

Development of Novel Techniques to Characterize the Traction-Separation Responses of Delamination in Fibre- Reinforced Plastic Laminates

by

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The following served on the Examining Committee for this thesis. The decision of the Examining Committee is by majority vote.

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Author's Declaration

This thesis consists of material all of which I authored or co-authored: see Statement of Contributions included in the thesis. This is a true copy of the thesis, including any required final revisions, as accepted by my examiners.

I understand that my thesis may be made electronically available to the public.

Statement of Contributions

Devon Hartlen developed all methodologies presented in this thesis, including but not limited to:

- Development of the rigid adherend Mode I, Mode II, and Mode III specimens for fibre-reinforced plastic laminates, including specific techniques and protocols for fabrication, testing, and data processing.
- Development of analysis procedures to post-process experimentally measured delamination data for numerical modelling of delamination
- Creation of finite element models of all developed specimens and additional specimens tested for independent assessment of experimental data, including explorations of how to model delamination with cohesive elements within a finite element framework.

Duane Cronin and John Montesano assisted in the formulation of a general plan to develop low-compliance specimens to study delamination, provided technical guidance and supervision through out this project, and gave editorial feedback on this thesis.

Some content in this thesis has been previously published in peer-reviewed journal articles:

Hartlen, D.C., Montesano, J., Cronin, D.S., 2023. A Composite Rigid Double Cantilever Beam Specimen for Assessing the Traction–Separation Response of Mode I Delamination in Composite Laminates. *Exp Mech* 63, 1273–1283. <https://doi.org/10.1007/s11340-023-00987-2>

DCH was responsible for the development of the Mode I specimen and associated fabrication, testing, and analysis protocol, as well as the drafting of the manuscript (70%). JM (15%) and DSC (15%) provided supervision, technical advice, and editorial feedback on the manuscript.

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DCH was responsible for creating all finite element models, developing and verifying the analysis procedures to enhance the modelling of delamination, and processing and assessing model results (70%). JM (15%) and DSC (15%) provided supervision, technical guidance, and editorial feedback on the manuscript.

The co-authors agree with the evaluation of the roles and contributions.

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Abstract

Fibre-reinforced plastic (FRP) composite laminates are a compelling class of materials for crash-resistant automotive structures owing to their excellent specific energy absorption and damage tolerance characteristics. However, the widespread adoption of FRP laminates in load-bearing structures requires accurate modelling of progressive damage under mechanical loading, including interlaminar delamination. Current experimental techniques cannot fully parameterize numerical techniques such as cohesive zone modelling (CZM), which represents the delamination interface using traction-separation responses. This thesis aimed to develop experimental tests to measure the Mode I and Mode II/III traction-separation responses of delamination in a unidirectional E-glass/epoxy (UE400/REM) FRP laminate for use in CZM.

A novel composite rigid double cantilever beam (cRDCB) Mode I test specimen was developed to assess delamination response using metallic adherends co-cured to the FRP laminate. The cRDCB and analysis procedures developed in this work enabled direct assessment of the Mode I traction-separation response. The measured cRDCB traction-separation response was verified by accurately modelling the cRDCB test and validated by modelling the double cantilever beam test.

Two specimen geometries were developed to measure the Modes II and III traction-separation response under shear loading. The Mode II composite rigid shear (cRS) specimen demonstrated excellent sensitivity to damage onset and early damage propagation but was limited in assessing the unload response due to the high stiffness of the specimen. The Mode III composite rigid Mode III (cR3) specimen progressively loaded the delamination interface, providing a more reliable technique for assessing damage behaviour response. Traction-separation responses were extracted using a physics-informed inverse technique, providing a promising method to characterize delamination shear response.

The primary contribution of this work was new experimental specimens and associated analysis procedures that provided complete traction-separation curves for CZM, validated using computational models of contemporary test specimens. The properties measured with the proposed specimens have applicability in a range of current and future modelling techniques and can be used to inform the development of FRP laminate automotive structures.

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List of Acronyms and Initialisms

cRDCB	Composite rigid double cantilever beam
cRS	Composite rigid shear
cR3	Composite rigid Mode III
CZM	Cohesive zone modelling
DCB	Double cantilever beam
DIC	Digital image correlation
ELS	End loaded splitting
ENF	End-notched flexure
ERR	Energy release rate
FE	Finite element
FRP	Fibre-reinforced plastic
PTFE	Polytetrafluoroethylene, also known by the tradename Teflon
R-curve	Resistance curve
RDCB	Rigid double cantilever beam
SD	Standard deviation
SIF	Stress intensity factor
TSR	Traction-separation response
VCCT	Virtual crack closure technique

Nomenclature

A_{delam}	Area of the delamination interface
$\alpha(\delta_m)$	Crack tip compensation function with respect to measured displacement
b	Initial delamination interface length
B	Specimen width
δ	Separation, normal direction
δ_c	Compressive separation of cRDCB specimen
δ_d	Separation used for damage-only TSRs with the effective stiffness method
δ_m	Measured specimen displacement
δ_o	Opening separation of a cRDCB specimen
δ_s	Shear separation
Δ	Displacement of the measurement point of the cRDCB specimen
Δ_m	Displacement vector of measurement point m of a cR3 specimen
$\Delta\theta$	Angular displacement of a single cR3 adherend
E_{eff}	Effective stiffness of a composite material
E_{ij}	Young's Modulus in the ij plane
$E_{ij,ps}$	Young's modulus in the ij plane, assuming plane strain conditions
G_{ij}	Shear modulus in the ij plane
G_I	Mode I energy release rate
J_I	Mode I J-integral
k_c	Effective cohesive stiffness of material adjacent to the delamination interface
k_f	Frame stiffness used to assess crack stability numerically
k_m	Initial interface stiffness
k_p	Initial penalty interface stiffness
K_I	Mode I stress intensity factor
L	Distance from the edge of a cRDCB specimen to where the load is applied
λ, n, ρ	Parameters used for effective stiffness calculations for stress intensity factor
$P, P(\cdot)$	Force, force with respect to some variable
$\mathbf{r}_{p/m}$	Vector from measurement point m to any point p
$R(\cdot)$	2D rotation matrix
$\sigma, \sigma(\cdot)$	Traction, traction with respect to some variable
σ_c	Compressive traction in a cRDCB interface
$\tau, \tau(\cdot)$	Shear traction, shear traction with respect to some variable
μ	Instantaneous pivot location of a cRDCB delamination interface
μ_e	Initial, elastic pivot location of a cRDCB delamination interface
V	Shear force, typically an internal reaction
ν_{ij}	Poisson's ratio in the ij plane
W	Work
x	cRDCB: any point along the interface length
$\mathbf{x}_p$	Position vector of any point p
$\mathbf{x}_m$	Position vector of the measurement point m of a cR3 specimen

Chapter 1

INTRODUCTION

Fibre-reinforced plastic (FRP) composite laminates present a compelling alternative to metallic alloys for structural applications. FRP laminates comprise several layers or plies, each consisting of reinforcing fibres encompassed in a polymer matrix. The reinforcing fibres can take multiple forms and orientations, including unidirectional plies where all fibres within the ply are aligned. Compared to metals, FRP laminates have higher specific strength and stiffness (Daniel and Ishai, 2006; Riccio, 2008), good fatigue resistance (Riccio, 2008), and excellent energy-absorbing characteristics (Agrawal and Ben Jar, 2003; Daniel and Ishai, 2006; Joosten et al., 2011; Kalhor and Case, 2015). Furthermore, by controlling the orientation of fibres in the laminate and the overall stacking sequence, FRP laminate components provide a unique design flexibility over metallic alloys, allowing for tailoring material properties (Daniel and Ishai, 2006; Isenstadt et al., 2016) and potentially reducing overall part counts (Isenstadt et al., 2016). For these reasons, FRP laminates have been widely adopted in the defence, aerospace, energy, and marine industries (Heuss et al., 2012; Mishnaevsky et al., 2017; Rubino et al., 2020; Vadiraj et al., 2019), where the reduction of overall weight is a key design priority and the historically high cost of production can be accepted.

With increasingly stringent fuel efficiency requirements being mandated around the globe (Caffrey et al., 2015; European Parliament, 2019; National Highway Traffic Safety Administration, 2020), automotive manufacturers are investing heavily in vehicle lightweighting to improve efficiency. For fossil-fuel vehicles, a 10% reduction in curb weight has been linked to a 6-7% improvement in fuel economy (Isenstadt et al., 2016; Pervaiz et al., 2016; Taub et al., 2019; Vadiraj et al., 2019). The benefits for electric vehicles are even more stark, with the same 10% curb weight reduction increasing range by upwards of 14% (Isenstadt et al., 2016; Taub et al., 2019). Plastics, including FRP laminates, have been introduced extensively into vehicles, accounting for up to 10% of the weight of modern vehicles (Isenstadt et al., 2016; Pervaiz et al., 2016) but upwards of 50% of the volume of components (Isenstadt and German, 2017).

Despite the widespread use of plastics in vehicles, the adoption of FRP laminates has been limited in areas where the high specific strength and energy-absorbing properties of FRP laminates could be most beneficial, such as structural or crash-resistant components. The lack of adoption of FRP laminates in critical structural roles starkly contrasts the widespread use of composite materials in aerospace vehicles and structures (Heuss et al., 2012). Historically, the high cost of manufacturing FRP laminate components (CompositesWorld, 2020; Heuss et al., 2012) and the relative immaturity of high-volume production techniques (Taub et al., 2019) have been cited as factors limiting adoption by automotive manufacturers. However, recent advancements in manufacturing processes and material technologies have provided a path toward cost-effective composite components suitable for the automotive sector (Sarfraz et al., 2021). Rather, a key limiting factor preventing the adoption of structural FRP laminate components is the difficulty associated with characterizing and modelling the orthotropic deformation response and failure behaviour of the FRP laminates in the types of loading regimes typical of safety-critical, crash-resistant structures (Isenstadt et al., 2016).

The excellent energy-absorbing potential of FRP laminates stems from the highly complex damage mechanisms of the material system (Agarwal et al., 2006; Daniel and Ishai, 2006). These damage mechanisms can be broadly separated into intralaminar damage mechanisms (damage occurring within the ply of the laminate) and interlaminar damage mechanisms (damage occurring between plies of the laminate), although it is important to note that both interlaminar and intralaminar damage behaviours are often coupled and occur simultaneously in most crash-type loading scenarios (Cherniaev et al., 2018; Joosten et al., 2011). When properly designed, the various damage mechanisms can allow structural FRP components to have greater crashworthiness (in terms of strength, resistance to deformation, and energy-absorption) than traditional metallic components (Joosten et al., 2011; Kalhor and Case, 2015; Reuter and Tröster, 2017; Song et al., 2000), while also simultaneously presenting the opportunity to tune the shape and design of components to optimize performance, minimize weight, and reduce the overall number of individual parts required to make a structure (Dharmaraj, 2020).

While key to the performance of FRP laminates, the complex damage mechanisms pose a two-pronged challenge for the design of structural or crash-resistant structures. First, the behaviour of these structures must be accurately predicted through numerical modelling,

specifically finite element (FE) modelling, to allow for the rapid product development cycle demanded by high-volume manufacturers (Isenstadt et al., 2016). Interlaminar and intralaminar damage mechanisms are often modelled separately to predict the structure's response under crash-type loading scenarios. Second, accurate modelling requires that the damage behaviour of FRP laminates can be accurately measured experimentally. The damage response of FRP laminates is difficult to measure experimentally, given the numerous and coupled interlaminar and intralaminar damage mechanisms that can arise (Sharifpour and Montesano, 2022). Experimental characterization of FRP laminates often requires a large suite of testing techniques to isolate specific responses (Davies, 2008). Moreover, it is critical that experimental techniques used to characterize laminates produce information suitable and applicable for numerical modelling.

Delamination is a critical interlaminar damage mode for FRP laminates wherein a crack propagates between the individual lamina of a component (Figure 1.1). While delamination is an energy absorption mechanism in and of itself (Wisnom, 2012), it represents a pervasive and dangerous failure mechanism in energy-absorbing structures that can reduce the overall energy-absorbing capacity of a structure, produce excessive deformation, and compromise overall crashworthiness and occupant safety (Ganesan, 2008; Riccio, 2008; Wisnom, 2012). Numerical modelling of delamination is critical for the safe, accurate, and cost-effective design of composite automotive structures.

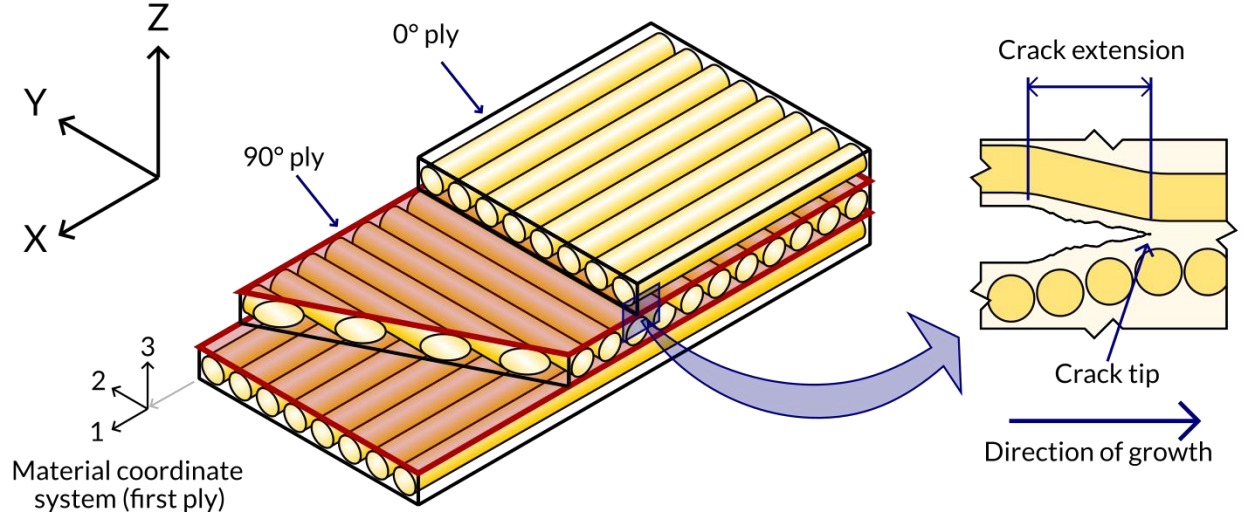


Figure 1.1: An illustrative [0/90/0] FRP laminate with a schematic of the tip of a delamination crack. Possible delamination interfaces are highlighted in red.

Cohesive zone modelling (CZM) is a widely used and accepted modelling technique for delamination in FRP laminates as it is computationally efficient and provides a flexible framework to capture delamination response under different modes of loadings. However, a key challenge exists when using CZM to model delamination in FRP laminates. Using CZM requires that the delamination response of a laminate is defined in the form of a traction-separation response (TSR) that provides information such as the strength of the delamination interface, the overall toughness of the interface, and a detailed description of how damage initiates and progresses (Tabiei and Zhang, 2018a; Watson et al., 2018). Unfortunately, contemporary experimental techniques for characterizing delamination in FRP laminates do not provide sufficient information to define a TSR (Tabiei and Zhang, 2018a). For instance, the current standardized test specimens for delamination, the Mode I double cantilever beam (DCB) and the Mode II end-notched flexure (ENF) specimens (ASTM International, 2019, 2013), were only developed to assess the delamination toughness of a laminate. The single scalar value these specimens provide is insufficient to define a TSR, necessitating additional test geometries or inverse analysis to generate enough information to use CZM to model delamination (Liu et al., 2019; Svensson et al., 2014).

The difficulty assessing the TSR of delamination in FRP laminates not only makes the adoption of CZM to model delamination difficult, the inability to measure TSRs directly has also limited the adoption of more recent modelling advancements, such as the extended finite

element method (Heidari-Rarani and Sayedain, 2019; Zhao et al., 2016) and non-local level set methods (Latifi et al., 2017; Y. Liu et al., 2018). While these modelling techniques are not widely implemented in numerical solvers suitable for designing and developing composite structures, each technique requires a detailed description of delamination behaviour that existing experimental methods cannot provide. To that end, there is a need for new experimental test specimens and analysis techniques to effectively and efficiently assess the TSR of delamination in FRP laminate to enable the modelling of delamination in composite structures.

1.1 Research Objectives and Scope

This research aimed to develop experimental techniques to measure the delamination response of FRP laminates, specifically unidirectional continuous FRP laminates, with a focus on measuring the information necessary for widely-used numerical modelling methods suited to large-scale composite structures, such as CZM. Specifically, new techniques and analysis procedures were developed to measure the TSR of a delamination interface using a single specimen geometry per mode of loading. Extracted TSRs were then assessed using numerical modelling to verify behaviour and the transferability of results between geometries.

The first objective of this thesis was to develop a set of low-compliance experimental specimens capable of measuring the TSR of Mode I and Mode II/III delamination. In contrast with compliant-beam-based specimens, these low-compliance specimens sought to provide more direct loading of the delamination interface and be small enough to be suitable for future high-deformation-rate testing.

The second objective of this thesis was to investigate CZM, an existing and widely accepted modelling technique, for the modelling of delamination using experimentally measured TSRs. While CZM is a well-established technique for modelling delamination, the use of experimentally measured TSR directly with CZM has not been thoroughly investigated. As such, this objective aimed to determine if elements of measured TSRs may not be properly accounted for in either the experimental analysis procedure or numerical modelling approach and to provide practical solutions to improve modelling fidelity.

The third objective of this thesis was to assess the TSRs measured with newly developed specimens in the context of existing, standardized methods for characterizing delamination

response. The focus of this objective was to understand how the measured responses of the proposed specimens compare with contemporary methods such as the DCB and ENF specimens.

1.2 Organization of Thesis

This thesis is organized into seven chapters, commencing with this introductory chapter. Chapter 2 provides background information on delamination mechanisms and a review of current experimental characterization and numerical modelling techniques for delamination in FRP laminates.

Chapter 3 presents the development of a low-compliance specimen, the composite rigid double cantilever beam (cRDCB), and the associated analysis scheme to assess the Mode I delamination response of FRP laminates. Testing of contemporary DCB specimens was performed to provide a point of reference for the newly developed cRDCB specimen.

Chapter 4 details the numerical modelling of Mode I delamination using experimentally measured TSR extracted from the newly developed cRDCB specimen. A set of analysis procedures based on fracture mechanics fundamentals was developed to enhance the experimentally measured TSRs to enable accurate modelling of Mode I delamination in composite structures.

Chapter 5 covers the development and experimental testing of two new specimens and associated data processing schema to characterize Mode II and Mode III shear delamination. The performance of the new specimens was assessed in the context of experimental ENF testing.

Chapter 6 documents the numerical modelling of Mode II and III delamination using the two newly developed shear specimens. Modelling sought to understand and explain the experimental behaviour of the specimens.

The thesis ends with a summary of contributions and suggested future work in Chapter 7.

Some of the work presented in this thesis has been published in peer-reviewed journals. Hartlen et al. (2023) covered the development of the new Mode I delamination specimen, which forms the bulk of Chapter 3. Much of the Mode I delamination modelling presented in Chapter 4, including the development of the two analysis procedures to enhance experimentally measured TSRs, was published in Hartlen et al. (2024). Finally, while not directly related to

delamination in composite materials, an analysis technique to assess the average response and statistical variability of a set of experimental curves was published in Hartlen and Cronin (2022). The analysis technique was used to analysis the experimental response of all specimens in this work.

Chapter 2

BACKGROUND AND LITERATURE REVIEW

Fibre-reinforced plastic (FRP) laminates comprise continuous or discontinuous reinforcing fibres embedded in a polymer matrix. These fibres, typically glass or carbon, provide the FRP laminate with strength and stiffness (Daniel and Ishai, 2006; Herakovich, 1998), while the matrix material binds and protects the fibres while facilitating load transfer (Daniel and Ishai, 2006; Hyer, 2009; Raju and O'Brien, 2008). Unidirectional FRP laminates are highly anisotropic, with material properties such as strength and stiffness maximized along the direction of the fibres. Multidirectional laminates are created by stacking layers, or plies, with fibres oriented along various angles to tailor the final material response (Frketic et al., 2017; Herakovich, 1998; Hyer, 2009).

While the fabrication of FRP laminates is accomplished through a wide range of techniques, this work focuses on prepreg tapes. Prepreg tapes are semi-finished composite materials consisting of reinforcing fibres impregnated in uncured matrix material. Prepreg tapes can be manufactured from a wide range of fibre materials (such as carbon, glass, and others), a range of reinforcement architectures (unidirectional fibres, woven fabrics, chopped or short fibres), and matrix formulations (thermoset or thermoplastic matrices with various curing requirements) (CompositesWorld, 2016; Herakovich, 1998; Sarfraz et al., 2021).

Prepreg tapes are formed into finished FRP laminate components by stacking layers of tapes in some form of tool or mould before being cured at elevated temperature and pressure. However, various processes can be used to form the final component. Perhaps one of the most mature processing techniques is laying up the layers of prepreg tapes in an open-sided mould. This process can be done manually or with robotic assistance. Once the tapes have been stacked, the component is enclosed in a vacuum bag material to compact the tapes and the entire tool is cured at elevated temperatures (Kulhan et al., 2022; Mishnaevsky et al., 2017). Autoclaves are often used to provide additional control over temperature and curing pressure for high-value, high-performance FRP laminate components (Mehdikhani et al., 2019). Compression moulding is another processing method for prepreg tapes that is more suitable for higher volume production, where prepreg tapes are cured in a closed two-sided mould. Similar to manual,

open-sided lay-up, prepreg tapes are stacked as required in a mould cavity inside a forming press. The mould is then closed to provide compaction of the prepreg tapes and cured at elevated temperature (Kulhan et al., 2022; Sarfraz et al., 2021). Depending on the formulation of the matrix phase, processing time can be less than ten minutes for some fast-setting thermoset epoxies (Sarfraz et al., 2021)

The high specific energy absorption properties of composite laminates arise from the numerous and complex damage mechanisms that arise during the mechanical loading of composite structures (Sharifpour and Montesano, 2022). In general, damage mechanisms can be divided into intralaminar damage mechanisms and interlaminar damage mechanisms. Intralaminar damage mechanisms are those which occur within a single ply or lamina of an FRP laminate and include features like matrix cracking, fibre-matrix debond, and fibre kinking and breakage (Sharifpour and Montesano, 2022; Talreja and Singh, 2012). The length scale of intralaminar damage mechanisms tends to be relatively small, given that the plies of an FRP laminate are generally thin. The primary interlaminar damage mechanism is delamination, wherein damage occurs between the plies of a laminate. In contrast to intralaminar mechanisms, delamination tends to occur over a relatively large scale and drives significant degradation in the performance of FRP laminates, including catastrophic failure (Sridharan, 2009; Wisnom, 2012).

2.1 Delamination Mechanism and Causes

Delamination is a damage mechanism characterized by the initiation and growth of a crack between plies in an FRP laminate and is driven by interlaminar normal (σ_z in Figure 2.1) and shear stresses (τ_{xz} in Figure 2.1). For materials with relatively weak fibre-matrix interfaces, a delamination crack may propagate within the plies of a laminate (Czabaj and Ratcliffe, 2013). Like classical homogeneous materials, delamination can be initiated or exacerbated by stress concentrators such as holes, bends, or other geometric features. However, the stacked nature of FRP laminates, in concert with the anisotropy of plies, may also promote high interlaminar stresses and the onset of delamination due to features such as at free edges or ply transitions (Raju and O'Brien, 2008; Riccio, 2008; Wisnom, 2012). Further, FRP laminates are particularly susceptible to interlaminar stresses, as there is generally no reinforcement in the stacking direction. As such, the strength of a laminate in the stacking direction, and hence resistance to

delamination, is limited by the properties of the matrix material (Mall, 2008) and, to a lesser extent, the fibre-volume fraction of the laminate (Compston and Jar, 1999).

In addition to fibre and matrix properties, the manufacturing process strongly influences the delamination resistance and behaviour of the final FRP laminate. Processing techniques and parameters can influence the final processed quality of the FRP laminate in various ways. For instance, changes in the local distribution and concentration of reinforcing fibres can lead to reduced load sharing (Mall, 2008), influencing the overall properties of the laminate. Other manufacturing defects, such as voids in the matrix material, act as stress concentrators, reducing the interlaminar strength and creating preferential crack paths (Glessner et al., 1989; Riccio, 2008). Improving matrix properties such as toughness and strength can improve the overall delamination resistance of a FRP laminate to a degree. However, Mall (2008) notes that beyond a certain point, the constraining effect between adjacent plies becomes the limiting factor to delamination resistance, as the fracture process zone at the tip of the delamination crack cannot fully develop and take full advantage of the matrix phase.

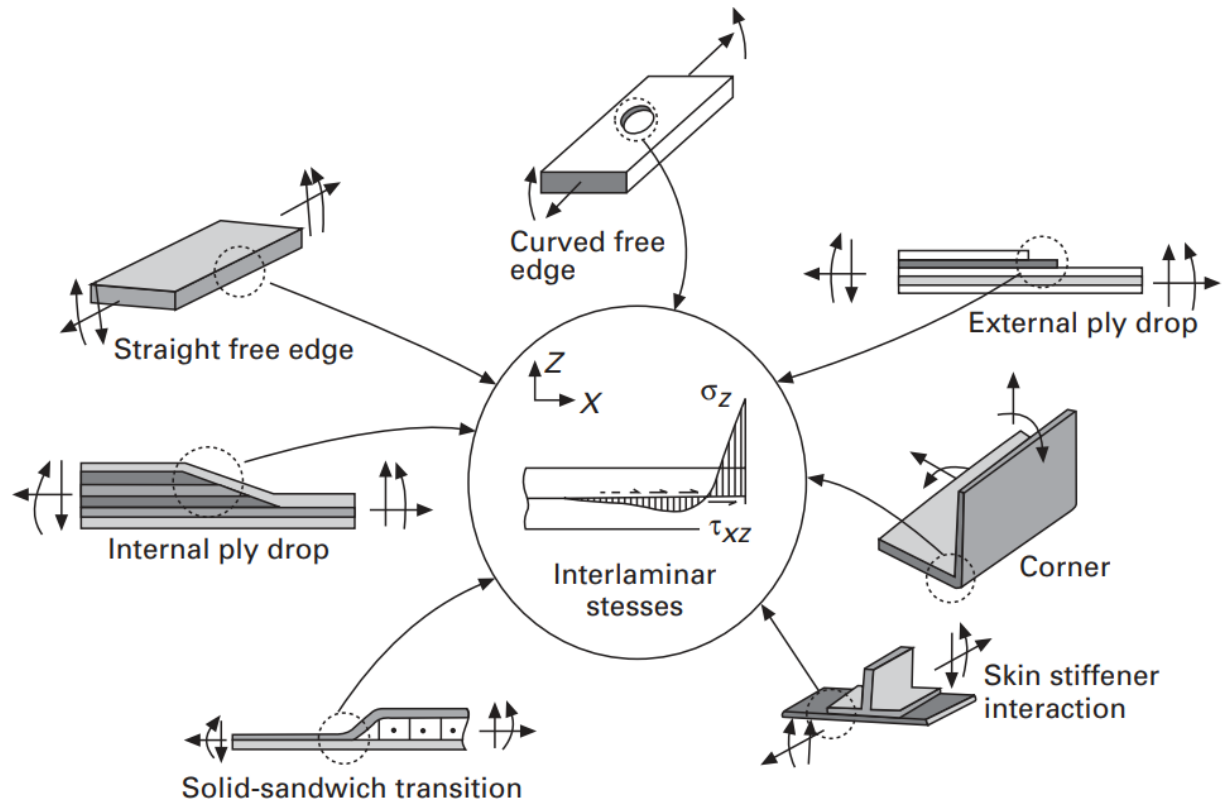


Figure 2.1: Factors and stress concentrators that contribute to interlaminar stresses and, hence, delamination in FRP laminates. From Raju & O'Brien (2008), used with permission.

Rather than describe delamination by the stress components acting at the delamination interface, delamination is typically categorized by the three classic fracture modes. These fracture modes (Figure 2.2) are defined based on the direction the crack is loaded, with Mode I loading corresponding to the normal direction and producing an “opening” of the crack faces (the 3-direction in the material coordinate system (Figure 1.1)) and Modes II and III responding to shear loadings perpendicular and parallel to the crack tip, respectively (the 1- and 2- directions in the material coordinate system, respectively (Figure 1.1)). Distinct damage mechanisms and behaviours have been identified between Mode I and Modes II/III, which has led to the development of different test geometries to experimentally characterize delamination under different loading modes (Section 2.2).

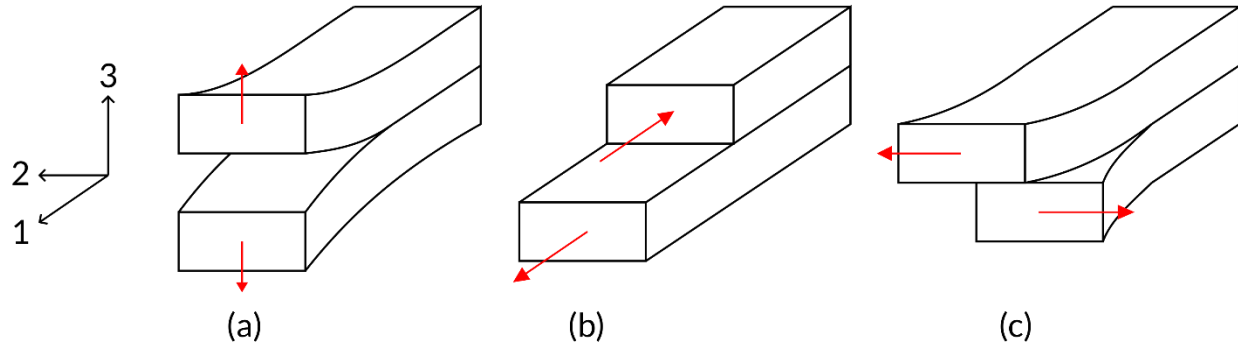


Figure 2.2: The three cardinal fracture modes or loading modes used in fracture mechanics: (a) Mode I normal or “opening”, (b) Mode II shear or “sliding”, and (c) Mode III shear or “tearing”. The coordinate system on the left relates to the material coordinate system.

Mode I delamination of FRP laminates is characterized by two damage regimes. The first is the development of a fracture process zone within the matrix material between adjacent plies. During this regime, damage mechanisms such as microcracking and void nucleation in the matrix material initiate and coalesce (Figure 2.3). The fracture process zone at this stage of Mode I delamination tends to be relatively small, usually less than a couple of millimetres, due to the constraining effects of adjacent plies (Farmand-Ashtiani et al., 2015; Mall, 2008; Pappas and Botsis, 2020). As damage accumulates, the crack tip begins to migrate away from the resin-rich region between the plies toward the fibres of one of the adjacent plies, leading to fibre-matrix debonding as the fracture process zone grows and the crack tip extends. With further crack tip extension, the second damage regime, large-scale fibre bridging, dominates. Fibre bridging, wherein the reinforcing fibres of the laminate extend across the crack faces behind the crack tip, acts as a crack shielding mechanism that progressively increases fracture resistance as the delamination crack extends (Wisnom, 2012). Large-scale fibre bridging increases the size of the fracture process zone significantly, with zones on the order of ten millimetres and more possible depending on the properties of the reinforcing fibres and geometry of the laminate (Manshadi et al., 2013; Pappas and Botsis, 2020). Both small scale matrix damage and large-scale fibre bridging are considered part of the same process zone. Studies have shown that Mode I delamination response is highly influenced by the properties of the fibre-matrix interface (Compston and Jar, 1999; Yao et al., 2016), processing technique and quality (Compston and Jar, 1999), and the physical dimensions of the composite (Compston and Jar, 1999; Farmand-Ashtiani et al., 2015; Pappas and Botsis, 2020).

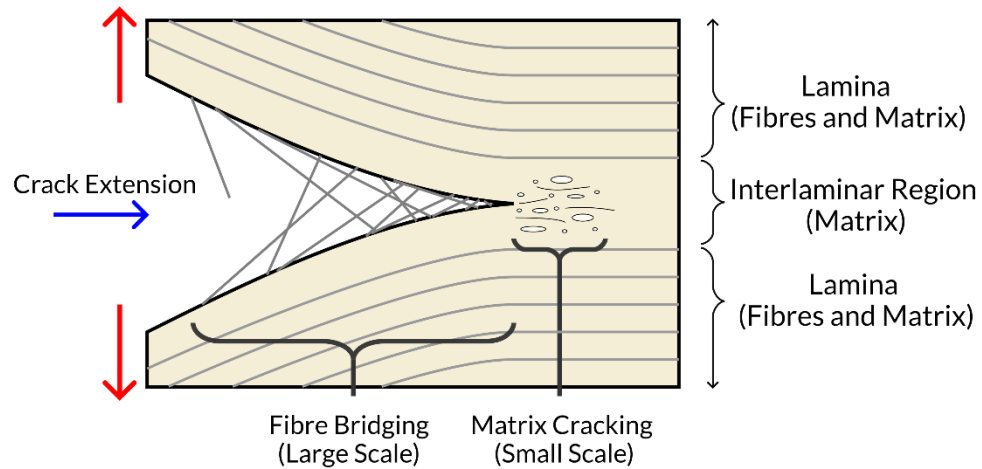


Figure 2.3: A representative fracture process zone for Mode I delamination consisting of small-scale matrix cracking and large-scale fibre bridging.

The mechanism of Mode II and III shear delamination differ significantly from Mode I delamination. While all modes of delamination begin with small-scale matrix cracking and microcracking (Figure 2.4a), the shear action imparted by Mode II and III loading leads to cracks that begin to coalesce in the plane of loading (Figure 2.4b). As cracks coalesce, thin ligaments of matrix material are formed. The formation of these ligaments generates instabilities within the delamination interface as the combination of loading direction and the geometry of the FRP laminate generate eccentric loading of the delamination. These instabilities cause the ligaments to rotate upward, generating separation between adjacent laminates (Figure 2.4c). Ligaments continue to rotate until the matrix material at the roots of the ligament finally ruptures, producing a characteristic microstructure on the fracture surface known as hackles or shear cusps (Figure 2.4d) (Lee, 1993; O'Brien, 1998a). Fracture process zones for Mode II and III delamination tend to be smaller than Mode I delamination as bridging of reinforcing fibres tends not to occur (assuming that fibre bridging is considered part of the FPZ). However, laminates tend to exhibit higher delamination toughness in shear modes of delamination (O'Brien, 1998a).

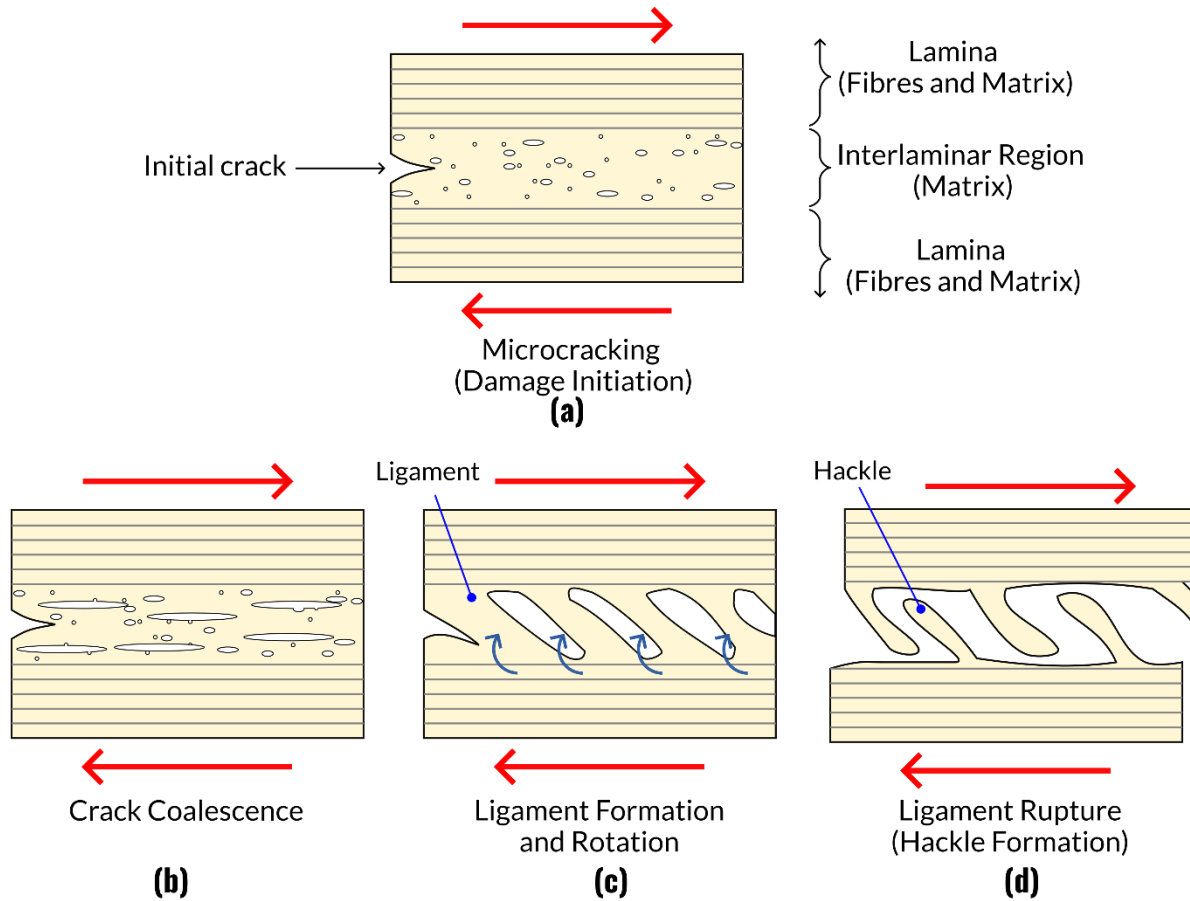


Figure 2.4: A schematic detailing the development of a delamination fracture process zone under Mode II/III. Microcracks (a) begin to coalesce in the region between plies (b). Coalescing cracks begin to form ligaments that are rotated out of plane due to load instabilities (c) until matrix material at the base of the ligaments ruptures (d).

2.2 Experimental Characterization of Delamination

As has been introduced, loading modes and combinations of loading modes are used when characterizing delamination response for testing and modelling, owing to the complex, multi-axial stress states at the delamination crack tip. As different modes of loading generate different behaviours in FRP laminates, experimental characterization of laminates is broken into the three classic loading modes for fracture mechanics. Of these loading modes, the greatest amount of research has been invested into Mode I or the opening mode of delamination, as it tends to be the most critical to the structural integrity of structures (Davies, 2008). However, numerical modelling of delamination in general composite structures requires information from each of

the three modes of loading, including Mode II and III shear. Mode II delamination has seen the most investigation of the two shear modes of loading, although the quantity of work into both shear modes of loading is much less than Mode I alone.

2.2.1 Testing of Mode I Material Properties

The *de facto* standard for characterizing Mode I delamination is the double cantilever beam (DCB, Figure 2.5a), which has been published by a number of standards organizations (ASTM International, 2013; International Organization for Standardization, 2001). DCB specimens are usually made by extracting specimen blanks from a single large FRP laminate panel consisting of 12-20 plies to create 25 mm wide by 125-150 mm long blanks. Waterjet cutting or water-cooled diamond saws are most often used to extract individual specimen blanks to minimize the chance of introducing cracks or other forms of damage to the FRP laminate, which might influence measured properties. A starter crack is created by placing a very thin polytetrafluoroethylene (PTFE, tradename Teflon) film at the midplane of the panel during processing. The specimens are completed by adhering either hinges or loading blocks to the top and bottom of the specimen at the cracked edge of the specimen (ASTM International, 2013).

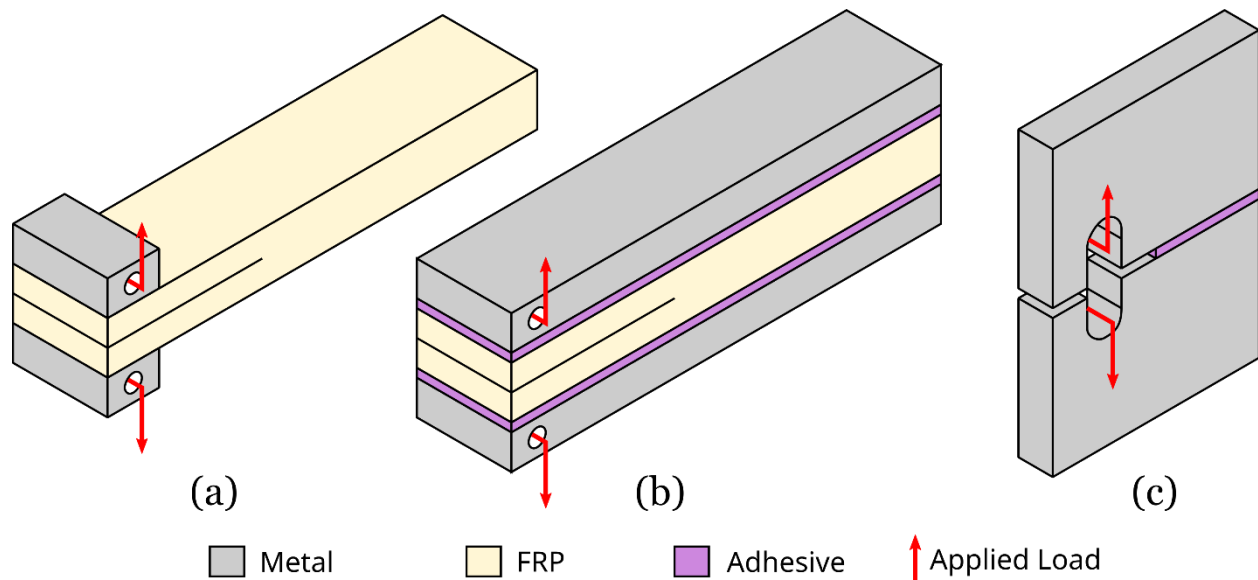


Figure 2.5: Two specimens used to assess Mode I delamination: (a) the DCB, and (b) the reinforced DCB. The Watson RDCB specimen (c) was developed for Mode I testing of adhesives but can extract a complete TSR without additional testing. Specimens not to scale.

While widely used and accepted, the DCB specimen was developed to assess a single delamination property: fracture toughness, in the form of critical energy release rate, G_{IC} . While this property was critical to early fracture mechanics modelling approaches, contemporary techniques such as cohesive zone modelling require additional information, such as interface strength and damage progression, which standard DCB analysis schemes cannot provide. As such, researchers must perform additional tests or resort to inverse analysis methods to parameterize CZM models (Jumel and Budzik, 2018; Murray et al., 2018; Svensson et al., 2014). Additional tests tend to increase the overall expense associated with characterizing delamination response, while studies have demonstrated that DCB specimens are not well suited to inverse analysis due to a lack of sensitivity for parameters other than energy release rate (Gustafson and Waas, 2009; Jumel and Budzik, 2018; Lu et al., 2019). Furthermore, it has been demonstrated that the critical energy release rate assessed with the DCB specimens exhibits a dependence on specimen thickness for the bridging phase of delamination, with thicker specimens exhibiting increased bridging and higher toughness (Agrawal and Ben Jar, 2003; Farmand-Ashtiani et al., 2015; Frossard et al., 2016; Pappas and Botsis, 2020).

Another major limitation of the DCB specimen is high specimen compliance. While this compliance is necessary for many DCB data analysis schemes, bending the DCB specimen during testing can store significant elastic deformation energy. This stored energy can produce unstable crack growth in some conditions such as for material with a strong interlaminar interface or brittle matrix material (Carlsson et al., 1986b; Guo et al., 2019; Yaniv and Daniel, 1988), which is incompatible with many data analysis schemes. While unstable crack growth can be avoided by loading the DCB specimen with pure applied moments, applying such a load condition requires specialized, complex equipment (Sørensen and Jacobsen, 2003). Furthermore, the high compliance of the DCB specimen, in concert with the large size of the specimen, makes high-deformation-rate testing difficult, even impractical, in many circumstances (Kusaka et al., 1998; Liu et al., 2016; Marzi et al., 2014). While some researchers have suggested using wedges to load DCB specimens for high deformation rate testing to eliminate some asymmetry when loading the specimen (Glessner et al., 1989; Kusaka et al., 1998; Oshima et al., 2018; Robinson and Song, 1992), these approaches do not eliminate high compliance of the DCB specimen.

While the DCB specimen was originally developed to assess only one component of Mode I delamination, specifically toughness, several analysis schemes have been put forward to assess more detailed information, such as traction-separation response (TSR). Several authors have presented analysis schemes based on the J-integral (discussed in detail in Section 2.3.1) to extract TSRs from standard DCB specimens (Gunderson et al., 2007; Högberg et al., 2007; Sørensen, 2002; Sørensen and Jacobsen, 2009). The J-integral is an analytical framework to assess stress and deformation in the region around a crack tip. While these techniques have been shown to be effective in some applications, several criteria must be met. First, the stiffness of the material must be known prior to testing. Second, the arms of the DCB specimen must be long enough to ensure no bending stresses occur in the area around where the load is applied. Finally, the DCB specimen must be loaded with a pure bending moment, necessitating a specialized testing apparatus (Sørensen and Jacobsen, 2009, 2003) or measurement of the rotation of the arms of the DCB specimen (Andersson and Stigh, 2004; Högberg et al., 2007; Svensson et al., 2014).

To fulfill the requirements of the J-integral analysis scheme and to reduce the effects of specimen compliance, Marzi et al. (2014) developed a modified version of the DCB specimen consisting of aluminum adherends adhesively bonded to the top and bottom of standard DCB specimen (Figure 2.5b). Dubbed the reinforced DCB, the aluminum adherends eliminated compliance concerns while providing a known stiffness for use with a J-integral analysis scheme, enabling extraction of TSR without *a priori* knowledge of laminate stiffness. While effective at quasi-static deformation rates, Marzi et al. found that high deformation rate testing was complicated by the large mass and moment of inertia of the specimen, which introduced significant experimental noise.

A promising alternative to the DCB specimen is the rigid double cantilever beam (RDCB, Figure 2.5c). Originally developed to characterize adhesives by Dastjerdi et al. (2013), the RDCB specimen was investigated in detail by Watson et al. (2018), culminating in an enhanced analysis scheme suitable across a range of ductile crash-resistant adhesives. Unlike the previously introduced specimens, the RDCB enabled measurement of the complete TSR of an adhesive from a single test specimen without *a priori* knowledge of material properties or measuring crack extension during a test. Further, the small size, low moment of inertia, and high stiffness of the RDCB specimen made it more suitable for high-deformation-rate testing than the

aforementioned compliant-beam-style specimens. However, while the RDCB was used effectively for structural adhesives, it has not been applied to assessing the delamination of FRP laminates.

2.2.2 Testing of Mode II Material Properties

The standard characterization method for Mode II delamination is the end-notched flexure (ENF) test (ASTM International, 2019). ENF specimens are often extracted from a single large FRP laminate panel consisting of between 12-20 plies to form rectangular specimens 25 mm wide and 150-200 mm long. Transverse shear stress is introduced along the midplane of the ENF specimen by loading the specimen in three-point bending (Figure 2.6a). A PTFE film included in the midplane of the layup serves to define the initial crack length and acts as a crack starter. The ENF specimen was developed to assess a single delamination property, fracture resistance in the form of critical energy release rate, G_{IIc} . Evaluation of this property required a specialized compliance-based assessment and measurement of crack extension in addition to force and displacement.

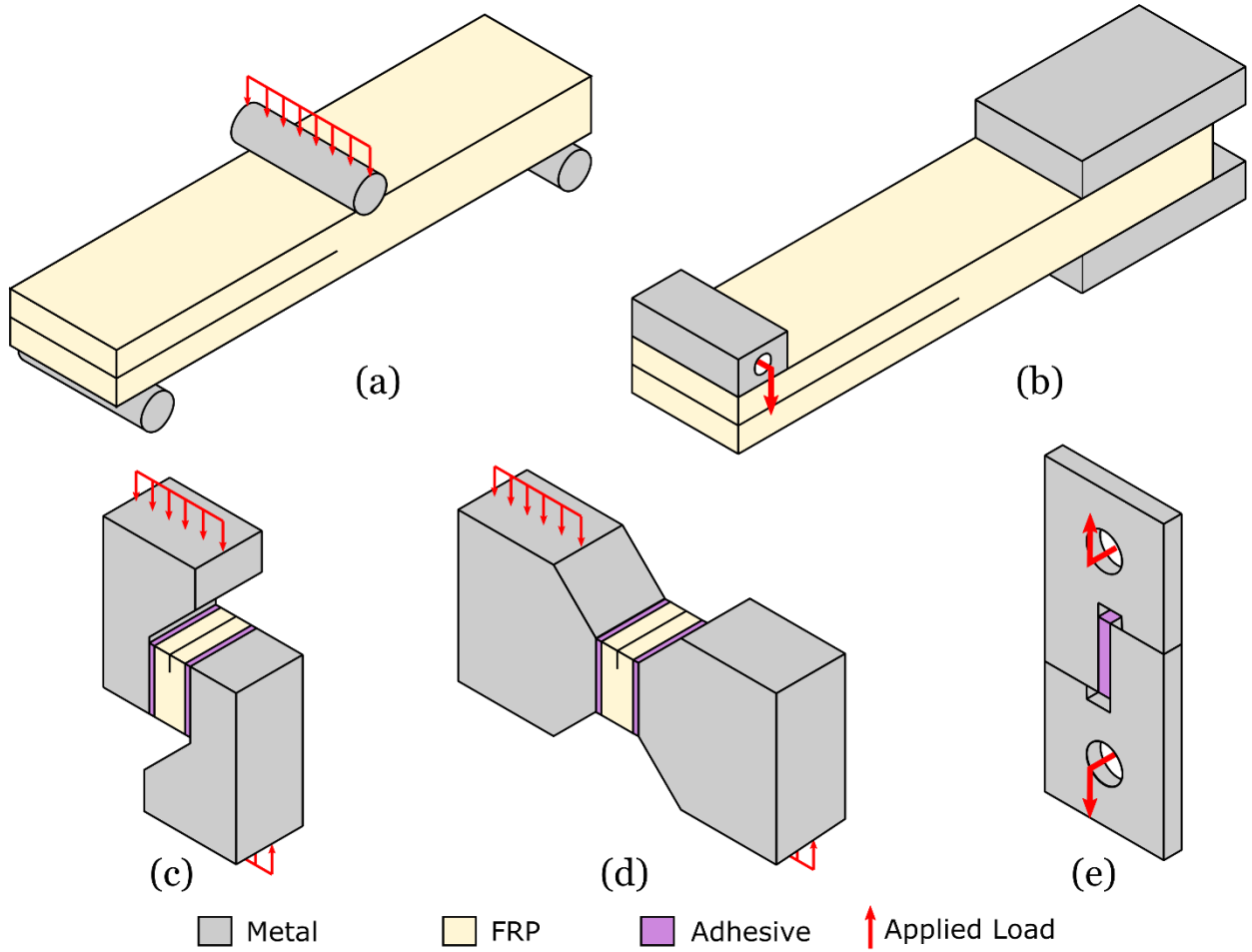


Figure 2.6: A variety of specimens used to assess Mode II delamination: a) ENF, (b) ELS, (c) Caimmi, and (d) McKinney. The Watson bonded shear (e) was developed to characterize adhesive materials but does allow for the direct measurement of TSR. Schematics not to scale.

Unlike the DCB specimen, standardization of the ENF specimen took much longer, partly due to Mode II delamination being considered less critical than Mode I delamination (Davies, 2008), but also due to the challenges and shortcomings associated with the ENF specimen. In addition to high specimen compliance and difficulty tracking crack extension (Davies, 2008; Svensson et al., 2014), the three-point bending method used to load the ENF specimen forces the fracture surfaces into contact, introducing frictional effects that can lead to overestimations of fracture resistance (Agrawal and Ben Jar, 2003; Caimmi et al., 2006; Davies, 2008; Hashemi et al., 1990). Perhaps more importantly, ENF specimens do not produce unconditionally stable crack growth. Therefore, special consideration must be given to ensure ENF specimen dimensions are selected to ensure stable crack extension (Carlsson et al., 1986).

Perhaps the most common alternative to the ENF specimen is the end-loaded splitting (ELS) test. Using the same pre-cracked blanks as the ENF, the uncracked end of the ELS specimen is clamped securely and then a cantilever load is applied at the free, pre-cracked end (Figure 2.6b). While the ELS shares many of the issues inherent in the ENF specimen, such as compliance, friction, and data reduction schemes limited to delamination toughness, the ELS specimen has been shown to more readily produce stable crack propagation (Davies, 2008; Hashemi et al., 1990; Jumel and Budzik, 2018). However, the clamping of the ELS specimen has been shown to influence measured properties, complicating the repeatability and reproducibility of the ELS.

As with the DCB specimen for Mode I testing, researchers have put forth alternative analysis schemes for both the ENF and ELS. Most of these techniques use a J-integral-based analysis to extract TSR (Marzi et al., 2014; Xavier et al., 2014; Y. Zhao et al., 2016). In addition to sharing the same limitations as Mode I J-integral-based methods presented earlier, these alternative analysis schemes for the ENF and ELS specimens do not account for frictional effects.

The limitations of the ENF and ELS specimens, in addition to the somewhat limited consensus on standardization, have led to the development of other specimen designs to assess Mode II delamination. Marzi et al. (2014) introduced a reinforced ENF specimen comprised of metallic blocks adhered to the top and bottom of an ENF specimen. Like the reinforced DCB specimen (Figure 2.5b), the reinforced ENF specimen was designed for use with a J-integral-based analysis scheme but experienced difficulties with high deformation-rate testing. Caimmi et al. (2006) and McKinney (2013) presented similar specimen designs consisting of small FRP laminate coupons bonded to metallic adherends to produce Mode II loading. Caimmi et al. introduced Mode II loading through axial compression (Figure 2.6c), while McKinney used an Iosipescu-like fixture (Figure 2.6d). Both specimens were sufficiently small and lightweight to be amenable to high-deformation-rate testing and did not experience the same frictional effects as the ENF and ELS specimens. However, neither specimen design was provided with data reduction schemes suitable for assessing TSR, limiting measured properties to toughness. A specimen with a similar design intent was introduced by Watson et al. (2019) for structural adhesive (Figure 2.6e). Unlike the compressively loaded specimens of Caimmi and McKinney, the Watson specimen introduced Mode II loading using tension to address alignment and buckling concerns. Additionally, Watson et al. presented an analysis scheme suitable for

measuring the TSR of an interface. However, the Watson specimen has yet to be adopted outside structural adhesives.

2.2.3 Testing of Mode III Material Properties

Of the three fracture modes, Mode III loading has received by far the least study, likely due to the difficulty in developing specimens to isolate Mode III loading (Davies, 2008; Robinson and Song, 1994). To that end, there are no standardized specimens for Mode III testing. The split cantilever beam (Figure 2.7b) specimen was perhaps the first specimen to assess the Mode III properties of FRP laminates (Donaldson, 1988). Essentially a DCB specimen placed on its side, the split cantilever beam required a specific ply layup to characterize Mode III delamination without introducing out-of-plane crack propagation.

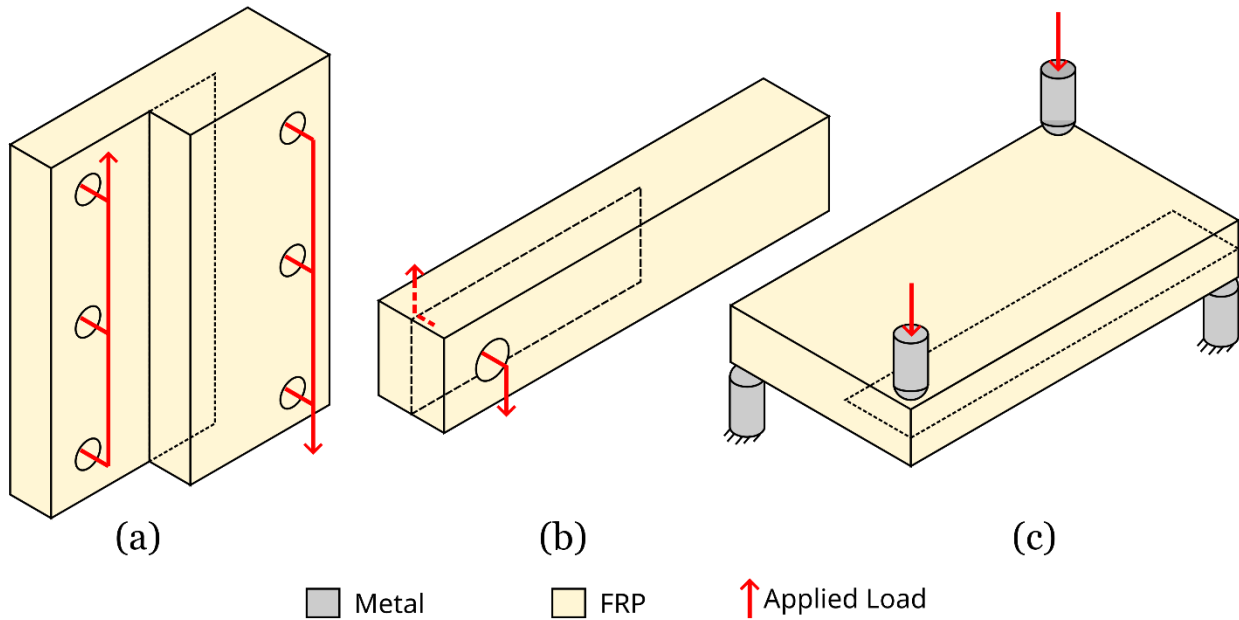


Figure 2.7: Three specimens used to assess Mode III delamination: a) cracked rail shear, (b) the split cantilever beam, and (c) the edge cracked torsion. Schematics not to scale.

An alternative specimen developed around the same time was the cracked rail shear specimen (Figure 2.7a) (Becht and Gillespie, 1989). In contrast with the split cantilever beam, the cracked rail shear specimen placed much more material in Mode III loading. However, the cracked rail shear specimen was more complex to manufacture, needed a specialized testing fixture, and required multiple holes drilled through the FRP laminate for fixturing. A third Mode III specimen was the edge crack torsion specimen (Figure 2.7c) (Lee, 1993). While simpler to

manufacture than the cracked rail shear specimen, the edge crack torsion specimen required a specialized eccentric loading fixture to introduce Mode III loading to the delamination interface. The FRP laminate being tested also required a special stacking sequence to prevent out-of-plane crack propagation. Furthermore, later researchers noted that complete isolation of Mode III loading in the edge crack torsion specimen was difficult to achieve (Davies, 2008; Robinson and Song, 1994). All Mode III specimens were developed to assess fracture toughness in terms of energy release rate, with no analysis schemes being presented to assess the TSR of Mode III delamination.

More recently, researchers have begun investigating Mode III loading to characterize adhesives. DCB-like specimens with metallic adherends were used to place an adhesive joint under Mode III loading and mixed Mode I-III loading in a similar vein as the split cantilever beam specimen (Loh and Marzi, 2019, 2018; Schrader and Marzi, 2023). While the researchers had claimed to develop a methodology to directly measure Mode III traction-separation behaviour, a recent correction (Loh and Marzi, 2022) recanted this claim, indicating that an “indirect” approach was required to measure traction-separation behaviour. This indirect approach required some *a priori* knowledge of material response, in addition to some inverse analysis.

There are unresolved questions about whether Mode III delamination differs from Mode II, despite both modes of loading being categorized as shear loading. Work by Donaldson (1988) reported Mode III toughness values as measured with the split cantilever beam eight times higher than those measured by Mode II ENF specimens. However, later work using an improved version of the same specimen did not produce Mode III toughness values significantly different than Mode II (Robinson and Song, 1994). Application of the edge crack torsion specimen to several composite systems produced Mode III toughness values that were perhaps somewhat higher than Mode II toughness measured with ENF specimens, but differences could not be shown to be statistically significant (Lee, 1993). In addition to inconclusive differences regarding fracture toughness, many Mode III specimens produce fracture surfaces with characteristics similar to those produced by Mode II loading, such as hackles (Becht and Gillespie, 1989; Lee, 1993; Robinson and Song, 1994), indicating some similarities in the damage mechanisms between two forms of shear loading. It is also important to note that many modelling approaches

do not distinguish Mode II and III loading and amalgamate both modes into a single effective shear mode of loading, owing to the complexity of isolating the two modes in numerical modelling and real composite structures.

2.3 Numerical Modelling of Composite Delamination

While the bulk of this section is devoted to the numerical modelling of delamination, it is important to introduce some salient background on the early analytical techniques that influenced later numerical methods as the computational power to analyze fracture became available. Perhaps the most influential work in the field of fracture mechanics is that of Griffith (Erdogan, 2000; Griffith, 1921). While it had been a well-known phenomenon that a stress singularity existed at the tip of a crack, Griffith proposed a strain energy-based analysis that overcame the challenges of previous analytical stress-based analysis. In doing so, the work of Griffith is largely considered to have sparked the field of linear elastic fracture mechanics (Erdogan, 2000).

Building upon the work of Griffith, Irwin made the next major contribution to the field of fracture mechanics by developing a general method to calculate not only the strain energy associated with fracture but also stress distributions in the vicinity of a crack tip (Erdogan, 2000; Irwin, 1957). The analysis of Irwin utilized a numerical factor specific to the geometry and loading condition of a fracture crack that represented the intensity of the stress field at and surrounding the crack tip, assuming a linear-elastic material response. This factor, known as the stress intensity factor, K , is perhaps the most influential single parameter in the field of fracture mechanics, with tremendous volumes of work devoted to developing stress intensity factors for various materials, crack and specimen geometries, and loading conditions (Erdogan, 2000; Tada et al., 2000).

One of the consequences of adopting stress intensity factor-based analysis techniques was a change in how loads applied to the crack tip were considered in fracture mechanics. Irwin demonstrated that any stress state could be described as a combination of three independent stress intensity factors (Irwin, 1957), leading to the adoption of the three cardinal fracture modes introduced in Section 2.1. A further consequence of the widespread adoption of analysis approaches based on Irwin's work was the development of experimental techniques intended to

measure a single energy-based parameter. The focus of fracture mechanics on the three classical modes of loading led to the development and extensive use of DCB, ENF, and other specimen geometries to characterize delamination response as a function of energy release rate, as documented in Section 2.2.

While the works of Irwin and Griffith form the foundation of fracture mechanics, their works are often not used directly in more contemporary numerical modelling techniques, such as finite element (FE) modelling. In an FE framework, the bulk behaviour of the FRP laminate is often captured using continuum elements to account for the anisotropic nature and complex intralaminar damage mechanisms associated with each ply. Delamination is generally treated separately and discretely, as the paths along which delamination cracks propagate are well-defined (Borg et al., 2002). Many modelling techniques have been developed to model crack growth and propagation in FE environments.

2.3.1 J-Integral and Virtual Crack Closure Technique

The J-integral was developed by Rice (1964) as an extension of the work of Irwin to compute the energy release rate of a crack for elastic-plastic materials. Mathematically, the J-integral consists of a contour integral encircling a crack or defect in a continuum that accounts for the local deformation of the body, local stress state, material properties, and externally applied forces (Figure 2.8). The two-dimensional J-integral defined by Rice is given by Eq. 2.1, where W is strain energy density, $\mathbf{t}$ represents a vector of surface tractions, $\mathbf{u}$ is a vector of local deformations and ds is a length increment along the contour Γ .

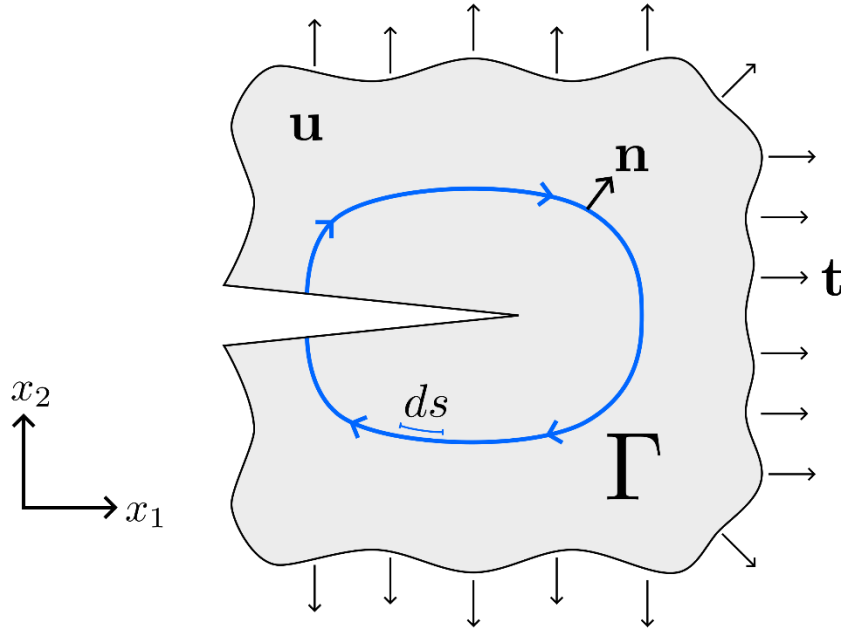


Figure 2.8: A schematic of a crack in a body subject to an arbitrary traction load, t . The blue contour, Γ , is used for the calculation of the J-integral.

$$J = \int_{\Gamma} \left(W dx_2 - \mathbf{t} \cdot \frac{\partial \mathbf{u}}{\partial x_1} ds \right) \quad \text{Eq. 2.1}$$

The J-integral has several useful properties. First, the aforementioned ability to account for both linear and non-linear material behaviour and boundary conditions. Second, the J-integral is path-independent such that any choice of contour, Γ , close to the crack tip provides the same value of energy release rate. Finally, the J-integral converges to the work of Irwin for linear elastic materials (i.e. $J = G$), providing a convenient method for computing stress intensity factors from both experimental test specimens and numerical models.

Given the flexibility and robustness of the J-integral, the analytical technique has seen broad application in several fracture mechanics applications. Focusing on the study of delamination, J-integral methods have been used as the basis of analysis techniques to infer the TSR of experimental compliant-beam-style specimens like the DCB (Högberg et al., 2007; Sørensen and Jacobsen, 2003) and the ENF (Xavier et al., 2014b). While more common in analytical problems, the J-integral has also been used with FE models to compute the energy release rate about a given crack front (Agrawal and Ben Jar, 2003; Lindström et al., 2015). However, it is important to note that the J-integral is limited to assessing a stationary crack tip, making it unsuitable for modelling

component-level delamination where a crack may propagate along an interface. Further, J-integral calculations are often performed as a post-processing task rather than being integrated into the finite element solver (Agrawal and Ben Jar, 2003; Lindström et al., 2015).

Owing to the flexibility of the J-integral in tackling a wide array of problems, it has been highly influential in the development of numerical techniques for modelling fracture. Perhaps one of the oldest numerical techniques is the virtual crack closure technique (VCCT), sometimes also called the modified crack closure technique, which formed the basis of many early FE fracture modelling approaches (Krueger, 2004; Rybicki and Kanninen, 1977; Tabiei and Zhang, 2018a). Effectively an adapted form of the J-integral applied to a discrete domain, VCCT utilizes the displacement and reaction forces at nodes at and adjacent to a pre-defined defect in an FE model (Figure 2.9) to compute energy release rate per Eq. 2.2 and Eq. 2.3, where G is energy release rate, δ are nodal displacements ahead of the crack tip, R are nodal reaction forces, and Δa is the mesh size

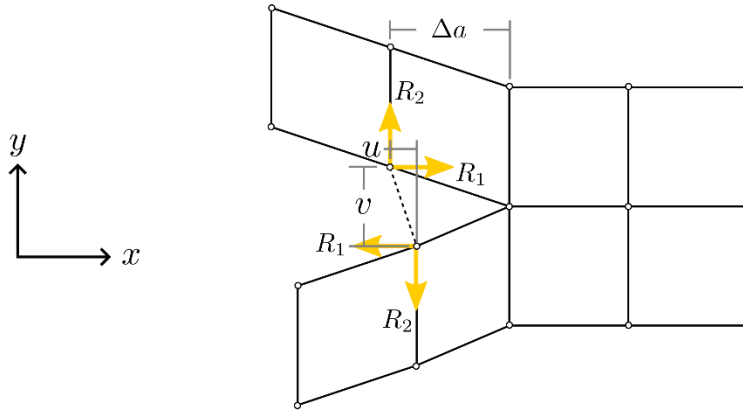


Figure 2.9: A schematic of a predefined flaw in a finite element model, with important parameters for VCCT labelled.

$$G_I = -\frac{1}{2\Delta a} R_1(u) \quad \text{Eq. 2.2}$$

$$G_{II} = -\frac{1}{2\Delta a} R_2(v) \quad \text{Eq. 2.3}$$

Like the J-integral, VCCT provides good flexibility to account for combined modes of loading (Krueger, 2004) and deformation rate dependency (H. Liu et al., 2018). However, VCCT does have

some limitations when modelling delamination. Although mathematically straightforward, VCCT results depend highly on the displacement of nodes adjacent to the crack tip, leading to a mesh size dependence (Heidari-Rarani and Sayedain, 2019; Raju and O'Brien, 2008). As a result of this dependency, Heidari-Rarani and Sayedain (2019) found that VCCT had a greater computational expense to attain similar levels of accuracy as other modelling techniques owing to the highly refined mesh required for accurate predictions. Additionally, VCCT must be computed for a pre-existing flaw, making it less suitable for simulations where the location of flaws may not be known or even exist (Floros et al., 2015; Hallett, 2008; Raju and O'Brien, 2008). A significant disadvantage of VCCT is the inability to capture crack growth, a limitation shared with the J-integral. As VCCT is based on nodal displacements and forces, a crack would need to advance between nodes in an FE model. From an FE perspective, this is more challenging than a crack propagating through an element. While techniques have been developed from VCCT to capture crack propagation, such as node splitting (Tabiei and Zhang, 2018a), phantom nodes (Tabiei and Zhang, 2018a), and adaptive remeshing (Camanho et al., 2003; Turon et al., 2007), these techniques have a high computational cost, especially when accounting for the highly refined mesh required. Additionally, limited implementation into commercial solvers has limited use of these advanced VCCT-based methods for design purposes, particularly outside research environments. Finally, VCCT does not model the macroscopic softening of a material as damage accumulates at the crack tip prior to crack extension, limiting the ability to account for how a material or structure may be compromised before ultimate failure (Floros et al., 2015; Krueger, 2004).

2.3.2 Tied Surfaces with Failure

Tied surfaces are a numerical method of joining parts within some commercial explicit FE solvers (such as LS-DYNA (LSTC, 2017a)) and implicit FE solvers (such as Abaqus). Tied surfaces are extensions of contact algorithms that track the distance between nodes of adjacent parts and apply reaction forces to hold parts together. Tied surfaces with failure, also known as tie-break contacts, introduce a failure criterion that releases the tied nodes based on either force-, displacement-, or energy-based criteria. As the technology underpinning contact algorithms can be harnessed, tied surfaces with failure are a computationally efficient method of modelling

delamination in large models (Mamalis et al., 2002; Reuter et al., 2017; Subbaramaiah et al., 2017).

While tie-break contact algorithms are convenient and numerically efficient ways to model delamination, these techniques have limitations. First, because tied surfaces with failure are linked closely to contact algorithms, the exact numerical implementation in commercial solvers varies between solvers and is often not fully documented, as contact algorithms are often protected intellectual property. As such, the types of behaviour that can be captured are limited to models implemented by the developer, with user-defined failure models often complex and time-consuming to implement. Additionally, tie-break contacts tend to suffer from a numerical instability known as unzipping, where an entire tied surface fails simultaneously non-physically (Trimino and Cronin, 2016). While some solvers have implemented tie-break contact formulations that incorporate separation criteria to stabilize the response of numerical models, these formulations are often simplistic and sometimes lack a physical context (LSTC, 2017a).

2.3.3 Cohesive Zone Modelling

Cohesive zone modelling (CZM) is a fracture modelling technique capable of capturing the non-linear, elastic-plastic response of material ahead of a crack tip. Developed from the foundational works of Dugdale (1960) and Barenblatt (1962), the concept behind CZM is that the singular stress state at the crack tip can be modelled as a gradient of tractions (reaction force per unit area) distributed ahead of the crack tip that resists crack growth. These tractions are, in turn, defined with respect to the crack tip opening displacement or the displacement of adjacent faces of the interface. The key advantage of CZM over other techniques is the ability to capture phenomena ahead of the crack tip within a long, thin FPZ. By explicitly defining traction with respect to separation (hence, traction-separation responses), CZM can be used to model the damage process at a crack tip from damage initiation through the full range of damage regimes an interface may experience up to the point of ultimate material failure, wherein the material no longer provides resistance to crack growth.

Hillerborg et al. (1976) are generally credited with being the first to implement CZM into an FE environment as they attempted to model the fracture response of concrete and capture the damage accumulation and progression ahead of a macroscopic crack tip in a quasi-brittle material. In their work, Hillerborg et al. noted that using CZM allowed for realistic depictions

of crack initiation and growth with relatively coarse meshes, which was vital given the limited computational resources of the time. Needleman (1987) later used CZM to model the interface debonding of inclusions in a matrix, which was later expanded to study brittle fracture in heterogeneous materials (Xu and Needleman, 1996, 1994). However, it is important to note that while pioneering, the interface models of Hillerborg and Needleman bear little resemblance to the CZM techniques available in modern commercial FE packages, which draw their lineage to the works of Borg et al. (2002) and Camanho et al. (2003).

CZM is implemented in most modern commercial FE packages as specialized eight-noded elements known as cohesive elements (sometimes referred to as interface elements). Unlike traditional eight-noded solid elements, wherein the response of the element is controlled using a full deformation tensor, the response of cohesive elements is controlled by the relative displacement of the top and bottom faces of the element (Borg et al., 2002; Camanho et al., 2003; LSTC, 2017a) (Figure 2.10a). Conceptually, the behaviour of a cohesive element can be thought of as three uncoupled, orthogonal springs aligned with the three fracture modes. The response of each spring to separation is controlled using a TSR that controls the initial stiffness of the spring, as well as material softening that would produce a decrease in interface stiffness that corresponds to the accumulation of damage (Figure 2.10b). TSRs are often simplified analytical formulations for computational reasons, with bi-linear and trapezoidal TSRs being the most common (Figure 2.10c). Different damage mechanisms can be captured in each fracture mode by defining a separate TSR for each displacement direction (or each spring, to continue the analogy).

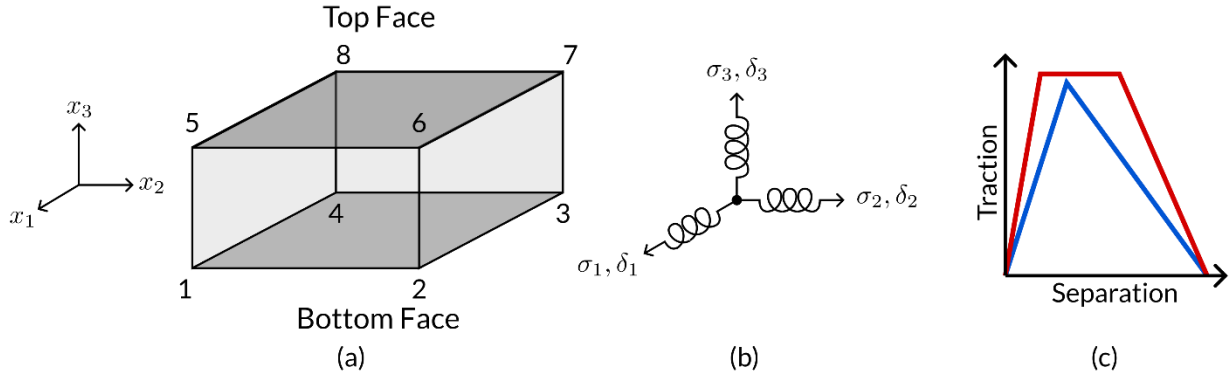


Figure 2.10: (a) a schematic of an eight-node cohesive element wherein the bottom face is defined by nodes 1-4 and the top face by nodes 5-8. (b) Three orthogonal springs that are analogous to the operation of the cohesive element. Nodes of the element define the orientation of the springs and the local coordinate system and assess displacement in each mode of loading. (c) A representation of a bilinear (blue) and a trilinear (red) traction-separation response.

To model fracture and larger-scale damage processes using CZM in an FE environment, cohesive elements are included in an FE model in a single layer along pre-defined fracture paths (Figure 2.11) or, in the most extreme cases, between every element in a model to model fracture along an undefined path (Tabiei and Zhang, 2018a; Zhang and Tabiei, 2020; Zhang et al., 2007). While the requirement of knowing the anticipated path of a crack *a priori* can be a limitation in some fracture modelling scenarios, the crack path for delamination in FRP laminates is well defined as the interface between adjacent plies.

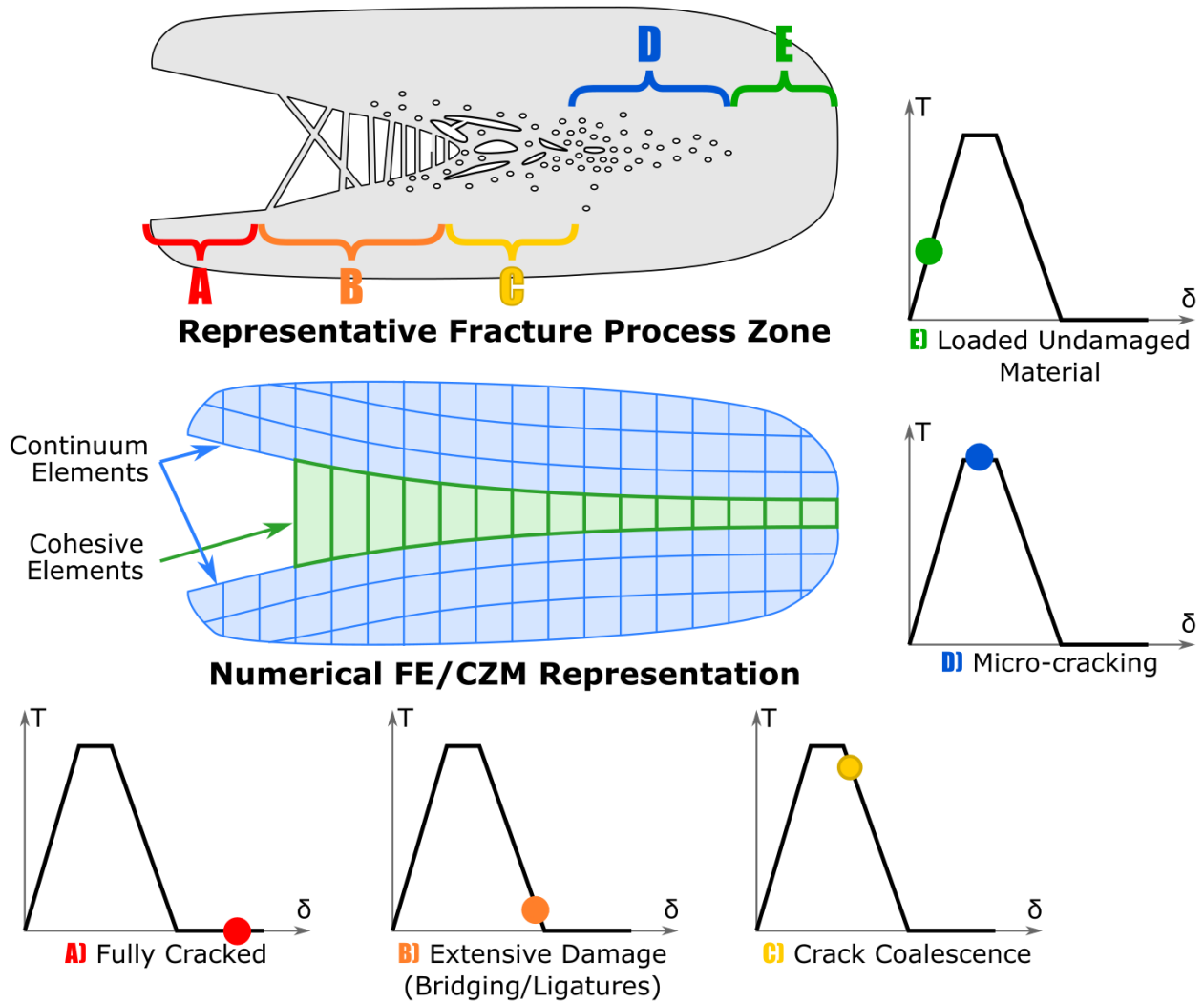


Figure 2.11: A schematic detailing the fracture process zone of a crack and how it is represented using cohesive elements. Representative regions of the fracture process zone are mapped to an exemplar trapezoidal traction-separation response.

When using CZM in an FE modelling environment, it is important that the term traction-separation response takes on an expanded interpretation compared to the original definitions of Dugdale (1960) and Barenblatt (1962). Rather than just representing the behaviour of the small process zone at the crack tip, the TSRs used by cohesive elements are intended to represent the entire mechanical response the interface can expect to experience, regardless of length scale. As such, cohesive elements and their associated TSRs may experience very large separations for large-scale damage phenomena such as large-scale fibre bridging under Mode I delamination.

The context in which CZM elements are implemented in FE modelling is an important consideration for fracture modelling, especially delamination modelling. Broadly, there are two approaches to using cohesive elements to model crack propagation: a combined elastic-damage approach and a damage-only approach. In the combined elastic-damage approach, finite-thickness cohesive elements are used to represent both the physical volume and response of the failure interface and the surrounding material (Figure 2.12). As such, the TSRs intended for the combined approach must include not only the damage response of the interface but also the elastic stiffness contribution of the material adjacent to the interface. The combined approach is most effective when modelling adhesive bond lines in large-scale bonded structures, where the bond line is modelled with a single layer of cohesive elements and the TSR captures both the elastic and inelastic response of the adhesive material (Lu et al., 2019; Park and Paulino, 2013). However, the combined approach is generally unsuitable for FRP laminates, where cohesive elements cannot approximate the complex intralaminar response of adjacent plies.

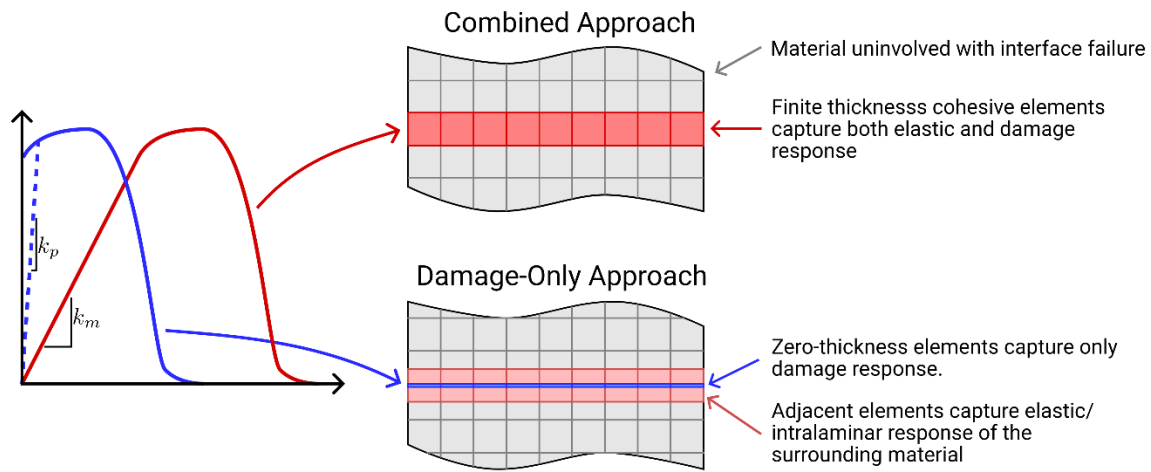


Figure 2.12: A schematic of the combined elastic-damage and damage-only cohesive modelling approaches with appropriate TSRs (left). The combined approach (top, red) uses finite thickness cohesive elements and a TSR that includes the stiffness contribution of the adjacent, undamaged material, k_m . The damage-only approach (blue, bottom) uses zero-thickness cohesive elements and a TSR that only includes the damage response of the interface or is assigned a high initial stiffness, k_p .

A more common modelling approach for delamination is the damage-only approach, wherein cohesive elements capture only the damage response of the delamination interface. All

contributions from surround material are captured using traditional continuum elements and constitutive models. The damage-only approach uses zero-thickness (or near-zero-thickness) cohesive elements inserted between the lamina or groups of lamina where delamination is expected to occur (Lu et al., 2019; Park et al., 2009; Park and Paulino, 2013). Using zero-thickness elements reflects the physical thickness of a delamination interface in an FRP laminate (Figure 2.12).

In the damage-only modelling approach, the undamaged stiffness of the modelled structure is captured with the continuum elements of the model. Ideally, the cohesive elements would be assigned a TSR with infinite initial stiffness, such that the cohesive elements introduce no unwanted compliance into the model (Lu et al., 2019; Turon et al., 2007). However, infinite stiffness is not feasible for numerical modelling as it can introduce artifacts and instabilities (Lu et al., 2019; Soto et al., 2016; Tabiei and Zhang, 2018a; Turon et al., 2007). As such, it is common practice to use TSR with a high initial stiffness of between 10^5 and 10^7 N/mm³ (Camanho et al., 2003; Soto et al., 2016; Turon et al., 2007). However, even with a high initial stiffness, cohesive elements can introduce artificial compliance into an FE model, reducing the overall stiffness of the model in a non-physical manner and, hence, effecting characteristics such as the propagation of stress waves through a model (Tabiei and Zhang, 2018a, 2018b). While approaches have been proposed to eliminate artificial compliance and issues associated with the selection of penalty stiffness by adaptively inserting cohesive elements into a model during runtime, these approaches are difficult to implement within existing solver frameworks (Latifi et al., 2017; Park et al., 2009; Zhang and Tabiei, 2020; Zhang et al., 2007), particularly those solvers widely used by commercial interests outside of research.

In addition to compliance issues, delamination response modelled with cohesive elements often exhibits an element size dependence. Turon et al. (2007) found that several cohesive elements must be placed within the fracture process zone of a material for accurate prediction of damage growth and propagation. Turon et al. suggested that the cohesive element size should be five to ten times smaller than the fracture process zone, although later studies suggested that elements three to five times smaller were sufficient (Hallett, 2008; Soto et al., 2016). Excessively large cohesive elements can produce non-physical damage responses, including unrealistically high forces and unstable crack growth. On the other hand, extremely small cohesive elements can

make modelling computationally prohibitive despite the inexpensive nature of cohesive elements. To that end, researchers have presented methods to use larger cohesive elements by artificially degrading the strength or other properties of the traction-separation response assigned to the cohesive element to maintain a realistic model response at the expense of physically meaningful interface properties (Lu et al., 2019; Soto et al., 2016; Turon et al., 2007). While correlations relating the reduction in properties to element size have been published (Turon et al., 2007), these correlations are based on calibration rather than physical laws. As such, the calculated scaled properties often no longer reflect actual physical values.

Despite compliance and size concerns, CZM is one of the most common techniques for modelling fracture and delamination, with most commercial finite element solvers implementing some form of cohesive elements. While more advanced methods of modelling delamination have been presented to address some of the issues inherent in cohesive elements, such as Galerkin-enhanced cohesive elements (Nguyen, 2014; Radovitzky et al., 2011), non-local level set techniques (Latifi et al., 2017; van der Meer, 2015), or the extended finite element method (Heidari-Rarani and Sayedain, 2019; Zhao et al., 2016), these advanced techniques have seen little adoption for modelling delamination outside of research applications. In part, these methods are not well suited to being implemented in existing FE solvers as the techniques tend to be non-local in nature (Zhang and Tabiei, 2020). Additionally, the improved modelling accuracy granted by these techniques can be difficult to justify, given the significant increase in computational expense. However, it is interesting to note that one limitation shared by both CZM and more advanced methods is that all require detailed descriptions of the damage response, often in the form of a traction-separation response, which are difficult to assess with existing experimental techniques.

Chapter 3

DEVELOPMENT, TESTING, AND ANALYSIS OF A MODE I DELAMINATION SPECIMEN

This chapter documents the development and testing of the composite rigid double cantilever beam (cRDCB) specimen to assess the traction-separation response (TSR) of Mode I delamination in FRP laminates. Special attention was given to the fabrication methods developed to ensure consistent quality specimens were produced and the measurement and analysis framework developed to provide high-quality experimental data. Traditional double cantilever beam (DCB) specimens were produced and tested to assess the performance of the newly developed cRDCB specimen,

3.1 Specimen Fabrication, Testing and Analysis Methodology

3.1.1 cRDCB Fabrication, Testing and Analysis Methodology

The cRDCB specimen comprised an FRP laminate sandwiched between two steel adherends (Figure 3.1). The rigidity of the adherends provided a highly constrained and uniform deformation across the delamination interface (the region of the specimen over which delamination can occur). A thin 12.5 μm thick PTFE film served as both a crack starter and to define the extent of the delamination interface in the cRDCB specimen.

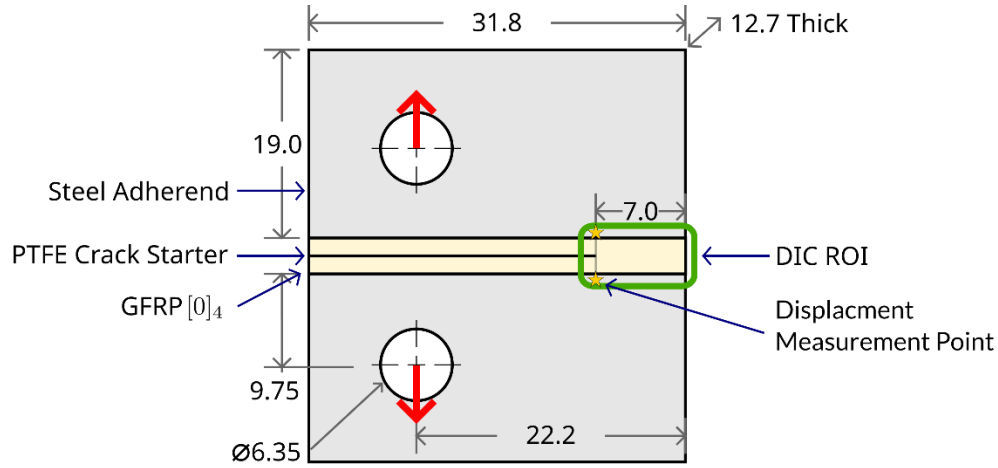


Figure 3.1: Schematic of the Mode I cRDCB specimen. The specimen was pin-loaded in tension (red arrows), and deformation of the interface was measured using DIC (green box), with separation being measured across the adherends (gold stars)

The cRDCB specimen underwent several developmental iterations to become the finalized design detailed in this thesis. These developmental iterations focused on two primary goals: 1) producing high-quality FRP laminate specimens with a controlled and reproducible fibre volume fraction and low void content and 2) generating stable crack growth. The earliest iterations of the cRDCB specimen used the exact adherend geometry as Watson et al. (2018). These adherends were only 6.3 mm thick and were cured in a manner that did not provide full confinement to the prepreg tapes used in the specimen. The relatively narrow specimen was deemed too small for the heterogeneous nature of FRP laminates, while the adherend design was unsuitable for more traditional compression moulding techniques for FRP laminates. These issues lead to the use of a 12.7 mm (0.5 inch) wide adherend and the custom design, spring-loaded curing fixture used herein.

Trials were carried out to investigate the effect of producing cRDCB specimens with FRP laminates that were two and four plies thick. While a thinner laminate would likely have produced a more rigid specimen that would adhere more closely to the underlying assumptions of the analysis technique used to extract traction-separation behaviour, two-ply cRDCB specimens consistently experienced failure between the adherend and the FRP laminate rather than delamination. Four-ply cRDCB specimens were much less susceptible to interface failure. The interface length of the specimen was reduced from the 13 mm length used by Watson et al. to 7 mm through experimentation to provide consistently stable crack growth. The change in

interface length, and hence reduction in overall specimen stiffness, was required by the much higher stiffness of the FRP laminate compared to the structural adhesives for which the RDCB specimen was developed. Without a reduction in the interface length of the cRDCB specimen, the stiffness of the test frame and fixturing would need to be prohibitively high to ensure stable delamination, as will be discussed later in this thesis.

One developmental trial was carried out to investigate if the FRP laminate could be manufactured separately using compression moulding to better control the quality of the laminate. The finished laminate was cut to appropriately sized coupons and bonded to the metallic adherends. Specimens produced during this trial were generally unsuccessful, as a good bond between the adherend and finished FRP laminate was not achieved. Additionally, the addition of adhesive introduced additional compliance into the specimen. While the challenges associated with bonded cRDCB specimens were likely solvable, the use of adhesives in the cRDCB specimen was abandoned for this work due to time constraints and to focus on the foundational development of the specimen itself.

While the term adherend is more commonly and accurately used in adhesive joining, the term adherend is maintained in this work even though no adhesive was used to make cRDCB specimens. This choice was made to provide better compatibility with existing RDCB literature (Dastjerdi et al., 2013; Watson et al., 2018) and the lack of a suitable term in composite structures, especially for co-cured structures.

Unidirectional E-glass/epoxy prepreg tapes (UE400/REM, CIT, Italy) were used exclusively throughout this work. These medium tack prepreg tapes had a nominal areal weight of 645 g/m². The resin component of the tapes had a nominal glass transition temperature of 125°C. A compression moulding process was adopted to fabricate the cRDCB specimens wherein the prepreg tapes were co-cured with the steel adherends of the specimen. Co-curing of the FRP laminate to the adherends was selected to eliminate the possible effects of additional adhesive on specimen compliance.

A specialized fabrication and processing methodology was developed to produce cRDCB with a high-quality, low-defect FRP laminate. While early developmental iterations used the same fixturing as Watson et al. (2018), it was found that existing fixturing was not compatible with FRP laminates. The open-faced fixtures used by Watson et al. did not provide confinement

of the prepreg tapes, causing a significant amount of fibres and matrix material to be expelled between the adherends. Additionally, constant pressure could not be maintained on the prepreg tapes during the temperature ramp of the curing cycle as the viscosity decreased. As such, early cRDCB specimens produced with existing fixturing had inconsistent fibre volume fractions and often contained many defects, including a lack of consolidation and squeeze out of fibres between the adherends during the curing process resulting in fibre misalignment.

A new, composite-specific processing methodology (summarized in Figure 3.2) was developed to replicate traditional compression moulding as closely as possible. Central to this approach was the development of a custom curing fixture that provided constant pressure during the curing process despite the compaction and change in viscosity of the prepreg tapes and full confinement of the laminate to ensure higher, more consistent fibre volume fractions.

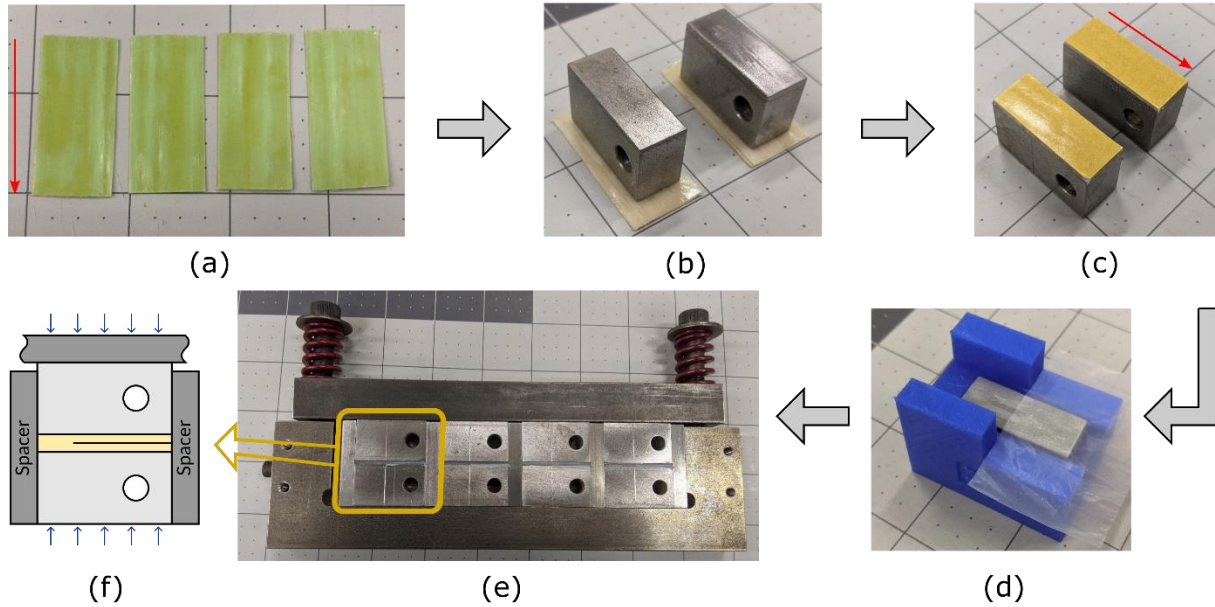


Figure 3.2: An overview of the cRDCB fabrication process. Prepreg tapes are cut oversized (a) and then stacked into two-ply sub-laminates. Sub-laminates are pressed into prepared metallic adherends (b) and trimmed flush with the adherend (c). A jig was used to place a PTFE crack starter (d) before two adherends were sandwiched together and placed in a curing fixture (e). (f) A schematic of the cRDCB specimen and the spacers used to control the final thickness of the FRP laminate. Not shown in (e) is an additional plate clamped over the open face of the specimens to provide full confinement during curing. Red arrows in (a) and (c) denote fibre direction.

The first step of the fabrication process was to prepare the metallic adherends, which were machined from mild steel. The surface of the adherends interfacing with the FRP laminate was roughened with abrasive blasting media to ensure a good connection with the FRP laminate. A minimum surface roughness of $2.0 \mu m r_a$ was found to be necessary to minimize the risk of adherend-laminate interface failure during testing.

cRDCB specimens were assembled by stacking prepreg tapes into two unidirectional sub-laminates, then pressing each laminate into the roughened surface of each metallic adherend (Figure 3.2b). Each sub-laminate was then trimmed to be flush with the metallic adherend (Figure 3.2c). The tacky, resin-rich nature of the prepreg tapes used in this work (UE400/REM, CIT, Italy) enabled the sub-laminates to stick to the adherends before curing, making handling and assembly easier. A $12.5 \mu m$ PTFE film was placed with the aid of a jig across one sub-laminate

to act as a crack starter (Figure 3.2d). As the placement of the crack starter can impact the final stiffness and measured properties of the cRDCB specimen, the jig helped provide greater repeatability between specimens. The two adherends and sub-laminates were then pressed together to form the final specimen ready for curing. The cRDCB specimens used in this work had a $[0]_4$ stacking sequence, with fibre direction aligned with the long edge of the adherend.

The completed specimens were placed in a custom-developed curing fixture that used a spring-loaded bar to provide consistent pressure during curing (Figure 3.2e). Application of consistent pressure throughout the curing cycle was important because the prepreg tapes, nominally a tacky solid at room temperature, become less viscous material at elevated temperatures. The spring-loaded bar allowed curing pressure to be controlled by setting the compression of the springs at room temperature. While the compaction of the prepreg tapes during the curing process caused some extension of the springs and, hence, a decrease in load, the amount of compaction was very small and resulted in a negligible change in curing pressure. Spacers (Figure 3.2f) were used to limit the displacement of the spring-loaded bar to control the final volume fraction of the laminate and prevent excessive compaction. While the curing fixture provided adequate confinement of the specimen to prevent fibre squeeze out during curing, the excess resin could still bleed out until the spring-load bar bottomed out on the spacers. Curing was performed by placing the prepared specimens and fixture in a preheated oven at 0.5 MPa and 140°C for 90 minutes to maintain compatibility with other work conducted at the University of Waterloo (Asaee et al., 2021). After 90 minutes, the specimens and fixture were removed from the oven and allowed to cool to room temperature before the specimens were removed from the fixture.

Once removed from the fixture, excess resin spew was removed from the specimens using abrasive paper. Several developmental iterations were performed to tune the curing fixture to provide a desirable fibre-volume fraction by incrementally adjusting the height of the spacers in the curing fixture. At each iteration, the thickness of the final laminate was measured using an optical digital microscope (VHX-5000, Keyence, Japan). Additionally, the fibre volume fraction was calculated using acid digestion (ASTM International, 2015). To perform acid digestion, the cured laminate was carefully removed from the cRDCB specimen with a chisel, measured, weighed, and then submerged in nitric acid to dissolve the matrix material. Once all matrix

material was dissolved, the fibres were removed, dried, and weighted again. The fibre volume fraction was then computed per ASTM D3171 (ASTM International, 2015). Given the time and effort involved in the acid digestion, the process was only performed on a select number of iterations, including the final developmental iteration. The final thickness of the laminate material was 1.05 mm (SD = 0.05 mm) with a fibre volume fraction of 47% (average of two measurements) as measured by acid digestion.

cRDCB specimens were loaded in tension using load pins to ensure freedom of rotation. The tensile load was applied using a 50 kN servo-electric universal test frame (AGX-50kN, Shimadzu, Japan) at a constant displacement of 0.001 mm/s. A servo-electric test frame with relatively high frame stiffness was found to be the best choice for testing cRDCB specimens. Given the high stiffness of the specimen and relatively low overall displacement of the tests, a high degree of displacement control was necessary. Early testing with servo-hydraulic frames did not provide sufficient displacement control. Additionally, tests with lower-capacity frames did not provide sufficient stiffness to ensure stable crack growth.

In early developmental iterations, specimen displacement was measured at the clevises used to load the cRDCB specimen using optical methods described by Watson et al. (2018). However, it was determined that the much higher stiffness of the cRDCB necessitated a more accurate, on-specimen displacement measurement. To that end, two-dimensional digital image correlation (DIC) was adopted to measure the displacement of the specimen directly at the crack tip rather than at the clevises. The use of DIC enabled full-field strain measurements of the FRP laminate, which enabled the qualitative assessment of delamination crack growth and the ability to provide significantly more information as to the behaviour of the laminate during testing.

A single 12 MP monochromatic USB camera (Grasshopper 3 GS3-U3-123S6M, FLIR, USA) with a 180 mm macro lens (Sigma APO MACRO 180 mm F2.8 EX, Sigma Corporation, Japan) was used to capture images at two frames per second. Given the small size of the region of interest (green box in Figure 3.1), the high magnification and resolution of the camera setup, and the small displacements typical of cRDCB specimens, care had to be taken to create an appropriate speckle pattern for DIC analysis. An airbrush was found to provide the best speckle pattern for this application. To further improve the quality of the DIC measurements, a small electric fan

was positioned to pass the air stream between the camera, lighting setup, and the specimen to eliminate heat distortion effects from DIC measurements. While the effect of heat haze may be small for larger-scale displacement measurements, using a fan with the cRDCB specimen reduced displacement measurement noise by almost three times compared to not using a fan.

Captured images were analyzed using the commercial DIC software (VIC-2D, Correlated Solutions, USA), with processing parameters provided in Table 3.1. DIC analysis had a virtual strain gauge size of 0.41 mm (Jones and Iadicola, 2018), although local strain measurements were of less importance for cRDCB specimens than displacement. Crack tip displacement was measured using a virtual extensometer that tracked displacement of the adherends at the crack tip (gold stars in Figure 3.1).

Table 3.1: DIC processing parameters used for cRDCB specimens

DIC Parameter	Value
Image Resolution	0.0049 mm/pixel
Subset Size	29 x 29 pixels
Step Size	9 pixels
Strain Filter Size	7x7 subsets
Virtual Strain Gauge Size	0.41 mm

A specialized analysis procedure was developed to extract TSRs from experimentally measured force and on-specimen displacement data. The analysis developed for the cRDCB was based on the work of Watson et al. (2018). The analysis assumed that stresses, and hence tractions, were distributed uniformly across the width of the specimen (into the page for the schematics in Figure 3.3) to allow for the creation of an analytical model of specimen behaviour. Additionally, it was assumed that the adherends were rigid and a perfect connection existed between the FRP laminate and adherends.

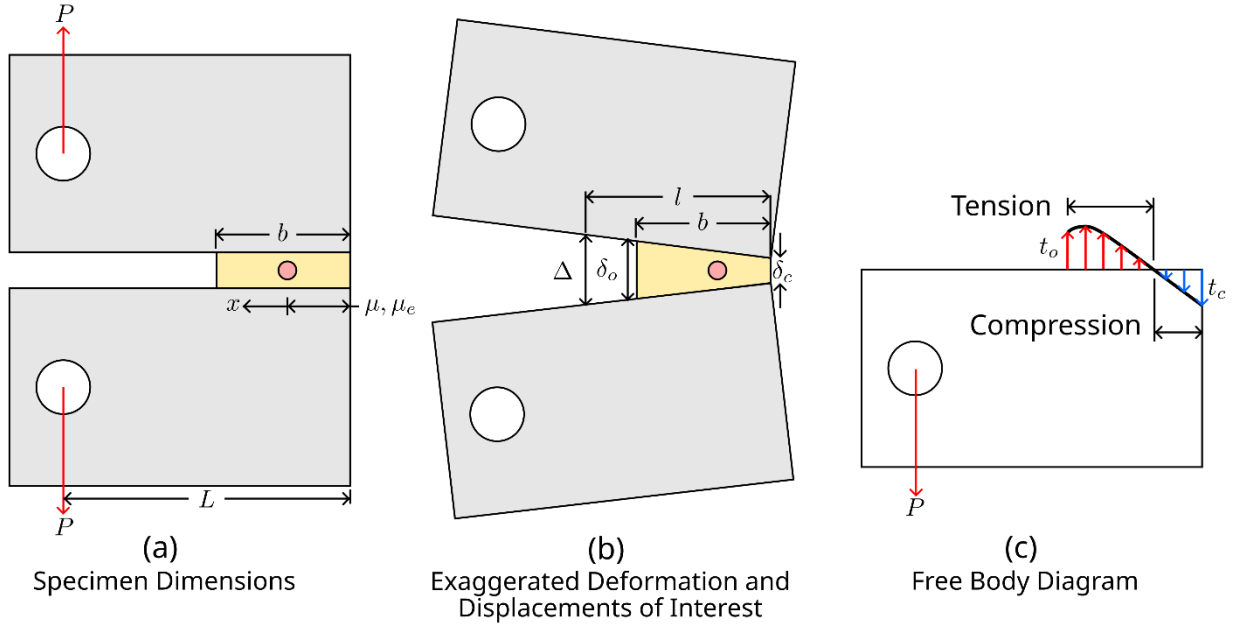


Figure 3.3: Critical dimensions (a) used to extract traction-separation behaviour from cRDCB specimens, along with an example of cRDCB undergoing exaggerated deformation (b) and a free body diagram showing the presence of both tensile and compressive tractions along the delamination interface (c). Adapted from Watson et al. (2018).

When the cRDCB specimen was loaded, the rigid adherend applied a deformation to the delamination interface that varied linearly from a positive displacement generating tensile traction at the crack tip to a negative displacement causing compression at the opposite edge of the delamination interface (Figure 3.3c). The location of zero traction between the two edges of the interface was known as the pivot location (denoted by μ) and, in concert with measured displacements, was used to compute the displacement of the delamination interface at any point. In the developed analysis procedure, the opening displacement at the crack tip of the delamination interface was denoted by δ_o while the compressive displacement at the undamaged edge of the delamination interface was denoted by δ_c . It is important to note that while this analysis procedure could extract non-linear traction-separation behaviours, it did assume that the compressive response of the interface is always linear.

The analysis procedure for the cRDCB specimen differed from the original analysis proposed by Watson in two key areas. First, the cRDCB analysis allowed for the use of on-specimen displacement. While this displacement can be measured at any point on the specimen (accounted for by the variable l (Figure 3.3a)), it was most appropriate to select the displacement

at the crack tip of the cRDCB specimen (denoted by variable b). However, as it can sometime be challenging to pinpoint the exact crack tip location, b , in an untested cRDCB specimen, the point at which displacement was measured, l , was considered to be independent for the development of this analysis procedure. Second, mathematics were simplified, and an improved root-finding technique was used to reduce the susceptibility of the analysis method to experimental noise.

Based on the assumption of rigid adherend behaviour, the displacement at any point, $\delta(x)$, along the delamination interface can be computed using the experimentally measured displacement, Δ , at the location l if the pivot location, μ , is known (Eq. 3.1).

$$\delta(x) = \frac{\Delta}{l - \mu} x \quad \text{Eq. 3.1}$$

Using Eq. 3.1, the displacement at the measurement point, Δ , can be used to determine displacement at the crack tip, δ_o , and compressive displacement at the opposite side of the interface, δ_c (Eq. 3.2).

$$\frac{\Delta}{l - \mu} = \frac{\delta_o}{b - \mu} = \frac{\delta_c}{\mu} \quad \text{Eq. 3.2}$$

If the compressive response of the interface is assumed to be linear and elastic (Watson et al., 2018), compressive traction, σ_c , can be defined with respect to measured displacement and the initial stiffness of the delamination interface, k_m (Eq. 3.3). It is further assumed that the material has the same stiffness in both tension and compression and is not loaded sufficiently in compression to induce damage into the composite material.

$$\sigma_c = \frac{k_m \mu}{l - \mu} \Delta \quad \text{Eq. 3.3}$$

Based on the free-body diagram of the cRDCB specimen (Figure 3.3c), force and moment balances (Eq. 3.4 and Eq. 3.5, respectively) can be derived assuming a variable and unknown tensile TSR, $\sigma(x)$, and a linear compressive response, σ_c .

$$P + \frac{1}{2} \sigma_c \mu B = B \int_0^{b-\mu} \sigma(x) dx \quad \text{Eq. 3.4}$$

$$P(L - \mu) - \frac{1}{3}\mu^2\sigma_c B = B \int_0^{b-\mu} x\sigma(x)dx \quad \text{Eq. 3.5}$$

Peak compressive traction, σ_c , is an unknown in both Eq. 3.4 and Eq. 3.5. However, based on the assumption that the compressive response of the interface is linear, σ_c can be defined with respect to the initial stiffness of the interface. As the delamination interface is initially elastic, σ_c can be resolved by assuming an initial linear traction response in the tensile region of the interface, σ_o (Eq. 3.6).

$$\sigma_c = \frac{\mu\sigma_o}{b - \mu} = \frac{\mu k_m \Delta}{b - \mu} \quad \text{Eq. 3.6}$$

Continuing the assumption of an initial linear response, the initial interface stiffness, k_m , and the initial pivot location, μ_e , can be solved by substituting Eq. 3.6 into the force and moment balance (Eq. 3.4 and Eq. 3.5, respectively). It is important to note that the material was assumed to have the same stiffness in tension and compression.

$$\mu_e = \frac{3Lb - 2b^2}{6L - 3b} \quad \text{Eq. 3.7}$$

$$k_m = \frac{F}{\delta} \left(\frac{3(L - \mu_e)(l - \mu_e) - 2(l - \mu_e)(b - \mu_e)}{\mu_e^2 B b} \right) \quad \text{Eq. 3.8}$$

Returning to the case of the general and unknown traction response, Eq. 3.6 can be substituted into Eq. 3.4 and Eq. 3.5 to remove σ_c from the force and moment balance. Additionally, it is important to note that the force and moment balances in Eq. 3.4 and Eq. 3.5 defines traction not with respect to interface displacement, which would be equivalent to a TSR, but rather the position along the interface, x . However, using Eq. 3.2 to relate position along the interface to displacement, the variable of integration in the force and moment balances can be changed to define traction with respect to interface displacement (Eq. 3.9 and Eq. 3.10, respectively), where $\sigma(\delta)$ is TSR of the delamination interface.

$$P + \frac{\mu^2 k_m \Delta}{2(l - \mu)} = \frac{B(l - \mu)}{\Delta} \int_0^{\delta_o} \sigma(\delta) d\delta \quad \text{Eq. 3.9}$$

$$P(L - \mu) - \frac{\mu^3 k_m \Delta}{3(l - \mu)} = B \left(\frac{l - \mu}{\Delta} \right)^2 \int_0^{\delta_o} \delta \sigma(\delta) d\delta \quad \text{Eq. 3.10}$$

$\sigma(\delta)$ can be isolated in each equation by differentiating with respect to separation.

$$\sigma(\delta) = \frac{d\Delta}{d\delta_o} \frac{d}{d\Delta} \left(\frac{P\Delta}{B(l-\mu)} + \frac{\mu^2 k_m \Delta^2}{2(l-\mu)^2} \right) \quad \text{Eq. 3.11}$$

$$\sigma(\delta) = \frac{l-\mu}{\Delta(b-\mu)} \frac{d\Delta}{d\delta_o} \frac{d}{d\Delta} \left(\frac{P(L-\mu)\Delta^2}{B(l-\mu)^2} - \frac{\mu^3 k_m \Delta^3}{3(l-\mu)^3} \right) \quad \text{Eq. 3.12}$$

Eq. 3.11 and Eq. 3.12 have two unknowns: the TSR, σ , and current pivot point, μ , at the current applied force, P , and measured specimen displacement, δ . While a closed form solution to both variables may be possible, the equations include derivatives of force and pivot location with respect to separation. In particular, the movement of the pivot point was very difficult to assess experimentally. To that end, both equations were solved simultaneously using an iterative root-finding algorithm to find the instantaneous pivot location, μ , which was then used to calculate the tensile traction at the current crack tip separation, $\sigma(\delta)$. The initial pivot location, μ_e , was an important input to this root-finding algorithm.

It is important to note that Rajan et al. (2020) proposed an alternative analysis scheme for RDCB-like specimens that eliminated the iterative root finding component of the analysis scheme by measuring separation at both the crack tip and the compressive edge of the specimen. This analysis scheme was investigated but ultimately rejected for this project. The Rajan method was difficult to implement directly as the exact compressive displacement could not be measured experimentally, which was not feasible due to the edge effects associated with DIC measurements. Early exploratory work attempted to extrapolate the compressive displacement at the edge of the specimen from measured DIC displacements, requiring a rigid body transformation. However, acceptable results could not be attained for the relatively simple rigid body translations adopted during exploratory work. Future work could consider a more detailed rigid-body analysis of adherend motion to extrapolate compressive displacements more accurately. Nevertheless, the exploratory work did not suggest that the Rajan method provided significantly better results than the analysis approach adopted here when applied to simulated cRDCB data where all required input parameters could be controlled.

The analysis technique to extract TSR from a cRDCB delamination interface was highly dependent on the first derivative of force-displacement behaviour. However, using the derivative in the analysis introduced a susceptibility to experimental measurement noise. When

computing the derivative with common numerical difference techniques, a small amount of noise in the force-displacement response yielded very large peaks in the derivative. These peaks, in turn, lead to extreme and unrealistic spikes in the extracted TSR. To that end, a regression-type, piecewise cubic spline (D’Errico, 2022) was fit to the experimental force-displacement response of each specimen for analysis. Using splines removed noise inherent in the experimental data and enabled easy calculation of the first derivative while enforcing physically representative constraints such as linear behaviour for the initial part of the force-displacement responses. Two physically representative constraints were enforced during spline fitting: 1) an initial linear slope followed by a period of concave down behaviour and 2) concave up behaviour during as the specimen unloaded at large applied displacements. These constraints intrinsically enforced a single inflection point that occurred just after peak load was reached in the cRDCB specimen.

3.1.2 DCB Specimen Fabrication, Testing and Analysis

DCB specimens used in this work were manufactured based on ASTM standard D5528 (ASTM International, 2013), which recommended extracting individual DCB specimens from a larger panel. First, a 10-ply unidirectional FRP laminate panel ($[0]_{10}$) was fabricated using the same E-glass/epoxy material as the cRDCB specimens. A 12.5 μm PTFE film was inserted at the midplane of the panel to serve as a crack starter. Processing was performed using compression moulding in a hydraulic press with temperature-controlled platens (G100H-24, Wabash, USA). Curing pressure, temperature, and time (0.5 MPa, 140°C, and 90 minutes, respectively) for the DCB panel were the same as those used with the cRDCB for consistency. It should be noted that the 10-ply panel used in this work was at the lower end of panel thicknesses recommended by ASTM D5528. The choice of ten plies was made to simplify the manufacturing process. Further, it was originally hypothesized that a thinner laminate might better compare the four-ply laminates used in cRDCB specimens. The completed panel had a fibre volume fraction of 53% as measured by acid digestion (average of two measurements) (ASTM International, 2015).

Once cured, DCB specimen blanks were extracted from the panel using waterjet cutting. The cut faces of the specimen were smoothed using fine-grit abrasive paper to minimize edge effects during testing. Mild steel load blocks were bonded to the specimens using a structural adhesive (Impact Resistant Structural Adhesive 7333, 3M Canada, Canada) to complete the DCB specimens

(Figure 3.4). Load blocks were used instead of the common alternative method of hinges to maintain the same mode of loading as the cRDCB specimens and for compatibility with existing test fixturing.

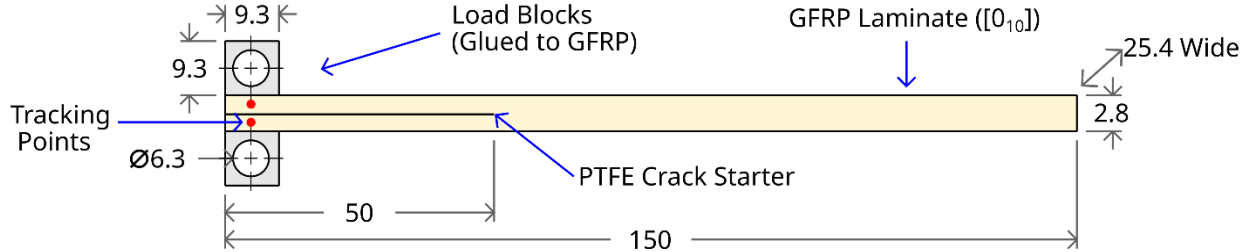


Figure 3.4: A schematic of the DCB specimen used in this work (not to scale).

DCB testing was performed with a 500 lb hydraulic test frame (custom-built) at a controlled displacement rate of 5 mm/min. No pre-cracking or other crack preconditioning was performed on the DCB specimens. A 12 MP USB camera (Grasshopper 3 GS3-U3-123S6M, FLIR, USA) with an 85 mm telephoto lens (Bower 85 mm f/1.4, Bower Inc, USA) was used to image each test to track the opening of the DCB arms as well as to monitor crack propagation and other delamination phenomena. Specimen displacement was measured using tracking points painted on the DCB specimen and tracked with open-sourced optical tracking software (Tracker (Brown, 2019)).

Force-displacement data from DCB specimens were used to compute resistance curves (R-curves) for each specimen, which plotted the relationship between fracture toughness and crack extension. A crack equivalence method was used for this process (De Gracia et al., 2015). While the ASTM standard recommends an alternative methodology requiring crack extension to be measured experimentally, tracking crack tip location was a task which needed to be accomplished manually. Further, manual crack tracking was difficult due to the low contrast between the crack and the colour of the GFRP laminate. The crack equivalence method produced very similar results to the ASTM method without the time-consuming and error-prone task of manually tracking.

3.2 Results and Analysis of Experimental cRDCB and DCB Testing

3.2.1 Results and Analysis of cRDCB Testing

Eight cRDCB specimens were tested, with all but one ($n = 7$) undergoing delamination, with the remaining specimen undergoing adherend-laminate interface failure. All valid specimens exhibited similar force-displacement trends (Figure 3.5). Beginning from an elastic response in the early loading regime (labelled as region 1 in Figure 3.5), the stiffness of the force-displacement response began to decrease as damage initiated at the crack tip (region 2). As the crack tip begins to extend, the force-displacement response exhibited a “plateau” region before experiencing a decrease in load (region 3). Finally, as the crack tip continued to extend, large-scale damage mechanisms such as large-scale fibre bridging dominated (region 4), providing relatively low carrying capacity over a large displacement. The crossover between matrix-dominated damage and fibre-bridging occurred at the inflection point in the post-peak portion of the force-displacement response.

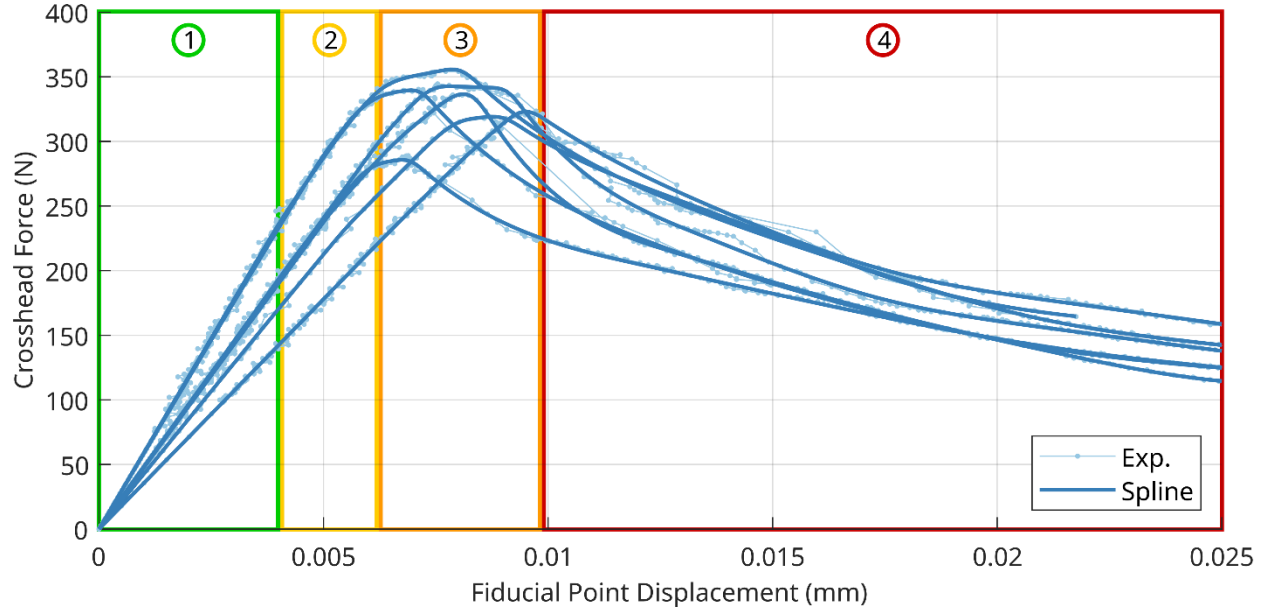


Figure 3.5: Force-displacement responses of cRDCB specimen ($n = 7$). Fitted smoothing splines are shown overtop experimental data. Distinct regions of specimen response are highlighted corresponding to 1) undamaged elastic response, 2) damage onset contributing to a reduction in specimen stiffness, 3) the start of crack propagation associated with a “plateau” and reduction in force, and finally, 4) a steady reduction in load carrying capacity as the delamination crack extends and large-scale fibre bridging dominates.

Full-field strain fields provided by DIC enabled damage progression to be observed throughout the test. During the elastic regime of the cRDCB specimen, the balanced tensile and compressive response of the FRP laminate was apparent (Figure 3.6a). As damage accumulated at the crack tip (Figure 3.6b), tensile strains extended vertically away from the crack starter and were arrested by the metallic adherends. Finally, as the delamination crack propagated, the movement of the crack tip was observed (Figure 3.6c). Throughout this range of behaviour, the compressive strain response of the specimen continued to respond linearly with no discernible damage accumulation.

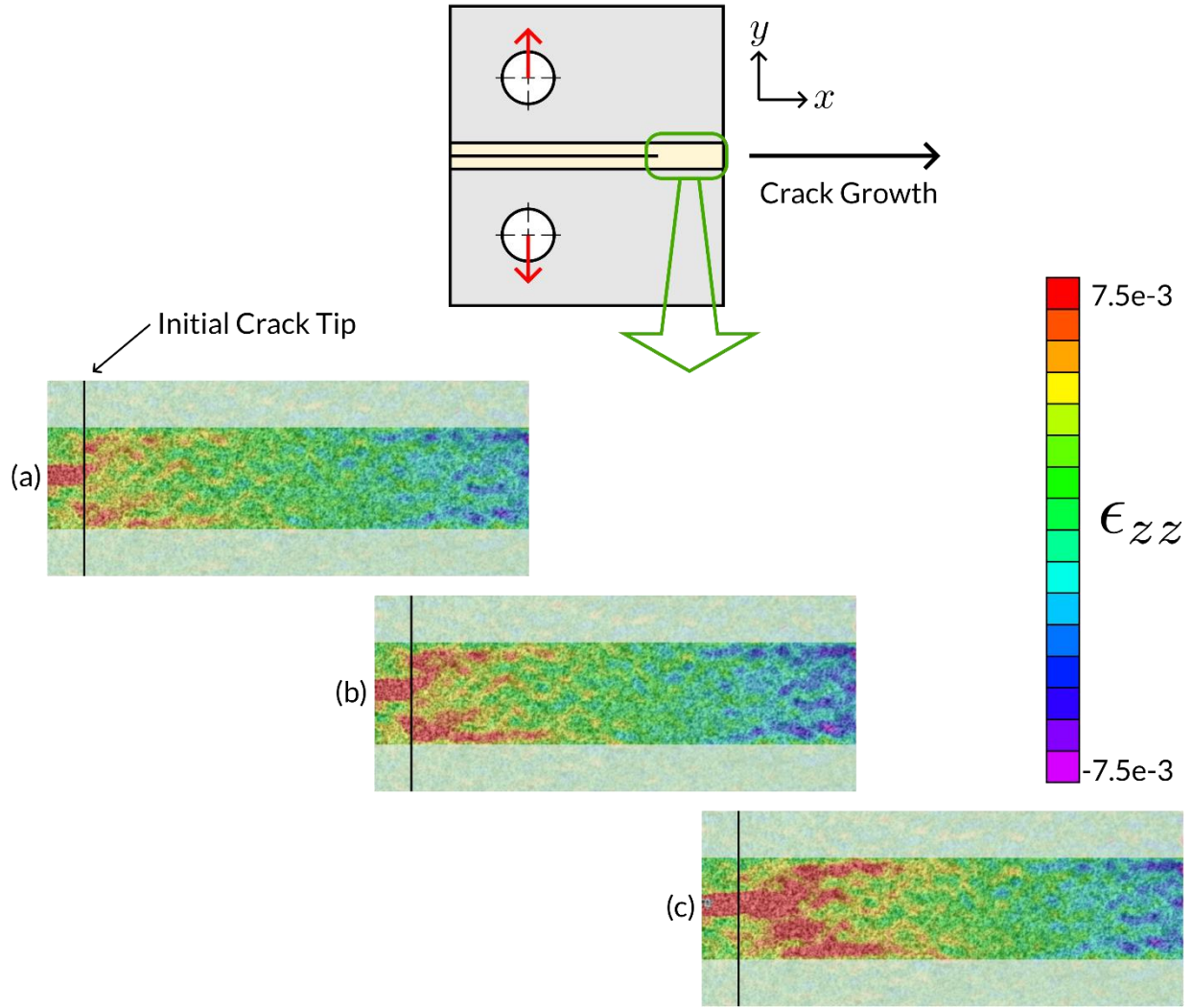


Figure 3.6: Full-field strain measurements of the cRDCB specimen centred on the delamination interface being loaded denoted with the green box. Strain field measurements capture the progression of delamination from (a) initial elastic loading through (b) damage accumulation at the initial crack tip and, finally, (c) crack propagation. The crack extended in the positive x -direction in these images. The strain fields do not show the exact crack tip location but indicate damage propagation.

TSRs were extracted from the fitted smoothing splines using the analysis scheme documented in Section 3.1.1. The extracted TSRs exhibited behaviours typically associated with Mode I delamination, specifically relatively high peak traction at low separation associated with the matrix material of laminate (Figure 3.7a, region 1) followed by a prolonged period of low, decreasing traction response associated with large-scale fibre bridging (Figure 3.7a, region 2).

Smoothing splines ensured that the unload portion of the TSR could be extracted. Applying the cRDCB analysis procedure to experimental force-displacement responses produced noisy, non-physical traction responses.

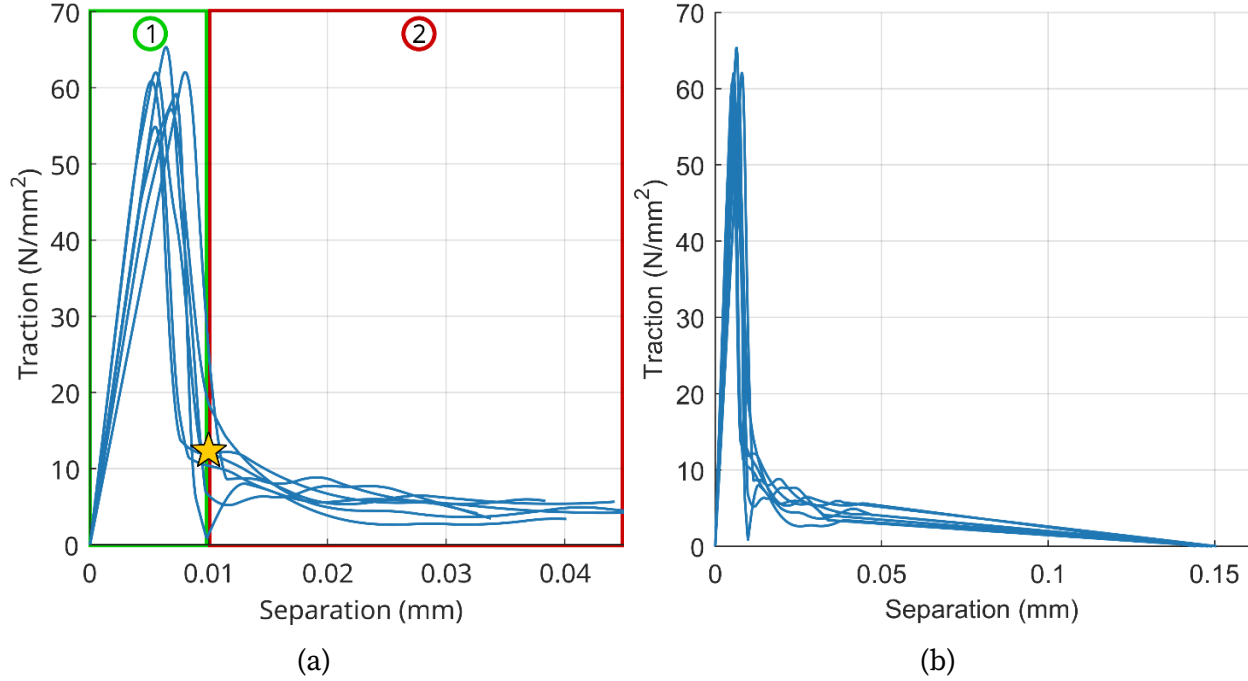


Figure 3.7: (a) TSRs extracted from cRDCB specimens with the annotated regions indicating the matrix-dominated response (1) and fibre-bridging-dominated behaviour (2). The gold star indicates the approximate location of the TSR inflection point. (b) Experimental TSR extrapolated to zero traction assuming linear unloading behaviour.

The toughness of the cRDCB specimen, in the form of energy release rate, was computed by integrating extracted TSRs. However, as the extracted TSRs did not return to zero traction, the full contribution of large-scale fibre bridging could not be assessed. Furthermore, the small interface length of the cRDCB specimen also limited the extent to which the bridging response could be assessed. Nevertheless, TSRs were extrapolated to zero traction, assuming a linear response regressed from the approximate slope of the measured portion of the bridging response (Figure 3.7b). A linear bridging response has been used in the literature to model large-scale fibre bridging in a unidirectional FRP laminate (Heidari-Rarani et al., 2013).

Toughness calculations were divided into two figures of merit: toughness associated with the matrix material and estimated total toughness. The matrix toughness, defined as the energy

release rate up to the inflection point of the force-displacement response, was calculated to be 0.32 J/mm^2 (SD = 0.04 J/mm^2). The total extrapolated toughness was estimated to be 0.7 J/mm^2 at an approximate separation of 0.15 mm across all seven specimens.

3.2.2 Results and Analysis of DCB Specimens

DCB specimens all exhibited predominantly stable crack growth and low variability in force-displacement response (Figure 3.8a). All specimens exhibited an initial linear response, followed by a short period of non-linear behaviour between $12\text{-}20 \text{ mm}$ of specimen displacement before transitioning to a relatively stable force plateau at large separation.

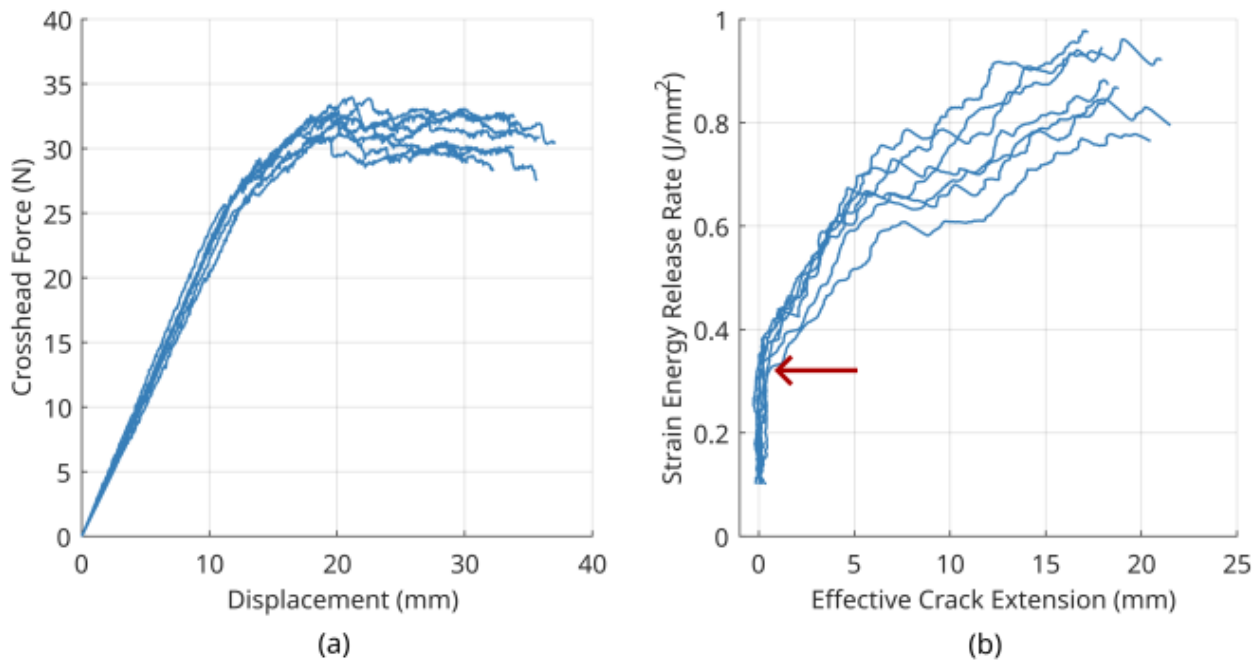


Figure 3.8: Force-displacement curves (a) and R-curves (b) measured from DCB specimens tested in this work ($n = 7$). The red arrow in (b) indicates where energy release rate deviates from vertical, indicating average initial toughness.

R-curves were extracted from force-displacement responses using the method of De Gracia et al. (2015). These curves exhibit the classic rising trend associated with large-scale fibre bridging (Farmand-Ashtiani et al., 2015). However, a steady-state toughness was not achieved with the crack extensions observed in this work, likely resulting from the specimen geometry used. Nevertheless, the calculated R-curves trended toward a steady state toughness between 0.8 and 1.1 J/mm^2 , which are typical values for similar E-glass/epoxy material systems (Marat-

Mendes and Freitas, 2006, 2010). Observing the onset of delamination propagation, where the R-curves diverged from vertical, yielded an average initial toughness of 0.35 J/mm^2 (SD = 0.03 J/mm^2).

3.2.3 Calculation of Average Response and Statistical Bounds

Calculating an average of experimental responses and variability was an important step. Not only did an average response and measure of experimental variability provide the basis to assess the performance of later numerical modelling, but the average response of cRDCD force-displacement behaviour and TSRs were important inputs for constitutive material models and supplemental analysis. However, computing an average response was somewhat complicated by the lack of a shared termination between all responses. To that end, an arc-length-based approach was adopted to compute an average response and statistical bounds (Hartlen and Cronin, 2022).

In short, the arc-length-based approach re-parameterized each response with respect to the normalized arc-length of each response, such that each point in the response can be parameterized with a value from zero to one. All responses were then uniformly re-sampled with respect to normalized arc-length, with the average and standard deviation at each arc-length in both axes used to construct an average response and statistical bounds. The benefit of this approach was two-fold. First, following arc-length re-parameterization, all responses shared a common sampling variable. Second, the variability of the responses in both measurement axes could be assessed simultaneously, not simply the variability in the y-axis like traditional averaging.

To further improve the performance of the arc-length-based approach, a process known as signal registration was performed to align shared features with respect to normalized arc-length. This process provided an average that captured the underlying shape of the experimental responses better than traditional pointwise averaging without altering the shape or location of features in the original responses.

The average response and statistical bounds for the force-displacement behaviour and TSRs of the cRDCB are shown in Figure 3.9, overlaid atop the original responses. The average responses and bounds capture the underlying shape of the responses well while representing

the variability in the magnitude and location of features. While average responses were used directly as inputs to analysis and constitutive models, the statistical bounds provided a means to assess if the response of a finite element model fell within experimental variability. The average response and statistical bounds of DCB force-displacement behaviour are provided in Figure 3.10.

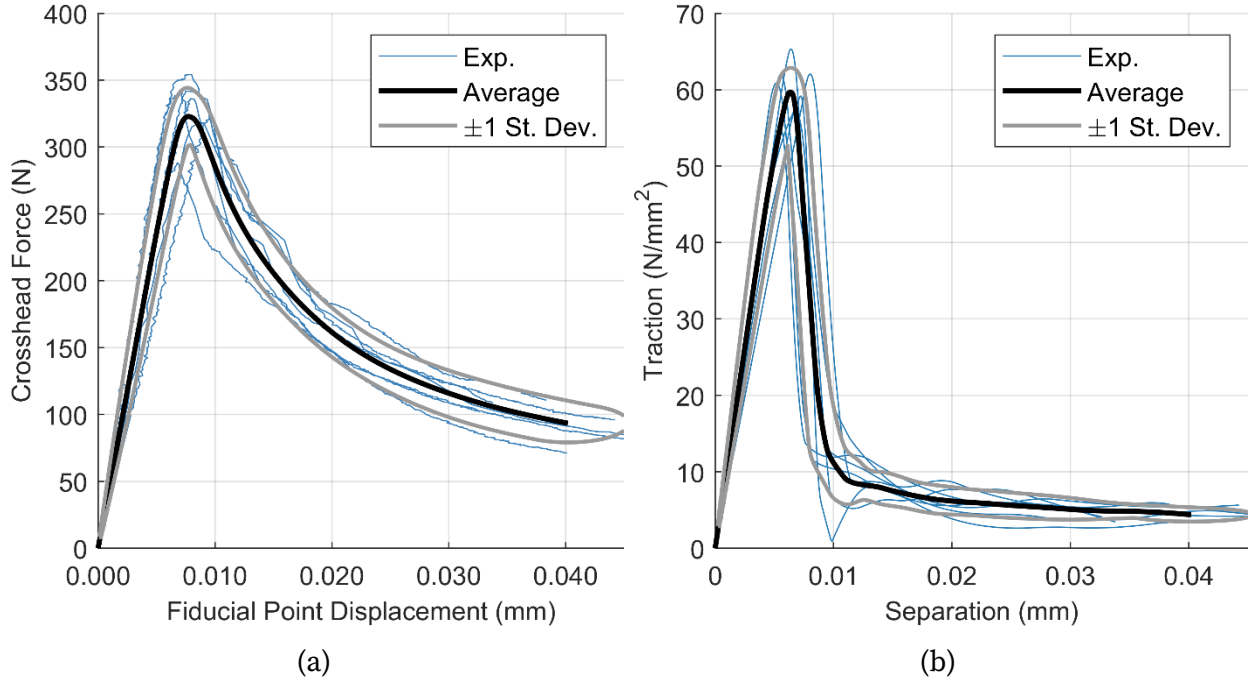


Figure 3.9: The average response and statistical bounds of cRDCB (a) force-displacement behaviour and (b) TSRs ($n = 7$), as computed with an arc-length-based approach (Hartlen and Cronin, 2022). In both cases, one warping control point and a warping penalty of 10^{-2} was used to control signal registration to align shared features.

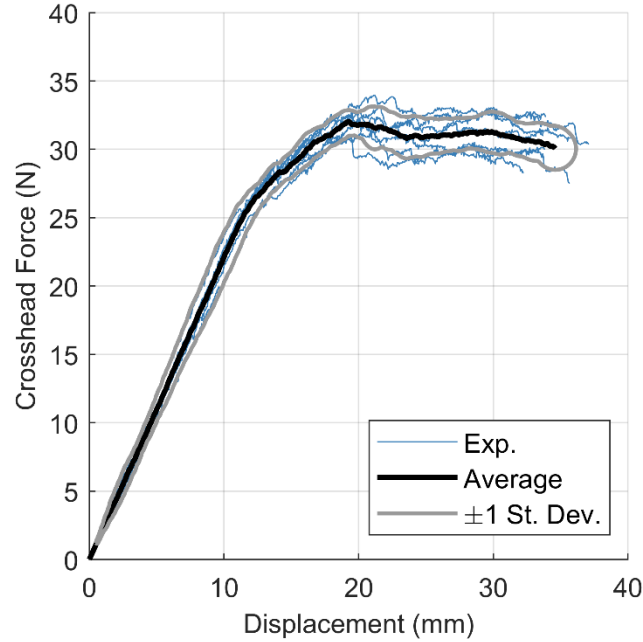


Figure 3.10: The average force-displacement response of DCB specimens ($n = 7$) with statistical bounds as computed with an arc-length-based method. One warping control point and a warping penalty of 10^{-2} was used to control signal registration.

3.2.4 Fractography of cRDCB and DCB specimens

The fracture surfaces of both the cRDCB and DCB specimens were imaged using an optical-digital microscope (VHS-5000, Keyence, Japan). Microscopy (Figure 3.11) revealed similar fracture morphologies in both specimens that were typical of Mode I mechanisms (including large-scale fibre bridging), such as matte, scalloped fracture surfaces on the matrix material, fibre-matrix interfacial debonding, and fibre breakage (Gilchrist and Svensson, 1995; Hashemi et al., 1990). Some small voids were observed on the fracture surfaces of both specimens, although there was no evidence to indicate that delamination initiated at these locations. Overall, there were substantial shared fracture morphologies between the two fracture surfaces of the cRDCB and DCB specimens.

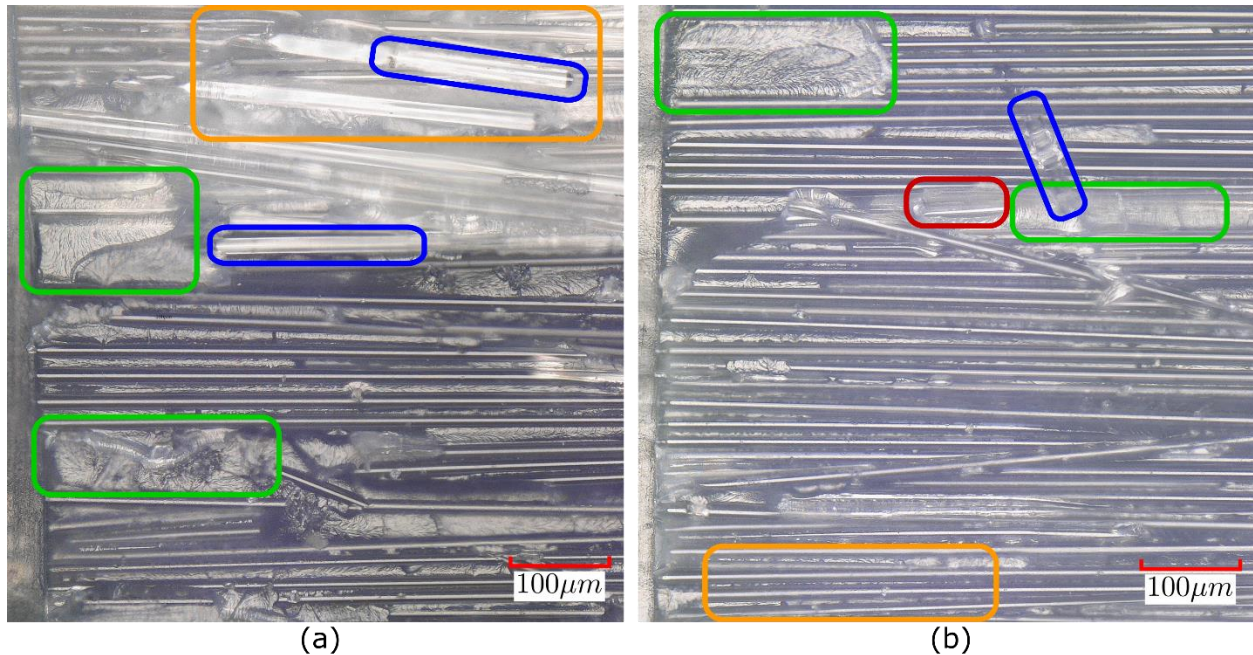


Figure 3.11: Microscopy of cRDCB (a) and DCB (b) fracture surfaces. Coloured annotations highlight specific morphologies, namely ductile matrix damage (green), fibre-matrix debond (orange), broken fibres (blue), and voids (red).

3.3 Discussion and Comparison of Specimen Geometries

3.3.1 Discussion of cRDCB Results

The experimental force-displacement response of the cRDCB specimens showed variation in both peak force and initial stiffness that could be attributed to two main sources. First, the placement of the PTFE crack starter could affect the stiffness and peak force associated with a specimen by changing the size of the delamination interface. Second, the condition of the crack tip at the tip of the PTFE crack starter could affect how damage initiates and propagates. As noted, no crack conditioning was performed, so the state of the crack tip was dependent on how well the laminate formed around the PTFE tape. A blunter crack would delay the onset of delamination, which would impact the peak force of the cRDCB specimen. While jigs were used to help ensure consistent placement of the PTFE crack starter and uniform manufacture, some variation was inevitable, especially given the small size of the cRDCB specimen. Additionally, the small size of the cRDCB specimen may have amplified the effect of defects in the laminate

material, such as micro-voids and variations in local fibre concentration. Specimen-to-specimen variation in fibre volume fraction and slight fibre misalignment in the prepreg tapes could also contribute to the variance in the observed force-displacement responses.

While DIC imagery was used primarily to provide accurate measures of displacement across the crack tip of the specimen, the full-field strain measurements provided a wealth of data to explore. One interesting observation permitted by DIC was that the largest through-thickness tensile strains formed at the interface between the adherend and laminate. The high strains could be attributed to the constraining effect generated by the interface of two materials with very different stiffness, as well as the anisotropic nature of the FRP laminate. The observation of high strains not only highlighted the importance of forming a good connection between the laminate and adherend but also shed some light on issues encountered during the early development of the cRDCB specimen.

Early developmental work on the cRDCB specimen investigated using two-ply and four-ply unidirectional FRP laminates. As the goal of the cRDCB specimen was to maximize the rigidity of the specimen to provide the most direct loading of the delamination interface, it was thought at the time that a thinner laminate would provide more representative traction-separation behaviour. Despite identical surface preparation, two-ply cRDCB specimens always underwent interface failure, which led to using four-ply cRDCB specimens in all subsequent work. While DIC imaging was not yet implemented at that early stage of development, the full-field strain data documented here explain the propensity of two-ply cRDCB specimens to undergo undesirable failure modes.

One important limitation of DIC imagery was that while the damage progression could be observed, the exact crack tip could not be directly isolated from strain contours. While strain contours do highlight where a crack has developed, the exact location was masked. Processing parameters like subset size and strain filter selection greatly impacted the resolution of strain fields around the crack tip (Taylor Owens and Tippur, 2023) and could mask the actual location of the crack tip. However, it is important to note that crack tip extension was not required to extract traction-separation behaviour from cRDCB specimens. Nevertheless, full-field strain data provided significant insight into developing Mode I delamination in the cRDCB specimen under load.

The extracted TSRs exhibited a similar degree of variability as the force-displacement behaviour, although the extracted TSRs did exhibit some non-physical oscillation, especially in the bridging response of the laminate. Oscillations stemmed from the challenges of dealing with the first and second derivatives of force-displacement response. While smoothing splines reduced much of the oscillatory behaviour, the behaviour could not be suppressed entirely. Other methods of filtering force-displacement data were investigated, such as traditional filtering and Savitsky-Golay filtering (Savitzky and Golay, 1964). However, none produced realistic TSRs without large amounts of noise, especially in the unload response of the specimen. When fitting smoothing splines, it was found that the transition from matrix-dominated behaviour to fibre-bridging-dominated behaviour was tied to the inflection point separating regions 3 and 4 in Figure 3.5. If the smoothing spline did not accurately capture the location of the inflection point or if a small amount of unstable crack propagation occurred, significant oscillation could be introduced into TSRs.

3.3.2 Discussion of DCB Results

The force-displacement behaviour of DCB specimens was very consistent between specimens. However, compared to the behaviour of DCB specimens observed in the literature, the peak force of the specimens tested in this work was somewhat low (Marat-Mendes and Freitas, 2006) owing to the relatively thin specimen geometry and long initial crack length, a . A thinner laminate was chosen on the assumption that a thinner DCB specimen would provide a more accurate comparison to the thin laminate used in the cRDCB specimens, while the long initial crack length was selected to help ensure stable crack growth.

While a steady-state toughness could not be assessed for the DCB specimens in this work, an average initial toughness of 0.35 J/mm^2 was found. However, it is important to note that extracting the measurement of initial toughness (the point at which the R-curve diverges from vertical) demonstrated the difficulty of accurately measuring this value, as the noise in the response often makes this process manual. Furthermore, the R-curves demonstrated how the DCB specimen was relatively insensitive to initial toughness, given that the transition from vertical occurs over a near negligible crack extension.

3.3.3 Comparison of the cRDCB and DCB Geometries

Fractography (Figure 3.11) was an important tool in comparing the behaviour of the cRDCB and DCB specimens to ensure that similar damage mechanisms were occurring. As noted, despite the fabrication techniques developed during this research to produce high-quality specimens, voids were still present in the laminate of the cRDCB specimen. However, despite the different processing techniques, voids were generally small and similar in morphology between the cRDCB and DCB specimens. Further, the location, pattern, and shape of the voids in both specimens suggest there may have been insufficient compaction of the plies during processing or entrained air between the plies that could not escape during the curing cycle. While more intensive processing techniques might have improved the quality of the FRP laminate, such as vacuum debulking, curing under a vacuum, or even processing in an autoclave environment, these approaches would have added significant complexity to the fabrication process. Nevertheless, the quality of the FRP laminate produced was sufficient for developing new delamination specimens.

Despite containing more voids than ideal, the overall morphologies of the fracture surfaces of both specimens were similar. Nevertheless, there were some minor differences. The fracture surfaces of the cRDCB specimen did exhibit somewhat more visible matrix damage, denoted by the matte, scalloped surfaces highlighted in green in Figure 3.11, than the DCB specimen. This feature could be due, in part, to the slightly lower fibre volume fraction of the cRDCB specimen. However, the prevalence of matrix damage could also indicate the greater sensitivity of the cRDCB specimen to early damage behaviours, which are driven primarily by the matrix material (Mall, 2008).

While the cRDCB and DCB specimens could not be directly compared with regard to force-displacement or traction-separation behaviour, comparisons could be made between the measured toughness of each specimen. Toughness, in the form of energy release rate, was provided directly from DCB R-curves and was computed by integrating cRDCB TSRs. Overall, there was good agreement between the cRDCB and DCB specimens for early damage behaviour. Integrated cRDCB TSRs up to the onset of fibre-bridging yielded an energy release rate of 0.32 J/mm^2 ($\text{SD} = 0.04 \text{ J/mm}^2$). cRDCB values agreed favourably with the initial toughness of the DCB specimen (where R-curves deviate from vertical at zero crack extension), which was measured

to be 0.35 J/mm^2 ($\text{SD} = 0.03 \text{ J/mm}^2$), both in terms of overall magnitude and variability. The agreement between the initial toughness of the cRDCB and DCB specimens showed that the response of the matrix material during delamination appeared to be independent of specimen design. However, it is important to note that the cRDCB provided a much more detailed description of the delamination response for early damage behaviour than the DCB specimen.

The toughness measurements of the cRDCB and DCB specimens diverged somewhat as large-scale fibre bridging began to dominate the delamination behaviour. The total extrapolated toughness of the cRDCB specimen was estimated to be 0.7 J/mm^2 . While measured DCB R-curves did not achieve a steady state and exhibited relatively high variability, all specimens trended to a toughness greater than 0.8 J/mm^2 . However, it is important to note that such high DCB toughnesses were attained at crack extensions greater than 20 mm. As the total interface of the cRDCB specimen was only 7 mm long, a steady-state comparison of toughness between the specimens was not directly possible.

However, a comparison could still be made between the two geometries at smaller crack extensions. The energy release rate of the cRDCB specimen at 5 mm of crack extension was approximately 0.7 J/mm^2 . From the DCB R-curves, the DCB specimens had an energy release rate of 0.62 J/mm^2 ($\text{SD} = 0.05 \text{ J/mm}^2$) at the same crack extension, demonstrating a relatively close agreement in toughness at a given crack extension between geometries. While this assessment provided some confidence when assessing the performance of the cRDCB specimen for large-scale damage mechanisms, it is important to note that behaviours such as fibre-bridging are inherently specimen-specific. Literature has shown that even for a standardized specimen geometry like the DCB, the thickness of the specimen has a significant impact on the steady-state toughness, with toughness generally increasing with increasing specimen thickness (Farmand-Ashtiani et al., 2015; Gong et al., 2021; Tabiei and Zhang, 2018a).

It is also important to note that the small size of the cRDCB specimen may preclude the ability to assess the performance of the specimen at very large crack extensions, like those attained with the DCB specimen. Crack extension was inherently limited by the length of the delamination interface of cRDCB geometry, which was 7 mm in this work. Testing during the development of the cRDCB specimen revealed that longer interface lengths increased specimen stiffness dramatically, which led to issues wherein the compliance of the experimental setup,

including test frame and fixturing, produced extremely rapid, unstable crack extension and made an assessment of damage response impossible.

While the cRDCB may be limited to lower crack extensions than the DCB specimen, it is worth reiterating that the cRDCB provided the TSR of a Mode I delamination interface with excellent resolution in early matrix-dominated delamination response. There are many practical applications, such as low-velocity impacts (Aymerich et al., 2009; De Moura et al., 1997; Zhang et al., 2019) or fatigue damage initiation (Yao et al., 2016), where a detailed understanding of damage onset and early behaviour is more important than the catastrophic damage regimes typically associated with large-scale fibre bridging. Further, while the DCB specimen may provide better toughness measurements at large crack extensions, toughness measurements are a singular value that cannot be directly used in contemporary modelling techniques such as CZM. In those cases, additional work would still be needed to fit a traction-separation response to measured DCB responses at large crack extensions, unlike the direct extraction provided by the cRDCB specimen.

3.4 Conclusions

The Mode I delamination traction-separation response (TSR) of an E-glass/epoxy laminate was successfully extracted from the composite rigid double cantilever beam (cRDCB) without resorting to additional tests or inverse methods. Assessment of the cRDCB specimen against the widely accepted double cantilever beam (DCB) specimen in terms of toughness showed very good agreement to damage onset and early, matrix-driven damage response. cRDCB TSRs specimens had excellent resolution in these early damage regimes where the DCB specimen has been documented to be insensitive. TSRs with detailed damage initiation behaviour would be useful when predicting the onset of delamination is important, such as low-velocity impacts or low-cycle fatigue. Furthermore, while not a part of this project, the small size and rigidity of the cRDCB specimen make it more suitable for high-deformation-rate testing than the traditional DCB specimen.

Chapter 4

FINITE ELEMENT MODELLING OF MODE I DELAMINATION USING A STRESS INTENSITY

FACTOR ENHANCED COHESIVE ZONE MODEL

This chapter documents finite element (FE) modelling of Mode I delamination in the composite rigid double cantilever beam (cRDCB) and double cantilever beam (DCB) specimens using cohesive zone modelling (CZM). The purpose of the FE modelling in this chapter was two-fold. First, the modelling served as a verification of the traction-separation responses (TSRs) measured with the cRDCB specimen in Chapter 3, as well as to understand how well TSRs measured with the cRDCB specimen transfer to other geometries. Second, the modelling provided insight into the two common CZM approaches used for delamination and helped guide the development of analysis procedures to convert between the two approaches easily. As noted in Section 2.3.3, there are two common modelling approaches when modelling delamination and interface failure with cohesive elements. First is the combined elastic-damage approach, where cohesive elements are used to capture the response of a large volume of material in the interface. Cohesive elements in the combined elastic-damage approach use TSRs that capture both the damage response of the interface and the stiffness of the material adjacent to the interface. While suitable for adhesive joints, the combined elastic-damage approach is not well suited for delamination, whereas the second approach, the damage-only approach, is more common. The damage-only approach uses zero or near-zero thickness cohesive elements to capture the damage response of the interface, with traditional continuum elements used to capture the behaviour of adjacent materials. When applied to FRP laminate materials, cohesive elements capture delamination, while continuum elements capture the intralaminar behaviour of the adjacent laminates.

A key challenge with implementing damage-only modelling of delamination is that TSRs used in this approach must have a very high initial stiffness to isolate the stiffness contribution of the surrounding continuum elements from the damage response of the cohesive elements. However, experimental TSRs measured with the cRDCB specimen have a finite initial stiffness influenced by the behaviour of the FRP laminate. Further, the analysis scheme used to extract

TSRs from experimental data used assumptions suitable for TSRs intended for the combined elastic-damage approach that are less well suited to damage-only modelling.

This chapter presents two analysis procedures to convert experimentally measured TSRs to those suitable for damage-only modelling of delamination with cohesive elements. The first analysis procedure was a compensation function based on linear elastic fracture mechanics developed to account for the complex stress state at the tip of a cRDCB specimen to enable better predictions of peak force using the damage-only modelling approach. The second analysis procedure was an energetically consistent technique of converting experimentally measured TSRs to those with a high, user-definable initial stiffness suitable for damage-only modelling of delamination. While both analysis procedures are used sequentially in this chapter, they are independent and can be combined or separated. Detailed FE modelling was used to demonstrate the need for the two analysis procedures developed here and assess the benefits and effectiveness of the techniques. Additionally, the detailed FE models of the cRDCB specimen are used to explore how altering the geometry of the cRDCB specimen changes the response of the specimen.

4.1 Specimen-Level Finite Element Model Development

All finite element models were meshed using commercial meshing software (HyperMesh R2020, Altair Engineering, USA). A commercial finite element solver (LS-DYNA R11.2 double precision, LST-ANSYS, USA) was used to solve the models. All models were run on Graham, a high-performance computer cluster located at the University of Waterloo and administered by the Digital Research Alliance of Canada.

4.1.1 cRDCB FE Model for Mode I Delamination

Modelling of Mode I delamination in the cRDCB specimen was investigated using two modelling approaches: the combined elastic-damage approach (shortened to the combined approach) and the damage-only approach. While it was established that the combined approach was not ideal for modelling delamination, the approach shared many of the assumptions used to extract TSR from experimental cRDCB data using the analysis procedure documented in Section 3.1.2. In contrast, while the damage-only approach provided a much more detailed and

fidelic analysis of delamination response, the approach was much more computationally expensive and required experimental TSRs to be adjusted to have an initial penalty stiffness.

In both modelling approaches, the steel adherends (grey, Figure 4.1) were modelled with fully integrated, selectively reduced solid elements (ELFORM -1 (LSTC, 2017a)) and were assigned linear elastic material properties (MAT_ELASTIC (LSTC, 2017b)) for steel (Table 4.1). Both modelling approaches used numerically rigid pins (red) to apply a prescribed displacement to the models. Half-model symmetry was used to reduce the overall computational expense of the models.

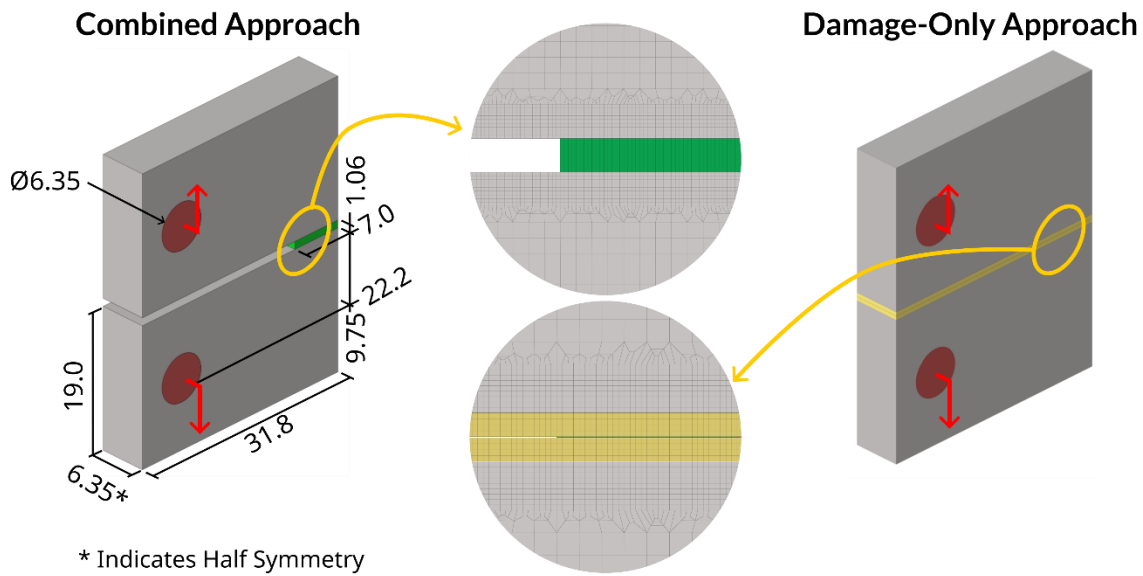


Figure 4.1: The combined elastic-damage (left) and damage-only (right) modelling approaches used to develop cRDCB FE models. The FRP laminate material (yellow, present only in the damage-only approach) was modelled with continuum solid elements, while the delamination interface was modelled with cohesive elements (green). Insets show the mesh refinement surrounding the initial crack tip. The thickness of the cohesive elements in the insert for the damage-only approach has been exaggerated for visibility.

Table 4.1: Material properties used for steel in the cRDCB model

Parameter	Value
E	200 GPa
ν	0.29

The combined elastic-damage modelling approach used an FE model that reflected the assumptions of the analysis method used to extract experimental TSRs (Section 3.1.2). A single layer of eight-noded cohesive elements (ELFORM 19) was inserted between the two adherends, capturing the volume taken up by the delamination interface and the FRP laminate material directly above and below the interface. As such, the cohesive elements in the combined modelling approach captured the homogenized behaviour of the FRP laminate material and the delamination interface. While the cohesive elements were the full thickness of the four-ply unidirectional laminate used in Chapter 3 (1.06 mm), a uniform in-plane mesh size of 0.25 mm was used to capture the delamination response. Cohesive elements shared nodes with the adherend, resulting in a direct connection between the parts. It is important to note that, unlike traditional continuum solid elements, the cohesive element formulation in LS-DYNA is generally unaffected by poor aspect ratios in the thickness direction. A mesh sensitivity study demonstrated that a finer mesh size produced negligible differences in the response of the model.

The cohesive elements of the combined elastic-damage required TSRs that captured not only the damage response of the delamination interface but also the intralaminar stiffness of the adjacent laminates. To that end, the average experimental TSR calculated from cRDCB specimens (Figure 4.2) was used. A cohesive model that allowed for tabulated responses to be entered (MAT_COHESIVE_GENERAL) was used. There were two benefits to using a tabulated cohesive model. First, no analytical cohesive models were available in LS-DYNA that could capture the protracted bridging response of the specimen. Second, a tabulated cohesive model permitted the exact shape of the average TSR to be reflected in the model, permitting the FE model to reflect the damage mechanisms of the physical cRDCB specimen more accurately.

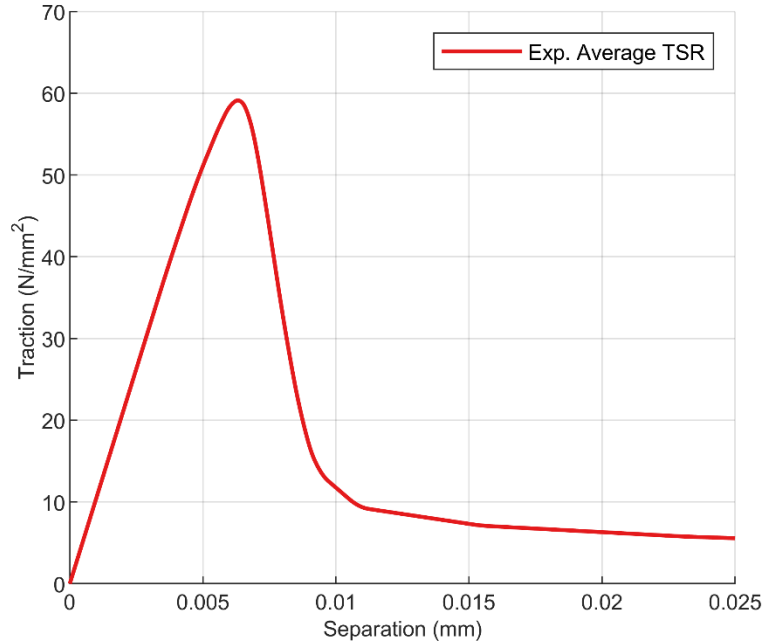


Figure 4.2: The experimental average cRDCB TSR used to model Mode I delamination response in the cRDCB specimen for the combined elastic-damage modelling approach. Tractions decreased linearly to zero at a separation of approximately 0.15 mm (refer to Figure 3.7).

Modelling delamination of the cRDCB specimen using the damage-only modelling approach decoupled the interlaminar response of the FRP laminate from the damage behaviour of the delamination interface. The FRP laminate was modelled with fully integrated, selectively reduced solid elements with a nominal size of 0.25 mm. While the delamination interface only encompassed 7 mm of the length of the cRDCB specimen, the solid elements were extended across the full length of the cRDCB specimen to capture the constraining effect of material behind the crack tip as well as to reflect the geometry of the physical cRDCB specimen. The laminate material was assigned an orthotropic-elastic material model (MAT_ORTHOTROPIC_ELASTIC) to reflect the assumption that the only damage occurring in the cRDCB specimen occurs from delamination. Material parameters (Table 4.2) were computed using the Halpin-Tsai equations (Halpin and Kardos, 1976) using the experimentally measured fibre volume fraction and manufacturer-supplied fibre and matrix properties (Table 4.3).

Table 4.2: Orthotropic-elastic properties used for the UE400/REM laminate ($V_f=47\%$)

Constitutive Parameter	Value
E_{11}	35.7 GPa
E_{22}, E_{33}	10.7 GPa
ν_{12}, ν_{13}	0.274
ν_{23}	0.356
G_{12}, G_{13}	3.72 GPa
G_{23}	3.95 GPa

Table 4.3: Fibre and matrix properties for UE400/REM provided by the manufacturers

Material Property	Value
E_{fibre}	72 GPa
G_{fibre}	30 GPa
ν_{fibre}	0.20
E_{matrix}	3.5 GPa
G_{matrix}	1.5 GPa
ν_{matrix}	0.34

The delamination interface was modelled with near-zero thickness (0.01 mm), eight-node cohesive elements (ELFORM 19) inserted at the geometric midplane of the laminate. Near-zero thickness cohesive elements were used instead of zero-thickness elements for model stability. Nodes between the cohesive elements and the laminate material, as well as between the laminate material and adherends were shared to form a numerically perfect connection. Cohesive elements in the damage-only approach also used a tabulated cohesive model (MAT_COHESIVE_GENERAL). The TSRs used as inputs for this model were based on the analysis discussed in section 4.2.3.

4.1.2 cRDCB FE Model for Stress Intensity Factor Calculations and Geometric Study

In addition to the two approaches used to model delamination in the cRDCB specimen, a third model was developed to study the effect of laminate thickness on the response of the cRDCB specimen and assess the stress intensity factor around the crack tip. This model was

identical to the damage-only model (Figure 4.1) but with the cohesive elements removed to provide a purely elastic model. The purely-elastic model used the same mesh size and material properties as presented in section 4.1.1. While it could be argued that the mesh surrounding the crack tip was not as refined as commonly used in the literature (Agrawal and Ben Jar, 2003), the same mesh size as used in the delamination models was maintained to ensure continuity of the stress intensity factors between purely-elastic and delamination models.

4.1.3 DCB FE Model for Assessment of Analysis Procedures Against Independent Data

A single FE model of the DCB specimen was created based on the damage-only modelling approach, in contrast with the two modelling approaches used for the cRDCB specimen. The importance of accurately capturing the bending of the arms of the DCB specimen necessitated that all FRP laminate components be modelled as deformable bodies. As such, the DCB specimen (Figure 4.3) was fully discretized with fully integrated, selectively reduced solid elements (ELFORM -1). The laminate material was assumed to be orthotropic-elastic (MAT_ORTHOTROPIC_ELASTIC) and was assigned the same material properties as the cRDCB specimen (Table 4.2). The delamination interface (green) was modelled using near-zero thickness (0.01 mm) cohesive elements (ELFORM 19). The DCB specimen was loaded in displacement control using numerically rigid load blocks (grey).

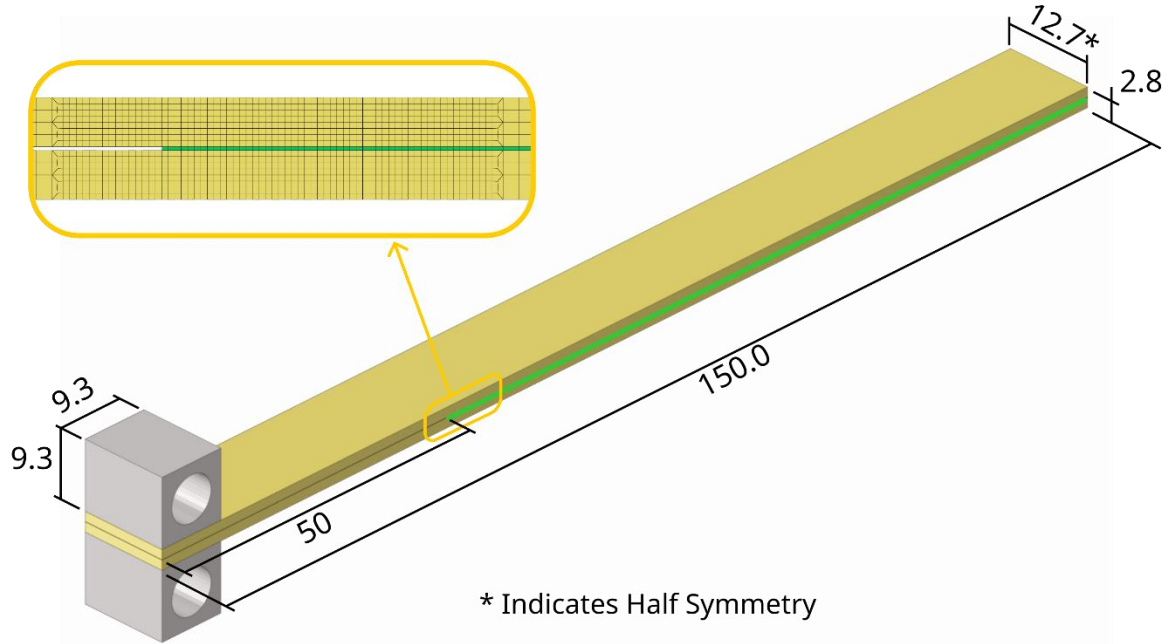


Figure 4.3: FE model of the DCB specimen with the FRP laminate material in yellow and the delamination interface in green. The model uses the damage-only modelling approach. The inset image shows mesh refinement around the crack tip, with the thickness of the cohesive elements exaggerated by ten times for visibility.

The mesh of the FRP laminate material and delamination interface was refined in the area extending 5 mm behind and 10 mm ahead of the crack tip to achieve more accurate predictions of delamination response without unstable crack growth induced by mesh size issues. Fully refining the entire model proved to be computationally prohibitive. To further reduce computational expense, half-model symmetry was exploited.

As the FE model of the DCB specimen adhered to the damage-only modelling approach, the cohesive models assigned to the cohesive elements required TSRs with high initial stiffness to ensure accurate bending of the arms of the DCB specimen. The experimental average TSR measured with the cRDCB specimen, modified with the analysis procedures documented in the following section, was used as a basis for inputs to the cohesive elements of the DCB FE model to explore the transferability of measured TSRs across geometries. The development of these TSRs is presented in Section 4.2.

4.2 Analysis Procedures to Enable Damage-Only Modelling of Delamination

Two analysis procedures were developed to convert experimentally measured TSRs into those suitable for modelling delamination with cohesive elements using the damage-only modelling approach. A key requirement of TSRs intended for the damage-only approach is a high initial stiffness. However, whereas experimentally measured TSRs intrinsically homogenize the response of the delamination interface and surrounding material, TSRs intended for damage-only modelling must capture phenomena such as the complex stress states surrounding a crack tip separate from surrounding material, necessitating the analysis procedures developed here. While used in concert in this research, the two procedures are independent and can be performed separately. The only requirement is that when performed together, the crack-tip compensation is applied to experimental TSRs prior to converting the initial stiffness of the TSR using the effective stiffness technique.

4.2.1 Crack Tip Compensation for cRDCB Traction-Separation Responses

The methodology used to extract the TSR of a cRDCB specimen from the force-displacement behaviour made two assumptions about the distribution of stress, and hence cohesive tractions, in the delamination interface. First, a uniform distribution of stress across the width of the specimen was assumed, corresponding to a plane-stress stress state. Second, it was assumed that the stress concentration at the crack tip of the cRDCB specimen was embedded in the extracted TSR. Both these assumptions have been shown to be valid for characterizing ductile structural adhesives (Dastjerdi et al., 2013; Watson et al., 2018), especially for the FE models using the combined elastic-damage approach (Liu, 2019). However, the far stiffer response of FRP laminates and the constraining effect of adjacent plies on the delamination interface exaggerate the effect of crack tip stress concentrations. Further, the transversely orthotropic nature of FRP laminates produced non-uniform traction distributions through the width of a specimen that can be attributed to the bend-twist coupling and anticlastic bending of the half laminate (Davidson, 1990; Davidson and Schapery, 1988). To that end, a crack tip compensation function for experimentally measured tractions was developed based on the stress intensity factor to approximate the stress state at the crack tip without changing the original cRDCB and RDCB analysis scheme.

The first step of defining the crack tip compensation function was calculating a stress intensity factor for the cRDCB specimen and the material system being analyzed. There was no analytical framework for the cRDCB geometry, so the stress intensity factor was calculated numerically from FE modelling. The purely elastic cRDCB FE model (Section 4.1.2) was loaded to a representative small displacement to develop a stress state surrounding the crack tip. Next, the J-integral, J_I , was computed around the crack tip using a post-processing tool (LS-PrePost 4.9, LST-ANSYS, USA) (Lindström et al., 2015). The crack tip was defined by selecting all nodes shared between the upper and lower FRP sub-laminate (Figure 4.4, red dot) and a contour radius of four elements from the crack tip (blue outline) was used to compute J_I . J-integral calculations using this tool were found to be largely independent of both mesh size and contour radii larger than three elements.

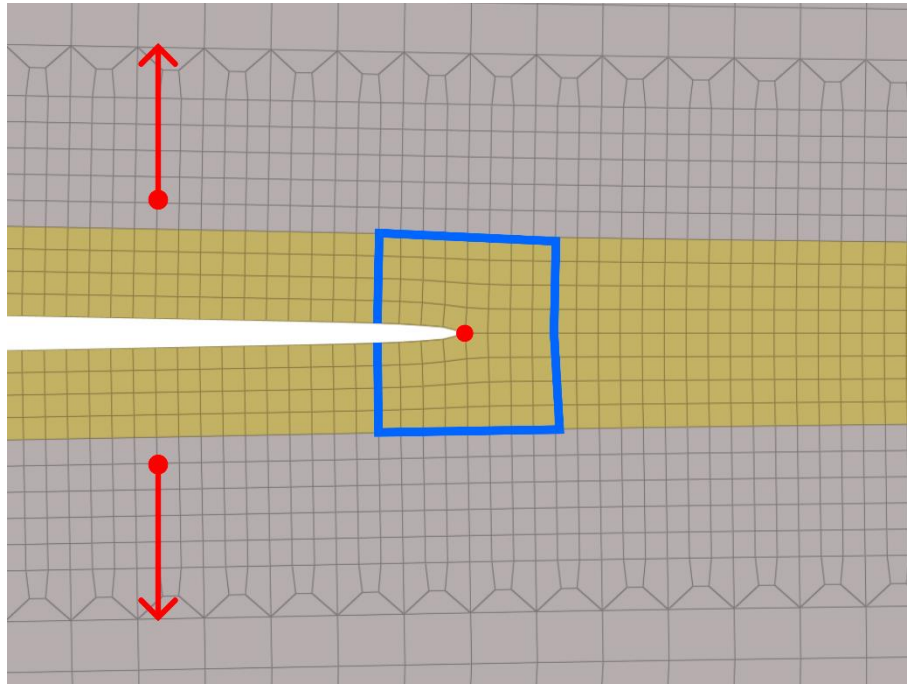


Figure 4.4: The crack tip (red dot) and contour (blue line) used to compute the cRDCB J-integral for the crack tip compensation function. All nodes at the crack tip across the width of the cRDCB were selected. Specimen deformation was exaggerated in this image by 20 times to highlight where the crack tip.

Assuming small crack tip deformation and linear-elastic material response, the J-integral was equated to the Mode I stress intensity factor, K_I per Eq. 4.1.

$$K_I = \sqrt{J_I E_{eff}} \quad \text{Eq. 4.1}$$

The effective stiffness of the FRP laminate, E_{eff} , accounted for plane stress or strain assumptions when computing K_I . While the cRDCB exhibited a mix of plane stress and strain behaviours, plane strain was assumed when computing E_{eff} to simplify analysis and provide a conservative estimate of the stress intensity factor. E_{eff} was calculated using the equations developed by Massabò et al. (2003) to account for the transversely isotropic nature of the FRP laminate in this work (Eq. 4.2). Laminate properties used here were the same as those used in the FE model (Table 4.2) to ensure that the computed stress intensity factor reflected the response of the cRDCB model. The subscript *ps* denotes the property calculated under the plane strain condition.

$$E_{eff} = \frac{E_{1,ps} \lambda^{3/4}}{n} \quad \text{Eq. 4.2}$$

$$\lambda = \frac{E_{3,ps}}{E_{1,ps}} \quad \text{Eq. 4.3}$$

$$n = \sqrt{\frac{1 + \rho}{2}} \quad \text{Eq. 4.4}$$

$$\rho = \frac{\sqrt{E_{1,ps} E_{3,ps}}}{2G_{13}} - \sqrt{\nu_{13,ps} \nu_{31,ps}} \quad \text{Eq. 4.5}$$

$$E_{1,ps} = \frac{E_1}{1 - \nu_{12} \nu_{21}} \quad \text{Eq. 4.6}$$

$$E_{3,ps} = \frac{E_3}{1 - \nu_{23} \nu_{32}} \quad \text{Eq. 4.7}$$

$$\nu_{13,ps} = \frac{\nu_{13} + \nu_{12} \nu_{23}}{1 - \nu_{12} \nu_{21}} \quad \text{Eq. 4.8}$$

$$\nu_{31,ps} = \frac{\nu_{31} + \nu_{32} \nu_{21}}{1 - \nu_{32} \nu_{23}} \quad \text{Eq. 4.9}$$

Using K_I , the energy release rate of the cRDCB specimen can be computed analytically, $G_{I,SIF}$, using the experimentally measured force-displacement response $P(\delta_m)$ (Eq. 4.10).

$$G_{I,SIF} = \frac{K_I(P(\delta_m))^2}{E_{eff}} \quad \text{Eq. 4.10}$$

Similarly, the energy release rate of the TSRs extracted from experimental data using the cRDCB analysis scheme, $G_{I,TSR}$, can be computed by integrating traction with respect to separation (Eq. 4.11).

$$G_{I,TSR} = \int_0^{\delta_m} \sigma(\delta) d\delta \quad \text{Eq. 4.11}$$

The effect of the stress concentration at the crack tip on measured TSRs was quantified by comparing the energy release rate computed with K_I to the energy release rate computed by integrating the experimental TSR. The crack tip compensation function, $\alpha(\delta_m)$, was defined as the ratio of these calculation methods with respect to measured crack tip displacement, δ_m . The crack tip compensation function (Eq. 4.12) was used to scale measured tractions to capture the effect of stress concentrations at the cRDCB crack tip.

$$\alpha(\delta_m) = \frac{G_{I,SIF}(\delta_m)}{G_{I,TSR}(\delta_m)} = \frac{\frac{K_I(P(\delta_m))^2}{E_{eff}}}{\int_0^{\delta_m} \sigma(\delta) d\delta} \quad \text{Eq. 4.12}$$

When the crack tip compensation function, $\alpha(\delta_m)$, was plotted alongside the energy release rate computed with the stress intensity factor and the energy release rate integrated from the experimental cRDCB TSR, it can be observed that $\alpha(\delta_m)$ provided near-constant positive scaling of traction prior to the initiation of delamination damage (Figure 4.5). However, as damage progressed and measured force decreased, the compensation function dropped significantly, which would attenuate tractions and greatly reduce the energy release rate of a TSR.

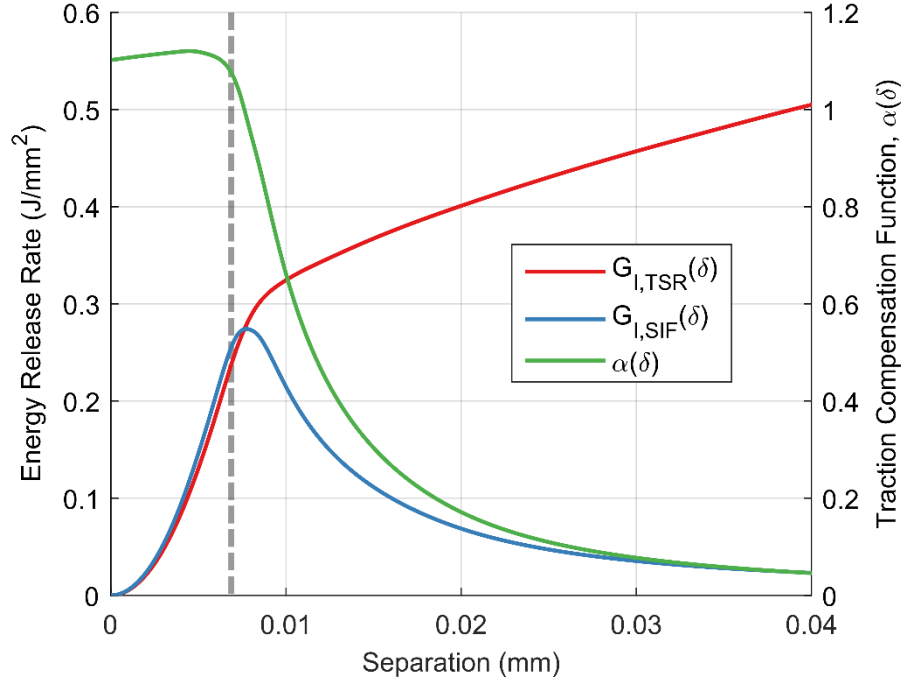


Figure 4.5: The crack tip compensation function $\alpha(\delta)$ (green line) as defined by the ratio of energy release rate computed analytically with the stress intensity factor (blue) and integrated from experimental TSRs (red). The dashed grey line indicates the approximate separation at the onset of delamination damage.

To apply the crack tip compensation function to an experimentally measured TSR, one scales the traction at each separation by the corresponding value of the crack tip compensation function. Applying the crack tip compensation function to the entire experimental TSR, referred to as the naïve approach in this study, violated the assumptions that govern the use and calculation of stress intensity factors. Stress intensity factors are based on linear elastic fracture mechanics and are only valid for small separations and fracture process zones. While these assumptions are valid during the early stages of delamination, where damage accumulates in the matrix phase of the FRP laminate, linear elastic fracture mechanics do not hold for the large separations associated with damage mechanisms such as large-scale fibre bridging. For modelling large-scale damage phenomena, researchers have suggested a separable approach whereby small-deformation assumptions are maintained when modelling matrix damage using the SIF-based approaches and large-scale fibre bridging is modelled independently with other techniques (Canal et al., 2017; Frossard et al., 2016; Manshadi et al., 2013; Massabò et al., 2003). To that end, a separable scaling approach was implemented in addition to naïve scaling of

traction. While invalid at large separations, the naïve approach was used in this work to demonstrate and quantify the importance of only applying the crack tip compensation function at small displacements, as well as to investigate how the crack tip compensation function applied to alternative geometries.

The separable scaling approach (Figure 4.6, dashed green line) only applied the crack tip compensation function to traction responses up to the displacement associated with the inflection point of the force-displacement response (gold star, Figure 3.7a and black arrow, Figure 4.6). As noted in Chapter 3, this inflection point tended to be the point at which large-scale fibre-bridging dominated over matrix-driven damage behaviour in the response of the cRDCB specimen. At displacements beyond the inflection point, the unscaled traction-separation response was used. The separable approach produced a TSR with a somewhat increased energy release rate, which physically reflected the effect of the stress concentration at the crack tip that was otherwise ignored with the existing cRDCB analysis scheme.

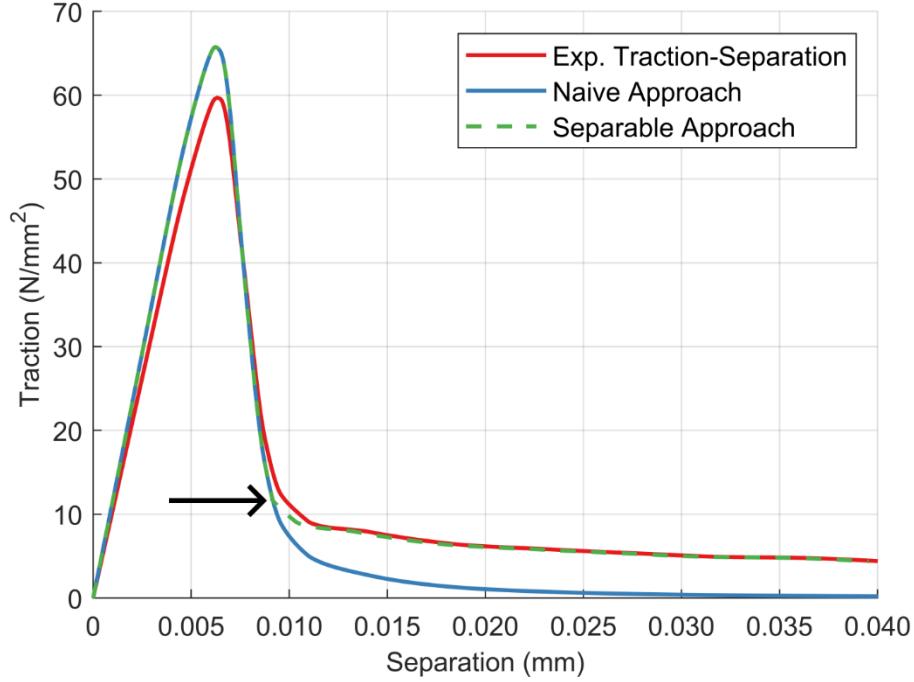


Figure 4.6: The effect of the crack tip compensation function $\alpha(\delta)$ on experimental cRDCB TSRs. The naïve scaling approach (blue) increased peak traction but attenuated large-scale damage behaviour. The separable scaling approach (dashed green) increased peak traction while maintaining as-measured tractions at large separations. The separation between the scaled and unscaled TSR is denoted by the black arrow.

4.2.2 Effective Stiffness Method to Generate TSRs with a High Initial Stiffness

Experimentally measured TSRs have an initial measured stiffness, k_m , which includes the stiffness contribution of the material adjacent to the delamination interface. However, TSRs intended for the damage-only modelling approach require a high initial stiffness, also referred to as a penalty stiffness, denoted by k_p . An analysis procedure was developed to convert experimentally measured TSRs to those suitable for damage-only modelling without altering the physical interpretation of how delamination occurs in FRP laminates.

The delamination crack tip was modelled as two springs in series (Figure 4.7). One spring, k_c , represents the cohesive stiffness contribution provided by the adjacent FRP laminate material, while the other, k_p , captures the penalty stiffness desired for the resulting delamination TSR. The effective stiffness of this system is equal to the initial measured stiffness of the TSR, k_m (Eq. 4.13).

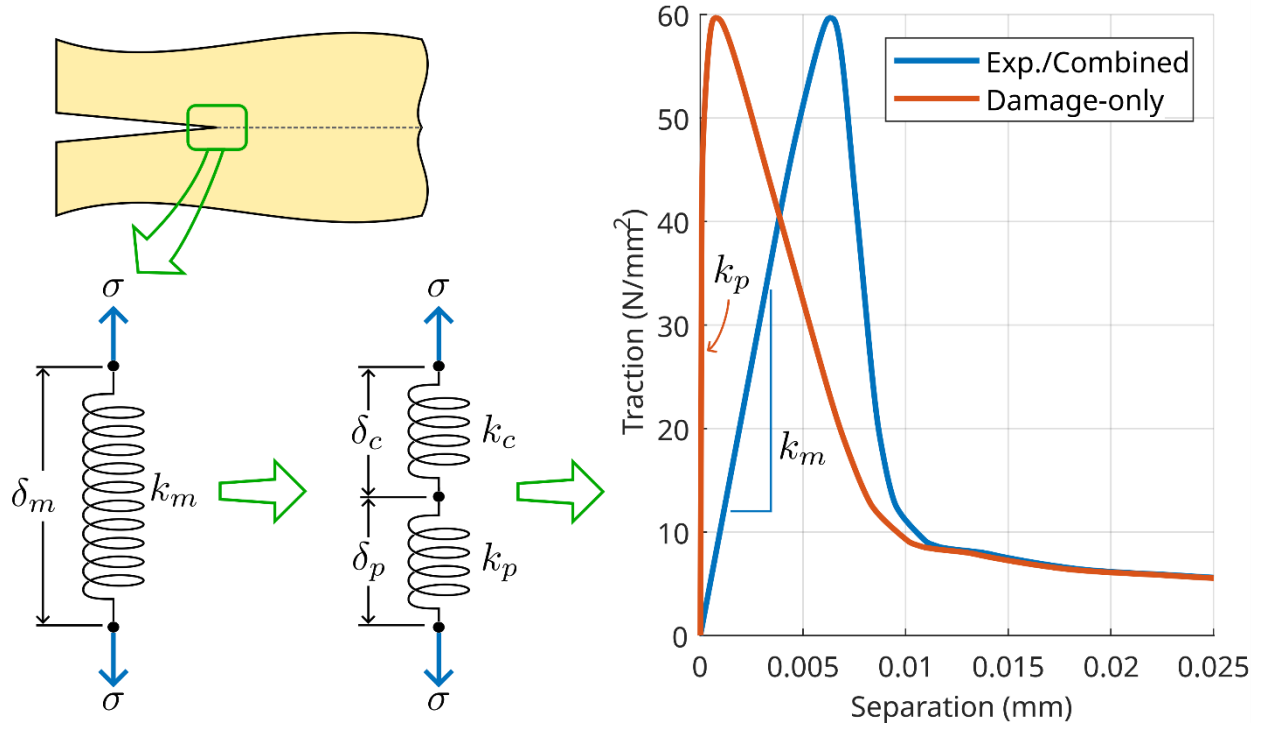


Figure 4.7: A schematic of the effective stiffness method, whereby the crack-tip is approximated as two springs in series representing the delamination interface k_p and the adjacent FRP laminate material k_c . This method converted experimentally measured TSRs suitable for the combined modelling approach into those with an initial, user-defined penalty stiffness suitable for damage-only modelling.

$$k_m^{-1} = k_c^{-1} + k_p^{-1} \quad \text{Eq. 4.13}$$

The effective stiffness of the two springs, k_m , can be computed from the initial linear portion of experimental TSRs, while the penalty stiffness, k_p , is defined by the user. As such, the only unknown is the stiffness contribution of the FRP laminate material, k_c , which can be isolated per Eq. 4.14.

$$k_c = (k_m^{-1} - k_p^{-1})^{-1} \quad \text{Eq. 4.14}$$

Assuming an elastic intralaminar behaviour, the stiffness contribution of the surrounding material can be subtracted from the measured separations to produce the separations necessary for a damage-only TSR, δ_d (Eq. 4.15). The final TSR, $\sigma(\delta_d)$, will have an initial stiffness k_p .

$$\delta_d = \delta_m - \frac{\sigma(\delta_m)}{k_c} \quad \text{Eq. 4.15}$$

The effective stiffness approach had several useful features. First, following the springs-in-series assumption, traction magnitudes were not altered, only separations. Second, the approach could be applied to TSRs without assuming a simplified analytical form (such as bilinear or trapezoidal shapes). Finally, and most importantly, the methodology was energy conserving. The original measured traction-separation response had the same SERR as the traction-separation response extracted for the delamination interface alone.

It is important to reiterate that the effective stiffness method does assume that any inelastic behaviour is caused by delamination rather than intralaminar damage in the laminate material. However, this was an assumption shared by most data reduction schemes for the DCB specimen (ASTM International, 2013; De Gracia et al., 2015; Xavier et al., 2014a). This assumption was further supported by the full field strain measurements provided by the cRDCB specimen, which did not reveal any damage outside of the delamination interface.

4.2.3 Traction-Separation Responses Developed using Analysis Procedures

The two analysis procedures developed in this work were used to generate three TSRs suitable for damage-only modelling in the cRDCB specimen (Figure 4.8). The multiple TSRs were generated to demonstrate how the analysis procedures can be combined or used independently.

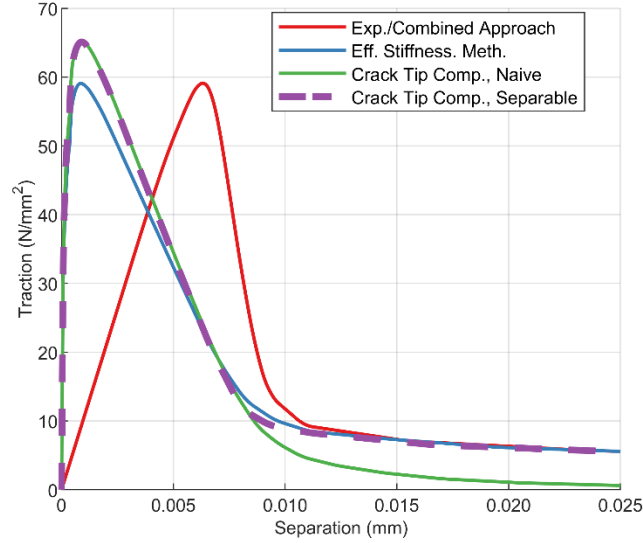


Figure 4.8: The three TSRs developed for the damage-only modelling approach by applying combinations of the analysis procedures developed here. The experimental average/combined elastic-damage TSR (red) was used as a basis for analysis.

The first TSR (blue, Figure 4.8) was created by applying only the effective stiffness technique to convert experimental TSRs to one with an initial stiffness $5(10^5)$ N/mm³. The initial stiffness value was identified after some experimentation to minimize the introduction of undesired compliance into the cRDCB model without causing unphysical behaviour or numerical instabilities resulting from excessively high stiffness. The effective stiffness TSR was created to demonstrate why the crack tip compensation function was required.

The second and third TSRs both applied the crack tip compensation function. A stress intensity factor of 0.3398, as measured from the cRDCB FE model, was used as an input to the compensation function, which positively scaled peak traction by 12%. The second TSR (green, Figure 4.8) corresponded to the naïve crack tip compensation function, wherein the assumption of small deformation that underly the stress intensity factor was not adherend to at large separations. To that end, tractions at separations beyond 0.0075 mm were attenuated significantly, reducing the overall energy release rate of the TSR. The third TSR (purple dashed, Figure 4.8) was computed using the separable crack tip compensation function, which increased the peak traction of the TSR without attenuating tractions at large separations.

4.3 Results

Results are divided into four sections. First, the results of the sensitivity analysis of the cRDCB specimen to FRP laminate thickness are presented, focusing on the distribution of stresses around the crack tip. Second, experimental TSRs were verified using the combined elastic-damage cRDCB FE model. This study demonstrates the necessity of the two-stage analysis procedure developed in this work. Third, the efficacy of the two-stage analysis procedure was investigated. Finally, the transferability of TSRs measured with the cRDCB specimen to the DCB geometry was explored.

4.3.1 Sensitivity of cRDCB Specimens to Laminate Thickness

As noted in Chapter 3, early experimental trials to develop the cRDCB specimen explored using two- and four-ply unidirectional FRP laminates. During testing, all two-ply laminates experienced interface failure between the adherend and the laminate. Further, DIC imagery of the four-ply cRDCB specimens showed high strain concentrations at the adherend-laminate interface (Figure 3.6). A study of five laminate thicknesses was carried out using the purely elastic cRDCB FE model (Section 4.1.2). The five thicknesses reflected the dimensions of FRP laminates two, four, six, eight, and ten plies thick. The same ply thickness was used throughout the study to ensure all models accurately reflected a constant fibre volume fraction. As the number of plies increased, both the peak stress occurring in the FRP laminate and the stiffness of the specimen decreased. The decrease was large between two and four plies (Figure 4.9) but smaller beyond six plies. Peak stress always occurred near the crack tip, at the exterior faces of the specimen.

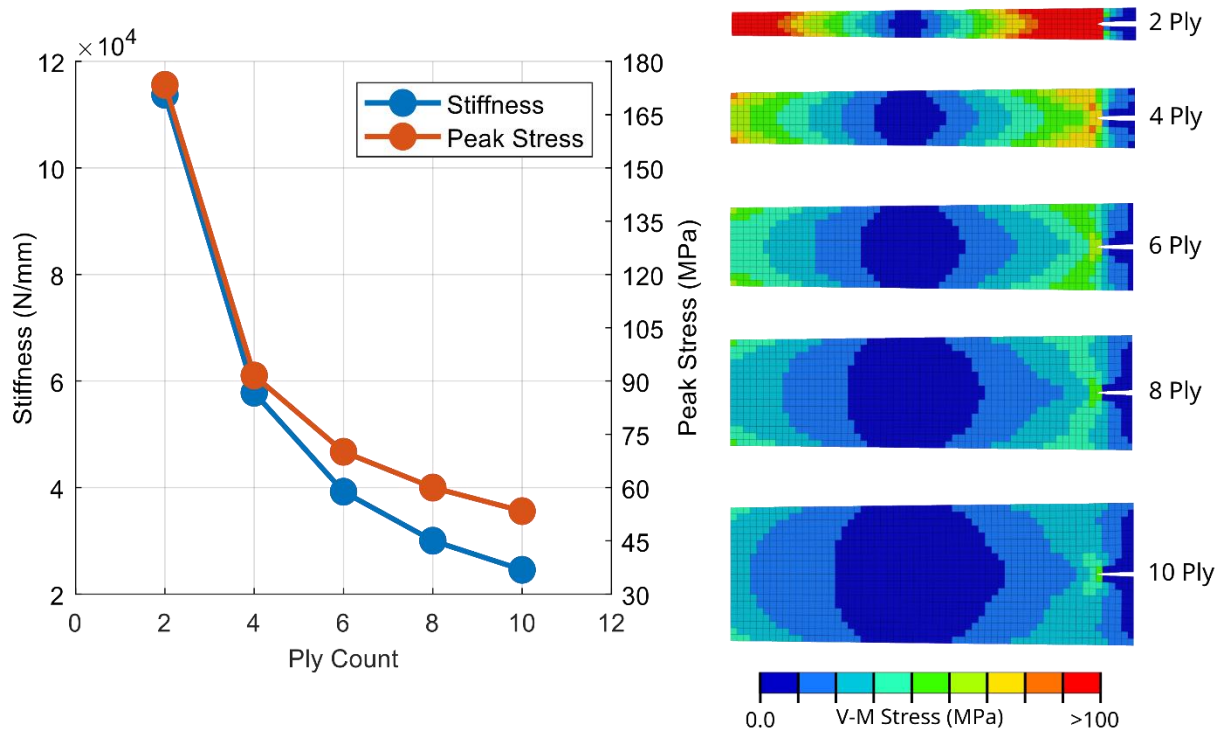


Figure 4.9: Peak stress and specimen stiffness of the cRDCB model for increasing ply counts and, hence, laminate thickness (left). As the number of plies increased, peak stress decreased, and stress contours about the crack tip (right) became less intense. Only the laminate material of the FE model is shown for the stress contours on the right.

As the number of plies increased, stress at the adherend-laminate interface also decreased. The decrease trended similarly to stiffness and peak stress, with a significant reduction between the two and four-ply models and less significant reductions between six to ten plies, likely owing to the reduced constraining effect of the adjacent plies. While not directly applicable to the remainder of this chapter, these results demonstrate the importance of laminate thickness on cRDCB behaviour for future application of the specimen to other problems and material systems.

4.3.2 Verification of Experimental TSRs using a Combined Elastic-Damage Approach

The experimental average TSR extracted from cRDCB specimens was verified using the combined elastic-damage FE model to verify the assumptions embedded in the analysis method presented in Chapter 3. The average experimental TSR, which included delamination damage and the stiffness contribution of the adjacent FRP laminate material, enabled the FE model to

reproduce the average experimental force-displacement response (Figure 4.10). Figure 4.10 also demonstrates the effect of using the same experimental TSR in the damage-only cRDCB FE model, wherein cohesive elements were intended to solely capture delamination behaviour and the stiffness of the FRP laminate was captured with traditional continuum solid elements. The damage-only cRDCB model (blue curve) had a much lower stiffness and lower peak force than the combined elastic-damage model for the same TSR. The reduced stiffness and peak force demonstrated the need for TSRs with high initial stiffness for damage-only modelling of delamination and demonstrated the impetus for the analysis procedures developed in this work.

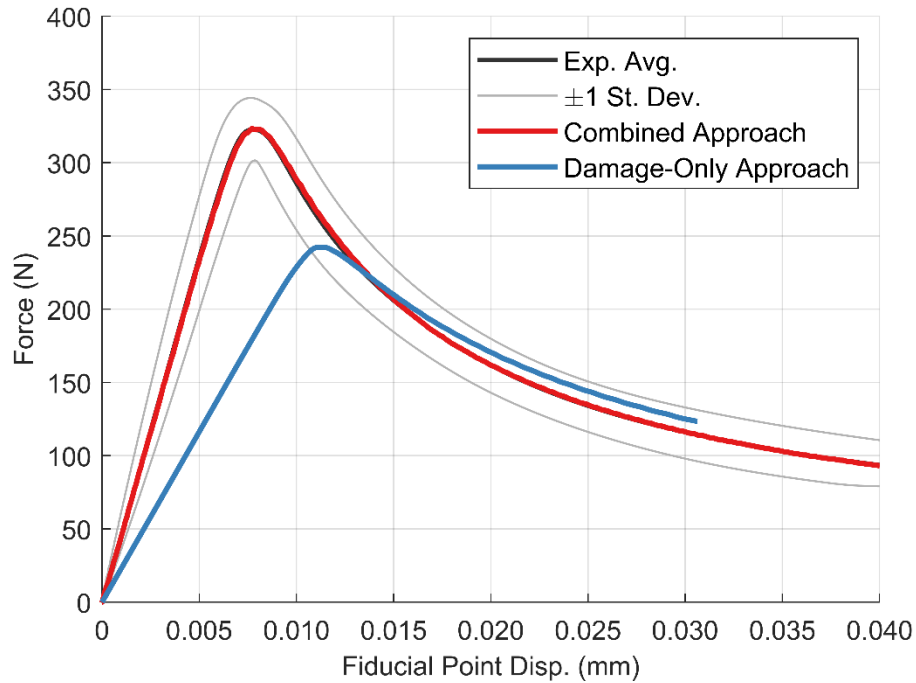


Figure 4.10: Force-displacement behaviour of the combined elastic-damage (red) and damage-only (blue) cRDCB models when using the experimental average cRDCB TSR. The average cRDCB force-displacement response with ± 1 standard deviation statistical bounds are included to assess model performance.

4.3.3 Verification of Analysis Procedures for Damage-Only Modelling of Delamination

As noted in Section 4.2.3, the analysis procedures developed in this work were used to create three TSRs with a high initial stiffness:

- One TSR using solely the effective stiffness method to directly convert the average experimental TSR to one with a high initial stiffness,

- One applying the naïve crack tip compensation function in addition to the effective stiffness method, increasing peak traction and attenuating tractions at large separations, and
- One applying the separable crack tip compensation function in addition to the effective stiffness method, wherein peak traction is positively scaled, but tractions at large separations are not altered.

Using the effective stiffness method TSR as input to the damage-only cRDCB FE model resulted in an underprediction of experimental cRDCB peak force, falling outside the ± 1 standard deviation statistical bounds (Figure 4.11). However, the use of this TSR did produce good predictions of larger-scale damage phenomena, indicating that the bridging response of TSRs was being captured well. Overall, the fact that the FE model trended well against experimental data verified that the effective stiffness method enabled damage-only modelling of delamination without introducing artificial compliance or changing the overall intended response of the FE model.

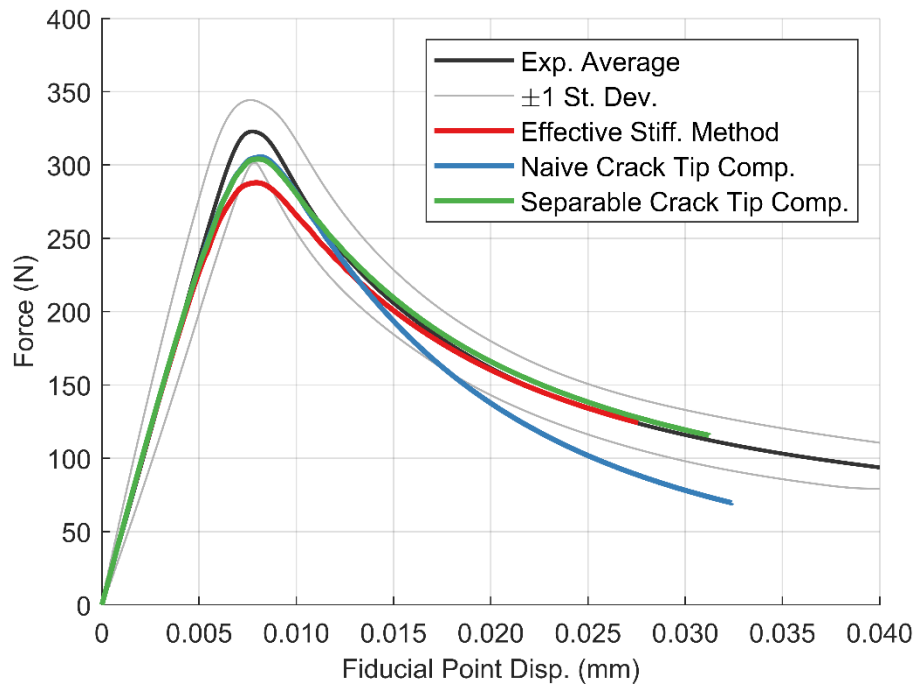


Figure 4.11: The response of the damage-only cRDCB FE model using the three damage-only TSRs developed for the damage-only modelling approaches compared against the average experimental force-displacement response and variance.

Using both the naïve and separable crack tip compensation function to scale TSRs for the damage-only cRDCB model improved the prediction of peak force to within one standard deviation of the average peak force, although the absolute peak force was underpredicted. However, the difference between the naïve and separable compensation functions became apparent at larger displacements, where the TSR processed with the naïve compensation function significantly underpredicted force, falling well below experimental variability. Using the TSR processed with the separable crack tip compensation function proved to be the best of both worlds, increasing estimates of peak force and predicting force at large separations well.

4.3.4 DCB Modelling to Assess the Transferability of cRDCB TSRs

The three TSRs developed for damage-only modelling of delamination with cohesive elements were used as inputs to the DCB FE model (which also utilized the damage-only modelling scheme). The TSR processed using the solely effective stiffness method and the TSR with the separable crack-tip compensation function resulted in a DCB model that did not follow experimental data. Force was significantly overpredicted (Figure 4.12), as the initial period of non-linearity seen in the experiment between 12-17 mm of displacement responded far too stiffly in the FE model. Using the TSR computed using the naïve compensation function also predicted experimental behaviour poorly, as the FE model did not capture the rising trend of the experimental force-displacement response after the initial onset of damage in the specimen. All DCB FE models showed a change in stiffness that occurred at the same displacement as seen in the experiments around 12 mm displacement, indicating that the initial onset of damage was being captured correctly. The increased peak traction produced by the crack tip compensation function did not significantly alter the displacement at which this change in slope occurred.

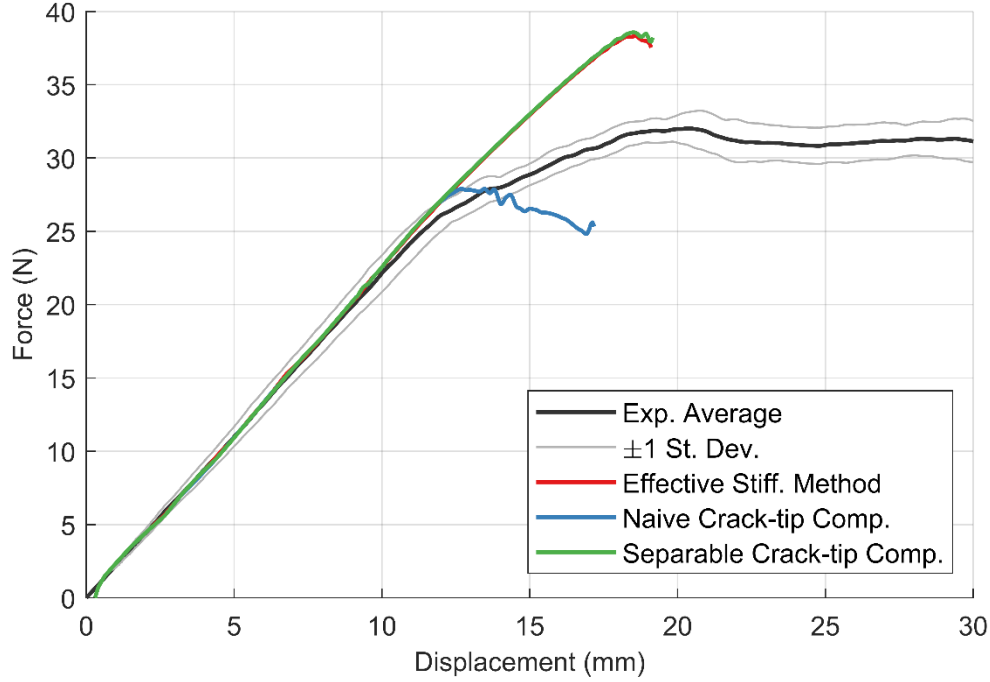


Figure 4.12: Force-displacement response of the DCB model using three different TSRs derived from experimental cRDCB measurements. The average experimental DCB response and ± 1 standard deviation statistical bounds are plotted for comparison.

The naïve compensation function, owing to the diminished bridging response, exhibited the same change in response around 12 mm of specimen displacement, but crack extension commenced almost immediately. Instead of the rising trend observed in the experimental force-displacement response, the naïve scaling approach exhibited a decreasing trend in force as specimen displacement increased and the delamination crack extended.

4.4 Discussion

4.4.1 Effect of Laminate Thickness on cRDCB Response

A clear trend of decreasing stiffness and peak stress was observed as the number of plies used in the cRDCB specimen increased (Figure 4.9), assuming a consistent fibre-volume fraction between configurations. The large change in stiffness and peak stress between two and four plies was likely driven by the reduced constraining effect of the metallic adherend. The constraining effect would also explain why the peak stress of the two-ply model predicted peak stress at the interface between laminate and adherend rather than the crack tip. The prediction of peak stress at the interface for two-ply cRDCB specimens also explained why experimental two-ply cRDCB

specimens explored during the development of the cRDCB specimen (Section 3.3.1) tended to exhibit interface failure rather than delamination.

Beyond four plies, the stiffness and peak stress in the cRDCB model decreased more slowly as the constraining effect diminished and the bulk properties of the laminate dominated. However, while the diminished stresses would make interface failure less likely, the reduced stiffness would compromise the rigidity assumption of the cRDCB specimen, which assumed a direct load path between the adherend and delamination interface. As such, the four-ply specimen appeared to be the best compromise between consistent delamination in specimens while maintaining the most accurate reflection of the assumptions governing the extraction of TSRs from the cRDCB specimen.

4.4.2 Verification of Experimental TSRs using a Combined Elastic-Damage Approach

The purpose of this set of models was to demonstrate that the combined elastic-damage modelling approach encapsulated the analysis scheme used to extract TSRs from experimental data. As can be seen in Figure 4.10, the experimental average TSR reproduced the experimental average force-displacement response. The exact reproduction of the force-displacement response verified that the analysis scheme used to extract TSRs from experimental data functioned correctly. Furthermore, using the experimental TSR with the damage-only cRDCB FE model demonstrated the need for TSRs with an initial high penalty stiffness. As the stiffness of the FRP laminate material in the damage-only cRDCB model was accounted for twice (once with the continuum elements and again with the experimental TSR), the overall stiffness of the model was lower, affecting how delamination response was modelled. The need for TSRs with a high initial stiffness was a key motivation for developing the two analysis procedures documented in this work.

4.4.3 Verification of the Analysis Procedures for Damage-Only Modelling of Delamination

A TSR processed solely with the effective stiffness method to produce a high initial stiffness with the damage-only FE model demonstrated two items (Figure 4.11). First, it demonstrated that a penalty stiffness of $5(10^5)$ N/mm³ was sufficient to avoid artificial compliance that would otherwise impact the accuracy of the model. Second, the ability of the cRDCB model to match the trend of the experimental average, even if peak force was underpredicted, demonstrated that

the changes in shape made to the TSR during the effective stiffness method did not change the physics of the situation. As the TSR did not change the force-displacement trend, it was safe to assume that the only damage processes occurring in the cRDCB specimen, at least in this work, were solely due to Mode I delamination.

The underprediction of peak force by the effective stiffness TSR can be attributed to delamination behaviour in the cRDCB specimen that was not accounted for when TSRs were extracted from experimental data, specifically the complex state of stress at the delamination crack tip. The analysis techniques of Watson et al. (2018) and in Chapter 3 did not model the stress concentration at the crack tip but homogenized the response of all material between the two adherends. As such, stress concentrations at the crack tip were effectively smeared out over a large volume of material. When using the combined elastic-damage modelling approach, the cohesive elements represent such a large material volume, so the stress concentration effect at the crack tip was captured. However, when performing detailed modelling with a damage-only modelling approach, the cohesive elements represented a vanishingly small volume of material, and the effect of the stress concentration must be captured, in part, with the continuum elements representing the material adjacent to the delamination interface. To that end, the damage-only TSR needed to be modified in some way to increase tractions at the delamination interface to enable a stress concentration reflective of the experiment to form at the crack tip, which drove the development of the crack tip compensation function.

Naively applying the crack tip compensation function to experimental TSRs scaled peak traction sufficiently for the cRDCB FE model to predict peak force within experimental variability but attenuated tractions at large separations such that the FE model underpredicted forces at large displacements. The attenuation highlighted the need to treat small-scale damage, where a stress intensity factor approach can be applied validly, from large-scale damage behaviours, where the large deformation and inelastic material response require a different approach. The use of the separable scaling approach, inspired by several approaches reported in the literature (Canal et al., 2017; Massabò et al., 2003; Pappas and Botsis, 2020), enabled the cRDCB model to predict peak force well without compromising the ability of the model to predict force at large displacements.

It should be noted that the crack tip compensation function did not allow the FE model to exactly match the average peak force of the experiment, with FE models predicting a 5% lower peak force. The underprediction of peak force likely arose from the assumptions used when calculating the stress intensity factor for the cRDCB specimen. While the J-integral computed from the FE model captured the mix of plane strain and plane stress conditions at the crack tip, the conversion from J-integral to stress intensity factor was not as sophisticated, with a plane strain assumption being used. The assumption of plane strain behaviour resulted in a conservative estimate of the stress intensity factor and an underprediction of peak force. A possible extension of the crack tip compensation function would be a more accurate method of calculating the stress intensity factor of a cRDCB specimen. However, the approach used in this study resulted in conservative predictions of peak force falling within the statistical variation of the experiment.

4.4.4 DCB Modelling to Assess the Transferability of cRDCB TSRs

It is clear from the force-displacement responses of the DCB FE models (Figure 4.12) that the TSRs measured with the cRDCB specimen are not entirely transferable between geometries. However, it is interesting to note how such differences in the inputted TSRs affect the response of the DCB model. When comparing the effective stiffness TSR to the TSR processed with the crack tip compensation function and the separable approach, one will note a nearly negligible change in peak force in the DCB model despite a 12% increase in peak traction. This result supported previous work that suggested the DCB specimen is insensitive to peak traction (Lu et al., 2019). Nevertheless, all three of the DCB models that used a cRDCB-based TSR exhibited a change in stiffness around 12 mm of displacement, which indicated the timing of the onset of damage was being captured correctly, even if the damage response was not.

Two observations can be drawn about the transferability of traction-separation responses measured with cRDCB specimens to other geometries, especially the DCB specimen. Most obviously, the large-scale bridging response of the two geometries differed quite substantially. As the response of the DCB specimen is largely dominated by large-scale fibre bridging (Compston and Jar, 1999; Lu et al., 2019), the effect was very noticeable in this work. Nevertheless, it is important to reiterate that exaction of complete TSRs from DCB specimens remains limited. While techniques to assess TSRs based on the J-integral have been used, these

TSRs often lack a detailed description of the early matrix-dominated phase of Mode I delamination, owing to the lack of sensitivity to this behaviour in the DCB specimen (Lu et al., 2019). Additionally, knowledge of intralaminar material properties (Gunderson et al., 2007) or specialized loading conditions (Sørensen and Jacobsen, 2003) are required to implement J-integral approaches for the DCB specimen.

Prior work has demonstrated that bridging behaviour is not simply a foundational material property but is geometry dependent. In particular, the thickness of a DCB specimen has a dramatic effect on fibre bridging, with thicker specimens contributing to increased bridging and generally higher toughness at large crack extensions (Agrawal and Ben Jar, 2003; Farmand-Ashtiani et al., 2015; Frossard et al., 2016; Pappas and Botsis, 2020). To that end, it was somewhat unsurprising that the large-scale damage response of the cRDCB specimen differed from that of the DCB specimen, given that the metallic adherends provided much different boundary conditions on the delamination interface and, consequently, produced a fibre bridging response that differed from DCB specimens.

Analysis by Pappas and Botsis (2020) investigated using local curvature at the crack tip to augment cohesive zone models toward geometry-independent traction-separation responses. They correlated increased bridging tractions with increased DCB specimen thickness to decreased local curvature at the crack tip. This trend could explain why cRDCB TSRs used in the DCB model yielded a significantly stiffer damage response compared to experiments, as metallic adherends of the cRDCB specimen would have loaded the delamination interface more directly than the DCB specimens, decreasing local curvature and increasing bridging traction at lower separations. Hypothetically, the TSRs extracted from the cRDCB specimens in this work might have shown better agreement with thicker, stiffer DCB specimens.

The second observation that can be drawn regarding the transferability of measured TSRs is that the small-scale, matrix-dominated response of the laminate was specimen-independent. The TSRs extracted from the cRDCB specimen correctly predicted the onset of damage in the DCB specimen, as denoted by the change in slope that occurred in the force-displacement response of the DCB FE model at 12 mm of displacement. While the effect was somewhat subtle in the case of the effective stiffness TSR and separable analysis TSR, this conclusion was supported by the elemental tractions that developed in the model. The change of slope and

progression of element tractions showed that the TSRs could be transferable between geometries depending on the intent of the modelling being performed. In situations where extensive damage is not expected, and the onset of delamination is of greater importance than extensive crack propagation, TSRs from the cRDCB specimen, with the analysis scheme presented in this work, could be used reliably.

The sensitivity of the cRDCB specimen also presented the opportunity to study better the thickness dependence of specimen thickness than contemporary DCB specimens. When studying the thickness effect of bridging with FRP laminates, increasing the thickness of the DCB specimen impacts the compliance of the specimen, and the stress states that develop at the crack tip. The rigidity provided by the metallic adherends of the cRDCB specimen would eliminate much of the coupled compliance effect and likely provide more consistent loading conditions across different laminate thicknesses. A possible extension of this work would be to utilize the cRDCB geometry to study the thickness effect of large-scale fibre bridging to work towards specimen-independent methods of characterizing and modelling large-scale damage responses. A modified cRDCB specimen design may be required to provide a longer interface length to accommodate bridging behaviour without impacting crack growth stability.

4.5 Conclusions

Experimentally measured traction-separation responses (TSRs) from composite rigid double cantilever beam (cRDCB) and other Mode I delamination specimens capture damage behaviour along the delamination interface as well as the stiffness contribution of the adjacent laminate material. In this chapter, two analysis procedures were presented to enhance the TSRs measured with the cRDCB specimen by providing 1) a systematic and energy-conserving approach to convert experimentally measured TSRs to those with an initial penalty stiffness suitable for damage-only modelling and 2) a fracture mechanics-based approach to account for the non-uniform stress concentrations ahead of the crack tip by scaling of the measured traction response.

The two analysis procedures were verified using FE modelling of the cRDCB specimen against experimental data. The stiffness method portion of the analysis procedure effectively isolated the delamination damage component of experimentally measured TSRs. Further, it was shown that the crack tip compensation function, based on the stress intensity factor, could

account for non-uniform stress concentrations at the crack tip and predict the peak force of the experimental cRDCB specimen within experimental variation. Validation of these techniques and the TSRs extracted from cRDCB specimens against experimental DCB data provided some interesting findings related to the transferability of measured properties across geometries. While the DCB and cRDCB specimens exhibited very different large-scale damage responses, which limited the overall transferability of properties, it was shown that small-scale damage behaviour, driven primarily by the matrix response of the material, appeared to be transferable. As such, cRDCB TSRs could be used to model delamination for cases where damage initiation and onset are of greater concern than catastrophic crack propagation.

Chapter 5

DEVELOPMENT AND TESTING OF SPECIMENS TO ASSESS MODE II AND III

DELAMINATION RESPONSE

A key goal of this thesis was the development of a complimentary specimen to extract traction-separation responses (TSR) from shear delamination. Shear delamination encompasses both Mode II and Mode III loading, which refer to in-plane deformation parallel and perpendicular to the crack front, respectively. The two modes of shear delamination stand in contrast to Mode I delamination, which can be uniquely defined as loading in a direction normal to the crack front. The fact that shear delamination encompasses both Mode II and III loading can introduce ambiguity and present a challenge to isolate specific shear modes in complex structures.

Research into shear delamination has been limited by technical difficulties in developing appropriate test specimens (O'Brien, 1998b) and the less common occurrence of critical shear delamination failures in real-world applications (O'Brien, 1998a). Many attempts to characterize shear delamination have relied on compliant-beam-style specimens and were largely limited to assessing fracture toughness in the form of energy release rate. Further, standardization of test methodologies for shear delamination was only accomplished relatively recently, with the Mode II end-notched flexure (ENF) specimen first adopted in 2019 by ASTM (ASTM International, 2019) despite several decades of development (O'Brien, 1998b). However, it is important to note that very few geometries or analysis techniques reported in the literature can measure the TSR of shear delamination, which is necessary for contemporary numerical modelling methods such as cohesive zone modelling (CZM). Additionally, a specimen for Mode III delamination has never been accepted by a standards organization.

Two prospective specimen geometries, the Mode II composite rigid shear (cRS) and the composite rigid Mode III (cR3), were investigated to measure the TSR of shear delamination. Both specimens used rigid metallic adherends co-cured to a FRP laminate to produce a low-compliance test geometry. The first specimen discussed in this chapter, the Mode II cRS specimen, provided Mode II shear loading (in-plane shear displacement parallel to the crack

front) while taking advantage of the same adherends and fabrication processes as the existing Mode I cRDCB specimen. The second shear specimen, the composite rigid Mode III (cR3) specimen, applied a scissoring Mode III deformation to a delamination interface (in-plane shear displacement perpendicular to the crack front). In addition, Mode II ENF specimens were tested to provide a comparison for the new specimen geometries.

5.1 Specimen Fabrication, Testing, and Analysis Methodology

The prospective shear specimens, the Mode II cRS and the Mode III cR3, followed a similar design philosophy, fabrication methodology, and experimental procedures to the Mode I cRDCB specimen, with specimen-specific analysis procedures. Nevertheless, both specimens were fabricated using a compression moulding process where the FRP laminate was co-cured between two rigid steel adherends. The same E-glass/epoxy prepreg tape material system (UE400/REM, CIT, Italy) used with the Mode I cRDCB specimen was used for all Mode II and III specimens. The resin-rich and tacky nature of this prepreg system helped to simplify specimen layup and enabled the effective co-curing process used for the prospective specimens. The specimens were co-cured to eliminate the need for additional adhesives that might impact the rigidity of the specimen.

5.1.1 Mode II cRS Specimen Fabrication, Testing, and Analysis

The cRS specimen comprised an FRP laminate sandwiched between two steel adherends (Figure 5.1a). In many respects, the cRS specimen resembled the Mode I cRDCB specimen presented in Chapter 3, as the same metallic adherends were used in both specimen designs. However, the cRS had two key differences in terms of design and testing.

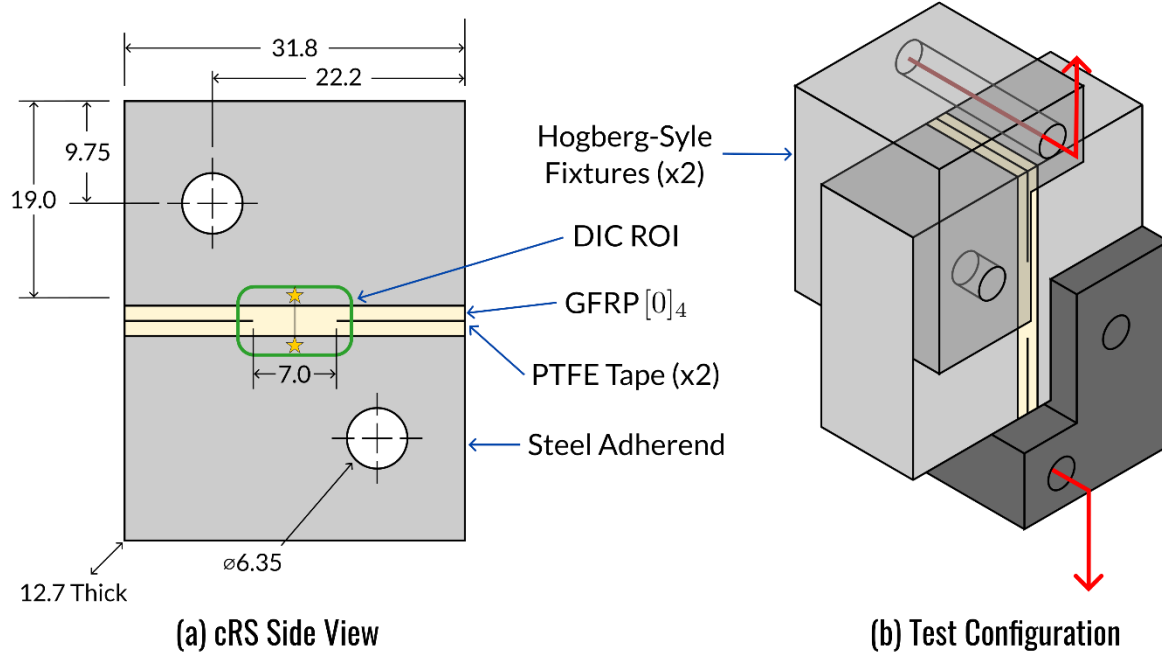


Figure 5.1: (a) Dimensioned schematic of the cRS specimen accompanied by (b) a schematic of the cRS specimen in testing configuration with specialized Hogberg-style fixtures. The stars in (a) indicate where adherend displacement was assessed.

First, to provide shear loading across the delamination interface, the load holes of the adherends were placed on opposite sides of the interface. The positioning of the holes allowed for shear loading to be generated by testing the specimen in tension. Second, the delamination interface was centred between the load holes of the cRS specimen to balance loads and moments applied to the specimen and to provide the greatest opportunity for symmetric shear loading on the interface. A delamination interface length of 7.0 mm was selected to maintain the same interface dimensions as the Mode I cRDCB specimen. A four-ply unidirectional laminate was selected for the cRS specimen for the same reasons of compatibility.

The cRS specimens were fabricated using the same curing fixturing and basic steps as the Mode I cRDCB specimen presented in Section 3.1.1. Steel adherends were first prepared by roughing the interfacing surfaces to a minimum roughness of $2.0 R_a$ using abrasive blasting media to promote a good connection between the adherend and laminate. The prepreg tapes were cut to size and stacked into two-ply sub-laminates before being pressed onto the interface surface of the adherend. The prepreg tapes were trimmed flush to the adherend, and two $12.5 \mu\text{m}$ PTFE sheets were used to define the size of the delamination interface, as well as to act as

crack starters. The two adherends were then pressed together, orienting the adherends such that the load holes were on opposite sides of the delamination interface. The prepared specimens were then placed in a spring-loaded curing fixture to provide constant curing pressure and confinement of the laminate during the curing process. The cRS specimen used the same curing fixture as the Mode I cRDCB specimen, allowing the development of the cRS specimen to take advantage of earlier work to provide quality, consistent laminates in cRDCB specimens. cRS specimens were cured at 140°C for 90 minutes under 0.5 MPa of pressure to maintain consistency with all other laminates fabricated in this thesis. The fabrication process for the cRS specimen was nearly identical to that depicted in Figure 3.2, with only the orientation of the adherends and the placement of the PTFE tapes being different.

Following the curing cycle, cRS specimens were removed from the curing fixture and any resin spew was removed with abrasive paper. The cRS specimens had a laminate thickness of 1.06 mm (SD = 0.05 mm) as measured with an optical digital microscope (VHX-5000, Keyence, Japan) and a fibre volume fraction of 47% as measured by acid digestion (ASTM International, 2015).

The offset load holes in the cRS specimen allowed Mode II shear to be introduced to the delamination interface by loading the specimen in tension. However, the offset also produced a moment that would cause the specimens to rotate, as pin-loaded connections cannot resist rotation. Specialized test fixtures inspired by the work of Högberg et al. (2007) were developed to prevent rotation (Figure 3.1b). These Hogberg-style fixtures were L-shaped, with one side of the L connecting to the cRS specimen using the load hole in the adherend and the other end of the L interfacing with the test frame. The Hogberg-style fixtures prevented rotation of the delamination interface during testing and ensured a direct load path through the delamination interface.

The cRS specimens were loaded using a 50kN servo-electric test frame (AGX-50kN, Shimadzu, Japan) at a constant crosshead displacement rate of 0.01 mm/s. Two-dimensional digital image correlation (DIC) (VIC-2D, Correlated Solutions, USA) was used to measure the relative shear displacement of the cRS adherends and qualitatively assess the onset and progression of delamination (processing parameters in Table 5.1). A single 12 MP monochromatic USB camera (Grasshopper 3 GS3-U3-123S6M, FLIR, USA) with a 180 mm macro

lens (Sigma APO MACRO 180 mm F2.8 EX, Sigma Corporation, Japan) was used to capture images at two frames per second. An airbrush was used to apply a fine speckle pattern suitable for the high-resolution imagery used in this work. Additionally, a small electric fan was used to eliminate any heat haze that would occur from the lighting setup and produce noise in the displacement measurements. DIC processing had a virtual strain gauge size of 0.29 mm (Jones and Iadicola, 2018). In a limited number of trials, a high-speed camera (FASTCAM SA-Z, Photron, USA) was used to observe crack growth at framerates up to 50 000 frames per second.

Table 5.1: DIC parameters used for the Mode II cRS specimen

DIC Parameter	Value
Image Resolution	0.0043 mm/pixel
Subset Size	25 x 25 pixels
Step Size	7 pixels
Strain Filter Size	7x7 subsets
Virtual Strain Gauge Size	0.29 mm

Extraction of Mode II TSRs from the cRS was straightforward. It was assumed that a uniform Mode II traction was generated across the delamination interface such that traction could be assessed directly per Eq. 5.1, where P was measured force with respect to relative adherend shear separation, δ_s , and A_{delam} was the area of the delamination interface being loaded. Shear separation, δ_s , was defined as the relative shear displacement of the cRS adherends.

$$\tau_{cRS}(\delta_s) = \frac{P(\delta_s)}{A_{delam}} \quad \text{Eq. 5.1}$$

5.1.2 Mode III cR3 Specimen Fabrication, Testing, and Analysis

The Mode III cR3 specimen comprised two metallic adherends co-cured to an FRP laminate. However, in contrast to the Mode I cRDCB and Mode II cRS specimens, the cR3 specimen required a different design approach to provide a Mode III or scissoring shear to the delamination interface. Whereas specimens presented to date stacked adherends and FRP laminate within the test plane, the cRS specimen was stacked “out-of-plane.” As a result, the adherends of the cR3 specimen overlapped (Figure 5.2b), such that the rear adherend was partly

covered by the front adherend when looking at the specimen from the front (as a camera imaging the test would). The FRP laminate was positioned between the two adherends and was visible from the front of the specimen.

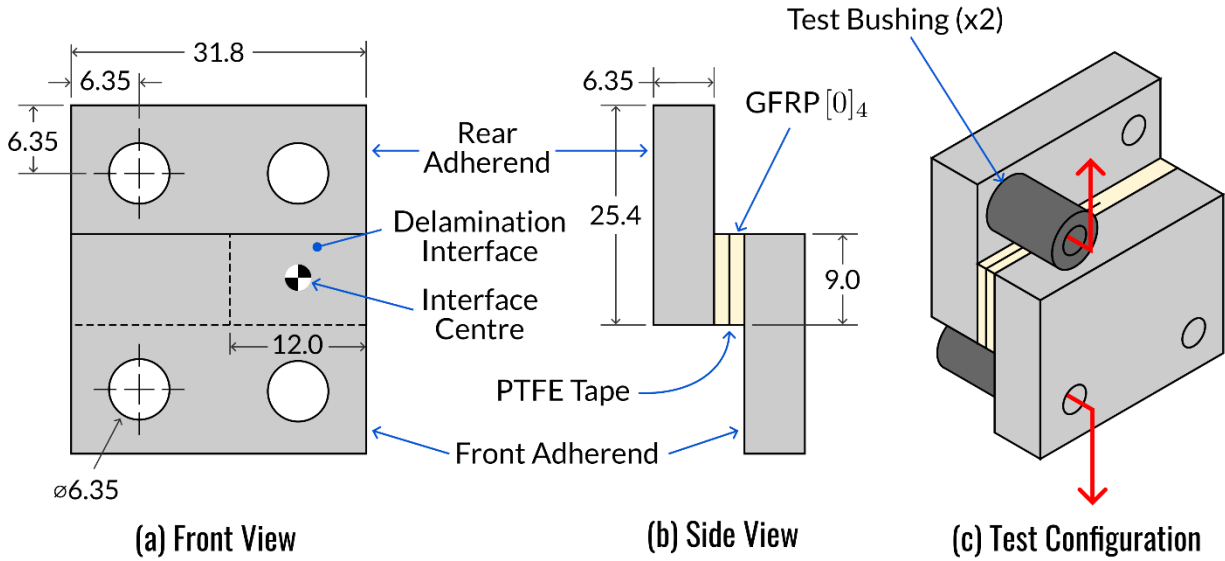


Figure 5.2: (a) Dimensioned (a) front view and (b) side view schematic of the Mode III cR3 specimen. (c) An isometric depiction of the cR3 specimen showing the test bushing used to minimize out-of-plane rotation and motion during testing. The additional holes in the cR3 adherend are included in this schematic but are not used during testing.

The primary motivation behind designing the Mode III cR3 specimen was to develop a geometry that progressively loaded the delamination interface. Whereas the Mode II cRS specimen loaded the entire delamination interface simultaneously, Mode III delamination would initiate at the edges of the delamination interface closest to where the load was applied and progressed rearward in the cR3 specimen.

The size of the delamination interface used in the cR3 specimen was 9 x 12 mm, with the 12 mm dimension aligned with the long edge of the adherends to produce a similar-sized delamination interface as other rigid-adherend specimens developed in this work. The size of the cR3 adherend was chosen to provide a similar interface area as the Mode I cRDCB and Mode II cRS. However, a slightly wider interface was necessary to simplify fabrication and to ensure that completed specimens would have sufficient structural integrity to be handled before testing.

The fabrication of cR3 specimens was procedurally similar to cRDCB and cRS specimens (Figure 5.3). Prepreg tapes were cut somewhat oversized and then stacked into two two-ply sub-laminates in preparation for assembly. Steel adherends were roughened using abrasive blasting to a minimum roughness of $2.0 \mu\text{m } R_a$ to promote a good co-cured connection between composite and adherend. A sub-laminate was pressed into the roughed adherends with the fibre direction running parallel to the long edge of the adherend. Reference lines scribed on the adherend before assembly were used to help position the prepreg material (not visible in Figure 5.3). Excess prepreg material, which extended only around the outer edges of the specimen, was trimmed flush to the edges of the adherend (Figure 5.3b). A $12.5 \mu\text{m}$ thick PTFE film was pressed into one sub-laminate as a crack starter with a 3D-printed jig to ensure specimen consistency (Figure 5.3c).

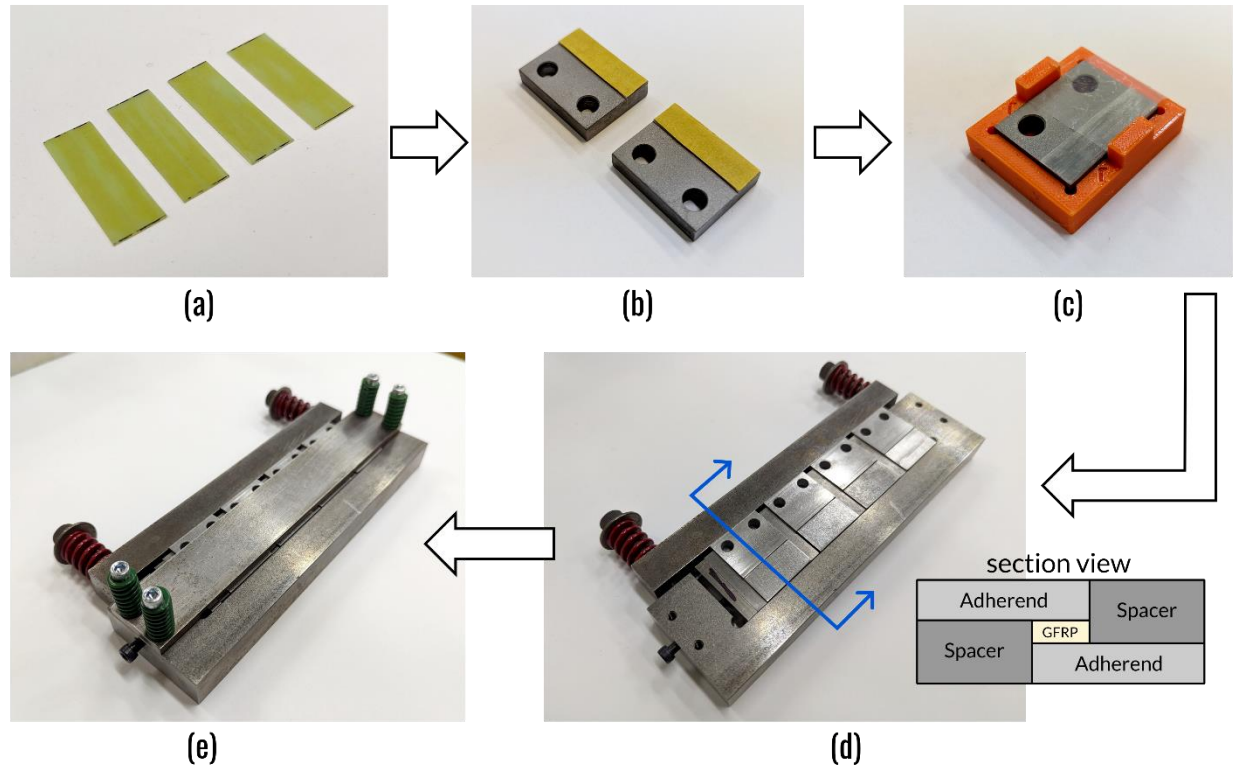


Figure 5.3: Processing methodology used to manufacture cR3 specimens. a) The E-glass prepreg tapes were cut slightly oversized. b) The prepreg tapes were stacked into two sub-laminates, then pressed into the roughened steel adherends and trimmed flush. The fibre direction was parallel to the long edge of the specimen. c) A jig helped ensure consistent PTFE tape crack starter placement. d) The two adherends were pressed together and placed in a curing jig with spacers of a specific thickness to ensure a consistent cured fibre volume fraction. The section view shows how adherends, spacers, and GFRP prepreg were stacked in the curing fixture. e) A spring-loaded plate ensured consistent pressure on the laminate during curing to provide good compaction of the prepreg tapes.

Once both adherends and sub-laminates were prepared, the cR3 specimens were assembled in the curing fixturing (Figure 5.3d). First, one adherend was placed in the curing fixturing with a specialized spacer. The spacer controlled the overlap of the two adherends and the thickness of the final FRP laminate, which in turn, controlled fibre volume fraction. Next, the second adherend and sub-laminate were positioned on top of the adherend already in the curing fixture such that the sub-laminates were pressed together and the free edge of the adherend was supported from underneath by the first spacer. In addition to controlling the thickness of the

final laminate, the second spacer ensured that both adherends were square. A schematic depiction of the adherend-prepreg-spacer stack up is provided in the section view of Figure 5.3d. Finally, a spring-loaded plate was placed over the specimen to apply appropriate pressure throughout the curing cycle (Figure 5.3e). The cR3 specimens were cured using the same processing parameters as the cRDCB and cRS specimens, specifically 0.5 MPa of pressure at 140°C for 90 minutes. Spew and resin bleed were cleaned up post-cure using abrasive paper. Laminate thickness was 1.21 mm (S.D. = 0.07 mm, $n=8$), which was about 12% thicker than Mode I cRDCB and Mode II cRS specimens. Fibre volume fraction was not specifically measured by approximated as 42%.

cR3 specimens were tested in tension using a servo-electric universal test frame (AGX-50kN, Shimadzu, Japan) at a constant crosshead speed of 0.002 mm/s. Specimens were pin-loaded to ensure no moments were transferred from fixturing during loading. The offset nature of the cR3 specimen necessitated bushings during testing to prevent any out-of-plane rotation that would introduce Mode I deformation (Figure 5.2c). These bushings fitted over the load pins and between the adherend of the cR3 specimen and the clevises used to load the specimen.

To accommodate the offset between the metallic adherends of the cR3 specimen, three-dimensional DIC (VIC-3D, Correlated Solutions, USA) was used to assess linear and angular displacement of both adherends simultaneously. Two 12 MP cameras (Grasshopper 3 GS3-U3-123S6M, FLIR, USA) with 180 mm macro lens (Sigma APO MACRO 180 mm F2.8 EX, Sigma Corporation, Japan) were used. The depth of field of the setup was sufficient to keep the measurement area of both adherends in focus simultaneously. An airbrush was used to apply a fine speckle pattern to both adherends. The linear and angular displacement at known locations on the adherends were extracted. Two responses of interest were selected for reporting purposes: resultant linear displacement at the center of the delamination interface and the angular displacement between the two adherends.

Ideally, the linear displacement of each adherend would be measured at the center of the delamination interface (Figure 5.2). However, the stacked nature of the cR3 covered this location on the rear adherend. Instead, a rigid body kinematic transform was used to calculate the linear displacement of the rear adherend using a known reference point that could be imaged. The angular displacement of the rear adherend did not require a rigid body transform.

Analysis of the delamination interface to extract TSR was complicated by two factors. First, direct imaging of the delamination interface was not possible because the FRP laminate was sandwiched between the two adherends. Second, the combination of linear and angular displacement of the adherends produced a complex deformation state. Combined with the lack of direct measurement and the rectangular interface area, an analytical approach to assessing the TSR of a cR3 specimen was not feasible. Instead, the virtual field method (VFM) (Notta-Cuvier et al., 2013; Shen and Paulino, 2011) was adopted to extract the TSR of the FRP laminate. VFM is an inverse analysis approach that uses full-field displacement measurements to optimize constitutive material model parameters directly. In comparison to inverse methods that rely on an FE model of the scenario and often a single measured response such as overall force-displacement to tune parameters, VFM generally represents the physics of the experiment with fewer assumptions, as well as being generally less computationally expensive (Shen and Paulino, 2011) as the FE model is not being solved during fitting.

VFM is based on the principle of virtual work, which, in the context of a material test specimen, states that the external work applied to a specimen must be balanced by an equivalent internal work (Eq. 5.2) for a specimen to remain in quasi-equilibrium.

$$W_{ext} = W_{int} \quad \text{Eq. 5.2}$$

In the context of the cR3 specimen and the intended purpose of extracting TSRs, external and internal work were replaced with externally measured specimen force, P_{ext} , and internal shear reaction force, V (Eq. 5.3). While there were internal forces transverse to the applied external force in the cR3 delamination interface, these are balanced internally and do not contribute to the work necessary to deform the specimen.

$$P_{ext} = V \quad \text{Eq. 5.3}$$

For the cR3 specimen, it was assumed that internal shear reaction forces were a direct result of the TSR of the laminate, $\tau(\delta_s)$. Shear separation, δ_s , was calculated for the entire delamination interface using measured linear and angular displacements of the two adherends. Using shear separation and a TSR, the distribution of tractions in the interface was computed to compute the overall internal reaction force using a surface integral over the delamination interface (Eq. 5.4), where dA is a differentiable area on the interface.

$$V = \int_S \sigma(\delta_s) dA \quad \text{Eq. 5.4}$$

It is important to note that some form of analytical constitutive relationship for TSR is required for VFM, as the material parameters were optimized numerically to balance internal reaction forces with externally applied forces.

The VFM implementation used to extract cR3 TSRs had two important features distinct from most VFM applications. First, as noted earlier, the delamination interface of the cR3 specimen was not directly measured. Instead, a rigid body transform was used to calculate the full-field shear deformation of the delamination interface based on measured linear and angular displacements of the adherends per Eq. 5.5 (Figure 5.4).

$$\mathbf{x}_p = \mathbf{x}_m + \Delta_m + R(\theta_m + \Delta\theta)\mathbf{r}_{p/m} \quad \text{Eq. 5.5}$$

Where:

- $\mathbf{x}_p$ is the vector representing the position of the point of interest on one adherend
- $\mathbf{x}_m$ is the vector representing the position on the adherend where linear and angular displacements were made
- Δ_m is the vector representing the linear displacement of the measurement point
- $\Delta\theta$ is the angular displacement of the adherend
- $R(\cdot)$ is the two-dimensional rotation matrix
- $\mathbf{r}_{p/m}$ is the vector distance from the measurement point to the point of interest.

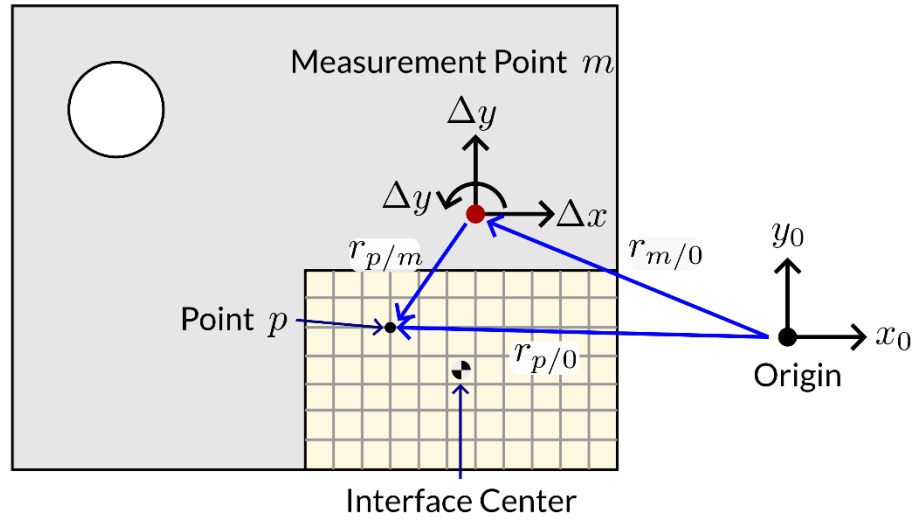


Figure 5.4: A schematic showing how the position of any point in the delamination interface, x_p , can be computed knowing the translational and rotational displacement of the rigid metallic adherend at a known reference point m .

Using Eq. 5.5 in concert with the location and dimensions of the delamination interface, a grid of points was created for each adherend (Figure 5.4). The position of the points in each grid was calculated for every measured force and DIC displacement collected from the cR3 specimen. Shear displacement at each point in the grid was calculated by taking the vector difference between matching grid points on each adherend. The calculation of shear displacement did assume that the metallic adherends were effectively rigid and that the FRP laminate had a perfect connection to the adherends. However, no assumptions about the uniformity of deformation across the delamination interface were necessary.

A four-parameter, trapezoidal-shaped TSR (May et al., 2015) was adopted for use with VFM to provide a larger degree of flexibility when fitting actual responses compared to more common bi-linear models as well as to match an existing constitutive model available in LS-DYNA (MAT_COHESIVE_MIXED_MODE_ELASTOPLASTIC_RATE). The four constitutive parameters were optimized using a bounded and constrained non-linear optimization algorithm ('fmincon' in MATLAB) to minimize the difference between measured external force with the computed internal shear reaction forces calculated with the TSR.

One limitation of the VFM approach is that a constitutive relation must be assumed *a priori*. In this case, a trapezoidal TSR was selected for development purposes as a good compromise between flexibility in modelling material response and computational simplicity. However, future work is recommended to investigate the fitting of arbitrary constitutive relations using the VFM approach. M-splines (Ramsay, 1988) were briefly investigated in this work to fit arbitrary TSRs without assuming an analytic shape. While promising, M-splines were not pursued in this work due to unrelated issues encountered in the measured cR3 behaviour discussed later in this chapter.

5.1.3 Mode II ENF Specimen Fabrication, Testing, and Analysis

ENF specimens were fabricated in accordance with the recommendations of ASTM Standard D7905 (ASTM International, 2019). A $[0]_{10}$ panel was fabricated with a $12.5\ \mu\text{m}$ PFTE film placed at the midplane of the layup to act as a crack starter. The panel was fabricated using compression moulding in a hydraulic press with temperature-controlled platens (G100H-24, Wabash, USA) using the same processing parameters as the prospective shear specimens (140°C and $0.5\ \text{MPa}$ for 90 minutes). Individual ENF specimens (Figure 5.5) were extracted from the completed panel using waterjet cutting. The cut faces of each specimen were smoothed with fine-grit abrasive paper to minimize any stress risers that may have arisen from waterjet cutting. ENF specimens had a fibre volume fraction of 52% as measured with acid digestion (ASTM International, 2015).

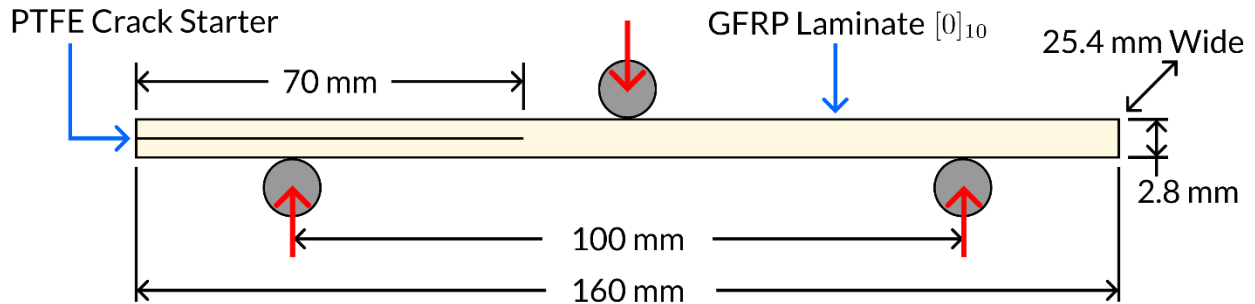


Figure 5.5: A schematic of the ENF specimen used in this work.

ENF specimens were tested in three-point bending per ASTM D7905 at a controlled displacement rate of $5\ \text{mm/min}$ in a $500\ \text{lb}$ hydraulic test frame. No pre-cracking or other crack conditioning was performed prior to testing. A $12\ \text{MP}$ USB camera (Grasshopper 3 GS3-U3-123S6M, FLIR, USA) with an $85\ \text{mm}$ telephoto lens (Bower $85\ \text{mm f/1.4}$, Bower Inc, USA) was used to image each test to assess specimen deformation and crack growth.

While a specific data reduction scheme for the ENF specimen was published (ASTM International, 2019), the approach relied on accurate measurement of the crack tip location of the ENF specimen. However, as the fracture surfaces of the ENF specimen were pressed together during testing, tracking crack extension throughout a test was extremely difficult and time-consuming. To that end, experimental ENF data was processed using the crack-equivalence method of Xavier et al. (Xavier et al., 2014b). An additional benefit of the Xavier method was that a full resistance curve (R-curve) could be computed. In contrast, the data reduction scheme in ASTM D7905 provided a single toughness value. A small number of ENF specimens were processed with the ASTM method to assess the performance of the Xavier method with good correlation. As such, only the results from the Xavier method are presented here.

5.2 Experimental Results for cRS, cR3 and ENF Specimens

The characteristic average response and statistical bounds were computed for the results of the Mode II cRS and ENF specimens using an arc-length-based approach to accommodate the different response terminations and capture variability in both measurement axes simultaneously (Hartlen and Cronin, 2022). In both cases, a single warping control point and a warping penalty of 10^{-2} was used to register features of the responses shared across specimens to prevent distortion of the average response and bounds. The arc-length-based approach was not applied for the Mode III cR3 specimen due to measurement difficulties that would impact the representative nature of the resulting average response.

5.2.1 Results and Analysis of cRS Specimens

Consistency amongst cRS specimens was good, with all specimens exhibiting similar force-displacement behaviour (Figure 5.6a). All specimens exhibited a short period of linear response followed by a gradual decrease in stiffness up to a peak force at which unstable crack growth occurred. All specimens underwent delamination without evidence of interface failure between the laminate and adherend. Further, analysis of DIC imagery showed that the Högborg-style test fixtures prevented specimen rotation during testing, limiting rotation of the delamination interface by less than 0.5 degrees and ensuring a direct shear load on the delamination interface.

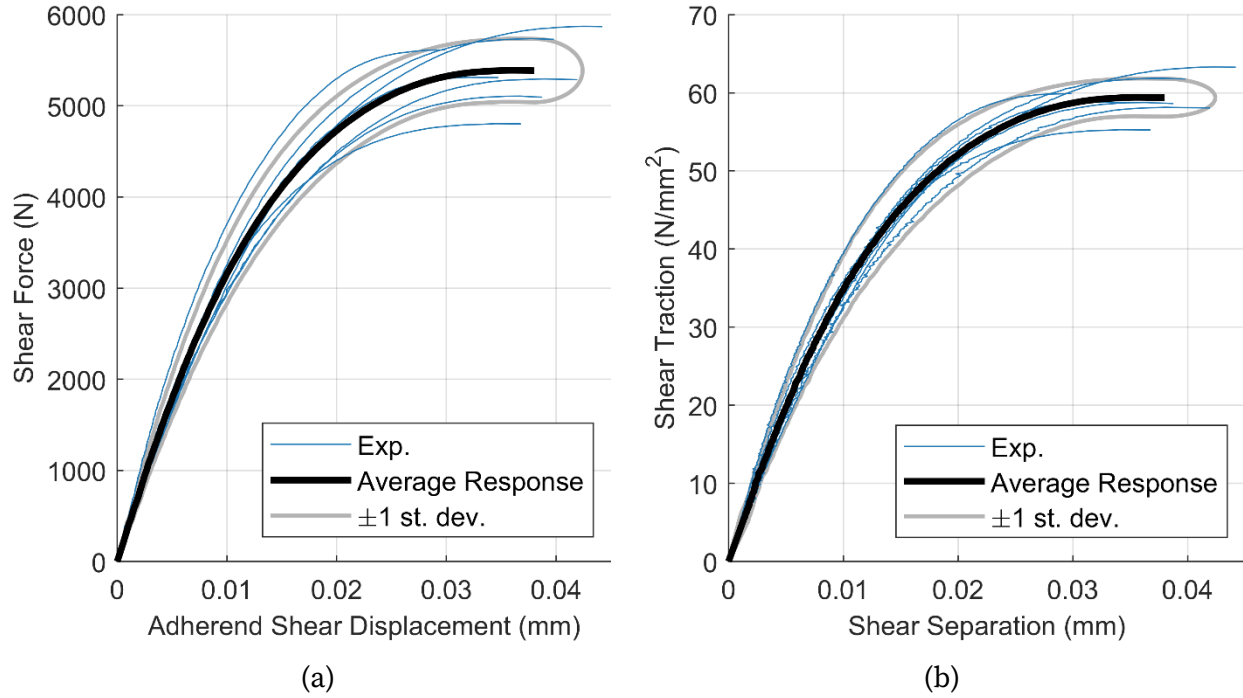


Figure 5.6: (a) The measured force-displacement response of all cRS specimens and (b) the extracted traction-separation responses. The average response and ± 1 standard deviation bounds were computed for each set of signals using an arc-length-based method (Hartlen and Cronin, 2022).

Examining the onset of delamination using full-field DIC strain measurements of the FRP laminate and delamination interface, it was observed that high shear strain concentrations developed symmetrically from both PTFE crack starters (Figure 5.7b). These concentrations, which can be correlated to crack tip location, progressed inward from each edge (Figure 5.7c-e) before meeting at the center of the delamination interface just prior to rapid crack growth (Figure 5.7f). The symmetry with which the crack progressed was a good indicator that the specimen was loaded uniformly along the delamination interface.

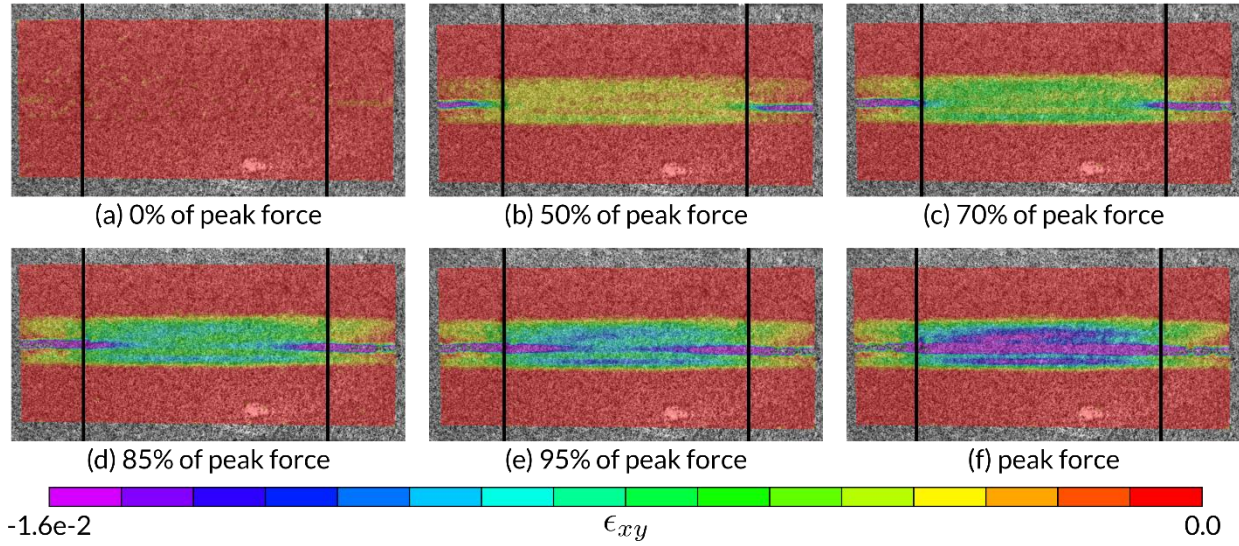


Figure 5.7: Progression of Mode II delamination damage observed in the cRS specimen using shear strain (ϵ_{xy}). Damage, correlated with high shear strains, initiates from the crack tips (denoted with black lines) and progresses symmetrically inward. Rapid crack growth occurred immediately after image (f) was captured.

High-speed imagery was used to study the unstable crack growth that occurred in all cRS specimens. In all cases, crack tip velocities in excess of 350 m/s were observed. The speed at which the delamination crack extended prevented any unload behaviour of the cRS specimen from being measured. To that end, it was not possible to assess the unload response of Mode II delamination, limiting measured responses to pre-peak behaviours.

TSRs were extracted from experimental force-displacement curves using Eq. 5.1 (Figure 5.6b). The extracted TSRs exhibited the same curvature as observed in force-displacement behaviour. However, Eq. 5.1 assumed a uniform shear strain developed across the length of the interface, from tip to tip. Due to the variation in strains across the interface observed with DIC, Eq. 5.1 likely produced conservative estimates of maximum traction and may have induced some of the non-linearity that developed prior to peak traction. Integrating the experimentally measured TSRs yield a value for overall energy release rate of Mode II delamination of 1.67 J/mm² (S.D. = 0.25) up to the point of unstable crack growth.

5.2.2 Results and Analysis of cR3 Specimens

The as-fabricated Mode III cR3 specimen had a larger, more variable laminate thickness (1.21 mm (SD=0.08)) than the Mode II cRS specimens (1.06 mm (SD=0.05)). As such, there was a relatively large degree of variability in specimen response. Particularly, the linear portion of the force-displacement response of the cR3 specimen exhibited relatively large variation in both peak force and stiffness (Figure 5.8a). These responses were predominantly linear up to a short plateau before a stable yet relatively quick unload. Force-angular displacement responses exhibited more non-linearity before peak force (Figure 5.8b) but demonstrated a similar short plateau region before unloading. Linear displacement measurements exhibited more measurement noise and specimen-to-specimen variability than angular displacements. 3D DIC measurements showed that the cR3 specimens did not exhibit out-of-plane motion arising from the offset nature of the adherends, indicating that the test bushings were effective at restraining the specimen.

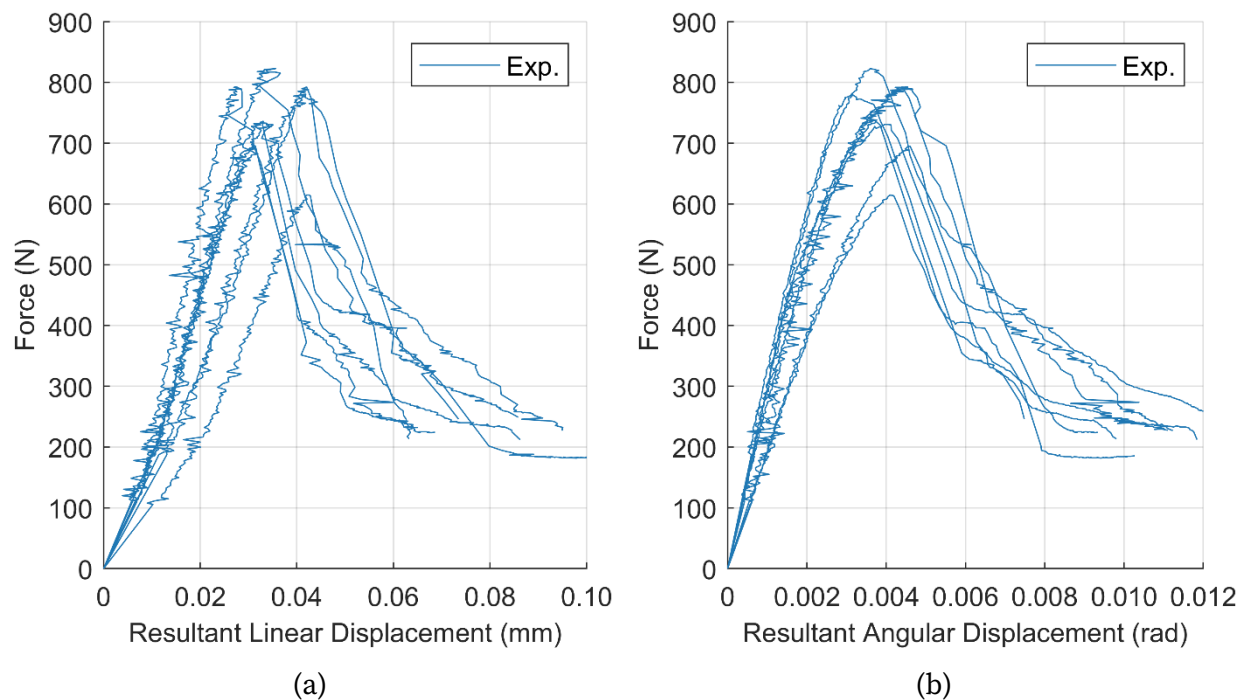


Figure 5.8: (a) Force-linear displacement and (b) force-angular displacement of the cR3 specimens tested in this work. Displacements are reported across the center of the delamination interface.

The force, linear displacement, and angular displacements of all cR3 specimens were used as inputs to the VFM analysis method to fit a trapezoidal TSR to each specimen. Valid TSRs could only be extracted from six of the eight cR3 specimens (Figure 5.9). Of the extracted TSRs, all trapezoidal TSRs bar one suggested a sharp or sudden unload response, which is not necessarily reflected in the measured responses, which did suggest that cR3 specimens should assess some unload response.

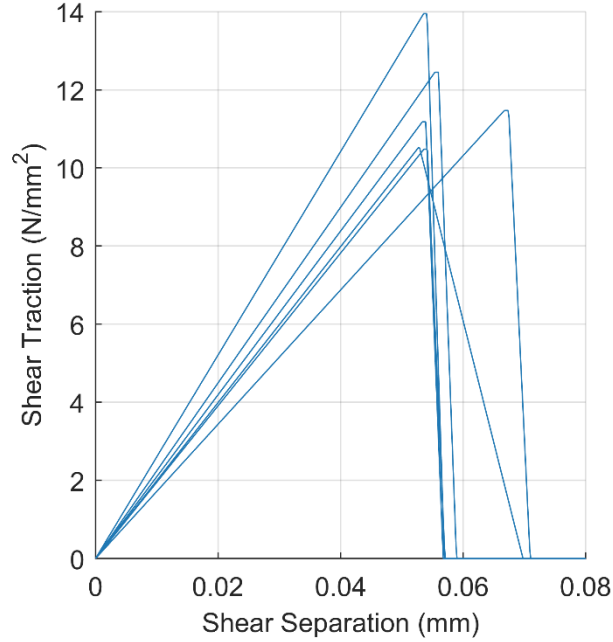


Figure 5.9: Trapezoidal TSRs extracted from experimental cR3 measurements using VFM (left). Valid TSRs could only be extracted from six of the eight cR3 specimens tested.

Examination of the average parameters defining the extracted trapezoidal TSRs (Table 5.2) showed lower-than-expected values and tended to exhibit bilinear behaviour, as no plateau period was enforced as a constraint during the VFM process. Strength (σ_s), stiffness (E), and critical energy release rate (G_c) were all lower than expected and inconsistent with measured Mode II cRS and ENF responses. Additionally, the energy release rate measured with the Mode III cR3 specimen was inconsistent with the values reported in the literature (Marat-Mendes and de Freitas, 2009; Marat-Mendes and Freitas, 2010).

Table 5.2: Average trapezoidal TSR parameters extracted from experimental cR3 specimens using VFM

Trapezoidal TSR	
Parameter	Value
E	210.3 N/mm ³
σ_s	11.7 N/mm ²
G_C	0.362 J/mm ²
f_G	0.0167

5.2.3 Results of ENF Specimen Testing

The force-displacement response of ENF specimens (Figure 5.10a) exhibited consistent stiffness, although variability in peak force was somewhat large. Additionally, due to a relatively low a/l ratio of the ENF specimens tested, some ENF specimens exhibited somewhat unstable crack growth denoted by a vertical drop in force with respect to crosshead displacement (Carlsson et al., 1986a).

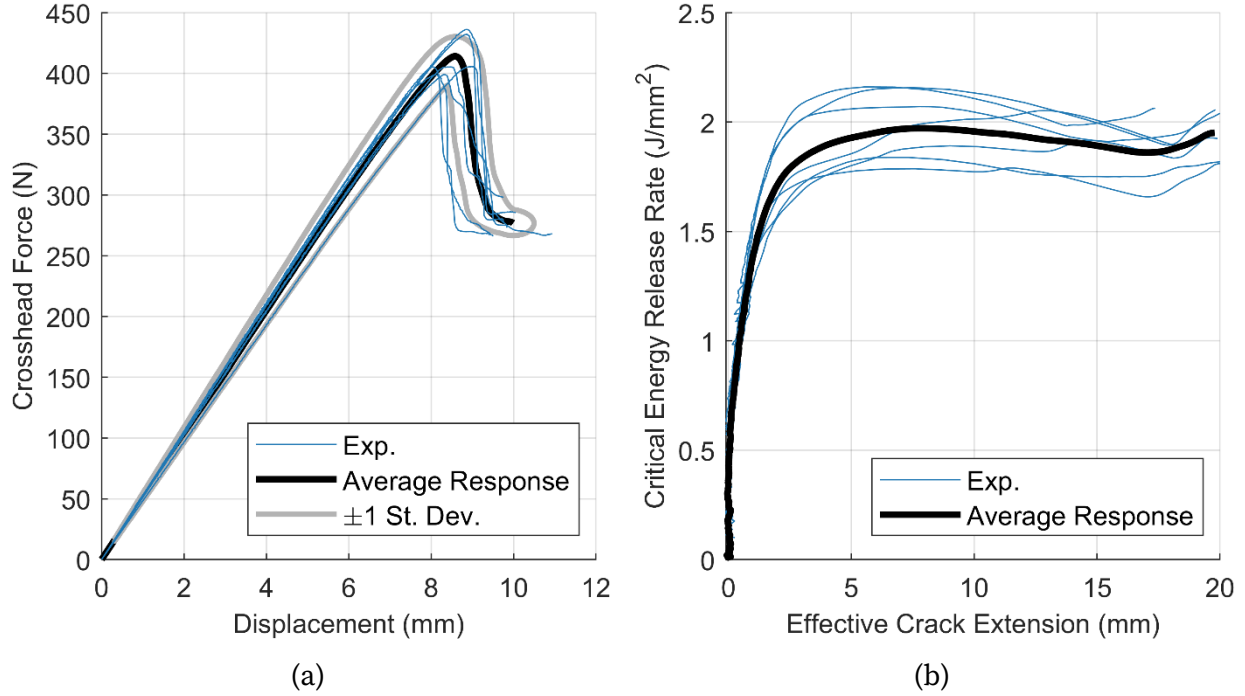


Figure 5.10: (a) Measured force-displacement and (b) extracted R-curves for the ENF specimens ($n = 7$). A characteristic average and ± 1 standard deviation statistical bounds were computed using an arc-length-based method (Hartlen and Cronin, 2022).

R-curves (Figure 5.10b) were extracted from each specimen using the crack equivalence technique of Xavier et al. (2014b). All ENF specimens underwent a relatively fast crack growth with a consistent average energy release rate of 1.92 J/mm^2 throughout the range of possible crack extensions.

5.3 Discussion

5.3.1 Discussion of Mode II cRS Results

All cRS specimens performed well in characterizing the onset of shear delamination, with excellent consistency in force-displacement behaviour between specimens. One key advantage of the Mode II cRS specimen was that the same fabrication process and curing fixtures developed for the Mode I cRDCB specimen could be used without modification. As such, cRS development work could take advantage of the significant amount of time devoted to developing and tuning the fabrication process of the cRDCB specimen to create consistent, high-quality specimens. Additionally, the Mode II cRS used the same adherend design as the cRDCB specimen, which

was appealing in terms of minimizing the number of unique parts and components that would need to be machined or ordered to fully characterize the delamination response of a laminate by reducing overall expense.

An interesting feature of the measured force-displacement curves and extracted TSRs was a prolonged period of non-linear response before attaining peak force and traction. The degree of non-linearity contrasts with the measured Mode I TSRs presented in Chapter 3, indicating that Mode II delamination may be more tolerant to the onset and initiation of damage. It is important to note that a relatively small body of work in the literature has reported the onset of Mode II delamination with the degree of resolution and sensitivity measured with the cRS specimen. While there is literature to support the high degree of non-linearity up to peak force or traction (Marzi et al., 2014), most existing compliant-beam-style specimens are not well suited to characterizing small-scale damage regimes (Lu et al., 2019).

Further supporting evidence of damage accumulation prior to peak loads was visible in DIC strain fields of the delamination interface (Figure 5.7). Strain fields showed strain concentrations forming at the tips of the delamination interface before progressing inward toward the center of the interface. These strain concentrations were not unexpected, but it is important to note that the data processing scheme used to assess traction-separation behaviour (Eq. 5.1) did assume that deformation along the delamination interface was uniform. The non-uniform strain development in the cRS specimen would likely result in a slight underprediction of peak traction and energy release rate. Further, the progression of the strain concentrations from the outer tip inward might explain some, although not all, of the non-linearity observed in force-displacement and traction-separation data.

While extracted TSRs provided a detailed description of damage initiation and early onset, unstable crack growth was observed in all cRS specimens as the carrying capacity of the specimens began to decrease. The speed at which the crack propagated prevented the unload response of the specimen from being assessed. Using the available portion of the measured TSRs, the average toughness (in the form of energy release rate) at the point of unstable crack growth was 1.67 J/mm^2 . ENF specimens, on the other hand, had a measured steady-state toughness of 1.92 J/mm^2 . As unstable delamination did not play as significant a role in ENF specimens, the energy release rate likely included some contributions from the larger-scale

damage behaviours that the cRS specimen could not measure, such as the late-stage ligature formation and rupture as reported by (O'Brien, 1998a). Additionally, it is possible that the energy release rate of the ENF specimen was impacted by the friction between the two arms of the specimen. To that end, it is unsurprising that the ENF specimen produced somewhat higher energy release rates. Further, it has been reported that toughness values measured with the ENF specimen can be affected by the friction between the two fracture surfaces as a Mode II delamination crack propagates. However, despite these two factors, the cRS and ENF specimens agree relatively well despite the inability to measure the unload response of the cRS specimen. The fracture surfaces of the two specimen geometries also exhibited good agreement (Figure 5.11).

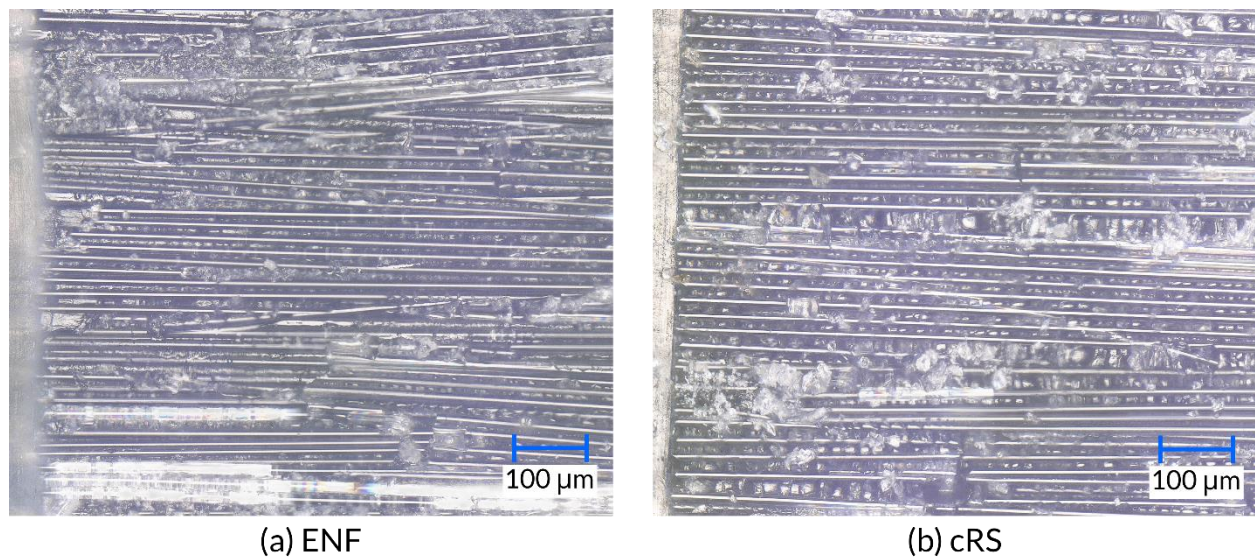


Figure 5.11: Fracture surfaces of the Mode II ENF (a) and Mode II cRS (b) specimens. Both fracture surfaces exhibited similar morphologies.

While unstable crack growth was a challenge during the early stages of Mode I specimen development, the cRS specimen was particularly susceptible to unstable delamination. The design of the cRS specimen placed the entire delamination interface into Mode II shear simultaneously, compared to the progressive loading of the Mode I cRDCB and similar compliant-beam-style specimens like the DCB specimen. As only a portion of the interface was undergoing delamination in a progressively loaded interface, the overall change in specimen stiffness for the cRDCB was reduced. However, the cRS specimen exhibits much larger stiffness changes than the cRDCB, as unload behaviour occurred almost simultaneously across the

delamination interface. Such a sudden change in interface stiffness can induce unstable crack growth by transitioning the specimen from displacement control to force control due to energy stored compliance in the test setup. As such, a progressively loaded interface was desirable in a novel specimen, which inspired and informed the development of the Mode III cR3 specimen.

5.3.2 Discussion of Mode III cR3 Results and Analysis

The primary motivation for developing the Mode III cR3 specimen was to reduce the high test system stiffness required for stable crack growth in the cRS specimen by encouraging progressive crack extension in the delamination interface, similar to the Mode I cRDCB specimen. While developing a specimen that provided progressive Mode II loading of a delamination interface was not feasible, Mode III provided an interesting opportunity. Of course, the switch to Mode III loading raised a significant question: are Mode II and Mode III behaviour equivalent? The limited literature on this subject is inconclusive, with most studies suggesting similar behaviour but some noting differences between loading modes, as noted in Section 2.2.3. One complicating factor in these literature studies was that Mode III delamination does not have a standardized characterization methodology. As such, a large variety of different Mode III specimens were used. From a material perspective, both Mode II and Mode III loading would introduce a similar shear stress state in the material. However, some considerations based on the heterogeneous nature of FRP laminates might influence measured properties, such as fibre orientation and how fibres in neighbouring plies interact under loading. Regardless, putting material considerations aside, it is important to note that the overwhelming majority of numerical CZM implementations and analytical TSRs amalgamate Mode II and Mode III loading into a single effective shear mode of loading. As the cRS and cR3 specimens were intended to provide TSRs for numerical modelling applications, attaining exact Mode II or Mode III loading was not as important as providing an effective shear loading without contamination from normal or Mode I loading modes.

Greater experimental variability was observed in the Mode III cR3 specimens than in the Mode II cRS specimens. The variability can be partly attributed to the lack of maturity in the curing fixtures used to fabricate cR3 specimens. The Mode II cRS specimens used identical tooling as the Mode I cRDCB specimens, which had been extensively tuned to produce specimens with a high fibre volume fraction and low defects. Less time had been spent adjusting

the Mode III cR3 tooling to produce specimens with a similar quality laminate. Additionally, examination of the tested cR3 specimen revealed a small number of specimens experienced interface failure between the laminate and adherend, although the onset of interface failure occurred during the unloading portion of specimen response. Further investigation revealed that specimens suffering from interface failure had a lower surface roughness than those that did not exhibit interface failure, indicating a quality control issue during fabrication. Both curing fixturing and surface roughness could be improved in future work.

The design of the cR3 specimen presented two major experimental challenges. First, it was desirable to visually observe the delamination interface during a test, as is possible with the Mode II cRS specimen, the Mode I cRDCB specimen, and most compliant-beam-based specimens. A clear view of the delamination interface allowed one to observe damage progression and identify test issues. However, the geometry of the cR3 specimen does not allow the direct imaging of the delamination interface. While the VFM analysis technique did not require direct observation of the interface, visual information could provide valuable information about how the laminate behaved during a test.

Second, the stepped nature of the cRS specimen in this work necessitated the use of 3D DIC imagery to assess the displacement of both adherends simultaneously. Using stereoscopic calibration with 3D DIC should have accommodated the in-plane distance between the two measurement planes. However, examination of 3D DIC results revealed that despite repeating calibration between every other cR3 specimen tested, the distance between the two measurement faces of the cR3 specimens was generally underpredicted. As 3D DIC relies on accurate estimation of the location of a point relative to the cameras to produce accurate linear displacement measurements, the underestimated face-to-face measurements observed in this work indicated that parallax error may have been introduced. While parallax error would impact the measured linear displacement of each adherend, angular displacement measurements would be relatively insulated from parallax error. Whereas linear displacements depended on estimating the absolute position of a point in three-dimensional space, angular displacement measurements were only referenced to an initial measured configuration and were much less insensitive to parallax error.

Stable crack growth was attained with the cR3 specimen owing largely to the progressive loading of the delamination interface in Mode III loading. However, while progressive loading helped ensure crack growth stability, it precluded direct assessment of shear TSRs from measured specimen responses. While the Mode I cRDCB specimen also took advantage of progressive loading of the delamination interface, the geometry of the delamination front that developed in the Mode III cR3 specimen was non-trivial. Broadly, the delamination front was cylindrical, with the axis of rotation normal to the delamination interface. However, as the crack front developed, the center of rotation shifted towards the unloaded side of the specimen. Further, the initial delamination crack front was rectangular, not circular. As such, an analytical or quasi-analytical description of crack front kinematics and kinetics would be extremely difficult to develop, prompting the adoption of the VFM approach used in this study.

VFM analysis to extract TSRs from cR3 specimens produced mixed results. Valid TSRs could only be extracted from three-quarters of the cR3 specimens tested, and all had unrealistically low stiffness, strength, and energy release rate values. However, the VFM analysis was not to blame for this result, as numerical modelling (detailed in Chapter 6) was used to verify that the VFM framework was functioning correctly. Rather, the cause of the unrealistic TSRs stemmed from issues with the experimental data. As noted above, 3D DIC measurements experienced issues with stereoscopic calibration, which would impact measurements of linear displacement of cR3 specimens. Specifically, the improper calibration would result in the same physical displacement of the front and rear cR3 adherend being measured differently. While it was not possible to correct the calibration in the as-measured data, some probing studies were carried out that scaled resultant linear displacement. These probing studies indicated that should the resultant linear displacement of the cR3 adherends be less than that measured, more physically realistic TSRs that agreed more closely with Mode II ENF and cRS tests could be extracted.

One conceptual issue associated with using VFM was that it is fundamentally an inverse technique despite relying on an objective function strongly grounded in the physics of the cR3 specimen. To that end, a TSR was not being extracted from experimental data inasmuch as a pre-defined analytical TSR was being fitted. In this work, a trapezoidal TSR was used for simplicity. However, some exploratory work was conducted using M-splines (Ramsay, 1988) to provide extremely flexible descriptions of traction-separation behaviour. Fitting the knots and

weights of the M-splines to experimental data rather than an analytical model provided VFM significantly greater flexibility in fitting TSRs without *a priori* assumptions of TSR shape. Further, it would be trivial to subsequently fit simpler analytical models such as bi-linear or trapezoidal TSRs to the fitted splines or use the M-splines directly as tabulated TSRs for input to FE models. However, further investigation into spline-based TSRs was not conducted owing to the discovery of the measurement issues discussed above.

The use of VFM highlighted some key advantages for the analysis of delamination response, principally the extensibility of the methodology to a range of different specimen geometries to minimize the assumptions used to extract TSRs from experimental data. Consider the cRS specimen, wherein TSRs were extracted assuming that deformation occurred uniformly across the delamination interface. Experimental DIC imagery showed that tractions did not develop uniformly. VFM has the potential to fit TSRs to cRS behaviour without relying on the assumption of uniform deformation by directly using DIC deformation measurements, although it would not solve the issue of unstable crack growth.

5.4 Conclusions

Two specimens were developed to assess the traction-separation response (TSR) of Mode II and III shear delamination in FRP laminates. Both specimens utilized metallic adherends to minimize the overall compliance of the specimen. The first specimen, the composite rigid shear (cRS), was designed to apply uniform Mode II deformation to a delamination interface. While the cRS specimen provided TSRs with excellent resolution of the onset of delamination response, unload behaviour could not be assessed. However, measured delamination toughness agreed well with the R-curves extracted from end-notched flexure (ENF) specimens up to unstable delamination.

The second specimen, the composite rigid Mode III (cR3) specimen, was developed to progressively load the delamination interface in Mode III shear to avoid the issues of unstable delamination that plagued the cRS specimen. Owing to the complexity of how delamination developed, the cR3 specimen utilized an indirect technique to extract TSRs. The virtual field method (VFM), a physics-informed inverse technique, fitted trapezoidal TSRs to cR3 behaviour using a combination of measured force, linear displacement, and angular displacement. While the VFM analysis approach functioned as intended, experimental difficulties in testing the cR3

specimen produced non-physical TSRs due to difficulties calibrating the 3D DIC measurement system.

The cR3 specimen presented a more compelling option to assess the complete shear TSR of a delamination interface compared to the cRS specimen. The progressive loading of the delamination interface enabled the cR3 specimen to be used on much less stiff test setups without experiencing unstable crack growth. Further, the VFM approach could be extended to fit a wide range of TSR shapes. To that end, it is recommended that future work should investigate changes to the experimental measurement system to eliminate the calibration issues experienced here. In particular, it is recommended that the step between the front and rear adherend of the cRS specimen be eliminated during testing with shims or spacers to create a flat imaging plane for DIC measurements.

Chapter 6

NUMERICAL MODELLING OF SPECIMENS FOR MODE II AND III DELAMINATION

This chapter documents finite element (FE) modelling undertaken to investigate and understand the behaviour and characteristics of the Mode II composite rigid shear (cRS) and Mode III composite rigid Mode III (cR3) specimens developed in Chapter 5, as well as to gain insight into how delamination response varied between the two modes of shear loading. Experimental traction-separation responses (TSRs) measured with the cRS and cR3 specimens were used as inputs to the FE models developed in this chapter. In addition to FE models of the cRS and cR3 specimens, modelling of the end-notched flexure (ENF) specimen was also undertaken to place the results of experimental TSRs measured in Chapter 5 into context.

Cohesive zone modelling (CZM) was used exclusively for the modelling of delamination, with both the combined elastic-damage modelling approach and the damage-only modelling approach being used. As a reminder, the combined elastic-damage modelling approach used cohesive elements that captured both the behaviour and physical volume of the delamination interface and surrounding material (Figure 2.12). In contrast, the damage-only modelling approach used zero-thickness cohesive elements that solely captured the damage behaviour of the delamination interface, with traditional continuum elements capturing the behaviour of material adjacent to the delamination interface.

6.1 Development of Finite Element Models

All FE models were meshed using commercial meshing software (Hypermesh R2020, Altair Engineering, USA). A commercial finite element solver (LS-DYNA R11.2 double precision, LST-ANSYS, USA) was used to carry out all simulations. All models were run on Graham, a high-performance computer cluster located at the University of Waterloo and administered by the Digital Research Alliance of Canada.

6.1.1 cRS Model for Investigation of Mode II Shear Response

Modelling of delamination in the Mode II cRS specimen was undertaken using cohesive elements implemented in two modelling approaches: the combined elastic-damage approach and the damage-only approach (Figure 6.1). The metallic adherends of the cRS specimens were

modelled as numerically rigid bodies in both modelling approaches. Rigid bodies were used instead of deformable bodies like earlier Mode I modelling to reduce model runtime for faster developmental iterations and the lower level of physical accuracy needed for this type of modelling. To simplify modelling, the Högberg-style fixtures used to load the cRS specimen were not fully modelled. The Högberg-style fixtures were simplified to a single node constrained to a rigid body to which the applied displacement was applied (Figure 6.1, wire frame and load arrows shown in the damage-only model). The simplified approach still ensured the applied line of action passed through the delamination interface while allowing the adherends to rotate like the pin-loading used in the experiment. The use of rigid bodies and constraints to a node in space was inherently more rigid than the experiment. However, the rigidity enabled FE models to investigate Mode II delamination under the best possible conditions to promote stable crack extension by excluding factors external to the specimen, such as test setup stiffness. Half-model symmetry was applied in both modelling approaches to reduce computational expense.

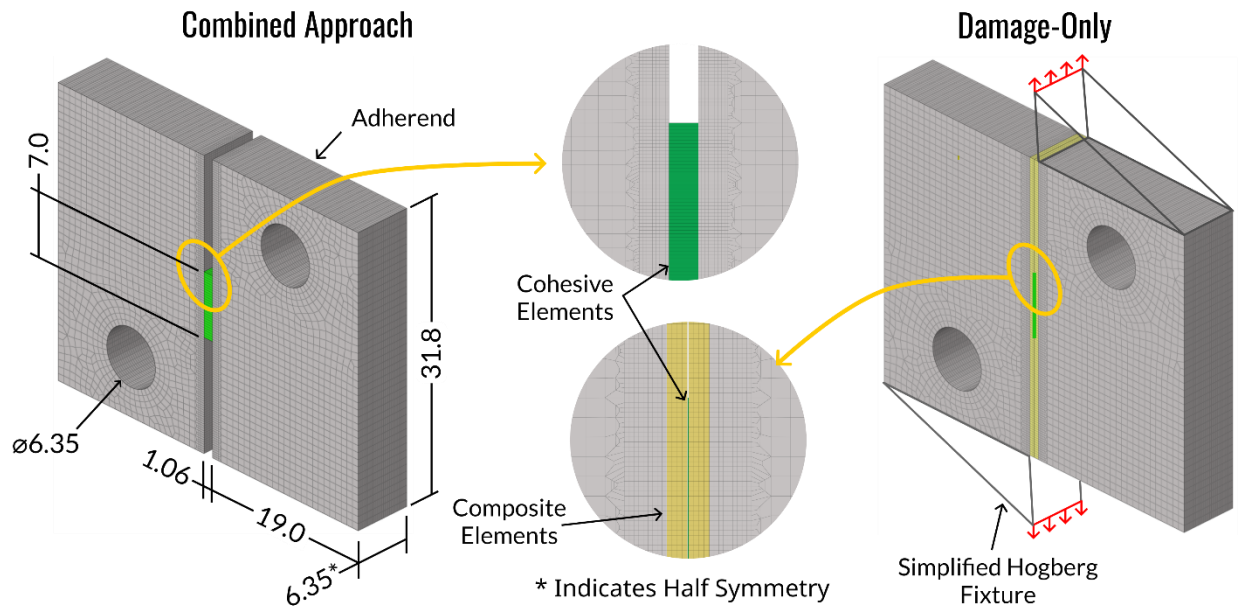


Figure 6.1: The combined elastic-damage (left) and damage-only (right) modelling approaches used to model the Mode II cRS specimen. The FRP laminate material (yellow) was modelled with continuum solid elements, whilst the delamination interface (green) was modelled with cohesive elements. The adherends (grey) were modelled as rigid bodies. The grey wireframe in the damage-only model indicates the projected nodes where displacements were prescribed. This loading method was used on both models, but lines were excluded from the combined approach for clarity.

The combined elastic-damage modelling approach used a single layer of eight-node cohesive elements (ELFORM 19 (LSTC, 2017a)) inserted between the two adherends of the cRS specimen (0.25 mm nominal in-plane element size, 1.06mm thick). These cohesive elements represented the volume of the FRP laminate sandwiched between the adherends. Cohesive elements were assigned a trapezoidal TSR (MAT_240). While earlier Mode I modelling work had used a tabulated TSR, a trapezoidal TSR was selected for modelling shear delamination for three reasons. First, defining a trapezoidal TSR was simpler than implementing a tabulated model for an arbitrary experimental TSR, as fewer parameters and less time were needed. Second, as a virtual field method (VFM) analysis technique was used to extract TSRs from the Mode III cR3 specimen presented in Chapter 5, a trapezoidal TSR would simplify the transferability of TSRs between the cRS and cR3 models. Finally, while experimental Mode II cRS TSRs exhibited significant non-linearity, the use of a trapezoidal TSR that exhibited no non-linear decrease in

stiffness up to peak traction would enable assessment of how much non-linearity arose from the response of the CRS specimen itself in contrast to the TSR. The TSRs used for all FE models are presented later in this chapter due to the processes involved in defining the trapezoidal responses.

The damage-only model used continuum solid elements (ELFORM -1) and an orthotropic elastic material model (MAT_ORTHOTROPIC_ELASTIC) to model the behaviour of the FRP laminate adjacent to the delamination interface. Continuum solid elements had a nominal size of 0.25 mm. Material properties (Table 6.1) assigned to the laminate were calculated using manufacturer-supplied fibre and matrix properties and a fibre volume fraction of 47% using the Halpin-Tsai equations (Halpin and Kardos, 1976). Near-zero-thickness (0.01 mm) cohesive elements (ELFORM 19) were used to model the delamination interface. TSRs used for this modelling approach used an initial stiffness of $5(10^5)$ N/mm³ to prevent the introducing undesired compliance into the model, as detailed later in section 6.1.4.

Table 6.1: Orthotropic-elastic properties used for UE400/REM laminate ($V_f=47\%$)

Constitutive Parameter	Value
E_{11}	35.7 GPa
E_{22}, E_{33}	10.7 GPa
ν_{12}, ν_{13}	0.274
ν_{23}	0.356
G_{12}, G_{13}	3.72 GPa

The damage-only modelling approach was augmented in a subset of investigations with a one-dimensional spring element (SECTION_DISCRETE) representing the amalgamated stiffness of the test setup to understand the effect of test setup stiffness on crack growth stability (Figure 6.2). One end of the spring was attached to the rigid body of the adherend whilst the other end was assigned a prescribed displacement. While modelling test setup stiffness using Hookean elasticity was simplistic, it was sufficient to estimate the limiting test setup stiffness needed to extract the full delamination damage response from CRS specimens.

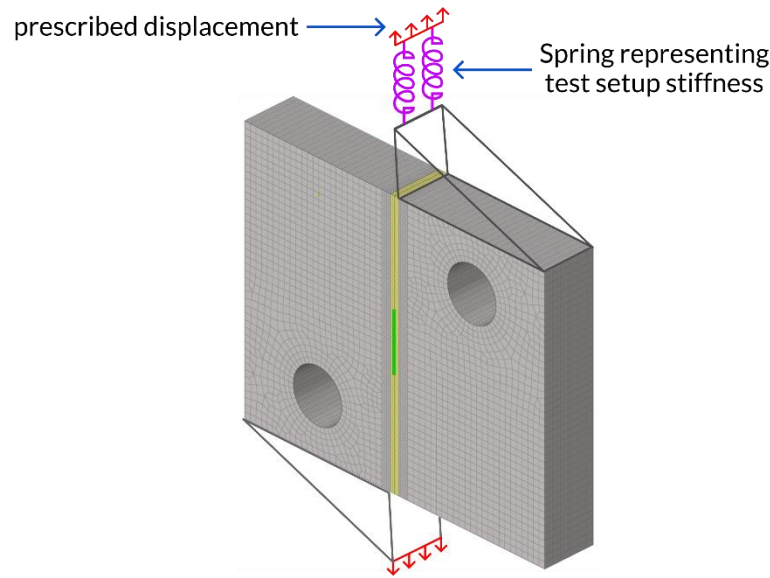


Figure 6.2: A schematic representation of the damage-only FE model adapted to study the impact of test setup stiffness on the response of the Mode II cRS specimen. One end of the spring element (purple) was constrained to the specimen, and a prescribed displacement was applied to the other end.

6.1.2 cR3 Model for Investigation of Mode III Delamination and Assessment of VFM

The Mode III cR3 specimen was modelled using the combined elastic-damage modelling approach (Figure 6.3) with the goals of understanding how delamination damage developed and accumulated under Mode III loading and verifying that the VFM analysis technique used to extract TSRs from experimental data functioned correctly. The more complex and fidelic damage-only modelling approach was not applied to the cR3 specimen. The level of detail provided by the damage-only modelling approach was unnecessary, given the stated intent of FE modelling and the difficulties experienced measuring TSRs in Chapter 5. Unlike all FE models presented in this work, the geometry of the cR3 specimen did not permit symmetry to be taken advantage of to reduce model runtime.

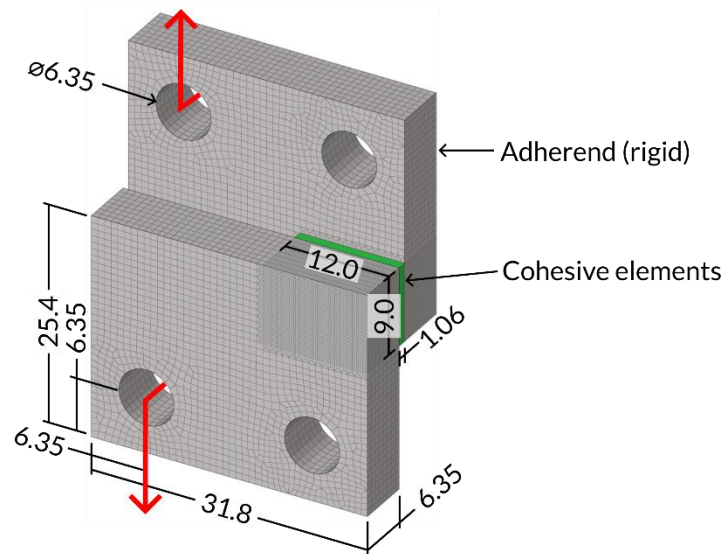


Figure 6.3: The Mode III cR3 model using the combined elastic-damage modelling approach used in this work. The adherends (grey) were modelled as rigid bodies. The cohesive elements (green) captured both the stiffness of the FRP laminate and the damage response of the delamination interface.

The metallic adherends of the cR3 specimen were modelled as rigid bodies and allowed to rotate about the pin holes used to load the experimental cR3 specimen. Out-of-plane rotation was restricted. A prescribed displacement was used to load the specimen. The delamination interface and FRP laminate adjacent to the interface were homogenized into a single layer of eight-node cohesive elements (ELFORM 19). Trapezoidal TSRs (MAT_240) were used as inputs to the cohesive elements to match the assumptions used to extract TSRs using the VFM analysis technique presented in Chapter 5. The development of TSRs used in this model is presented in Section 6.1.4.

As with the Mode II cRS specimen discussed in the previous section, the crack growth stability of Mode III delamination in the cR3 specimen was investigated using a one-dimensional spring element (SECTION_DISCRETE with MAT_SPRING_ELASTIC) to capture the stiffness of the test setup (Figure 6.4).

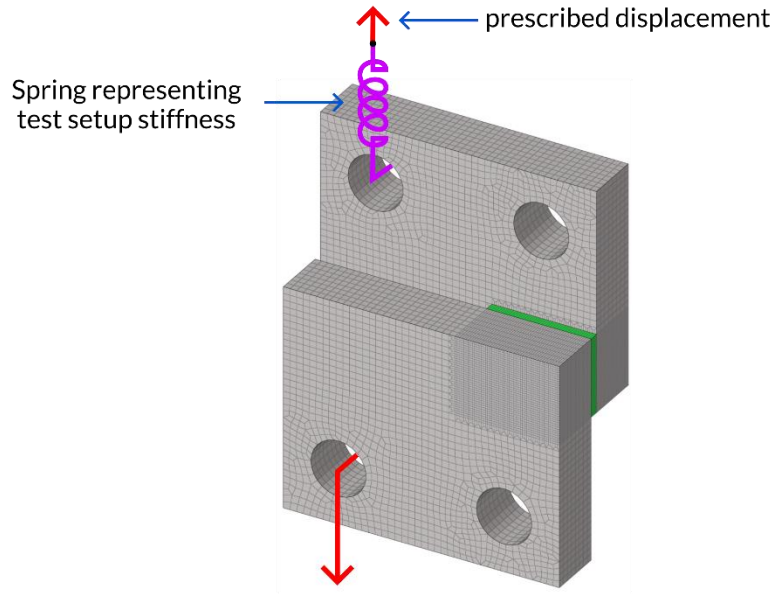


Figure 6.4: A schematic representation of the cR3 FE model used to assess the effect of test setup stiffness on crack growth stability. A spring element (purple) was used as a surrogate for setup stiffness.

6.1.3 ENF Modelling to Assess Experimental TSRs and Toughness

An FE model of the Mode II ENF specimen was created using the damage-only modelling approach. The combined elastic-damage approach was not used for the ENF as the approach would be unsuited to capture the bending of the specimen accurately. The ENF specimen was meshed with fully integrated, selectively reduced solid elements (ELFORM -1). The delamination interface was modelled with near-zero (0.01mm) eight-node cohesive elements (ELFORM 19). Development of the TSRs used in the ENF model is presented in Section 6.1.4.

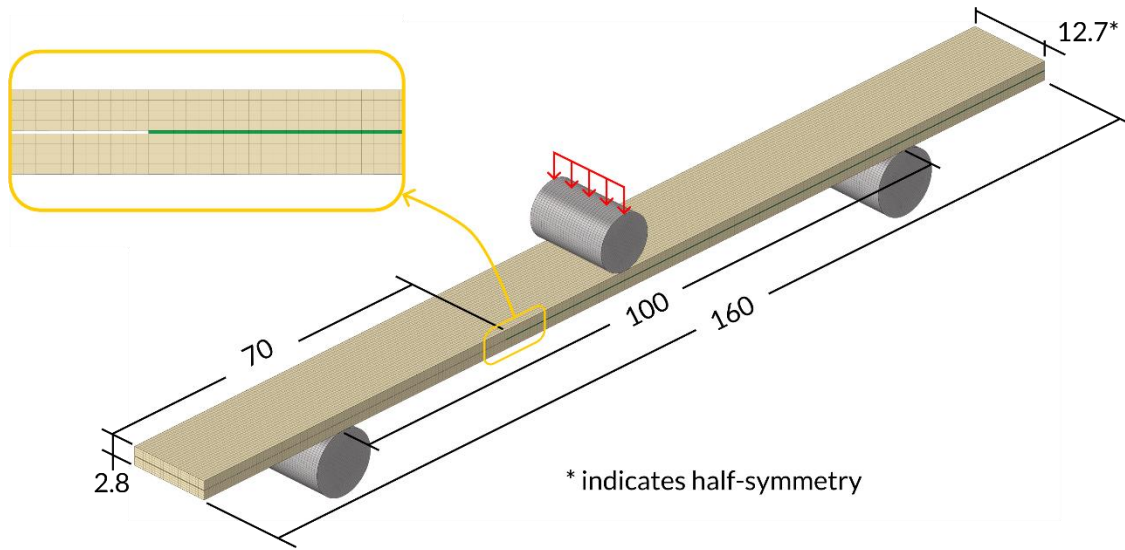


Figure 6.5: The Mode II ENF FE model with FRP laminate material is shown in yellow, and the delamination interface is shown in green. Numerically rigid rollers (grey) placed the specimen in three-point bending with a prescribed displacement. The inset image shows the cohesive elements used to model delamination with element thickness exaggerated ten times for visibility.

The ENF specimen was placed into three-point bending using numerically rigid rollers given a prescribed displacement. Contact between the specimen and rollers was assumed to be frictionless (*CONTACT_AUTOMATIC_SURFACE_TO_SURFACE). As three-point bending forced the delamination interface together, frictionless contact was also specified between the faces at the uncracked end of the ENF specimen, with the ability to capture the erosion of cohesive elements as delamination progressed (*CONTACT_ERODING_SURFACE_TO_SURFACE). Half-model symmetry was exploited to reduce computational runtime.

6.1.4 Development of TSRs for Mode II and III Shear from Experimental Data

TSRs used in the FE modelling of Mode II and Mode III shear delamination were based on the experimentally measured traction-separation behaviour of the Mode II cRS specimens and the experimentally measured toughness of the ENF specimens presented in Chapter 5. Experimental TSRs extracted from the Mode II cRS specimen exhibited significant non-linearity prior to reaching peak traction and did not contain any unload information. cRS specimens had an average energy-release rate of 1.67 J/mm^2 up to the point where unstable crack growth

occurred. Mode II ENF specimens, which did not exhibit significant unstable crack growth, had an average energy release rate of 1.92 J/mm^2 . Assuming the cRS and ENF specimens had the same total energy release rate, then the unload response of the cRS TSR can be assumed to have an energy release rate of 0.25 J/mm^2 . It is important to note that it was not established that the ENF and cRS will produce the same value of energy release rate for Mode II delamination. As the two geometries load the delamination interface different, it is likely that there will be some difference, although one should expect values in a similar range. Nevertheless, the assumption of the same energy release rate in the context of developing TSRs for numerical modelling allowed for the exploration of unload response on cRS and ENF models.

Four-parameter trapezoidal TSRs were fit to the average cRS TSR for use in the FE models presented above. A trapezoidal TSR was selected to provide greater control over the initial stiffness and peak traction of the TSR, as well as to constrain the energy release rate of the TSR under the three regions of the response independently. First, a TSR was numerically fitted to the experimental average of the Mode II cRS specimen while constraining the initial stiffness to match experimental data, fixing the total energy release rate to 1.67 J/mm^2 , and enforcing a sharp unload response. This TSR, referred to as the cRS-based TSR, aimed to replicate experimentally measured Mode II cRS behaviour, including the unstable crack growth, for models using the combined elastic-damage modelling approach (solid red curve, Figure 6.6).

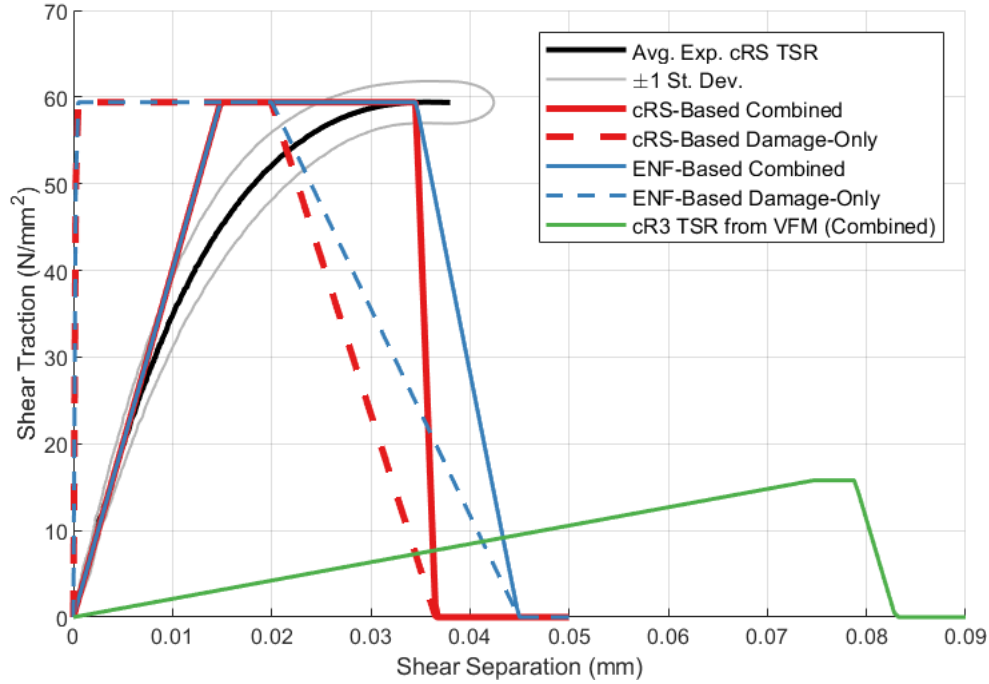


Figure 6.6: TSRs developed for FE modelling of Mode II and III shear delamination for the combined elastic-damage and damage-only modelling approaches. The experimental average cRS TSR with statistical bounds is provided as a reference.

The baseline cRS TSR was augmented using the energy release rate experimentally measured ENF data. Specifically, the duration of the unload response was lengthened to provide 0.25 J/mm^2 of energy release rate in the unload region and a total energy release rate of the TSR of 1.92 J/mm^2 . All behaviour up to the unload response was kept fixed to isolate the effect of the unload response on the Mode II cRS and ENF models. This TSR was referred to as the ENF-based TSR for the combined modelling approach (solid blue line, Figure 6.6).

For both the cRS-based and ENF-based TSRs, the effective stiffness method presented in Section 4.2.2 was used to generate TSRs suitable for the damage-only modelling approach with an initial stiffness of $5(10^5) \text{ N/mm}^3$ (dashed curves, Figure 6.6).

A final TSR was developed specifically for the Mode III cR3 model based on the average TSR response extracted using the VFM method (solid green curve, Figure 6.6). As was established in Chapter 5, TSRs extracted from experimental cR3 data were much lower than expected for the GFRP laminate used in this work. Nevertheless, using the experimental cR3 TSR in the cR3 FE model provided the ability to assess where and why the VFM approach produced unrealistic

responses. The cR3-based TSR was only used with the cR3 FE model. Model parameters for MAT_240 for all trapezoidal TSRs are summarized in Table 6.2.

Table 6.2: Trapezoidal TSR parameters required by MAT_240 for all TSRs used for Mode II and III modelling.

	E (N/mm ³)	σ_s (N/mm ²)	G_{II} (J/mm ²)	f_g
cRS-based Combined TSR	4000	59.4	1.67	0.700
cRS-based Damage-only TSR	5(10 ⁵)	59.4	1.67	0.700
ENF-based Combined TSR	4000	59.4	1.92	0.610
ENF-based Damage-only TSR	5(10 ⁵)	59.4	1.92	0.610
cR3 TSR from VFM	210	11.7	0.362	0.0167

6.2 Results of Mode II and III Delamination Modelling

The results of Mode II cRS and Mode III cR3 FE models are presented in the context of verifying measured TSRs, understanding how specimens behave when loaded, and investigating the effects of machine compliance. The experimental average response and statistical bounds were used to assess the response of cRS and ENF models against experimental data. Owing to high variability and measurement difficulties, cR3 models were only compared against a representative response from a single experimental specimen.

6.2.1 Results of Mode II cRS Modelling

The combined elastic-damage FE model of the Mode II cRS specimen was run using the cRS-based and ENF-based trapezoidal TSRs. As expected, the combined modelling approach reproduced the shape of the input TSR with none of the non-linearity observed in the experimental force-displacement responses (Figure 6.7a). As no experimental unload data was measured, the unload response of the cRS FE models could not be verified. However, regardless of whether a TSR with a sharp unload or a progressive unload was specified, uniform tractions developed across the delamination interface, as the single layer of cohesive elements used in the combined modelling approach could not capture the anisotropy or Poisson's effects of the FRP laminate material.

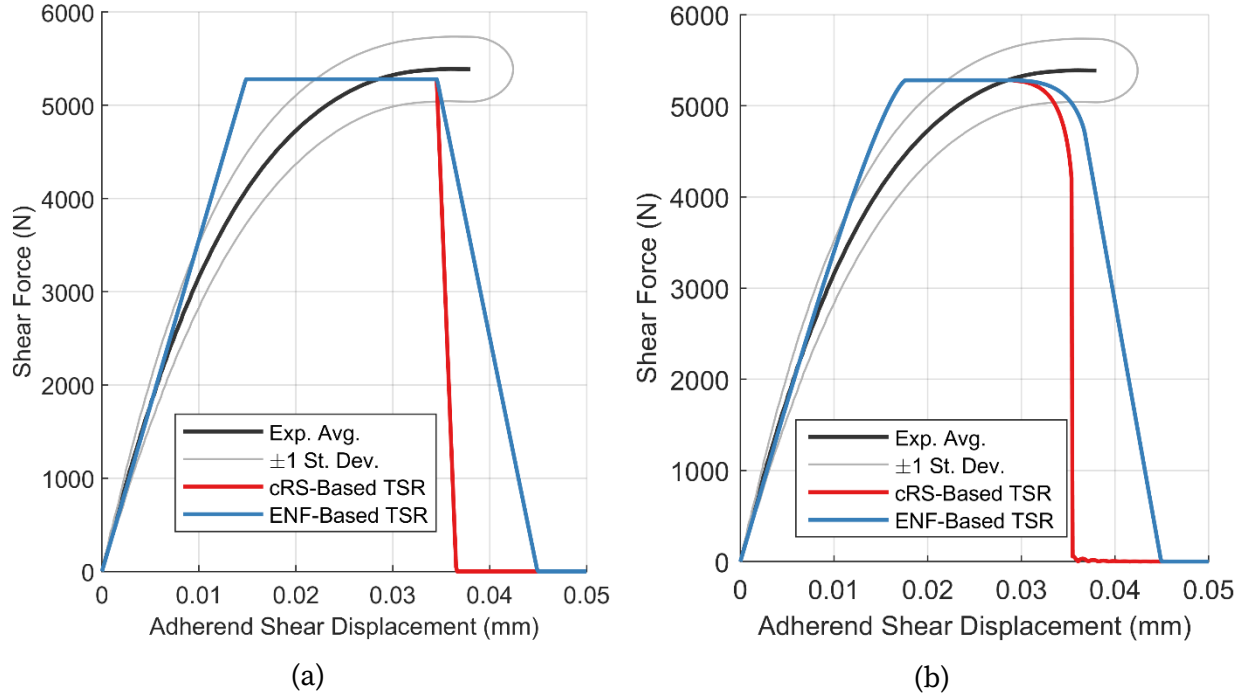


Figure 6.7: The response of FE models of the cRS specimen using (a) the combined elastic-damage modelling approach and (b) the damage-only modelling approach. Two trapezoidal TSRs based on experimental cRS and ENF data were used. Model responses compared against average experimental cRS behaviour.

In contrast with the combined elastic-damage modelling approach, the damage-only modelling approach produced cRS models that exhibited force-displacement responses with some non-linearity prior to peak load (Figure 6.7b), even though the material model used for the FRP laminate did not include damage behaviour and the TSR did not allow for softening before reaching peak traction. Nevertheless, the extent of this non-linearity was much less than that seen in experimental cRS behaviour. Examination of the cohesive elements of the delamination interface showed that a non-uniform distribution of tractions developed within the FE model similar to those observed with DIC in the experiments (Figure 6.8). Peak tractions developed symmetrically at either side of the cRS delamination interface and progressed inward. Non-linearity in the pre-peak response of the cRS FE model was caused by the cohesive elements at the edges of the delamination interface reaching the peak traction of the trapezoidal TSR before the center, reducing the stiffness of the interface as a whole. As with the combined modelling approach, conclusions related to unload response cannot be verified due to the lack of experimental data, although non-linearity was observed when transitioning from a plateau at

peak force to unload response in the FE model. However, unlike experimental cRS results, the same type of unstable crack growth was not observed in numerical modelling.

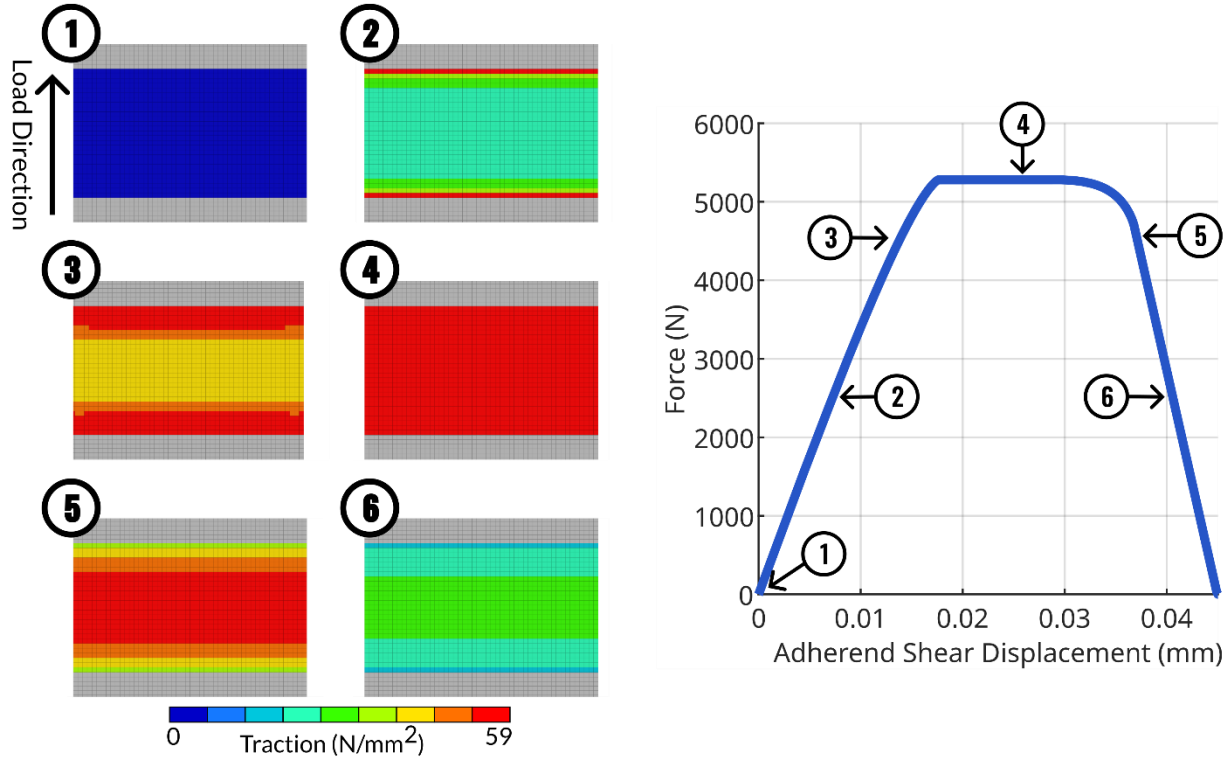


Figure 6.8: Development of tractions in the cohesive elements of the cRS model using the damage-only modelling approach and an ENF-based TSR. Traction contours are shown across the width of the cRS specimen, which would otherwise be covered with the other half of the FRP laminate.

To gain insight into unstable crack propagation in the cRS specimen, which was believed to be a result of the stiffness of the experimental test setup, the damage-only cRS model augmented with a spring was run with stiffness (k_f) of 10^4 , 10^5 , and 10^6 N/mm (Figure 6.2). These stiffnesses broadly corresponded to the range of stiffness common for most physical test frames and test fixturing. The purpose of these models was to estimate the lowest idealized stiffness necessary to achieve stable crack growth.

Stable crack growth required a stiffness approaching 10^6 N/mm (Figure 6.9). At stiffness less than 10^6 N/mm, FE models experienced the rapid deletion of cohesive elements as all cohesive elements reached the plateau or unload phase of the TSR, which is accompanied by an overall decrease in specimen stiffness. During this period of rapid element erosion, similar to unstable

crack growth, elastic energy stored in the spring element was released into the cRS specimen to promulgate crack growth.

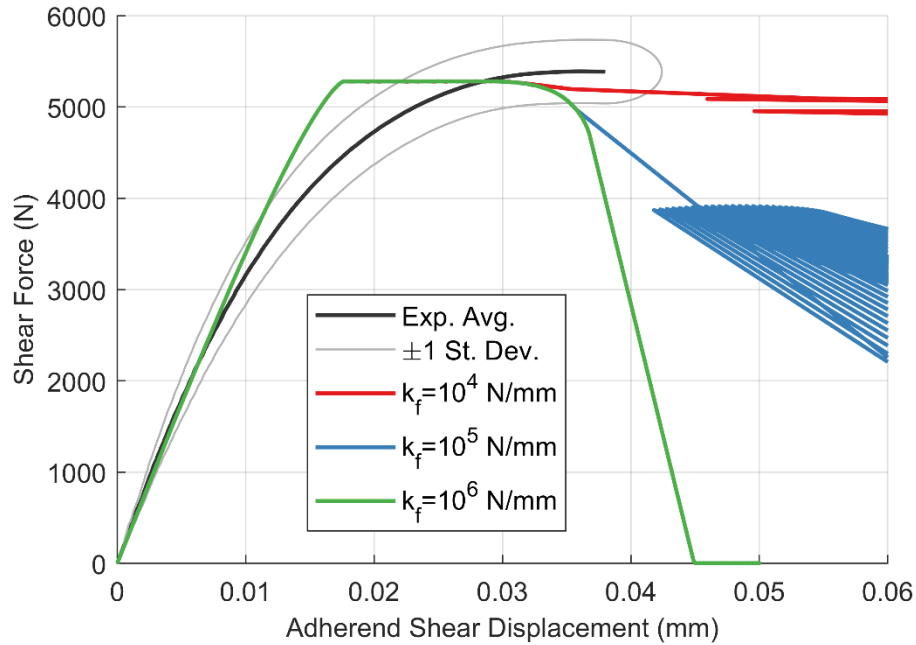


Figure 6.9: The response of the damage-only cRS FE model used to explore the effect of machine and test setup stiffness, k_m , using a single spring element. Three different test setup stiffnesses were tested to determine the limiting stiffness to achieve stable crack growth and assess the unload response of the cRS specimen.

6.2.2 Results of Mode III cR3 Modelling

The Mode III cR3 FE model, using the combined elastic-damage modelling approach, was run using two trapezoidal TSRs: the experimental average trapezoidal TSR extracted using the VFM approach from cR3 specimens and the ENF-based trapezoidal TSR. The ENF-based TSR was selected because the TSR provided a more gradual unload region, in contrast to the vertical unload of the cRS-based TSR. As unload behaviour was observed in the experiment, the cRS-based TSR was not considered appropriate for the Mode III cR3 model. The primary goal of this modelling was to understand why experimental cR3 TSRs extracted in Chapter 5 appeared non-physical and verify the VFM analysis approach.

Neither the average cRS TSR nor the ENF-based TSR predicted force-linear displacement or force-angular displacement well, although for different reasons. The average cR3 TSR

underpredicted peak force but matched the initial slope of the force-linear displacement response (Figure 6.10a). However, the initial slope of the force-angular displacement response was well below the experiment response, with the location of the peak occurring at a much larger angular displacement (Figure 6.10b).

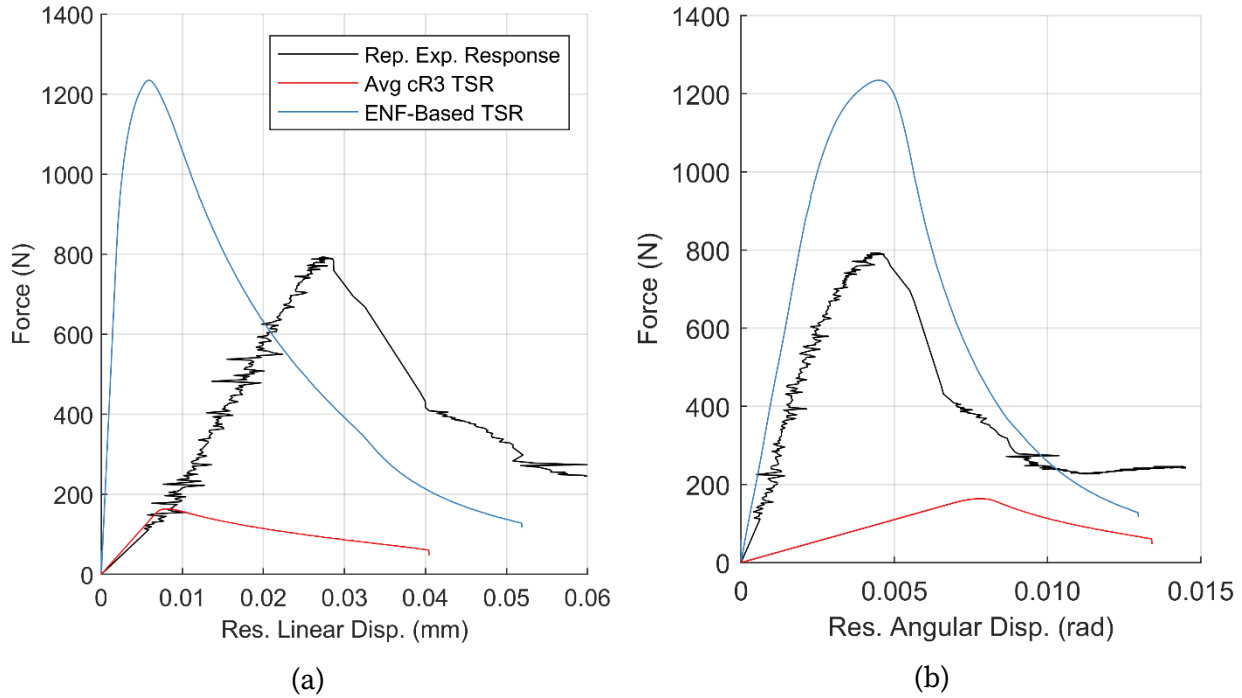


Figure 6.10: The (a) force-linear displacement and (b) force-angular displacement responses of interest for the cR3 FE model. Two TSRs were used, one with parameters based on experimental cR3 data obtained with VFM (red) and the other using the ENF-based TSR providing a progressive unload.

The cR3 model using the ENF-based TSR overpredicted peak force (Figure 6.10), although predicted values for peak force were closer to the experimental data than the average cR3 TSR. Whereas using the cRS TSR produced a model that matched force-linear displacement well, the ENF-based TSR matched the slope of the force-angular displacement better. Further, despite the overprediction of peak force, the overall shape of the ENF-based model matched the trend of the experimental data better. It is also important to note that the cR3 model using the ENF-based TSR greatly underpredicted experimentally measured linear displacement.

An effort was undertaken to find parameters for a trapezoidal TSR that could match the stiffness of the force-linear displacement and force-angular displacement responses

simultaneously. However, no single set of parameters could be found. If the linear displacement of the cR3 FE model were made to fit the experiment data, angular displacement would be too large. Conversely, if the angular displacement of the model were made to fit the experiment, linear displacements would be too small. However, the dichotomy of fitting TSR to the cR3 model revealed some interesting behaviours. When the cR3 FE model was made to fit experimental angular displacement, the strength and energy release rate values of the resulting TSR had magnitudes that agreed relatively well with published literature. However, TSRs made fit to experimental linear displacement tended to produce parameters that agreed more with the average experimental TSRs extracted from the Mode III cR3 specimens tested in this work.

To ensure that the VFM analysis approach developed for the cR3 specimen was correctly implemented, VFM was applied to the cR3 FE model to determine if the TSR inputted to the cohesive elements could be reproduced. As the FE model provided an idealized cR3 response, complicating experimental factors like noise and possible errors in the experimental setup could be eliminated, and only the impact of VFM on measured TSR could be assessed. VFM applied to the FE model used the same kinematic responses measured experimentally. All TSRs used in the cR3 FE model were reproduced using VFM, verifying that the VFM process was implemented correctly.

To highlight the challenges of developing an analytical method for extracting TSRs from cR3 specimens, traction contours for the cRS-based TSR model are shown in Figure 6.11. Peak tractions initiated from the corners of the cR3 delamination interface and progressed inward radially. Very little angular or linear displacement was observed at the center of rotation between the two adherends. Angular displacement dominated cR3 responses prior to reaching peak carrying capacity. As elements along the outer edges of the delamination interface were deleted, the center of rotation shifted rearward, and larger linear displacements of the adherends were observed. As delamination progressed, the center of rotation crossed parts of the interface that had previously experienced inelastic behaviours caused by angular displacement. The complexity of these responses drove the adoption of the VFM analysis approach.

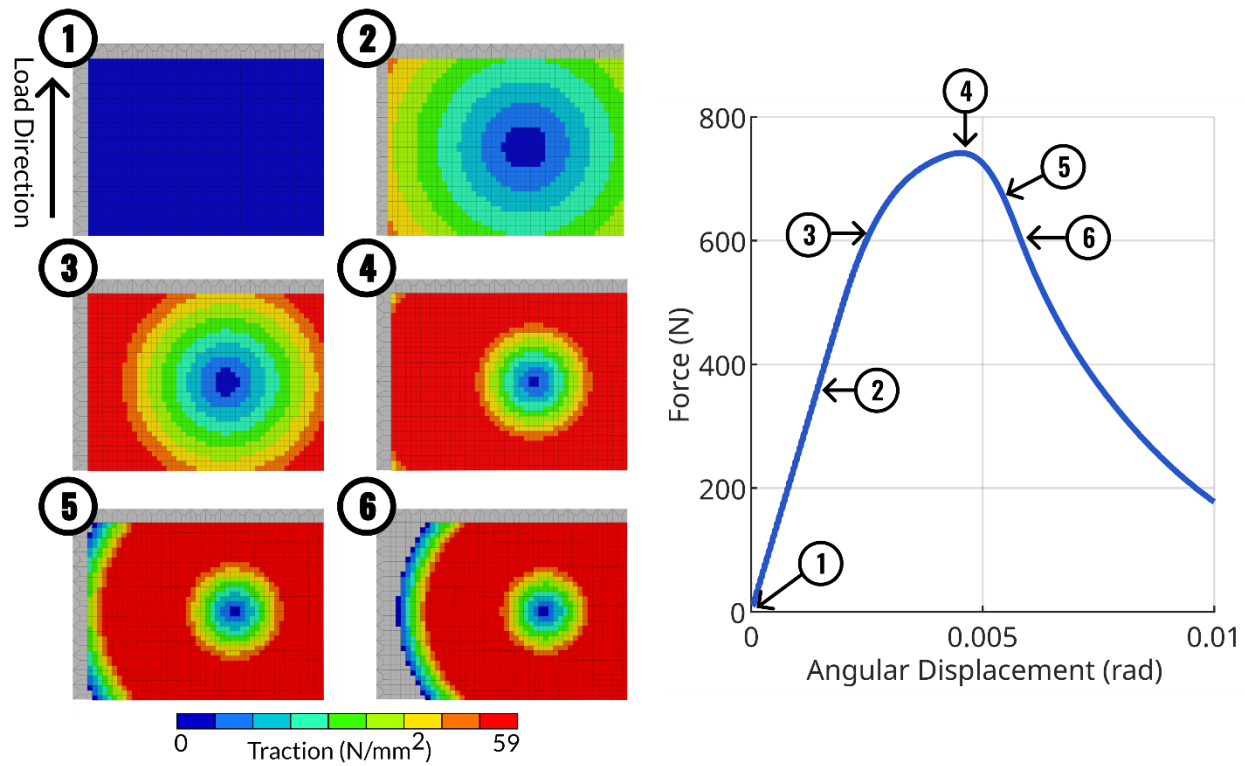


Figure 6.11: The development of traction within the delamination interface of an exemplar cR3 model. Peak tractions initially develop at the corners of the interface before progressing inward radially toward the center of rotation between the two adherends. At large deformation, the center of rotation shifts to the right.

While unstable crack growth was not observed in experimental work (aside from some interface failure due to poor adherend preparation), the effect of the test frame and fixturing stiffness on the cR3 specimen was explored. As with the cRS specimen, machine stiffnesses of 10^4 , 10^5 , and 10^6 N/mm were investigated. Only the stiffer, more physically realistic ENF-based TSR was used to provide a more conservative estimate. Stable crack growth was observed for all stiffness, although the rate of crack growth with a test setup stiffness of 10^4 N/mm was faster than the two explored stiffness. While this rapid crack growth did make extraction of TSRs more difficult for the VFM method due to the reduced information about specimen unload, inputted TSRs could still be reproduced. Regardless, the limiting machine stiffness required to produce stable crack growth in the Mode III cR3 model was more than two orders of magnitude lower than in the Mode II cRS model and on the same order of magnitude as the Mode I cRDCB specimen.

6.2.3 Results of Mode II ENF Modelling

The FE model of the ENF specimen was run using the trapezoidal cRS-based and ENF-based TSRs to assess how the difference in experimentally measured energy release rate impacted modelling accuracy. Of the two TSRs, the ENF-based TSR produced an FE model that predicted peak force very well, almost matching the experimental average peak force (Figure 6.12). However, peak load occurred in the FE model at an earlier displacement than the observed experimentally. The cRS-based ENF, which had a lower energy release rate due to being unable to capture the unload response of the experimental cRS specimen, underpredicted peak force and displacement at peak load. Both TSRs produced FE models that did not reproduce the shape of the experimental force-displacement response. Rather than a smooth transition to unload response post-peak force, the FE models predicted an abrupt drop immediately after achieving peak load.

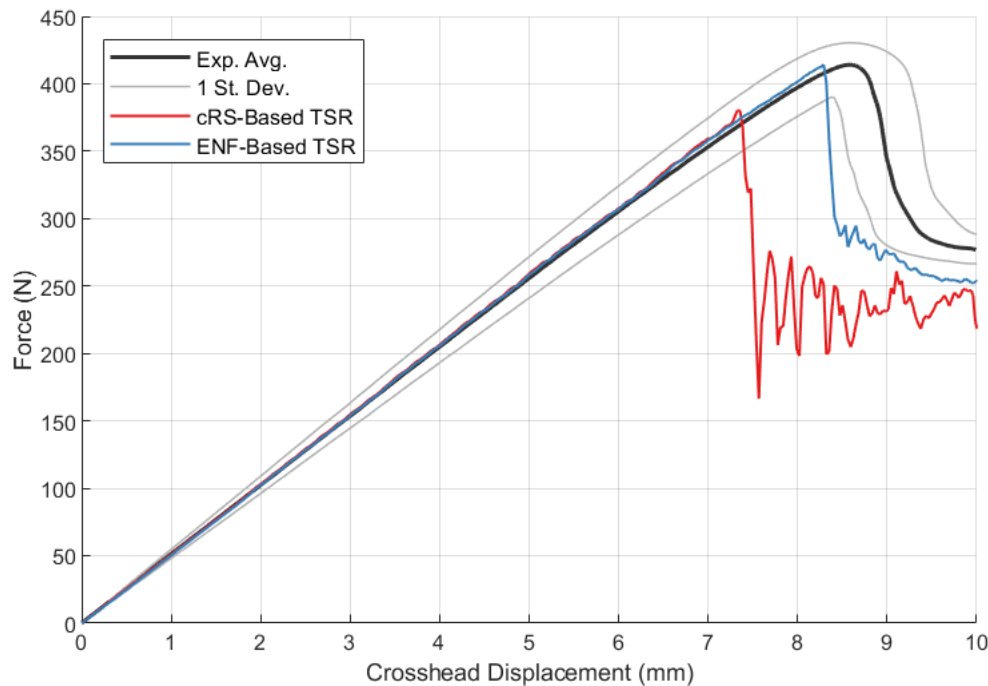


Figure 6.12: Force-displacement response of the ENF FE model using the cRS- and ENF-based trapezoidal TSRs.

6.3 Discussion

6.3.1 Modelling of the Mode II cRS Specimen

One of the reasons for using trapezoidal TSRs to represent delamination response in this chapter was to understand if the non-linearity observed in experimental Mode II cRS TSRs was due to an intrinsic damage response or the non-uniform development of tractions across the cRS delamination interface. DIC measurements of the cRS specimen in Chapter 5 revealed strain concentrations developed at the edges of the delamination interface before propagating inward, an expected behaviour physically that was also replicated using the damage-only modelling approach to predict cRS behaviour (Figure 6.8). Interestingly, the cRS FE model using the damage-only modelling approach did exhibit some non-linearity in the force-displacement response for both TSRs that were used, despite the TSRs themselves having no non-linear decrease in stiffness response (Figure 6.7b). As the non-linearity did not develop using the combined elastic-damage modelling approach, which reproduced the shape of the trapezoidal TSR (Figure 6.7a), it can be concluded that some of the non-linearity observed in the experimental TSR response does stem from an interaction between the delamination interface and the constraint imposed by the adjacent plies. Although comparing the extent of the non-linearity between the FE model using trapezoidal TSRs and the actual experimental data, it was apparent that the underlying TSR measured with the Mode II cRS specimen had to have exhibited softening prior to reaching peak force. While a limited amount of work has been conducted, there is some literature to support that TSRs for Mode II delamination exhibit some degree of softening prior to reaching peak force (de Morais, 2019; Marzi et al., 2014).

The presence of a constraining effect and the stress concentrations in the cRS delamination interface also suggested that directly extracting traction-separation behaviour assuming a uniform distribution of tractions was not completely accurate. An analysis procedure similar to the crack tip compensation function developed for the Mode I cRDCB specimen in Chapter 4 would likely be required to achieve the best accuracy for damage-only TSRs extracted from the cRS specimen.

The same unstable delamination behaviour observed experimentally was not fully replicated in the FE modelling of the cRS specimen using the combined elastic-damage and

damage-only modelling approaches. However, it is important to note that the FE models included two significant simplifying assumptions. First, the adherends were modelled as rigid bodies. Second, the Högberg-style test fixtures were not included. Both assumptions would have allowed elastic deformation energy to be stored in the FE model as the delamination interface was loaded. However, the stiffness of the metallic adherend and fixtures would have been high in comparison to the FRP laminate and the delamination interface, storing relatively little energy. While this small amount of energy would be important to consider for highly detailed modelling, such as that performed in Chapter 4, the significant increase in computational runtimes was not deemed a suitable trade-off for the level of accuracy required for the modelling detailed in this chapter.

While Mode II cRS specimens were tested, both experimentally and numerically, under displacement-controlled conditions, the elastic deformation energy stored in the specimen or the surrounding test frame and fixturing could result in a transition to load-controlled testing if the overall stiffness of the system decreased (Noels, 2015). Loading a cRS specimen into the unload regime of a TSR would reduce the overall stiffness of a specimen, which could promote the transition to load control. Once the transition occurred, stored energy would be released to drive crack growth, further decreasing system stiffness and resulting in the unstable crack growth observed in this work.

To estimate a lower bound on the combined test frame and fixturing stiffness necessary to measure the unload response of cRS specimens, FE models were run with a single spring used to approximate the machine stiffness of the test setup. Modelling revealed that a minimum machine stiffness of 10^6 N/mm was necessary to promote stable crack growth (Figure 6.9). It was not feasible to attain such a high test setup stiffness given the small size of the cRS specimen, as an extremely stiff, large-capacity test frame would be required. Additionally, such a high stiffness requirement would likely preclude using the Högberg-style test fixtures or pin-loading of the cRS specimen due to tolerancing issues. In contrast, FE models suggested that the Mode I cRDCB specimen only required 10^4 N/mm to attain stable crack growth for a delamination interface with the same dimensions.

6.3.2 Modelling of Mode III Delamination with the cR3 Specimen

There were large differences in the response of the Mode III cR3 model using the ENF-based TSR and the experimental average cR3 TSR. Given the previously noted lower-than-expected parameters for the cR3 TSR, such differences were not unexpected. While using the cR3-based TSR in the cR3 FE model produced predictions of peak force that were much lower than expected, comparing the results of the FE model to the relatively independent ENF-based TSR provided a means to understand where experimental measurements differed from numerical predictions. Exploring the impact of linear and angular displacement between the FE model and the experiment revealed that the experimental linear displacements were likely too large. Considering that 3D DIC underestimated the distance between the two measurement faces of the cR3 specimen discussed in Chapter 5, it made sense that experimental linear displacements could be too large. If the rear measurement face were estimated to be closer to the camera than in actuality, the parallax error would produce larger displacements. Since angular displacement measurements were referenced to the initial measured configuration rather than the absolute position in space, such measurements would be relatively immune to parallax error.

While the ENF-based TSR overpredicted peak force compared to the experiment, the force-angular displacement response of the model matched the overall shape of the experimental data well. However, the difference in peak force was quite dramatic. Aside from the possible difference in the fundamental response between Mode II and Mode III delamination, it is important to note that only the combined elastic-damage modelling approach was used with the cR3 specimen in this work. As was noted in the modelling of Mode I delamination in the cRDCB specimen in Chapter 4 and the Mode II cRS specimen in the previous section, the damage-only modelling approach for delamination captures physical phenomena outside the delamination interface that impact delamination response. In particular, both lower peak forces and increased non-linearity were observed when switching from the combined elastic-damage approach to the damage-only approach. The impact of the change could be particularly profound for the Mode III cR3 specimen, given the relative complexity of how delamination damage was initiated and progressed.

In contrast with the Mode I cRDCB and Mode II cRS specimens, the delamination interface of the Mode III cR3 specimen did not experience the onset of damage uniformly across a defined

edge of the delamination interface. Instead, damage was initiated at the corners of the delamination interface due to the rotation of the adherends. While not captured within the combined elastic-damage modelling approach, these corners would be significant stress concentration factors that would likely have been initiated sooner in a damage-only model, where the behaviour of the FRP laminate adjacent to the delamination interface was being modelled independently. As a result, a damage-only cR3 model likely would have experienced a lower peak force. It is also important to note that the VFM analysis used to extract TSRs from experimental Mode III cR3 specimens should have been able to incorporate the effect of the stress concentration.

Given the issues with extracting experimental Mode III cR3 TSRs, the VFM analysis approach was applied to the FE models of the cR3 specimen to ensure the approach functioned as expected to ensure a given TSR could be reproduced. As noted in Section 6.2.2, this verification process successfully demonstrated that the VFM approach worked as expected, providing further evidence of an issue with experimentally measured displacements. The parametric study performed to manually find a TSR that would fit both measured linear and angular displacement helped finally isolate the issue with the experimental data and how it impacted the VFM analysis. As noted, fitting TSRs to match experimental linear displacement produced unrealistically low values of strength and energy release rate, while fitting TSRs to angular displacement produced values that were of the same magnitude as published literature. Given the low values of strength and stiffness extracted from experimental data and the responses of the cR3 and ENF-based TSRs in the cR3 FE model (Figure 6.10), it can be shown that experimentally-measured linear displacements were too large and were dominating any effect of angular displacement during the VFM analysis process. The issue with extracted cR3 TSRs was not that the VFM process was incorrect but that a key part of the experimental data was not being measured correctly, likely due to the stereoscopic calibration used for 3D DIC measurements.

6.3.3 Modelling of the Mode II ENF Specimen

Modelling of the Mode II ENF specimen was carried out to understand how the difference in energy release rate between the cRS-based TSR, which had a near vertical unload response, and ENF-based TSR, which has a more gradual unload response and a higher overall energy release rate that match experimental ENF measurements. Perhaps unexpectedly, the ENF-based

TSR predicted the average peak force of the ENF specimens well. The underprediction of the cRS-based TSR showed that the missing unload response in modelling delamination was important.

Another interesting feature when modelling delamination in the ENF specimen with both the cRS- and ENF-based TSRs was the sharp unload response observed in the FE model, in contrast to the more gradual shape observed in the experiment (Figure 6.12). As both TSRs predicted a sharp unload response, it can be concluded that unload behaviour in the TSR alone was insufficient to model the gradual ENF unload. More likely, a TSR with some degree of pre-peak softening would be required, which is, interestingly, exactly what was measured by experimental cRS specimens. Alternatively, as contact between the two fracture surfaces of the ENF specimen was modelled as frictionless, the introduction of friction could affect the unloading behaviour of the ENF specimen.

6.4 Conclusions

Finite element modelling of Mode II and III delamination using the cRS and cR3 specimens was undertaken to assess the accuracy of experimentally measured TSRs and to gain insight into the response of each specimen. Modelling of the Mode II cRS specimen revealed that some of the non-linearity in experimental TSRs did come from the non-uniform distribution of tractions that developed along the delamination interface. Specifically, stress concentrations at the edges of the delamination interface caused damage to propagate inward from the edges. However, while modelling indicated that a future analysis technique might be needed to account for the stress concentrations at the edges, a more important finding was that most of the measured non-linearity was a result of the physical response of the laminate under Mode II delamination rather than a flaw in the experimental setup. Another key finding was that the Mode II cRS specimen required an extremely high test stiffness to prevent unstable crack growth. Unfortunately, the extremely high stiffness was generally outside the capability of most test frames, limiting the possibility that the cRS specimen could be used to assess unload behaviour.

Modelling of the Mode III cR3 specimen provided vital understanding into why experimentally measured TSRs had unrealistically low values of strength, stiffness, and energy release rate. Modelling revealed that the experimentally measured linear displacement of the adherends was too high, which overwhelmed more accurate angular displacement

measurements when using the VFM analysis approach. In addition to finding areas where future experiments can be improved, FE modelling of the Mode III cR3 specimen also verified that the VFM analysis framework operated as expected and that the cR3 specimen required a far lower test setup stiffness than the Mode II cRS specimen.

Finally, modelling of Mode II delamination in the ENF specimen showed good agreement between the energy release rate measured experimentally for the ENF specimen and predictions of peak traction. However, trapezoidal TSRs were not well suited for predicting the gradual unload response observed in experimental ENF specimens. Rather, modelling suggested that TSRs that exhibited non-linear, softening might predict such behaviours better, which supported the non-linearity measured in experimental Mode II cRS responses.

Chapter 7

CONCLUSIONS AND RECOMMENDED FUTURE WORK

The primary goal of this thesis work was to develop experimental techniques to measure the traction-separation response (TSR) of delamination in fibre-reinforced plastic (FRP) composite laminates. TSRs are important for modern numerical modelling techniques such as cohesive zone modelling (CZM). As was established in Chapter 2, no contemporary techniques directly measure the TSR of delamination for any of the three modes of loading. In particular, standardized specimens such as the Mode I double cantilever beam (DCB) specimen and Mode II end-notched flexure (ENF) specimen do not provide sufficient information to parameterize a TSR fully, necessitating additional test or inverse techniques to fit delamination response for numerical modelling.

The development of a specimen to directly measure Mode I TSRs was detailed in Chapter 3. The composite rigid double cantilever beam (cRDCB) specimen consisted of small metallic adherends co-cured with an E-glass/epoxy laminate. A TSR could be extracted through on-specimen displacement measurement in concert with a specialized analysis technique that produced energy release rates similar to those measured with contemporary experimental specimens.

Chapter 4 presented two analysis procedures to convert experimentally measured TSRs to those suitable for damage-only modelling of delamination with CZM. The analysis procedures provided a physics-informed, energy-conserving approach to convert such TSRs to those with a high initial stiffness suitable for numerical modelling. Additionally, TSRs measured with the cRDCB specimen were enhanced with a stress intensity factor scaling scheme to improve the accuracy of measured cRDCB TSRs by accounting for the complex stress state at the tip of a delamination crack. While the stress intensity factor scaling scheme is currently limited to TSRs measured with the cRDCB specimen, the conversion of experimental TSRs to those suitable for numerical modelling can be applied to other geometries and modes of loading.

Chapter 5 and 6 documented experimental and numerical modelling work focused on developing a low-compliance specimen to assess the shear delamination response of an FRP

laminate. Two specimens were developed: the Mode II composite rigid shear (cRS) specimen and the Mode III composite rigid Mode III (cR3) specimen. The cRS specimen provided excellent resolution of the early-stage damage mechanism. However, the very high stiffness of the cRS specimen produced unstable crack growth. Numerical modelling showed that an unrealistically stiff test setup was required to achieve stable crack growth in the cRS specimen. In contrast, the cR3 specimen provided stable delamination behaviour but utilized an indirect analysis scheme. Regardless, the cR3 specimen enabled the extraction of TSRs describing the full range of delamination response. However, while numerical modelling successfully verified the basic specimen response and the effectiveness of the analysis scheme, experimental difficulties with measuring specimen displacement prevented the physical measurement of TSRs with the cR3 specimen.

7.1 Contributions

The primary contributions of this thesis are as follows:

1. A set of experimental specimens capable of assessing the TSR of Mode I, II, and III delamination of FRP laminates. These specimens, the Mode I cRDCB, the Mode II cRS, and the Mode III cR3, all used rigid metallic adherends to direct load the delamination interface while being small enough to be suitable for future high-rate testing. While two of the three specimens were capable of assessing the full range of delamination damage from onset to ultimate failure, all three specimens provided highly detailed descriptions of the onset and early progression of delamination damage.
2. Detailed modelling of delamination using cohesive zone modelling revealed that as-measured experimental TSRs required additional processing to be suitable for the most common methods of modelling delamination in FRP laminates. Two analysis procedures were developed for TSRs extracted from the Mode I cRDCB specimen to increase the accuracy of numerical modelling within the variability of experimental data. Additionally, it was shown that one of these procedures, the effective stiffness method, was also suitable for Mode II/III TSRs.

3. It was shown that the TSRs measured with the newly developed specimens exhibited some transferability to contemporary specimens such as the DCB and ENF specimens. TSRs from the Mode I cRDCB specimen agreed well with DCB data for early damage behaviours. Additionally, TSRs extracted from the cRDCB supported literature indicating the large-scale Mode I damage response was geometry-dependent. TSRs extracted from the Mode II cRS specimen had energy release rates similar to those measured with the ENF specimen despite challenges with unstable delamination encountered in this work.

7.2 Future Work

This thesis has spawned several avenues for future development and application of low-compliance test specimens to characterize delamination response in FRP laminates. While some of these avenues are devoted to improvements to the process and analysis of the newly developed specimens, the newly developed specimens could provide insight into a range of challenges currently faced by the composite community. However, it is the author's opinion that more work should be devoted to understanding the basic behaviours of the newly developed specimens to ensure that any future conclusions formed with the specimens proposed herein can be confidently attributed to actual material response, not peculiarities of the specimen. To that end, the following work is proposed.

1. An experimental exploration of the effect of laminate thickness on the Mode I cRDCB specimen. In addition to verifying the numerical modelling carried out in Chapter 4, this thickness study would reveal if the TSRs extracted from cRDCB specimens exhibit a geometric dependence like contemporary specimens, particularly for large-scale damage behaviours such as fibre bridging. Furthermore, this research would support other recommended future research, specifically fabricating low-compliance specimens from finished FRP laminates rather than fabricating specimens from prepreg tapes using co-curing. For the E-glass/epoxy material system used in this study, it is recommended to investigate laminate thicknesses of two, four, six, and eight plies.
2. As a compliment to the cRDCB thickness study, it is recommended that much thicker DCB specimens be fabricated and tested. This study would examine if the large-scale

damage response of the thicker DCB specimens would agree better with those of cRDCB specimens. Per the work carried out by Botsis and associates (Canal et al., 2016; Farmand-Ashtiani et al., 2015; Pappas and Botsis, 2020), there is evidence to suggest that large-scale bridging response is tied to the curvature of the fracture surfaces at the crack tip. Thicker DCB specimens would reduce the curvature of the crack faces, which would likely provide boundary conditions more similar to the cRDCB specimen.

3. In addition to assessing the effect of laminate thickness on the cRDCB, a study should be carried out to investigate if a modified cRDCB specimen geometry can be created with a larger interface length to assess the response of large-scale Mode I damage mechanisms in greater detail. In order to maintain specimen rigidity while maintaining the ability to achieve stable crack growth, a modified specimen would need to be scaled in terms of overall length as well as interface length.
4. Work should be conducted to improve the accuracy of the two analysis procedures developed to convert experimental TSR to those suitable for damage-only modelling of delamination. While the present analysis enabled numerical modelling to within experimental variability, two elements of the analysis procedure could be improved. First, the stress intensity factor calculation for the cRDCB specimen could be improved to account for the mixed plane strain and plane stress condition at the crack tip. Second, the separation of small-scale matrix damage from large-scale fibre bridging could likely be improved to enhance the latter and provide a pathway toward geometry-independent results.
5. Further development work should be committed to the Mode III cR3 specimen. Several process and measurement improvements are suggested for the next round of specimen fabrication and testing, including:
 - a. Refinements to fabrication and curing fixturing to increase fibre volume fraction and improve the quality of the FRP laminate to be comparable to the cRDCB specimens presented in Chapter 3. Only minor changes to the curing fixturing presented in Chapter 5 should be required, such as adjusting the thickness of spacers to promote more compaction of the prepreg tapes.

- b. 2D DIC should be utilized to measure the linear and angular displacement of the specimen more accurately. 3D measurements could not account for the stepped measurement planes in the cR3 specimen accurately. Further, the stereoscopic calibration required for 3D measurements creates an inherently higher measurement noise floor than 2D DIC, which is important given the small linear displacements measured in this work. Custom 3D-printed shims are recommended to provide a flat imaging plane. These shims would be printed near the step size of the specimen, secured to the rear face of the specimen with fasteners, and ground plane with the face of the front adherend using abrasive paper. These shims must not extend to the pin used to load the rear adherend to eliminate the possibility of the loading pin perturbing the measurement plane.
- c. The VFM process should be enhanced to fit TSRs without relying on a specific analytical form, such as the trapezoidal TSR used in Chapter 5. While any TSR extracted with VFM will require a functional form with parameters that can be optimized, a spline-based approach is recommended for maximum flexibility. In particular, M-splines (Ramsay, 1988) are suggested as the basis functions are non-negative, and both weights and knot locations can be tuned.

Once it has been shown that cR3 specimens can extract TSRs and be produced repeatedly and with good quality, work should be carried out to explore the optimal fibre orientation for the laminate in the specimen. In this thesis, fibres were parallel with the long edge of the specimen. Fibres perpendicular to the long edge of the specimen should be investigated to determine if the probability of intralaminar ply splitting is minimized or if fibre orientation has any effect at all.

- 6. A detailed assessment of shear properties measured with the Mode III cR3 specimen against standardized ENF specimens should be performed. This assessment would determine if there were significant differences between Mode II and Mode III delamination. Numerical modelling would play a critical role in this assessment, similar to the DCB modelling conducted in Chapter 4. This work would also

determine if a similar stress intensity factor-based analysis scheme developed for the cRDCB specimen is necessary for the cR3 specimen to enable damage-only modelling of shear delamination.

7. A large-scale, independent validation of the delamination response is recommended to provide confidence in TSRs measured with the newly developed specimens. The nature of this testing would be challenging, as there are no well-established techniques for this type of work. Further, this type of testing would likely introduce intralaminar damage in addition to the desired interlaminar failure. One proposed specimen geometry is a plate with a circular delamination defect introduced at the midplane of the plate during layup. A hole, centred in the defect, would extend from the top face of the specimen down to a circle PTFE film crack starter, where a load would be applied to the lower portion of the plate to introduce nominally Mode I delamination on a large scale (Figure 7.1). Provided the overall applied displacement is low, large-scale damage regimes such as fibre bridging should play a relatively small role in the overall response.

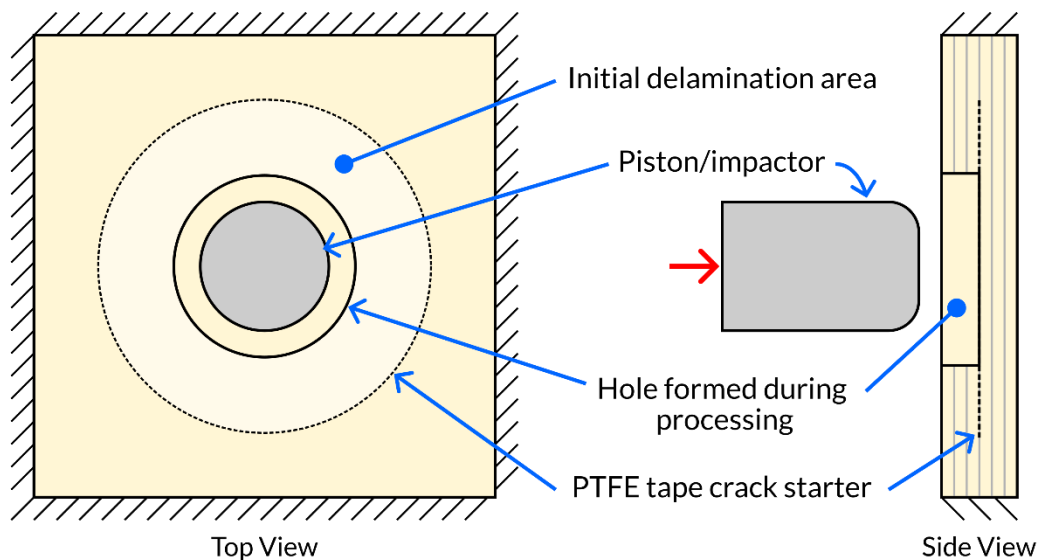


Figure 7.1: The proposed partial hole-in-plate (PHIP) specimen for large-scale delamination response validation. The plate would be securely fixed at all sides, and a piston or impactor would be used to introduce a load through a pre-fabricated hole in the specimen. Specimen features are not to scale.

8. Mixed-mode testing of delamination should be investigated using modified versions of the low-compliance specimens used here. However, the development of mixed-mode, low-compliance specimens would likely entail a large amount of development work both experimentally and in terms of analysis techniques.
9. The compatibility of the rigid adherend, low-compliance specimens with alternative material systems and processing methodologies should be investigated. Current work relied on co-curing the adherend to prepreg tapes. Not all processing methods lend themselves to making specimens in this way. Investigations should be conducted to determine if the low-compliance specimens can be adapted for use with finished FRP laminates, similar to how DCB and ENF specimens are extracted from larger plates. The obvious solution to this problem is adhesively bonding the adherend to the finished laminates. However, the effect of the adhesive on overall specimen compliance and local deformation around the crack tip will need to be investigated. Modifications of the analysis methods presented in this work will likely be needed.
10. High-deformation rate testing should be explored. High-deformation rate testing was the primary goal of producing small and lightweight specimens. The deformation rate sensitivity of delamination remains a relatively underexplored subject, with great uncertainty regarding the overall trends in material response.

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APPENDIX A

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Figure 2.1: Raju, I.S., O’Brien, T.K., 2008. Fracture mechanics concepts, stress fields, strain energy release rates, delamination initiation and growth criteria, in: Sridharan, S. (Ed.), Delamination Behaviour of Composites, Woodhead Publishing Series in Composites Science and Engineering. Woodhead Publishing, pp. 3–27. <https://doi.org/10.1533/9781845694821.1.3>

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