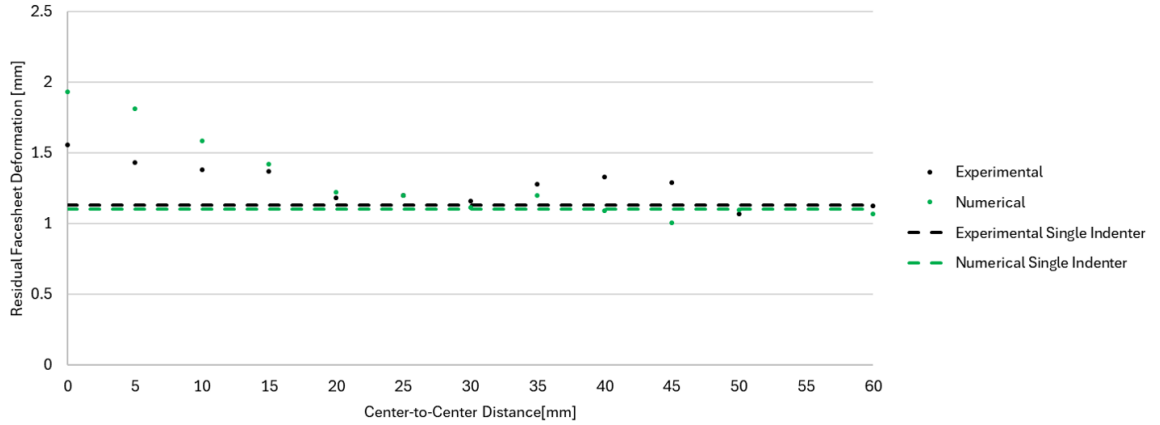


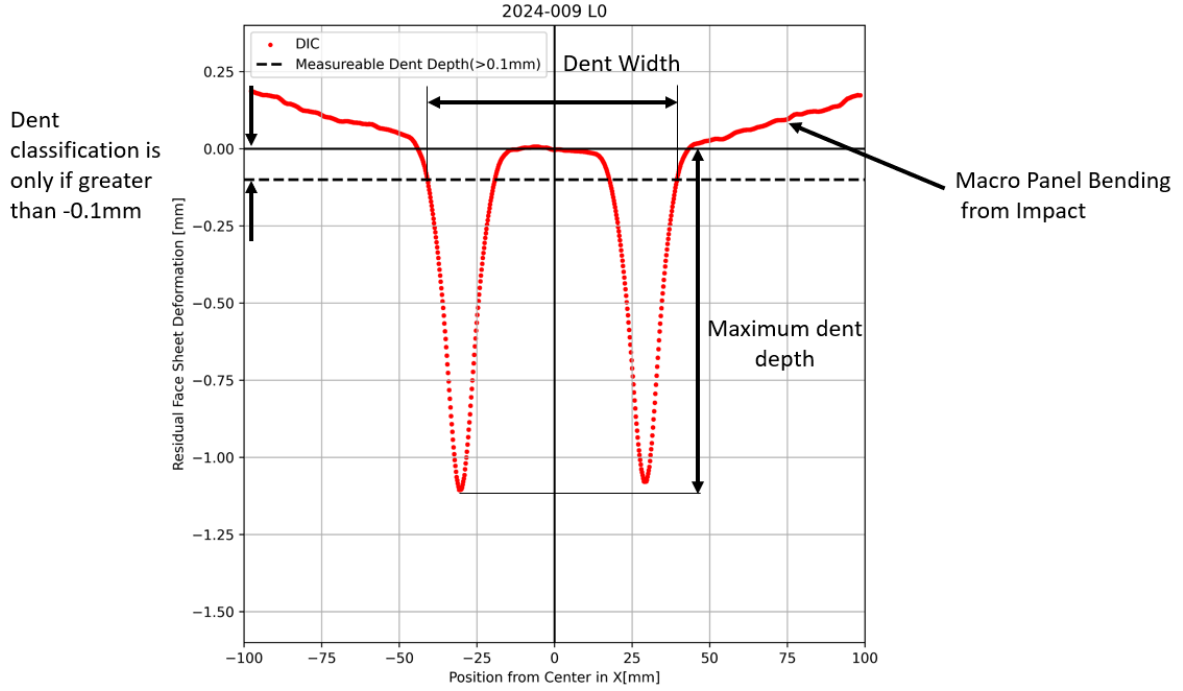
Figure 4.4: Maximum dent depth plotted against the dent center-to-center distance of the panel for both the FE model and experimental measurements.

Width

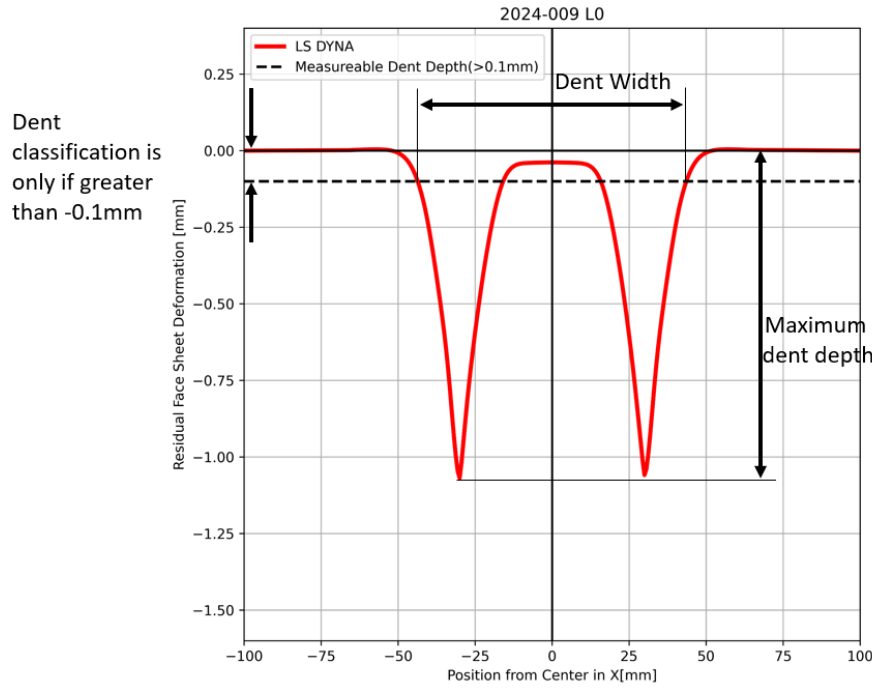
The dent width was taken from where the permanent deformation exceeded 0.10mm from the inspection lines shown in Figure 4.2. Figure 4.5 shows examples of an L1 inspection line from both the experiment and FE model of a 60mm center-to-center dent spacing and the dent width retrieved from the plot. The dent width was defined as the outer-to-outer width of the overall damage site.

Dent width was included as an additional validation metric to assess the spatial extent of the permanent deformation and the interaction between adjacent dents. While dent depth evaluates the severity of the local deformation, dent width provides insight into how the deformation spreads across the panel. As a result, comparing dent width trends between the experiment and numerical model helps determine whether the simulation accurately captures the overall damage footprint.

Figure 4.6 shows the dent width from each test panel as a function of center-to-center dent spacing (0mm to 60mm). Data points in black are from experimental tests, and green data points are from numerical simulations. The black dashed line represents the dent width for a single impact experimental result while the green dashed line represents the dent width for a single impact numerical simulation.



(a) Dent width and dent depth retrieved from the L0 inspection line from an experimental sample with a 60mm center-to-center indentation spacing.



(b) Dent width and dent depth retrieved from the L0 inspection line from a FE model with a 60mm center-to-center indentation spacing.

Figure 4.5: Dent width and dent depth retrieved from the L0 inspection line from an experimental sample and the equivalent FE model with a 60mm center-to-center indentation spacing.

Figure 4.6 shows the overall trend of the data, which shows that the larger the dent spacing, the larger the dent width. The baseline single indentations for both the experimental results and the numerical predictions show a 29% wider dent than the experimental result. At a 0mm center-to-center dent spacing, the simulation has a 41% larger width than the experimental dent. For the remaining dent widths, the difference between the numerical and the experimental values remained below 20% and achieving a minimum difference of 1.5% at a 45mm dent spacing.

Similar to the dent depth, the greatest discrepancy between the simulation and the experiment occurs at the deepest dent with a center-to-center spacing of 0mm, and this difference decreases with increasing dent spacing.

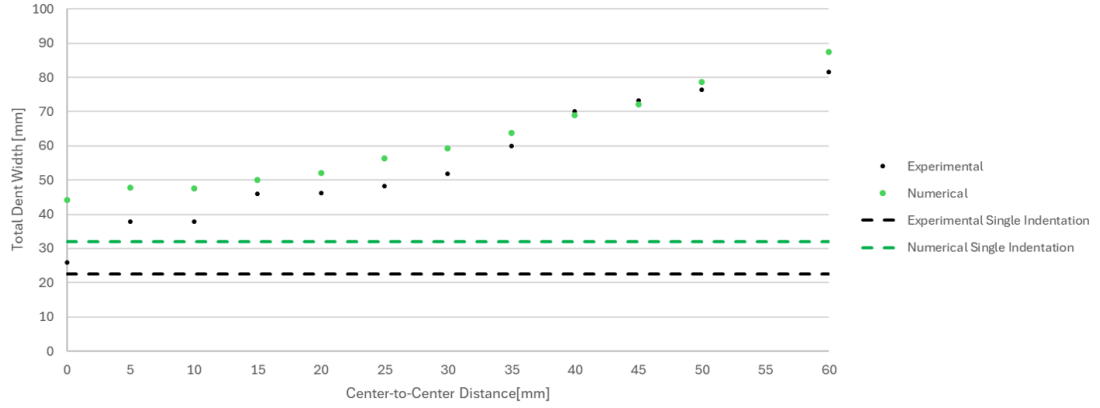


Figure 4.6: Dent width plotted against the center-to-center distance between impact sites.

4.2.2 Stage 2 Results

The second stage of testing consisted of compressing the dented panel beyond the peak force. In the damaged panel, yielding preceded the peak force, after which the damage grew perpendicular to the in-plane loading direction and caused final failure of the panel.

Overall Behaviour of the Panels

Figure 4.7 and Figure 4.8 present examples of the numerical and experimental panel out-of-plane deformation, respectively, from the initial dent through final failure for a 60mm center-to-center dent spacing containing two dents. In both figures, the facesheet deformation is shown following indentation, after the onset of compression, before peak force, at peak force, immediately post-peak force, and at final failure.

For the FE model, Figure 4.7 shows that the dent deformation increases progressively with applied crosshead displacement. Immediately after indentation (Figure 4.7a), two distinct impact sites are visible. Since there is no connecting deformation greater than -0.10mm, there are two separate dents present. After compression is applied (Figure 4.7b), the dents deepen in the out-of-plane direction, indicated by the connection of the yellow and orange contour bands (-0.10 to -0.15mm). As loading continues (Figure 4.7c), deformation increases further as the panel approaches its residual strength (peak force), with expansion of the yellow, orange, and green contours (-0.20 to -0.25mm). At this stage, deformation spreads across the panel primarily along the X-axis, with comparatively little growth along the Y-axis.

After peak force (Figure 4.7d), the out-of-plane deformation increases further, and the green contours connect the two damage sites, indicating interaction between the dents. Although the deformation field has merged, two distinct deformation peaks remain at the original impact locations, identified by the dark blue contours representing deformations greater than -0.50mm. These contours continue to expand gradually in both the X- and Y-directions throughout Figures 4.7a-d. In Figure 4.7e, the material between the impact sites folds inward, producing a bow-tie-shaped deformation. The dent spans almost the full panel width while becoming increasingly localized, with deformation above and below the dent relaxing. By final failure (Figure 4.7f), the deformation fully connects from one panel edge to the other along the X-axis and continues to expand in the Y-direction. The deformation is ultimately constrained by the boundary conditions, as shown by the flattened contours near the top and bottom panel edges.

The deformation contours in Figures 4.7c-e show that both damage sites become oval before coalescing into a single damage region. Once merged, the deformation outside the primary dent reduces as the dominant damage zone propagates toward the panel edges. The final contours indicate global panel buckling associated with failure.

Throughout loading, the FE model predicts highly symmetric damage growth about both the X- and Y-axes. Following the onset of compression, the dents expand uniformly in all directions, with interaction initiating primarily along the X-axis between the two impact sites.

Figure 4.9 and Figure 4.10 present the numerical and experimental out-of-plane deformation for the 20mm centre-to-centre dent spacing configuration. As with the 60 mm spacing, deformation is shown following indentation, after the onset of compression, before peak force, at peak force, immediately after peak force, and at final failure.

Unlike the 60mm configuration, which initially contains two distinct dents, the 20mm configuration exhibits a single combined dent immediately after indentation, identified by the continuous yellow contour in Figure 4.9a. This single deformation region persists throughout loading until final failure. Following the application of compression (Figure 4.9b), the deformation grows in both the X- and Y-directions and continues to expand until peak force (Figure 4.9c). After peak force (Figures 4.9d and e), the deformation propagates toward the panel edges before increasing primarily in depth, as indicated by the contour progression, until final failure (Figure 4.9f).

The experimental results shown in Figures 4.8 and 4.10 follow the same general deformation progression as the simulations, with dent interaction initiating first along the X-axis. However, the experimental deformation is noticeably less symmetric. The contours are more irregular, with greater deformation developing above the dents than below, producing a more circular deformation pattern. Figures 4.7e and 4.8e, together with Figures 4.9e and 4.10e, also show that deformation localization is less pronounced in the experiments than in the simulations. In both dent spacing configurations, final failure results in a single dominant deformation region (Figures 4.7f, 4.8f, 4.9f, and 4.10f). However, the FE model maintains smooth, symmetric deformation contours, whereas the experimental panels exhibit irregular boundaries and greater vertical spread, reflecting the influence of real boundary conditions, material properties, and natural variation in the core geometry.

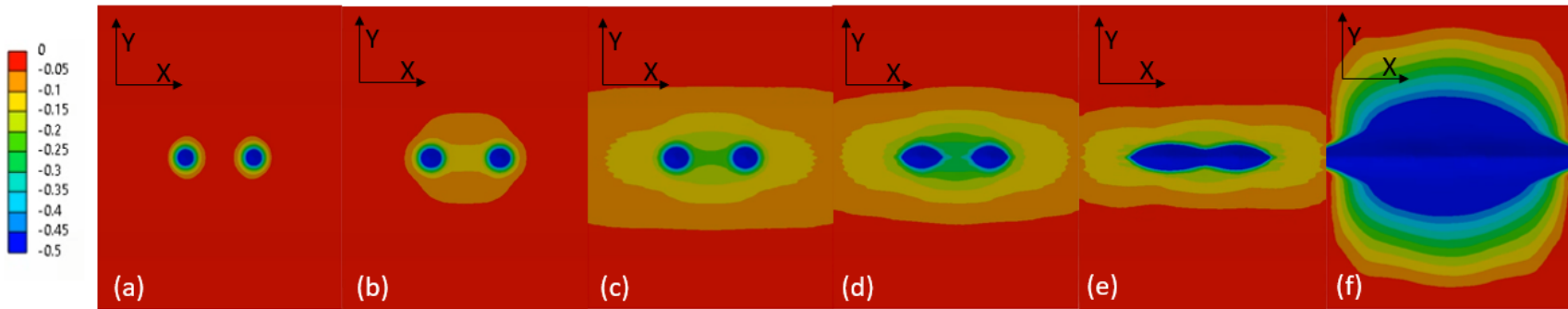


Figure 4.7: Typical dent growth for two dents in the FE model shown with a 60mm center-to-center dent configuration. (a) Deformation caused by the indentation creating two separate dents. (b) After application of compression. (c) At peak force. (d) After peak force, the dents deepen and increase in size. (e) The damage damage extends outwards the outside of the panel. (f) At complete panel failure.

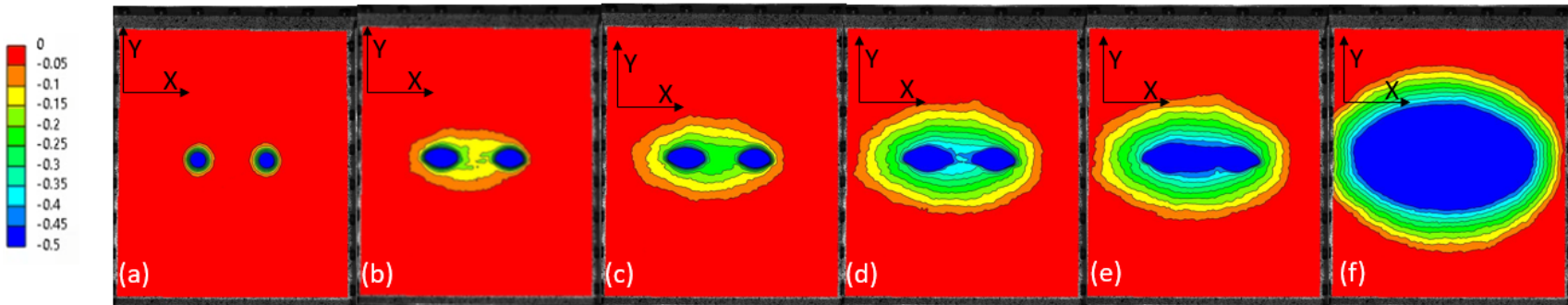


Figure 4.8: Typical dent growth for two dents in an experiment is shown with a 60mm center-to-center dent configuration. (a) Deformation caused by the indentation creating two separate dents. (b) After application of compression. (c) At peak force. (d) After peak force, the dents deepen and increase in size. (e) The damage damage extends outwards the outside of the panel. (f) At complete panel failure

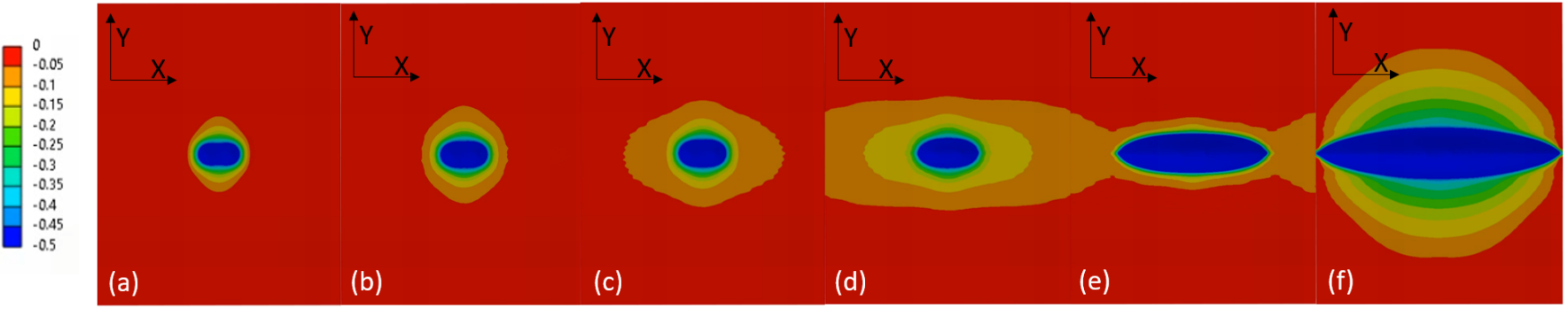


Figure 4.9: Typical dent growth for a single dent in the FE model shown with a 20mm center-to-center dent configuration. (a) Deformation caused by the indentation creating one dent. (b) After application of compression. (c) At peak force. (d), (e) After the peak force, the dent grows. (f) At complete panel failure.

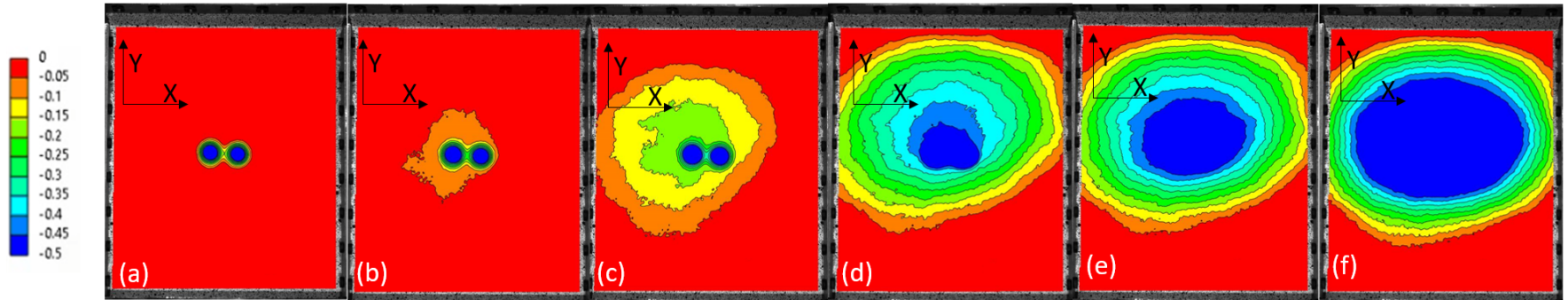


Figure 4.10: Typical dent growth for a single dent in an experiment is shown with a 20mm center-to-center dent configuration. (a) Deformation caused by the indentation creating one dent. (b) After application of compression. (c) At peak force. (d), (e) After the peak force, the dent grows. (f) At complete panel failure.

The out-of-plane deformation of the front facesheet in Figures 4.7 and 4.9 corresponds to intracell buckling, consistent with the failure mode shown in Figure 2.9. This occurs because the crumpled core beneath the dent cannot provide sufficient stiffness to resist the facesheet deformation. Comparison with the DIC measurements in Figures 4.8 and 4.10 confirms the same failure mechanism, indicating that the crushed core beneath the dent reduces support for the front facesheet and promotes localized facesheet buckling.

Overall, the simulations and experiments shown in Figures 4.7, 4.8, 4.9, and 4.10 exhibit similar deformation evolution, with progressive dent growth up to peak force. Because peak force corresponds to structural instability, close agreement in the post-peak deformation contours is difficult to achieve. Despite this, the numerical and experimental results demonstrate strong qualitative agreement, with the primary differences being the greater symmetry and smoother deformation contours predicted by the FE model compared with the experimentally observed response.

Stress-Strain Curves

The overall panel behaviours in terms of stress-strain curves were calculated from both the experimental and FE models. The true stresses and true strains were calculated using the methodology results outlined in Section 3.3.2 for the overall panel. All 12 of the experimental true stress-strain curves are overlaid in Figure 4.11 with the numerical true stress-strain curve for a 60mm center-to-center spacing shown in black.

The numerical model employed a modulus of elasticity of 73.1 GPa, as reported in Table 3.1, while the experimentally determined average modulus was 71.4 GPa resulting in a difference of 1.7 GPa.

As shown in Figure 4.11, the experimental and numerical curves yield at approximately the same strain. The experimental panels do not exhibit a distinct yield stress like that of the simulation before reaching peak stress. The experimental data displays a slightly lower initial stiffness, consistent with the lower measured modulus. A modest deviation is observed before the yield strain, between 0.003 and 0.0037 mm/mm; the FE model predicts higher stresses than those measured experimentally, possibly because the idealized numerical model lacks imperfections.

Overall, the plastic response and peak behaviour predicted by the FE model remain consistent with the experimental observations. Figure 4.11, therefore, demonstrates that the numerical model provides a good representation of the global panel behaviour.

von Mises Strain

The von Mises strain was used to compare the numerical and experimental facesheet strain fields at the peak force.

Equation 4.1 calculates the yield strain of the 2024-T3-clad facesheets. From Table 3.1, a yield stress of 270 MPa and a modulus of elasticity of 73.1 GPa were used to calculate a facesheet yield strain of 0.37%. Figures 4.12c-f and 4.13c-f show that most of the panels front facesheet is yielding as the majority of the facesheet appears green.

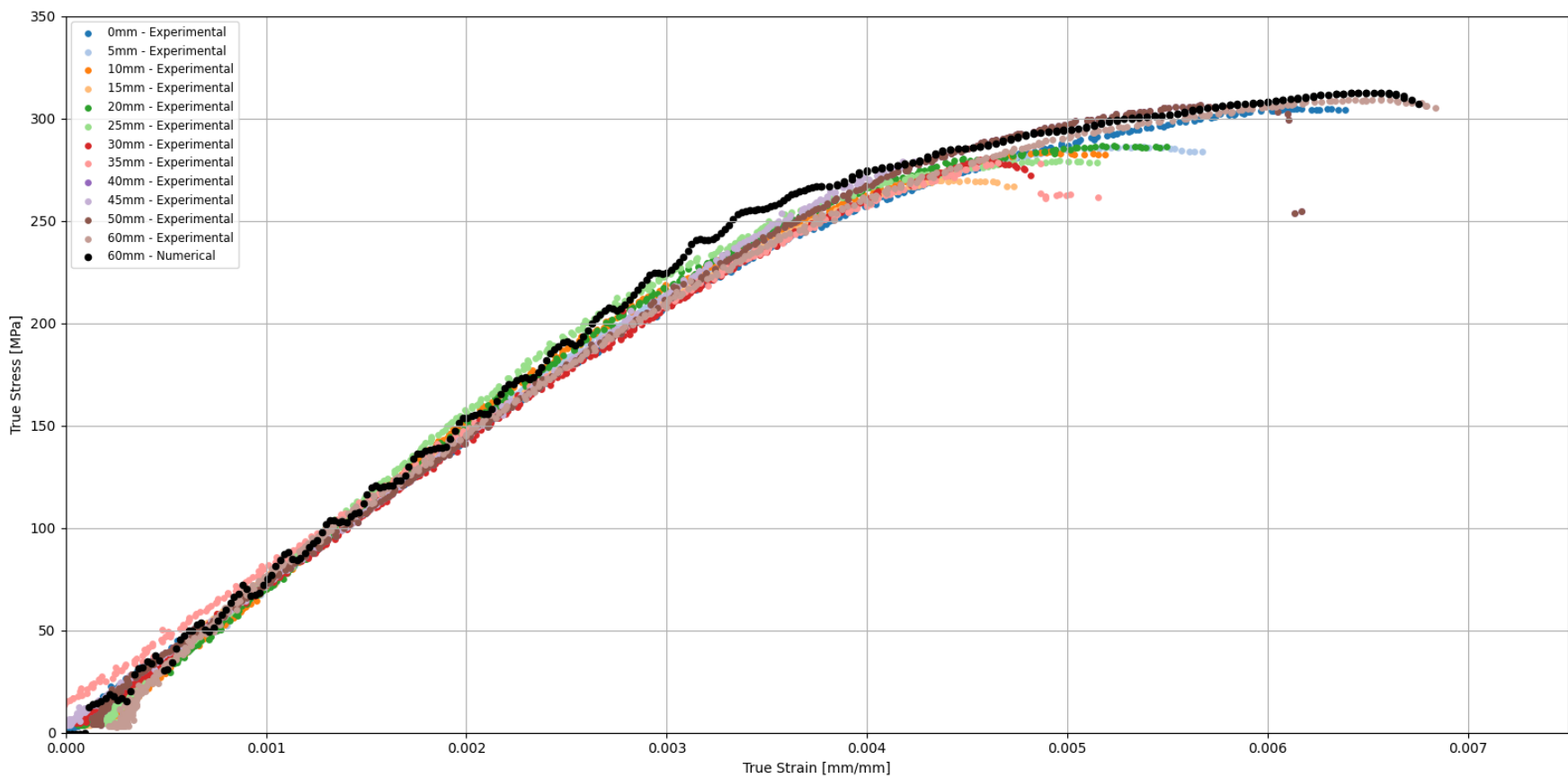


Figure 4.11: True stress-strain curves of all the experimental data (0 to 60mm spacing) overlaid with the 60mm center-to-center numerical solution.

$$\epsilon_y = \frac{\sigma_y}{E} = \frac{270MPa}{73.1GPa} = 0.0037mm/mm \quad (4.1)$$

A limitation of the DIC system is that strain can only be measured within the camera's field of view. Because the facesheet indentation is obscured by the indenter, the associated indentation strains cannot be captured experimentally. In order to compare the strains between the simulation in Figure 4.12 and the experimental results in Figure 4.13, the simulation results are presented by subtracting the strain results predicted at the end of Stage 1. The scale used in these figures was chosen to provide resolution to more than twice the material's yield strain.

Figure 4.12 illustrates the von Mises strains on a simulated panel during Stage 2 with a center-to-center spacing of 60 mm. Because the Stage 1 strains were subtracted, Figure 4.12a shows zero strain. Figure 4.12b captures the onset of Stage 2, where yielding initiates between the dents and propagates outward toward the panel exterior, while regions above and below the dents, as well as near the top and bottom boundary conditions, remain elastic.

In Figure 4.12c, the panel approaches peak force, with most of the panel yielding and localized strain increasing at the dents, between them, and at the corners due to boundary constraints applied in Section 3.4 along the top and bottom (+/- Y axis) panel ends. Figure 4.12d shows the panel at peak force, where strain intensified and spread both between and beyond the dents.

Beyond peak force (Figure 4.12e), yielding continues, and strain concentrations at the dents become more pronounced. Figure 4.12f, taken just prior to final failure, shows elevated strains concentrated around the critical dent that ultimately lead to failure in Figure 4.12g. After failure, Figure 4.12g indicates that yielding localizes at the failure site, while much of the remaining panel unloads and returns to elastic behaviour.

Figure 4.13 shows the von Mises strains on an experimental panel during Stage 2 with a center-to-center spacing of 60 mm. Since there was zero strain at the start of the test, Figure 4.13a is identical to Figure 4.12a. Figure 4.13b shows smaller areas yielding between and outside the dents than in the simulation, while regions above and below the dents was also smaller and remain elastic. Unlike the idealized and nearly symmetric simulation (Figure 4.12b), the experimental panel exhibits uneven strain distributions near the edges, likely due to imperfections in the panel.

In Figure 4.13c, the panel approaches peak force, with most regions yielding, consistent with the simulation (Figure 4.12c). Some differences are noticeable, such as the corners not exhibiting high strains since the use of potting compound outlined in Section 3.5 enabled a better dissipation of loading into the panel. However, higher strains developed on either side of the dents, with smaller areas elastically deforming above and below them. Overall, the strain patterns and magnitudes were similar.

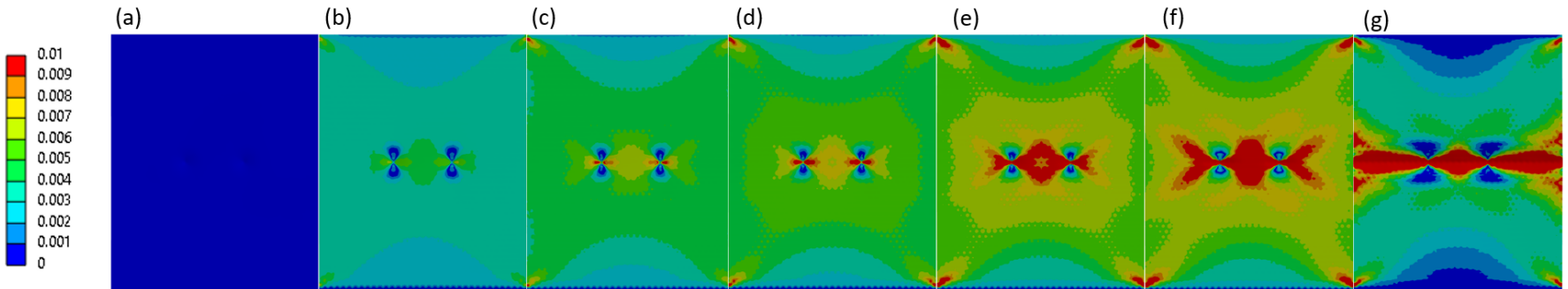


Figure 4.12: Images of strain growth in the facesheet of an FE model panel with a center-to-center distance of 60mm. (a) Beginning of stage 2. (b) After the panel compression begins. (c) Dent strain growth approaching peak force. (d) At peak force. (e) After peak force. (f) Before panel failure. (g) At final failure.

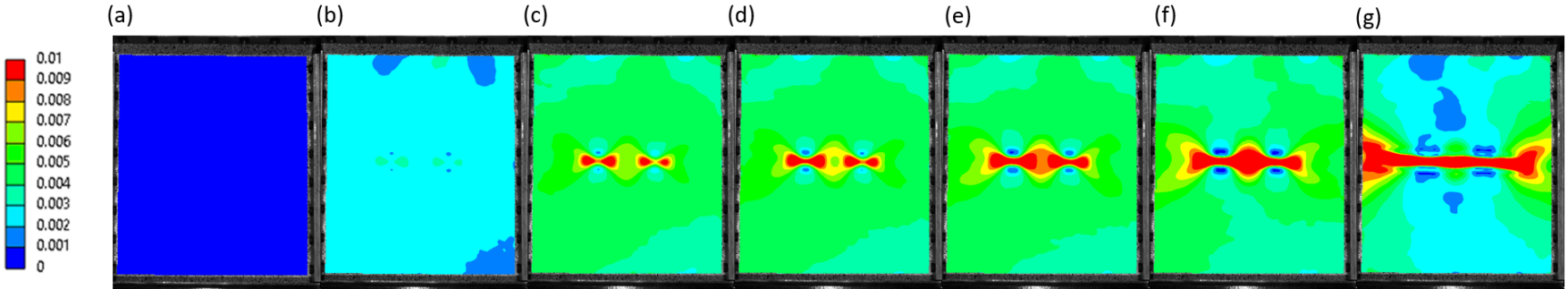


Figure 4.13: Images of strain growth in the facesheet of the experimental test with a center-to-center distance of 60mm. (a) Beginning of stage 2. (b) After the panel compression begins. (c) Dent strain growth approaching peak force. (d) At peak force. (e) After peak force. (f) Before panel failure. (g) At final failure.

Figure 4.13d shows the panel at peak force, where the strain distribution closely matched that of the simulation (Figure 4.12d). Elastic regions persisted above and below the dents, while the region between them yielded at approximately 0.005 mm/mm, consistent with the simulation. The von Mises strain reaches its highest magnitude at the impact sites. Both Figure 4.13d and Figure 4.12d exhibit a shear-dominated “butterfly” pattern characteristic of interacting plastic zones forming strain contours at $+45^\circ$ and -45° on both the left side and the right side that are present in all panel configurations tested.

Beyond peak force (Figures 4.13e and f), strain concentrations intensify around the dents, although the experimental panel exhibits more localized concentrations around the dents compared to the broader elevated strain regions (0.007–0.008 mm/mm) observed in the simulation.

In Figure 4.13g, the panel failure was influenced by geometric asymmetry, with the left dent being deeper and wider than the right. As a result, failure initiates at the left dent and propagates toward the right. This behaviour was further discussed in Section 5.4, which addresses the asymmetry in panel failure. At final failure the overall strain contours and magnitudes remain comparable.

Overall, the two figures show comparable strain contours and similar magnitudes throughout Stage 2, indicating strong agreement between the simulation and experimental results.

4.2.3 Stage 1 and Stage 2 Summary

The Stage 1 FE results predicted the overall trends accurately; however, individual data points showed some variation. For example, the residual facesheet deformation varied between 0.2% and 19%, while the dent width varied between 1.5% and 41%. These differences are attributed to the simulated boundary conditions, with smaller dent spacings producing larger variations, whereas larger dent spacings (>20 mm) resulted in reduced discrepancies. In Stage 2, strong qualitative agreement was observed between the FE model and experimental results in terms of deformation progression and von Mises strain evolution. Both approaches exhibited similar strain localization patterns and comparable von Mises strain distributions at peak force. Additionally, the overall stress–strain responses in Stage 2 were consistent between the numerical and experimental results. The primary quantitative difference was a 1.7 GPa discrepancy in the measured modulus of elasticity. The Stage 1 and Stage 2 results from the FE model captured the behaviour observed experimentally and provided confidence that the peak force was well predicted.

4.3 Peak Force

Figure 4.14 shows the peak force plotted against the center-to-center dent distance of the numerical solution compared to that of the experimental results extracted from the reaction force plots such as the one in Figure 3.15. A baseline for a single indentation on a honeycomb panel was created and plotted as a dashed line in Figure 4.14. The single-indentation panel served as a reference damaged panel, with an indentation applied at the center using the same initial drop parameters as those in the experimental and numerical models. The FE model predicted the single indent to have a peak force of 67.3kN, and the equivalent experimental test had a peak force of 66.9kN, resulting in a 0.6% difference between the FE model and the experimental test.

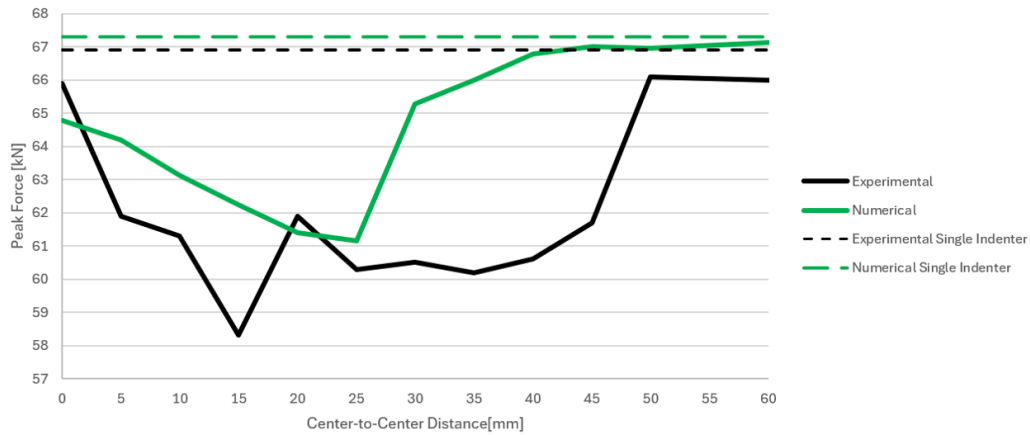


Figure 4.14: Peak force plotted against dent center-to-center distance of the panel.

With two indenters at a 0mm spacing, both the numerical and experimental peak forces are lower than those in the single-indenter numerical and experimental tests. At 0 mm spacing, the simulated peak force differed from the experimental value by 1.8%. As the spacing increased, the peak force gradually decreased, indicating that the two dents act together in compression to reduce the panel's residual strength. In the FE model, the predicted peak force predominantly increases from a dent spacing of 25mm to 40mm. The increase in the peak force indicates that the two dents are beginning to behave independently. At a spacing greater than 40mm, the peak force of the panel is predicted to differ by less than 1% from the single indenter model. Because only one experimental test was performed, inherent fluctuations in the peak force due to imperfections in the panels remain present in the experimental data. Similar to the FE model, the experimental peak force initially decreased as the spacing increased from 0mm to 15mm before plateauing. Then, at a spacing of 35mm, the peak force begins to increase until it achieves a peak force 1.2% from that of the experimental single indentation for center-to-center dent spacings 50mm and larger.

The three major points of the graph demonstrate:

1. Both numerical and experimental results show that two indentations at the same location reduce the peak force compared to a single indentation.
2. As the center-to-center dent spacing increases, impact sites that overlap decrease the peak force of the panel compared to that of a single indentation.

3. The FE model and experimental results also show that at a certain distance apart, the two indents begin to behave independently of one another and initiate failure independently of each other.

The mechanisms behind this behaviour will be discussed further in Chapter 5.

4.4 Results Summary

The Stage 1 and Stage 2 results indicate that the numerical model is representative of the experimental response. Because only one experimental specimen was tested for each spacing configuration, the experimental data are used primarily to assess whether the overall trends align with those predicted by the FE model. In contrast, the numerical results provide clearer trend identification due to the absence of experimental scatter.

The FE model captures the general trend in peak force, showing an initial reduction with increasing dent spacing followed by an increase in peak force beyond a certain dent spacing while always remaining below that of a single dent. Although the exact peak force values and spacing at which the transition occurs do not perfectly match the experimental results, this discrepancy is expected. Peak force represents an instability and is therefore highly sensitive to small variations from manufacturing or preparation, such as cells that are not perfectly hexagonal, non-uniform thickness, or differences in adhesive thickness. An example of this would be the alignment of the panel's in-plane direction, or, more likely, the difference in indentation method. Future work can focus on matching the indentation methods to either have two impacts in series in the FE model, or have two simultaneous impacts with a larger cut-out in the experimental testing.

The results of the numerical study correlate with those of the experimental testing. Moving forward, the discussion will use the FE model exclusively to understand the effects of dent spacing.

5 Discussion

5.1 Introduction

The results from Chapter 4 provided insight into the panel's behaviour across different dent spacings. The peak force plot in Figure 4.14 outlined the influence of dent spacing on the peak force showing that overlapping dents reduced the residual strength of the honeycomb panels below that of a single dent. The behaviour of cross-sectioned dented panels, differences in indentation methodology, and the relationship between peak force and other dent characteristics are discussed in this chapter.

5.2 Influence of Dent Interaction on Peak Force

Recall that the peak residual strength of the panel is the maximum force the panel can withstand before failure. In Chapter 4, the peak force was shown to be related to the dent spacing. However, SRMs do not use center-to-center indentation spacing as a metric since dents can be irregular or non-standard geometries. Instead, this section examines peak force in the context of other dent characteristics to be tailored for SRM use and to determine whether other factors influence peak force.

5.2.1 Dent Growth

The honeycomb sandwich panel exhibits three different behaviours that depend on the dent spacing. Figures 5.1, 5.2, and 5.3 are the out-of-plane Z deformation plots of the cross-section taken normal to the XZ plane at 5, 35, and 60mm center-to-center dent spacings. In each of these figures, (a) was taken at the start of Stage 2 to show the dent depth following impact, (b) shows the panel at yield as an intermediate progression to peak force, (c) shows the cross-sectional deformation at peak force, and (d) shows the cross-section after the panel fails. In each of the cross-sectional images, the images have been cropped to focus on the dent region.

For a 5mm spacing, Figure 5.1a shows that the two impact sites are closely spaced, resulting in a single dent on the front facesheet. This dent is formed by plastic deformation in the facesheet and folding of the underlying cell walls, forming three distinct folds. Because of the cut-out in the base plate during the indentation in Stage 1, the bottom facesheet also deformed out-of-plane more than 0.5mm. The width of the core damage corresponds directly to the width of the surface dent. Cell wall folding decreases with increasing distance from the center of the impact locations. Figure 5.1b illustrates the panel at yield. The deformation contours indicate that both the depth and width of the dent have increased. The dent

effectively penetrates further into the panel, expanding as loading progresses. At peak force, shown in Figure 5.1c, this trend continues. The dent deepens further into the panel, pushing the back facesheet farther out of plane. The front facesheet progressively deforms inward, and the increasing dent width causes additional core buckling and folding beyond the immediate vicinity of the impact sites. Figure 5.1d presents the panel response after peak load, during failure. Failure initiates through localized buckling of the facesheet. As compressive loading continues, the dent deepens until the bottom surface of the front facesheet experiences tension while the top surface is in compression, creating an unstable stress state that triggers instability. Additionally, the core contains permanent folds from the initial impact, reducing its stiffness and diminishing its ability to resist further dent growth.

Figure 5.2 presents the response for two dents spaced 35mm center-to-center. In Figure 5.2a, the two indentations produce localized deformation in the front facesheet, with core buckling directly beneath each dent. The front facesheet also exhibits out-of-plane deformation between the dents, with a minimum displacement of approximately 0.15mm. The bottom facesheet deforms across the full width, as influenced by each dent. As compressive loading increases from Figure 5.2b to c, buckling initiates at the damaged regions beneath the indentations. Only limited buckling occurs in the core between the dents, delaying interaction and preventing the two dents from merging before panel failure. Figure 5.2d shows that, at failure, buckling develops in the region between the dents. This interaction leads to the formation of a larger, combined deformation zone that expands laterally toward the panel edges.

Figure 5.3 illustrates the response of a honeycomb panel with a 60mm center-to-center dent spacing. In Figure 5.3a, the dents in the front facesheet induce localized damage in both the core and the bottom facesheet, although the magnitude of deformation is less than that observed in Figures 5.1a and 5.2a. An undamaged region of facesheet and core remains between the two damage sites, indicating that they behave as fully independent dents. Figures 5.3b and c show the panel at yield and peak force, respectively. The dents deepen and widen with increasing compressive load, but independently of one another. Core damage beneath each dent also increases and spreads locally. However, the core between the dents remains intact at this spacing, preventing interaction or coalescence. Finally, Figure 5.3d shows that failure occurs before the dents grow sufficiently wide to influence one another. Damage propagates outward from each dent and only connects near the middle of the panel during the complete panel failure.

The cross-sectional views of the FE model reveal three distinct behavioural regimes governed by dent spacing and the resulting localized dent width. For center-to-center spacings between 0 and 25mm, the dents overlap sufficiently to cause continuous core damage between them. At spacings of 30 to 40mm, core damage between the dents is limited. However, the facesheet between the dents still exhibits noticeable out-of-plane deformation, indicating partial interaction. For spacings greater than 40mm, an intact region of core remains between the dents, allowing them to behave independently with no significant interaction.

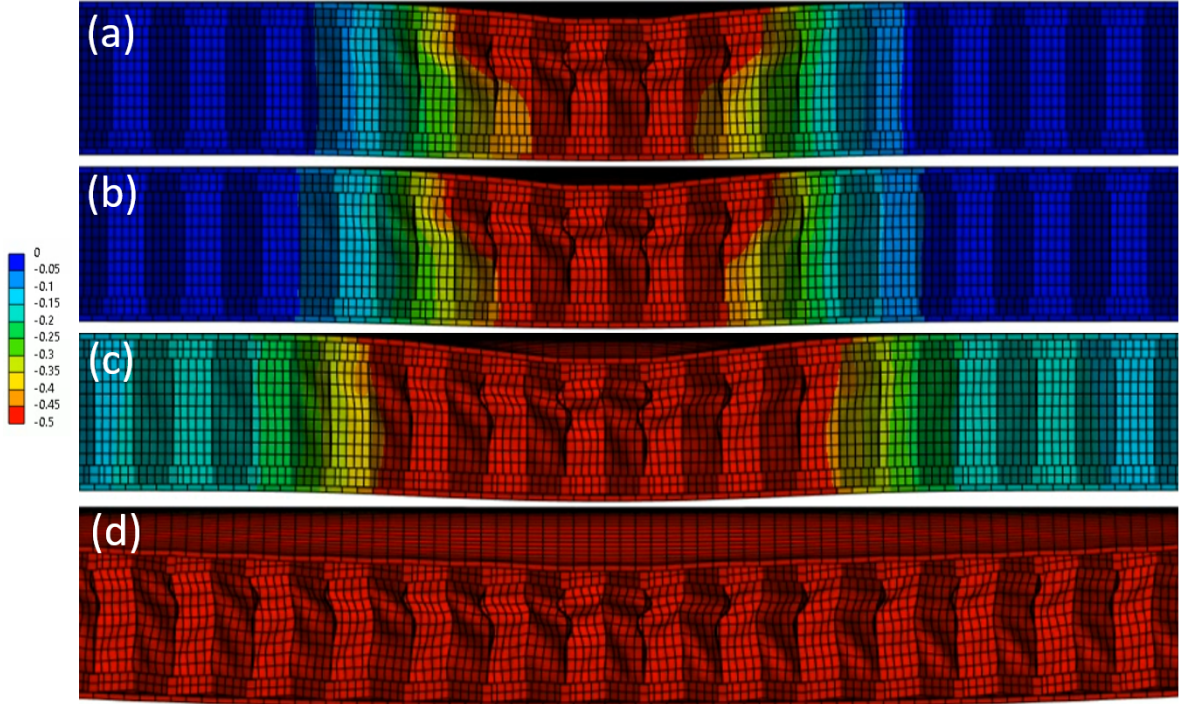


Figure 5.1: Z Deformation(mm) of a 5mm center-to-center spacing (a) Beginning of Stage 2 (b) Panel yield, (c) At peak force (d) Panel failure.

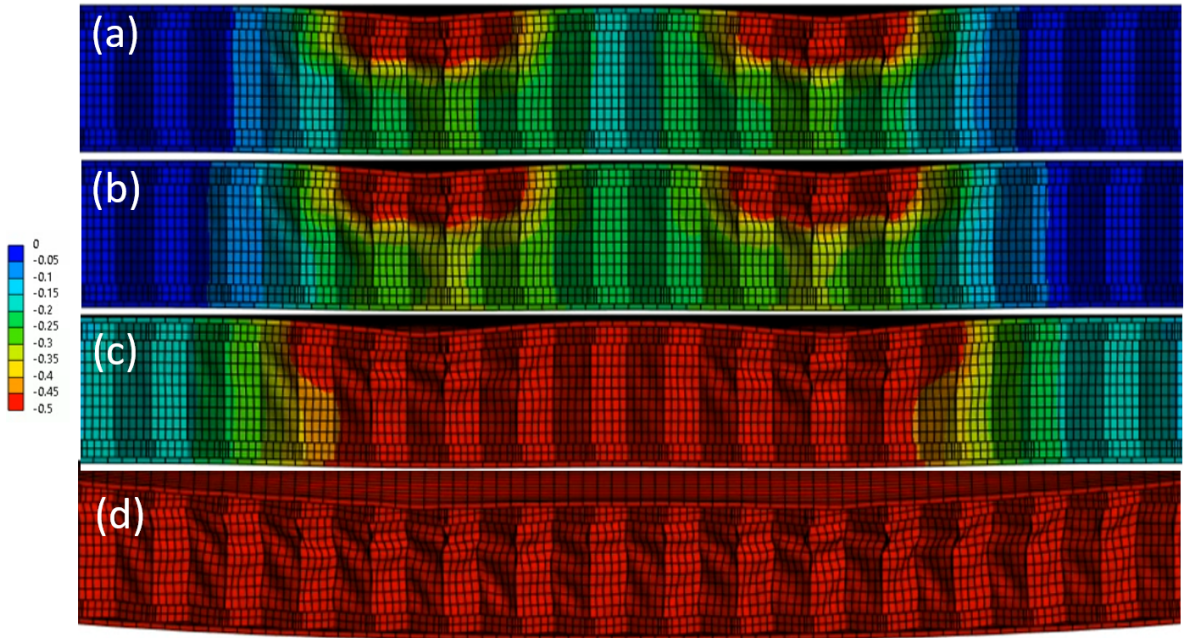


Figure 5.2: Z Deformation(mm) of a 35mm center-to-center spacing (a) Beginning of Stage 2 (b) Panel yield, (c) At peak force (d) Panel failure.

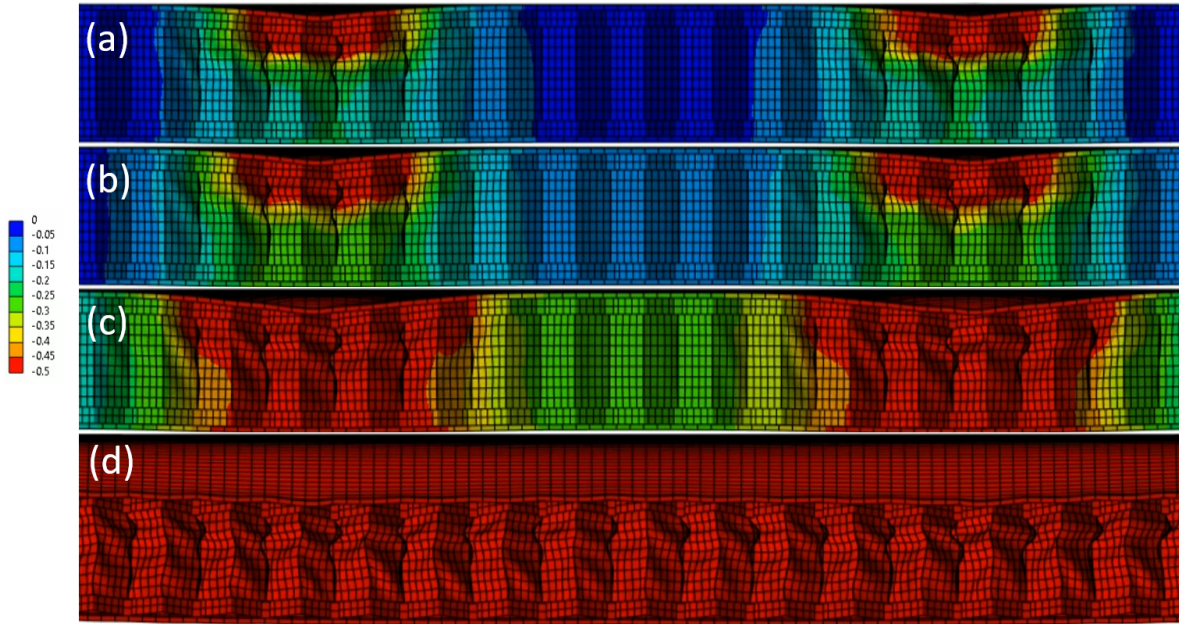


Figure 5.3: Z Deformation(mm) of a 60mm center-to-center spacing (a) Beginning of Stage 2 (b) Panel yield, (c) At peak force (d) Panel failure.

In Figure 2.14 and as outlined in Chapter 2, Bector studied dents parallel to the loading direction, and also identified three behavioural regimes similar to this thesis: the panel acting as a single dent, two interacting impact sites, or two independent dents. Bector initially found that the peak force decreased by up to 11% compared to the single impactor when the dents acted as a single dent [26]. The peak force increased to a maximum of 7% greater than that of the single indentation when there were two interacting impact sites. When the dents acted independently, the peak force was the same as that of a single dent. Figures 5.1, 5.2, and 5.3 show the three behaviours present in this thesis. When two damage sites overlap to form a single dent, the peak force decreases by up to 9%. As the sites separate but continue to interact, the peak force remains 0.9-3% lower than that of a single indentation. Once the sites behave independently as separate dents, the peak force equals that of a single dent. Compared with perpendicular dent spacing, Bector's parallel dent spacing can result in residual strengths exceeding that of a single dent, whereas perpendicular spacing produces residual strengths equal to or lower than that of a single dent.

5.2.2 Peak Force and Effective Width

In this section, the effective dent width is defined as the width of a single continuous dent ($>0.1\text{mm}$ deformation out-of-plane) at the onset of Stage 2. This differs from the overall dent width shown in Figure 3.6, previously presented in Chapter 4, which measures the total width spanning the outermost edges of the visible dent or dents. For configurations in which the region between two damage sites remains above the 0.10mm dent-classification threshold, the overall dent width and the effective dent width are identical.

Figures 5.4, 5.5, and 5.6 present three examples of L0 inspection line measurements used to determine the effective dent width profile for different dent spacings in the FE model.

Figure 5.4 shows the L0 inspection line for a panel with a 15mm dent spacing. The two damage sites are sufficiently close that the L0 profile between them achieves a local maximum of -1.14mm which is above the -0.10mm dent-classification threshold as indicated. As a result, the damage is classified as a single continuous dent, and the overall dent width is equal to the effective dent width.

Figure 5.5 presents the L0 inspection line for a 40mm dent spacing. Although the front facesheet deformation between the two damage sites reaches a local maximum deformation of -0.11mm, it remains above the threshold of -0.10mm. Therefore, the panel is still classified as having a single dent, and the overall and effective dent widths remain equivalent.

In contrast, Figure 5.6 shows the L0 inspection line for a panel with a 50mm dent spacing. Here, the deformation profile between the two damage sites achieves a local maximum deformation of -0.037mm, indicating the presence of two distinct dents since it is less than the -0.10mm threshold. In this case, the effective dent width corresponds to the width of an individual dent rather than the combined outer width of both dents.

Figure 5.7 presents the peak force plotted against the effective dent width. The graph shows three distinct groups of behaviour that reflect the behaviours outlined in Section 5.2.2.

- Group 1: Dents with dent spacings of 0-25mm are overlapping and exhibit core crushing between them. The damage sites are close and are considered one dent.
- Group 2: Dents measuring 30-40mm dent spacing also overlap, but show limited core crushing. The overall damage site still remains with one dent.
- Group 3: At 45mm dent spacing and above, dents no longer overlap and do not exhibit core crushing between them. The dents become independent, forming two separate damage sites.

These three behaviours are only evident when plotted against the effective width, as this parameter captures whether the dents overlap or remain distinct.

In the first group, where center-to-center dent spacing is 0-25mm, the dent is behaving as a single dent; the wider the dent, the more significant the reduction in peak force. In the simulations, this is shown by a reduction in peak force from 64.8 kN to 61.1 kN.

In the second group, for dent spacings of 30-40mm, the dents overlap. This creates a region of undeformed core in between that prevent the dents from coalescing. The greater the dent width, the larger the region of undeformed core between the damage sites and the higher the peak force. The predicted results show an increase in the peak force from 65.2 kN to 66.7 kN.

In the third group for dent spacings 45mm and larger, the dents act independently of one another. Dent spacing has no effect on peak force, and the simulated data for group 3 appear clustered around the single dent (67.3 kN).

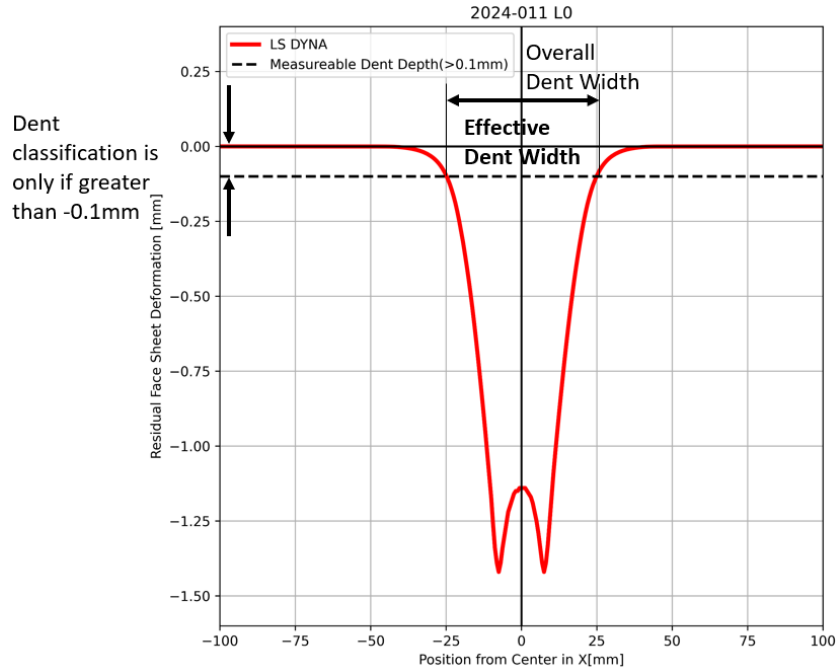


Figure 5.4: Measurement of effective dent width, overall dent width, and dent depth retrieved from the L0 inspection line from a FE model with overlapping dents with a 15mm center-to-center spacing.

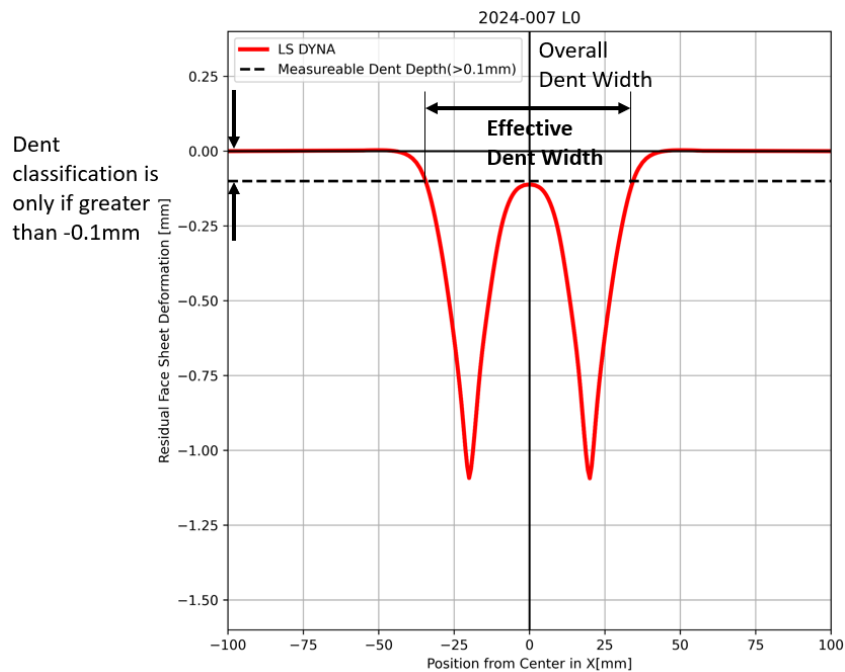


Figure 5.5: Measurement of effective dent width, overall dent width, and dent depth retrieved from the L0 inspection line from a FE model with overlapping dents with a 40mm center-to-center spacing.

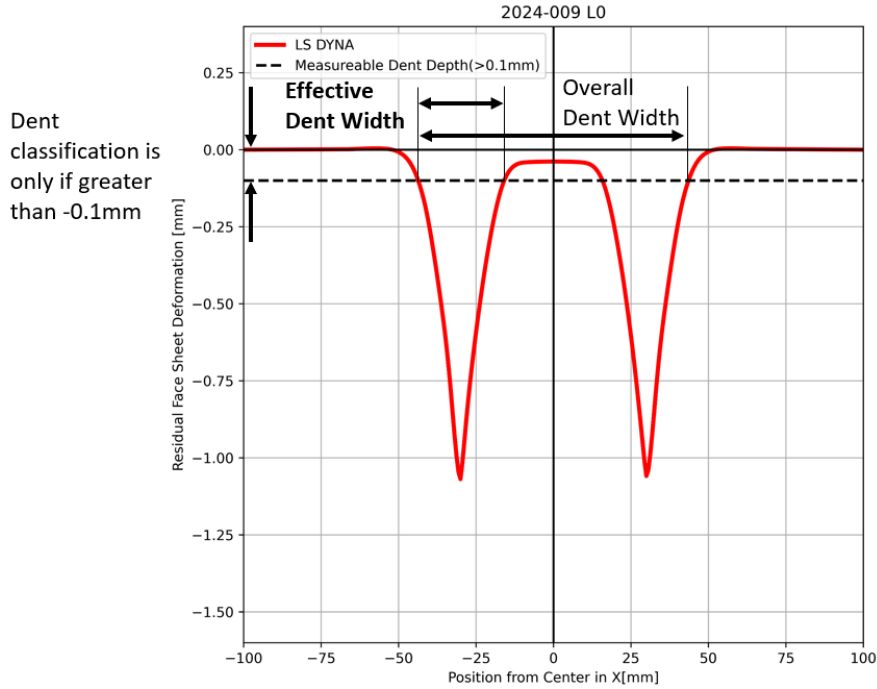


Figure 5.6: Measurement of effective dent width, overall dent width, and dent depth retrieved from the L0 inspection line from a FE model with separate dents with a 50mm center-to-center spacing.

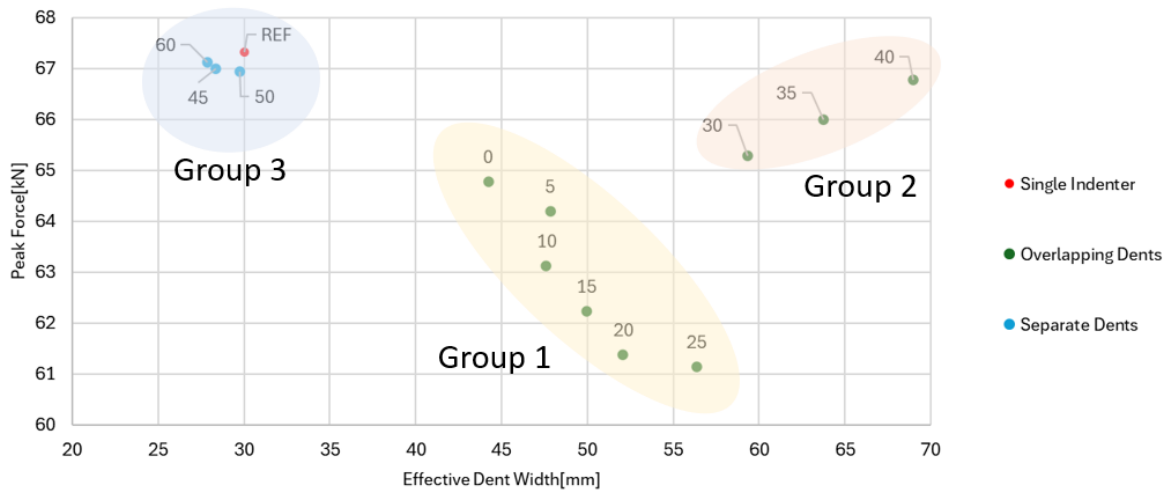


Figure 5.7: The peak force plotted against the effective dent width. The labels are the center-to-center configuration distance. The reference point is that of a single indentation.

Figures 5.8 and 5.9 show the Z-displacement for 25mm and 30mm center-to-center configurations at (a) the onset of Stage 2, (b) the panel yield, (c) peak force, and (d) failure. The 25 mm and 30 mm dent spacings were selected because, as shown in Figure 5.7, this range corresponds to a significant increase of 4.2 kN in peak force with increasing dent spacing and effective dent width, distinguishing Group 1 from Group 2.

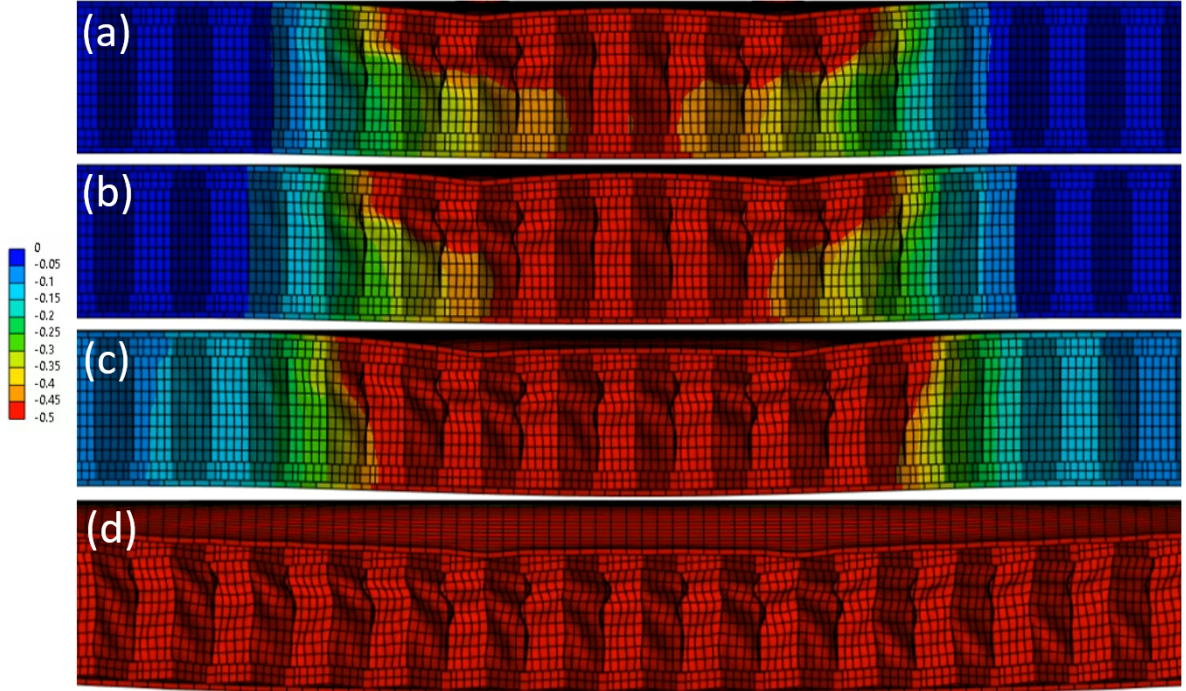


Figure 5.8: Z Deformation(mm) of a 25mm center-to-center spacing at (a) beginning of Stage 2, (b) panel yield, (c) at peak force, and at (d) panel failure.

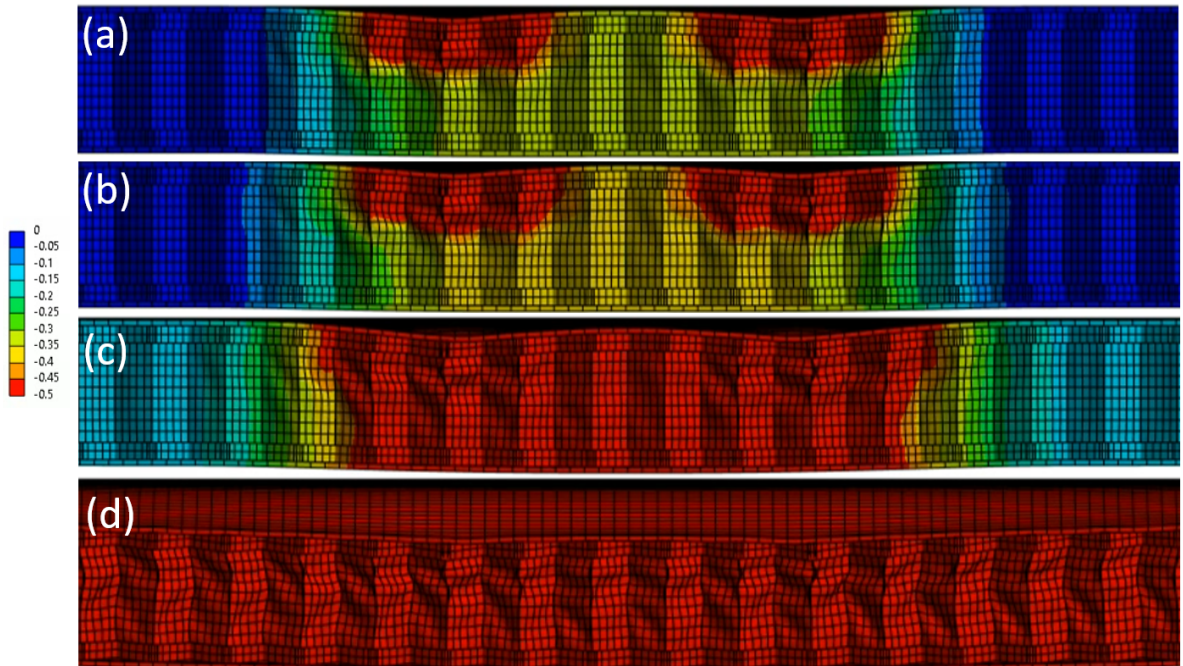


Figure 5.9: Z Deformation(mm) of a 30mm center-to-center spacing at (a) beginning of Stage 2, (b) panel yield, (c) at peak force, and at (d) panel failure.

At the onset of Stage 2, Figure 5.8a shows greater deformation between the damage sites than Figure 5.9a. This is evident from both the more intense contour colouring and the pronounced folding of the cell walls between the damage sites in the 25mm configuration.

At yield, the dents in both Figure 5.8b and Figure 5.9b deepen, as indicated by the change in contour colours. Consistent with the earlier stage, the 25mm spacing exhibits more severe cell wall folding between the damage sites than the 30mm spacing.

At peak force, the out-of-plane deformation at and between the damage sites exceeds 0.5 mm for both the 25mm (Figure 5.8c) and 30mm (Figure 5.9c) configurations. As the dents deepen, the 25mm spacing shows collapse of the material between the damage sites into the panel. In contrast, the 30mm spacing exhibits less pronounced collapse, as the cell walls between the damage sites are less affected by the indentations.

At failure, the dent width expands across the panel, the out-of-plane deformation increases further, and cell wall folding extends from edge to edge in both the 25mm (Figure 5.8d) and 30mm (Figure 5.9d) configurations.

Figures 5.10 and 5.11 show the 40mm and 45mm center-to-center dent spacings at (a) the onset of Stage 2, (b) yield, (c) peak force, and (d) failure. Unlike the transition from Group 1 to Group 2, which exhibits a significant increase in peak force in Figure 5.7, the difference in peak force between Group 2 and Group 3 is less than 200 N. More notably, the effective dent width decreases from 68.9 mm to 28.3 mm. Based on the definition of effective dent width outlined earlier in this section, this transition corresponds to a change from a single dent to two distinct dents.

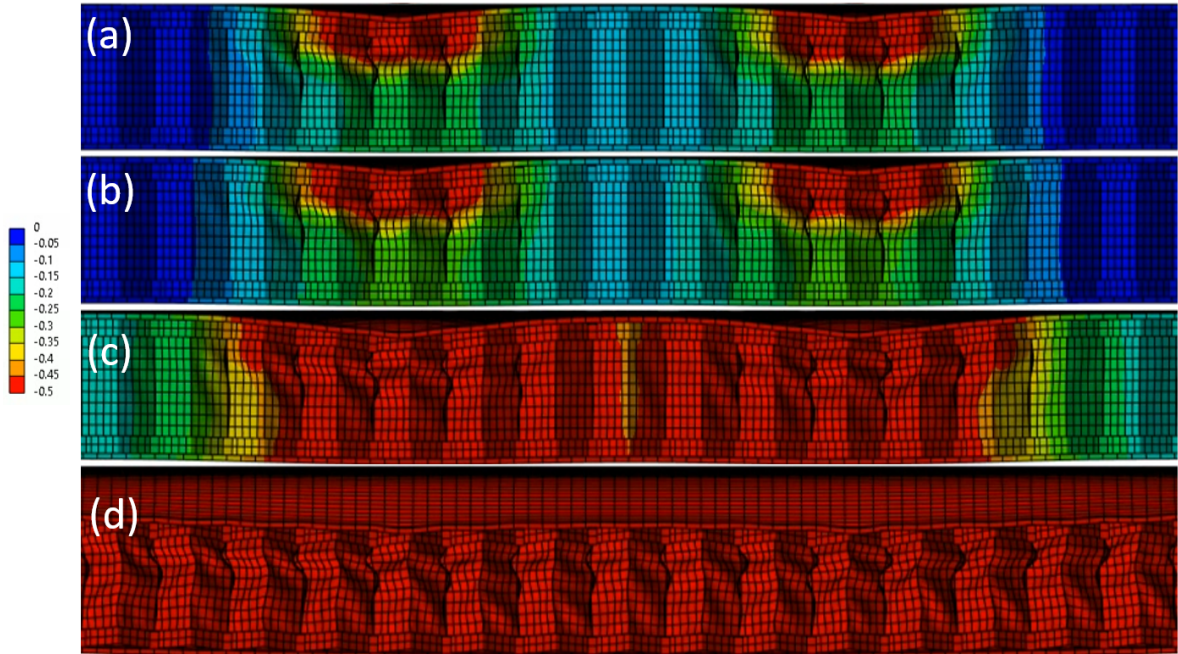


Figure 5.10: Z Deformation(mm) of a 40mm center-to-center spacing at (a) beginning of Stage 2, (b) panel yield, (c) at peak force, and (d) panel failure.



Figure 5.11: Z Deformation(mm) of a 45mm center-to-center spacing at (a) beginning of Stage 2, (b) panel yield, (c) at peak force, and (d) panel failure.

This distinction is evident in the cross-sections. At the onset of Stage 2, the 40mm spacing (Figure 5.10a) shows two pronounced damage sites, with no observable folding of the cell walls between them. The contour deformation indicates an out-of-plane deflection of approximately -0.10 mm to -0.15 mm. In contrast, the 45mm spacing (Figure 5.11a) appears similar in overall shape; however, the deformation between the damage sites is less than -0.10 mm, reinforcing the presence of two distinct dents.

At yield (Figures 5.10b and 5.11b) and peak force (Figures 5.10c and 5.11c), both configurations exhibit similar behaviour. The cell walls beneath the damage sites deform in a similar manner, with only minor differences in the out-of-plane contour distribution arising from the dent spacing. In both cases, cell wall folding between the damage sites remains limited.

At failure (Figures 5.10d and 5.11d), the responses are nearly indistinguishable, aside from the difference in dent spacing.

5.2.3 Relationship between Peak Force and Other Dent Characteristics

The peak force has been shown to depend on the localized dent width. However, to confirm that this relationship is governed primarily by the effective dent width, it is necessary to also evaluate the influence of the other dent characteristics, namely dent depth and dent diameter. This section therefore examines these additional metrics to determine whether any clear relationship exists with peak force.

Peak Force and Dent Diameter

Figure 5.12 shows the peak force plotted against the dent diameter defined in Figure 3.6. This plot shows that the peak force does not have a clear trend with the dent diameter, therefore, dent diameter is not the primary driver of the residual strength of honeycomb panels.

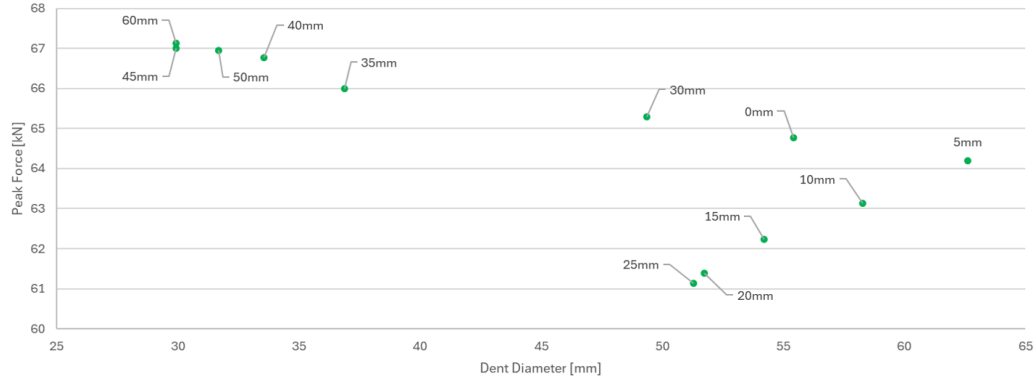


Figure 5.12: The peak force plotted against the dent diameter. The labels are the center-to-center configuration distance.

Peak Force and Dent Depth

Figure 5.13 shows the peak force plotted against dent depth. The relationship does not follow the expected trend. Bactor investigated the effect of dent depth on peak force using a single indenter and found that increasing dent depth reduced peak force [26]. A similar response might therefore be expected for the simulated dent spacings. However, the results show a decrease in peak force between 1.0mm and 1.25 mm dent depths, followed by an increase as dent depth increases from 1.25 mm to 2.0mm.

The red region represents 0-25mm center-to-center dent spacings, where the core material between the two dents is damaged. In this range, peak force unexpectedly increases with increasing dent depth. The yellow region corresponds to the transitional zone of 30-40mm center-to-center spacing, where the core between the dents experiences limited damage. This region also unexpectedly shows an increase in peak force with increasing dent depth. The green region highlights configurations 40mm and larger where the dents behave similarly to a single indentation. Here, no clear increase or decrease in peak force with dent depth is observed.

Overall, Figure 5.13 suggests that although increasing dent depth would generally be expected to reduce peak force, this effect is not dominant in the present study. Instead, dent width has a greater influence on peak force, particularly when the dents overlap.

5.3 Influence of Dent Proximity

The present research found that the peak force of the aluminum honeycomb panels decreased relative to that of a single dent when the dents, spaced perpendicular to the loading direction, overlapped. As a result, three distinct behavioural regimes were identified for different dent spacings, depending on the effective dent width.

Similarly, Walker et al. developed a multi-site correction factor (MCF_o) for composite sandwich panels to predict the peak force based on dent width and dent spacing. Their study examined panels containing fibre-reinforced polymer facesheets with Nomex[®] cores subjected

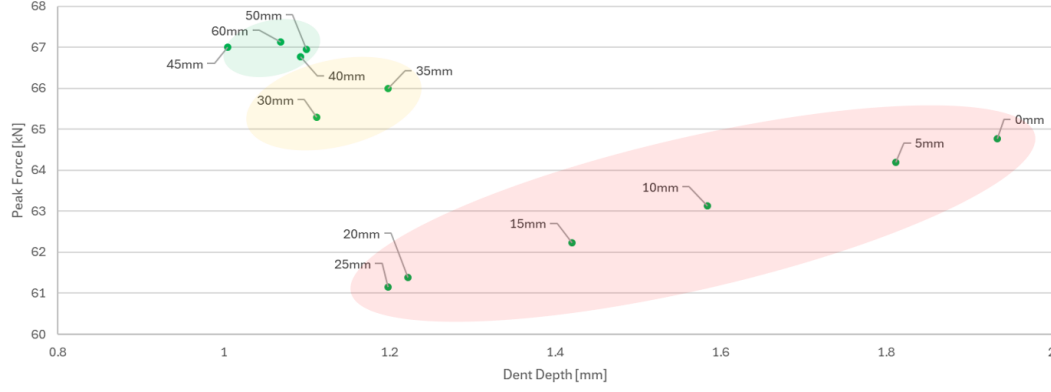


Figure 5.13: The peak force plotted against the dent depth. The labels are the center-to-center configuration distance.

to two dents. Equation 5.1 expresses the multi-indentation site strength (ε_{ms}) as the single-indentation site strength (ε_{ss}) modified by the multi-site correction factor for dent spacing (MCF_o).

$$\varepsilon_{ms} = MCF_o \times \varepsilon_{ss} \quad (5.1)$$

The multi-site correction factor is based on the spacing between dents from edge to edge (S) and the larger diameter of each damage pair (D), assuming circular damage geometry.

$$MCF_o = \frac{(\frac{S}{D})^2}{0.08 + (\frac{S}{D})^2} \quad 0.5 \leq S/D \leq 4.0 \quad (5.2)$$

As the spacing between dents increases, the predicted peak strength of the multi-site panel also increases. Conversely, increasing dent diameter without increasing spacing reduces the predicted panel strength.

Walker et al. adopted a lower bound of $S/D = 0.5$, above which damage sites were treated independently because of limited experimental data, and an upper bound of $S/D = 4.0$, above which the correction factor becomes negligible and asymptotically approaches 1.0. Walker's equation does not consider overlapping dents; the minimum correction factor occurs at $S/D = 0.5$, where $MCF_o = 0.75$, corresponding to a maximum reduction in peak force of 25% relative to the single-site panel strength.

In the present study, the first dent spacing at which two distinct dents formed occurred at 45mm, corresponding to $S/D = 0.59$. Based on the relationship proposed by Walker et al., this spacing yields an MCF_o value of 0.8145, corresponding to a predicted reduction in residual strength of approximately 19%. However, the experimental results obtained in the present study showed a reduction of less than 1%, indicating that the peak force had effectively converged to that of a single-dent configuration at $S/D = 0.59$. This contrasts with the behaviour predicted by Walker et al., where convergence toward the single-site strength does not occur until approximately $S/D = 4.0$.

The reduction in peak force cannot be accurately predicted using Equation 5.2, since the crush region and damage mechanisms differ between metallic honeycomb panels and composite sandwich panels. However, the dependence of peak force on dent spacing exhibits a similar overall trend.

Walker et al.'s approach of defining behaviour using bounded values of S/D can be extended to the present study to define the three distinct behavioural regions shown in Figure 5.7. Current work on aluminium honeycomb panels shows that three distinct behavioural regions exist in which the panel's residual strength, F_P , varies with dent spacing, d , measured perpendicular to the loading direction. Developing a fully predictive relationship for residual strength was beyond the scope of this thesis; however, the observed behaviour can be described using a piecewise framework.

For Group 1 (Equation 5.3), the center-to-center dent spacing is inversely related to peak force for spacings between 0mm and the first critical distance, $d_{Critical,1}$. For Group 2 (Equation 5.4), residual strength varies directly with dent spacing between $d_{Critical,1}$ and $d_{Critical,2}$, where interaction effects between dents are most pronounced. For Group 3 (Equation 5.5), residual strength becomes independent of dent spacing once the center-to-center spacing exceeds $d_{Critical,2}$.

$$F_P \propto \frac{1}{d} \text{ for } 0 < d < d_{Critical,1} \quad (5.3)$$

$$F_P \propto d \text{ for } d_{Critical,1} < d < d_{Critical,2} \quad (5.4)$$

$$F_P \not\propto d \text{ for } d_{Critical,2} < d \quad (5.5)$$

Figure 5.14 shows the critical spacings of $d_{Critical,1} = 25\text{mm}$ and $d_{Critical,2} = 40\text{mm}$ applied to the simulated data. Notably, these three equations are broadly applicable and may be used across varying impact energies, panel designs, and impactor geometries, with the critical distances ($d_{Critical,1}$ and $d_{Critical,2}$), changing depending on the configuration.

These equations are formulated specifically for panel configurations in which the dent spacing is oriented perpendicular to the loading direction. Configurations with dents aligned parallel to the loading direction, such as those reported by Boctor in Figure 2.14, exhibit an increase in peak force that exceeds the peak force of a panel with a single-indentation site [26]. Consequently, the relationships presented in these equations are not intended for such cases.

5.4 Indentation Methodology

One factor that may have contributed to a difference between the FE and experimental results is how the impacts were created. The primary difference in the testing methodology is that the experimental damage was created by impacting the panel in series, and the numerical model used simultaneous indentation. The selection of the simultaneous impacts followed

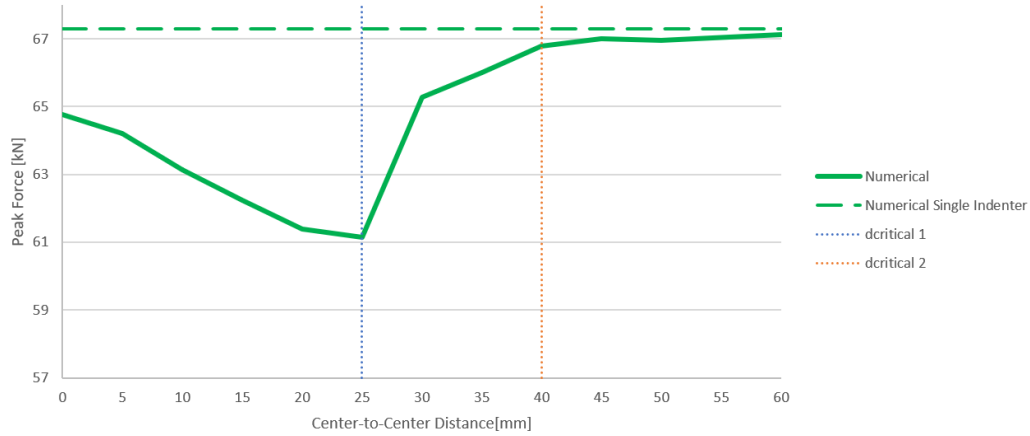


Figure 5.14: Peak force plotted against dent center-to-center distance of the panel.

Boctor's research in examining the impacts of dents spaced parallel to the loading direction [26]. The selection was made to reduce computational requirements for solving time and storage. This also means that in the experimental methodology outlined in Section 3.5.3, the cut-out was constantly the same size (75mm x 125mm), whereas the numerical model, as outlined in Section 3.4.7, had the width of the cut-out increase by the center-to-center distance.

A new numerical model was created to simulate the 30mm center-to-center impact to compare the differences between the sequential impact method and the simultaneous impact method. In Stage 1, the indenters were redefined to impact one at a time. A 75mm x 125mm cut-out was created and centered with the first indenter. The first cut-out lasted the duration of the first impact. Once the first indenter rebounded, the cut-out was redefined to be centered with that of the second indenter. A comparison of Stage 1 simultaneous indentation is shown in Figure 5.15, and that of Stage 1 series indentation is shown in Figure 5.16.

Figures 5.15 and 5.16 illustrate the different Stage 1 deformation profiles. The simultaneous impacts, shown in Figure 5.15, apply the total energy at once and use a larger cut-out compared to the individual impacts. In contrast, the individual impacts in Figure 5.16 occur as two separate events with smaller cut-outs. The series indentation case exhibits two dents, with the second indentation being larger than the first. Compared to the simultaneous indentation case, the series indentation shows a 2% increase in dent depth, and an 8% increase in dent width. This results in a deeper and wider dent, indicating more severe panel damage. However, this increased damage led to only a 1.6% reduction in peak force versus simulated impacts, while increasing the computational solve time and storage requirements by 60%.

Figure 5.17 is an overlay of the 30mm center-to-center dent configuration made in series and simultaneously. The damage created in series fails with a 7% reduced strain and a 1.6% reduction in stress compared to the simultaneous impact model.

This section highlights differences in Stage 1, with the different impact and cut-out methodologies primarily affecting dent characteristics and overall panel bending. The effect on the peak force is small and does not significantly change the trend in the peak force.

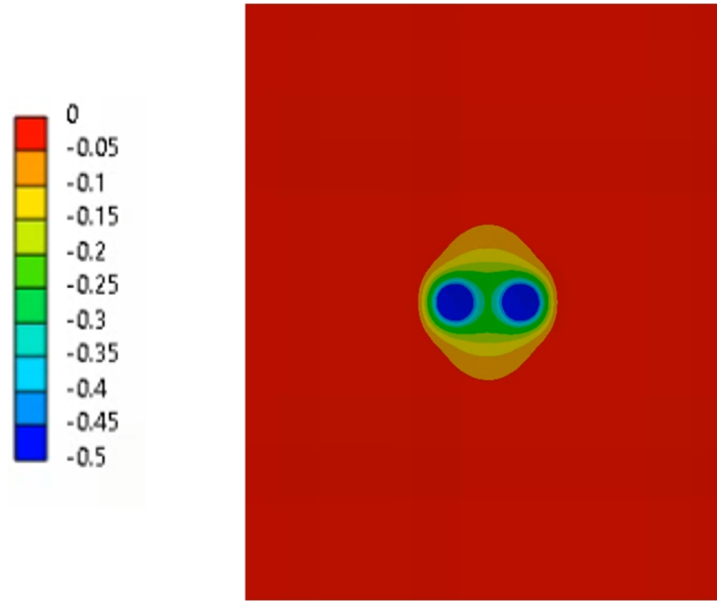


Figure 5.15: Stage 1 of a 30mm center-to-center indentation configuration after simultaneous indentation.

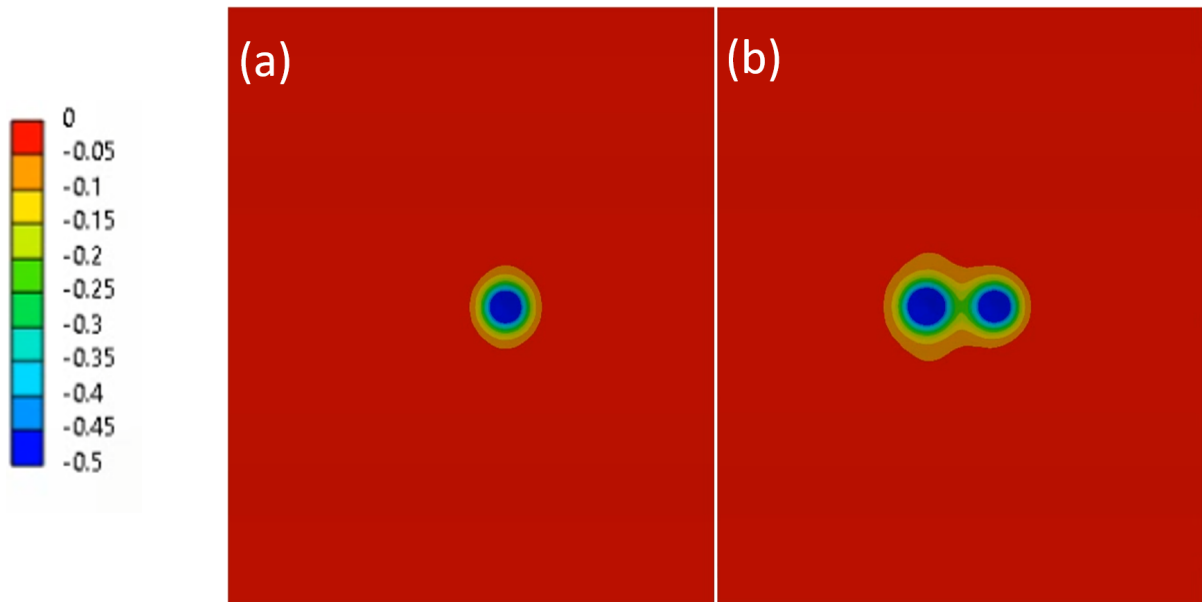


Figure 5.16: Stage 1 of a 30mm center-to-center series indentation configuration at (a) after first indentation, (b) after the second indentation.

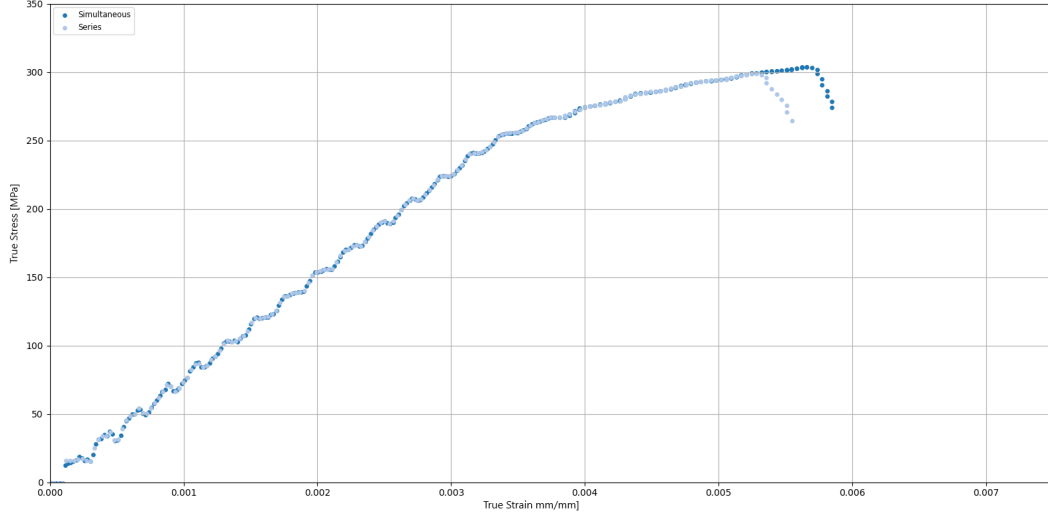


Figure 5.17: True stress-strain curve of the 30mm center-to-center dent configuration comparing the damage created in series and simultaneously.

5.5 Field Application

5.5.1 Spacing Definition

Low-velocity impact allowables are defined by several factors, including the proximity and location of impacts, dent size (width and depth), and the overall dent coverage on the panel. The present study shows that when two impact sites overlap, the residual strength decreases below that of a single dent. Conversely, when the dent edges remain separated, the two sites behave independently and the residual strength approaches that of a single-dent configuration. In practice, this behavior underscores that edge-to-edge spacing provides a more meaningful and practical criterion than center-to-center distance, since dent interactions occur only when their edges overlap. Furthermore, the use of center-to-center distance is limited to idealized circular dents and does not adequately characterize irregular dent geometries encountered in practice.

This observation is supported by the work of Wowk et al. [35], who showed that core damage in aluminum honeycomb panels extends only to the boundary of the facesheet surface damage. Consequently, dents that do not overlap retain an undamaged core region between them, resulting in independent structural behaviour of the damage sites. This behaviour is illustrated in Figure 5.18, where the peak force is plotted against the inner edge-to-edge spacing. At $d_{Critical,2}$, the inner edge-to-edge spacing becomes 0 mm, corresponding to the transition point between overlapping damage sites and two distinct dents. For positive edge-to-edge spacings, the peak force converges to that of independent dent behaviour. In contrast, negative edge-to-edge spacings, corresponding to overlapping dents, result in a reduction in panel strength as a function of the overlap distance. Overall, these results demonstrate that edge-to-edge spacing, rather than center-to-center distance, is the more appropriate metric for characterizing dent interaction and predicting reductions in residual strength.

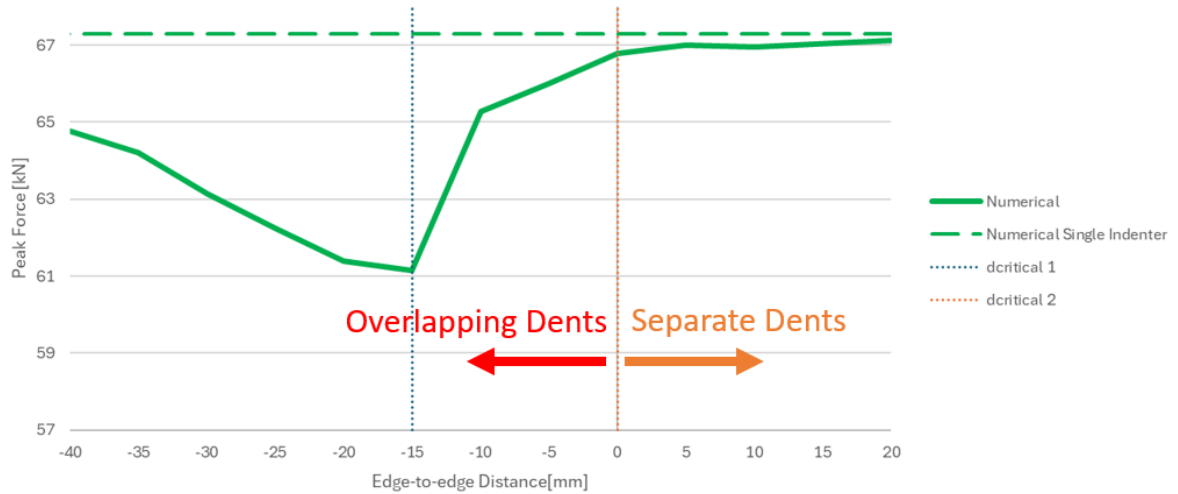


Figure 5.18: Peak force plotted against dent inner edge-to-edge distance between.

5.5.2 Dent Allowables

Compared to a single dent, the presence of two closely spaced dents does not significantly reduce the static residual strength provided that the dents do not overlap. This result has important implications for SRMs for metallic honeycomb panels, as it suggests that current dent-spacing limits may be overly conservative, potentially leading to unnecessary repair or replacement of panels. However, before any revision of allowable limits can be considered, this behaviour must be validated under additional loading conditions, including fatigue and shear, to confirm that the observed insensitivity to dent interaction is not limited to static compression. Further work should also investigate a wider range of honeycomb geometries and cell sizes to assess the generality of the observed trends. In addition, testing configurations involving two dents of differing sizes would enable the development of a more generalised predictive relationship, analogous to the framework proposed by Walker et al. [61], which could support the refinement of dent interaction criteria and spacing allowables in future SRM guidelines.

6 Conclusions and Recommendations

6.1 Conclusions

The effect of proximity between multiple dents spaced perpendicular to the in-plane compressive loading on the residual strength of metallic honeycomb sandwich panels subjected to low-velocity impact damage was investigated. This was accomplished through the development and calibration of a FE model against experimental testing. The following conclusions can be made:

- The peak force (residual strength) was shown to be dependent on the dent spacing and the effective width of the damage region and governed by three different behaviours.
 1. From 0 mm to $d_{Critical,1}$ (0–25 mm), the peak force decreased as dent spacing increased. In this region, the overlapping damage sites caused core crushing between the indentations, effectively forming a single larger dent and thereby reducing the panel's peak force as dent spacing and dent width is increased.
 2. Between $d_{Critical,1}$ and $d_{Critical,2}$ (25mm to 40mm), the peak force increases with increasing dent spacing. The dent spacing also overlaps, forming one dent, but shows limited core crushing between the damage sites, increasing the panel's peak force as dent spacing and width increase.
 3. For dent spacings greater than $d_{Critical,2}$ (40mm), the two damage sites no longer overlap, forming two separate dents with no damage to the core between the dents. If the dents do not overlap, they behave independently and have the same peak force as a single dent.
- For the panel configuration tested, a second dent spaced perpendicular to the loading direction with the same size and depth that does not overlap the first, is shown to have no additional effect on the panels strength.
- For the spacings studied, the panel experiences a maximum strength reduction of 9% compared to a single indentation. This occurs at a 25mm dent spacing.
- Simultaneous indentation for introducing multiple dents can be used as a computationally more feasible method than series indentation for predicting peak force.

6.2 Recommendations for Future Research

This study used coupons from a single aluminium honeycomb panel configuration, which limits the wider applicability of the findings. Specifically, the investigation considered only one combination of panel material, facesheet material and thickness, core material and thickness, and core density, subjected to impact from a single impactor size at one specific impact energy. Consequently, the conclusions are restricted to this particular configuration and highlight opportunities for further investigation. Accordingly, recommendations for future research include:

- Investigating alternative aluminium honeycomb panel configurations by varying facesheet thickness, core density, and foil thickness to promote dent localization prior to yielding of the facesheet to assess the influence of panel configurations on the interaction between the dents.
- Evaluating the effects of different impactor diameters and impact energies to produce a wider range of dent depth-to-width ratios, including both shallower and deeper dents, and determining their influence on the residual compressive strength of the panel.
- Investigating damage scenarios involving three or more dents to assess the interaction effects within dent clusters, including dents arranged both parallel and perpendicular to the loading direction, and to establish whether the interaction trends identified for two dents remain applicable to more complex damage configurations.
- Further improving the developed model through:
 - Additional repetitions of the experimental testing to strengthen confidence in the observed trends.
 - Refinements to address the differences in panel bending observed between the numerical and experimental results.
 - Further developing the model so that the predicted failure strain more closely matches the experimental failure strain.

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Appendices

A Test Reports and Certificate of Conformance

Test Report

Upper Canada Steel Inc.
P.O. Box 917
253 Industrial Park Road
Colborne, Ontario K0K 1S0 Canada

Date Tested: 10/18/2023
Test Report Number: 23J0822
PO #: 10012

DESCRIPTION OF PART

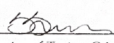
Part #	Thick	Width	Length	Face Material Back Material	Core Material	Lot No.	Shop No.	Qty
900x48x96	.500	48	96	.020-2024-T3-Clad-48 .020-2024-T3-Clad-48	1/4-001N-5052-(2.3pcf)	101723	AE 2395-1	2

Test Type	T-ID	Requirec	Results	Average
Climbing Drum Peel	66219	Avg 30 in-lbs/3	1 75.6 2 85.6 3 89.4	83.5 in-lbs/3 in.
Flatwise Tensile	66228	Avg 750 psi	1 752.6 2 691.6 3 700.3	714.8 psi
100% Core Failure.				
Thickness	66229	Avg 0.500±0.010 in.	1 497 2 504	.501 in.

Remarks:
2 PCS Released 10/31/2023 DT # 20120

THIS TEST MADE ACCORDING TO SPECIFICATION: Geneeco No. 3, Type 119C

I hereby certify that the above tests were made
and the results were as shown hereon.


Director of Tests QA Manager

Page 1 of 1

Figure A.1: Product data sheet for Aluminum Honeycomb panel used for experimental testing

Upper Canada Steel Inc.
P.O. Box 917

CERTIFICATE OF CONFORMANCE

10/31/2023

Customer P.O.: 10012 Item # 1

Shop No.: AE 2395 - 1 A

Quantity: 2 Geneerco Honeycomb Panels

Material: .500x48x96

Geneerco No. 3 Type 119C

Skins: .020-2024-T3-Clad

Core: 1/4-.001N-5052-(2.3pcf)

Face: .020-2024-T3-Clad-48, AMS QQ-A-250/5

Back: .020-2024-T3-Clad-48, AMS QQ-A-250/5

Core: 1/4-.001N-5056-(2.3pcf), AMS-C-7438

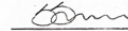
Manufactured and tested in accordance with

Geneerco No. 3, Type 119C

Test Reports as follow have been reviewed, are maintained on file and are enclosed with shipment.

Qty	Test No.	Lot No.	DT #
2	23J0822	101723	20120

We certify that all material shipped to you on the above
Purchase Order number meets all specifications shown therein.



10-31-2023

Date



Figure A.2: Certificate of conformance from sandwich panel manufacturer- original honey-comb sandwich panel sheet

B Test Coupons Measurements and Measurement Locations

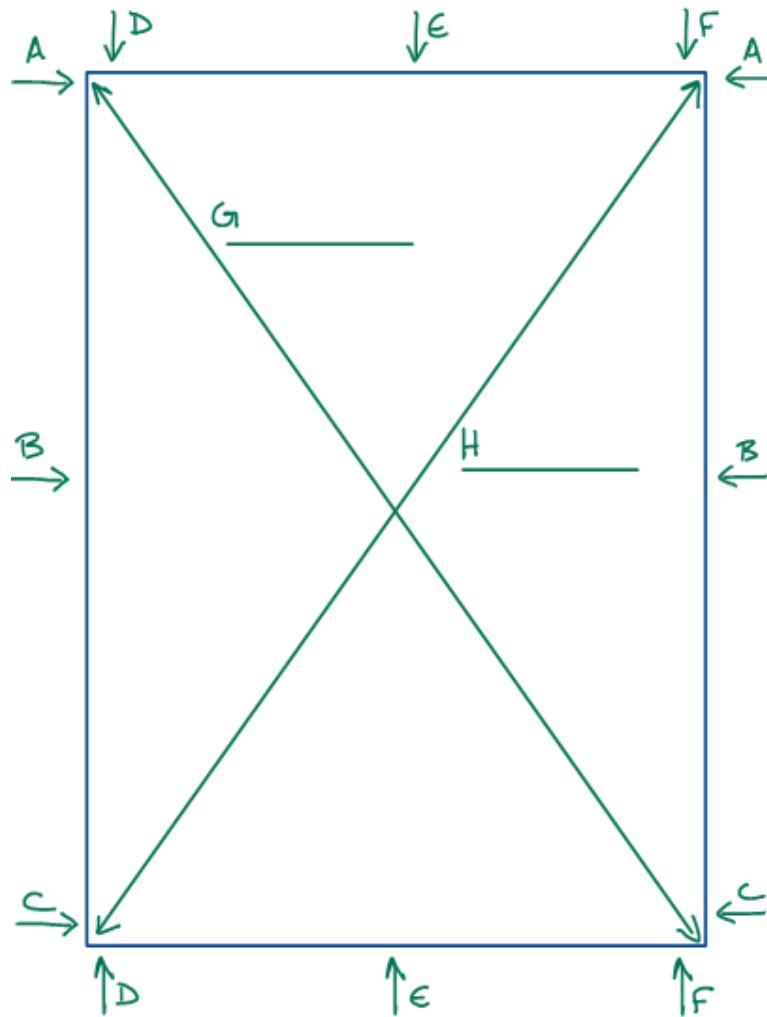


Figure B.1: Panel Measurement Points - Graphic by Major Patrick Wheeler [27]

Table B.1: Test panel center-to-center spacing measurements compared with targets.

Center to Center Configuration [mm]	Measurements [mm]							
	A	B	C	D	E	F	G	H
Target	215 $\pm$ 0.25	215 $\pm$ 0.25	215 $\pm$ 0.25	265 $\pm$ 0.25	265 $\pm$ 0.25	265 $\pm$ 0.25	-	-
0 (REF)	215.25	215.13	215.18	265.11	265.16	265.08	341.32	340.70
0	215.07	215.25	215.00	264.87	264.98	265.04	340.30	340.78
5	215.17	214.99	215.13	265.04	264.99	265.16	341.48	340.52
10	214.98	215.15	215.04	265.17	265.13	265.24	341.36	340.30
15	215.06	215.09	215.20	264.94	264.97	265.14	340.40	341.42
20	215.17	215.19	215.24	265.08	265.17	265.23	341.54	340.84
25	215.25	215.20	215.07	265.18	265.12	265.17	341.60	340.80
30	215.15	215.24	215.20	265.15	265.05	265.25	340.00	340.86
35	215.17	215.08	215.19	264.86	265.02	265.14	341.40	340.40
40	215.15	215.12	215.02	264.82	265.06	265.13	341.30	340.46
45	215.19	215.15	215.14	265.24	264.96	265.24	341.00	341.57
50	215.22	215.01	215.17	265.07	265.05	265.23	341.64	340.48
60	215.16	215.09	214.89	265.04	264.98	265.10	341.24	340.74