

EVALUATING THE EFFECT OF DEFECTS ON CFRP- CONCRETE BOND PERFORMANCE: EXPERIMENTAL AND ANALYTICAL INSIGHTS

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ABSTRACT

The use of Carbon Fibre Reinforced Polymer (CFRP) as a strengthening material for concrete structures has gained popularity in recent decades due to its lightweight, high stiffness, high strength, and corrosion resistance, making it a viable alternative to conventional materials like steel plates. However, the effectiveness of this repair technique depends largely on its bond performance and interfacial quality, which ensure the structural integrity and reliability of the strengthening solution. A gap currently exists in efficiently investigating bond integrity in the presence of interfacial deteriorations and defects.

This study investigates the bond behaviour of ten high-strength concrete prisms externally strengthened with CFRP laminates under various ply and defect conditions. Single-Lap Shear Tests (SLST) and 3D Digital Image Correlation (DIC) analysis were employed to examine strain distribution, shear and slip behaviour, and load-slip characteristics in the presence of interfacial imperfections.

The results revealed that specimens primarily failed due to adhesive cohesive failure in a mixed-mode rupture, with stress concentrations near defects leading to localized peeling and bond deterioration. Reduced axial stiffness decreased load capacity, caused less uniform stress distribution, and promoted localized peeling, ultimately reducing system efficiency. Experimental bond-slip curves were derived for control specimens, and a unified model based on the bilinear model by Lu et al. (2005) showed strong agreement with experimental results, offering a balance of practicality and accuracy. Future work should focus on refining the calibration with specimen-specific coefficients and incorporating environmental factors to improve predictive accuracy.

Defects near the loaded end weakened stiffness locally, increasing slip around the defect region. Full-width defects showed limited capacity for stress redistribution, while middle-positioned defects caused load drops proportional to their size, potentially triggering localized debonding. Edge-aligned defects reduced load capacity by 33% compared to the control specimen and created stress concentrations on the opposite side of the bond area. Dual defects exhibited reduced initial stiffness, intermediate load drops, and potential recovery toward ultimate capacity, though not always resulting in significant strength reduction. Calibration of Li et al.'s (2023) load drop model reduced mean absolute percent error (MAPE) from 45.1% to 13.5%.

Overall, this study provides valuable insights into CFRP-concrete bond behaviour in the presence of defects and offers a solid foundation for future research and practical applications.

RÉSUMÉ

L'utilisation de polymères renforcés de fibres de carbone (PRFC) comme matériau de réparation pour les structures en béton armé a gagné en popularité ces dernières décennies de par leur légèreté, leur rigidité élevée et leur résistance au milieu corrosifs, faisant de ce matériau une alternative viable aux méthodes conventionnelles tels que les plaques en acier. Cependant, l'efficacité de cette technique de réparation dépend largement de la performance de l'adhérence et de la qualité de l'interface, qui garantissent l'intégrité structurelle et la fiabilité de la solution de renforcement. Bien que certaines recherches se penchent sur la durabilité des structures réparées, une lacune persiste quant à la compréhension de l'intégrité et de la qualité de l'adhérence en présence de détériorations et de défauts interfaciaux.

L'étude actuelle examine le comportement de l'adhérence de dix prismes en béton à haute résistance renforcés extérieurement avec des laminés PRFC sous différentes configurations de plis et de défauts. Des essais de cisaillement monotones (Single-Lap Shear Tests) et une analyse par corrélation d'images numériques en 3D (3D DIC) ont été utilisés pour étudier la distribution des contraintes, le comportement en cisaillement et glissement, ainsi que les caractéristiques charge-glissement en présence d'imperfections interfaciales.

Les résultats ont révélé que les échantillons ont principalement failli en raison d'une rupture cohésive de l'adhésif dans un mode de rupture mixte, avec des concentrations de contraintes près des défauts entraînant un décollement localisé et une détérioration de l'adhérence. De plus, il a été observé que la diminution de la rigidité axiale du PRFC a induite une diminution la capacité de charge, entraînant une distribution moins uniforme des contraintes, favorisant ainsi le décollement localisé tout en réduisant l'efficacité du système. Des courbes expérimentales charge-glissement

ont été établies pour les spécimens de contrôle, et un modèle unifié basé sur le modèle bilinéaire de Lu et al. (2005) a montré une forte concordance avec les résultats expérimentaux, offrant un équilibre entre praticité et précision. Les travaux futurs devraient se concentrer sur l'affinement de l'étalonnage avec des coefficients spécifiques aux échantillons et l'incorporation de facteurs environnementaux pour améliorer la précision prédictive.

Les défauts situés près de l'extrémité chargée ont affaibli localement la rigidité, augmentant le glissement dans la région du défaut. Les défauts de pleine largeur ont montré une capacité limitée à redistribuer les contraintes, tandis que les défauts positionnés au centre ont causé des chutes de charge proportionnelles à leur taille, pouvant déclencher un décollement localisé. Les défauts alignés sur les bords ont réduit la capacité de charge de 33 % par rapport au spécimen de contrôle et créé des concentrations de contraintes du côté opposé de la zone d'adhérence. Les doubles défauts ont présenté une rigidité initiale réduite, des chutes de charge intermédiaires et une récupération potentielle vers la capacité ultime, bien que cela ne réduise pas toujours de manière significative les performances. L'étalonnage du modèle de chute de charge de Li et al. (2023) a réduit l'erreur absolue moyenne (MAPE) de 45,1 % à 13,5 %, soulignant la robustesse du modèle.

Dans l'ensemble, cette étude apporte des informations précieuses sur le comportement de l'adhérence PRFC-béton en présence de défauts et offre une base solide pour les recherches futures et les applications pratiques.

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LIST OF ABBREVIATIONS & SYMBOLS

Symbol	Definition
ω	= adhesive parameter (Wang and Wu 2018)
α, β	= empirical coefficient for exponential bond slip model
A, B	= empirical coefficient for exponential bond slip model
a, b, c, d, e, f, g, h	= empirical coefficients
A_b	= bond area
A_d	= defect area
AVG	= average
b_c	= width of concrete block
b_f	= width of FRP laminate
CSP	= concrete surface profile
CV	= coefficient of variation
DIC	= digital image correlation
$d\varepsilon_f/dy$	= FRP strain gradient
E_a	= adhesive modulus of elasticity
EB FRP	= externally bonded FRP
EBL	= effective bond length
E_c	= concrete modulus of elasticity
E_f	= FRP modulus of elasticity
f_c	= concrete compressive strength
f_{au}	= adhesive ultimate strength
f_{fu}	= FRP ultimate tensile strength
FRP	= fibre reinforced polymers
f_t	= concrete tensile split strength
G_a	= shear modulus of the adhesive
G_f	= interfacial fracture energy
$G_{\text{interface}}$	= shear modulus of the interface (Lorenzis et al. 2001)
G_{primer}	= shear modulus of the primer (Lorenzis et al. 2001)

G_{resin}	=	shear modulus of the epoxy resin (Lorenzis et al. 2001)
k_s	=	defect area ratio
L_b	=	bonded length of FRP sheet
L_c	=	length of concrete block
L_e	=	effective length of FRP
L_f	=	total length of FRP sheet
L_o	=	overhand length of FRP sheet
L_u	=	unbonded length of FRP sheet
MAE	=	mean absolute error
MAPE	=	mean absolute percentage error
MAX	=	maximum
MIN	=	minimum
n	=	specimens number, popovic coefficient
n_f	=	number of plies
P_u	=	ultimate failure load
$P_{u, \text{experimental}}$	=	experimental ultimate load
$P_{u, \text{theoretical}}$	=	theoretical ultimate load
s	=	slip
s_i	=	slip at a point y_i
SLST	=	single lap shear test
s_m	=	peak slip
s_u, s_f	=	ultimate slip
t_a	=	thickness of adhesive
t_f	=	thickness of FRP
t_{primer}	=	thickness of the primer (Lorenzis et al. 2001)
t_{resin}	=	thickness of the epoxy resin (Lorenzis et al. 2001)
w/c	=	water to cement ratio
y	=	longitudinal coordinate
β_l	=	bond length factor
β_w	=	bond width factor

δ_c	=	concrete displacement
δ_{FRP}	=	FRP displacement
ΔP	=	load drop
ϵ_{cu}	=	concrete ultimate strain
ϵ_d	=	debonding strain
ϵ_f	=	FRP strain
$\epsilon_{f,max}$	=	maximum strain during shear test
ϵ_{fu}	=	FRP ultimate strain
ν	=	poisson ratio
σ_f	=	FRP axial stress
τ_i	=	shear stress at a point y_i
τ_m	=	maximum bond stres

CHAPTER 1. INTRODUCTION

1.1. Background

The use of externally bonded (EB) fibre-reinforced polymers (FRP) for strengthening and repairing reinforced concrete (RC) structures has gained significant popularity in recent decades. These systems represent an innovative and efficient alternative to conventional materials, such as steel, due to their superior strength-to-weight ratio, lightweight nature, and excellent resistance to corrosion (fib 2001; Sen 2015; Li et al. 2023; Dolati et al. 2023).

Despite these advantages, the governing failure mode of EB FRP is brittle debonding, which occurs before reaching full capacity and compromises their efficiency. Such failures can be initiated or exacerbated by degradation at the FRP-concrete interface, caused by factors including poor workmanship, improper curing, substandard material quality, and adverse exposure conditions. Among these, moisture penetration is particularly critical, as it can significantly compromise the interface integrity, leading to premature brittle rupture (Sen 2015).

The quality of the interface plays a crucial role in the overall performance of EB FRPs, with numerous factors contributing to its potential weakening over time. Defects introduced during installation or service can exacerbate this issue. A notable example is the Champlain Bridge, which underwent multiple strengthening efforts during its service life. Initially repaired with carbon fibre-reinforced polymer (CFRP) to address corrosion-related deterioration, the structure was exposed to harsh environmental conditions, including freeze-thaw cycles, de-icing salts, and other aggressive factors (Fowai et al. 2023).

Non-destructive testing (NDT) assessments revealed numerous delamination and defects within the interface of the strengthened diaphragm, varying in depth, severity, and location. These debonding issues were attributed to a combination of external loading, harsh exposure conditions, and man-made installation errors, such as improper epoxy rolling and fabric misalignment. While visual inspections identified severe delamination in substrate regions, infrared thermography further detected delaminated areas along the strengthened diaphragm, particularly in the anchorage strips, as illustrated in Fig. 1-1 (Fowai et al. 2023).

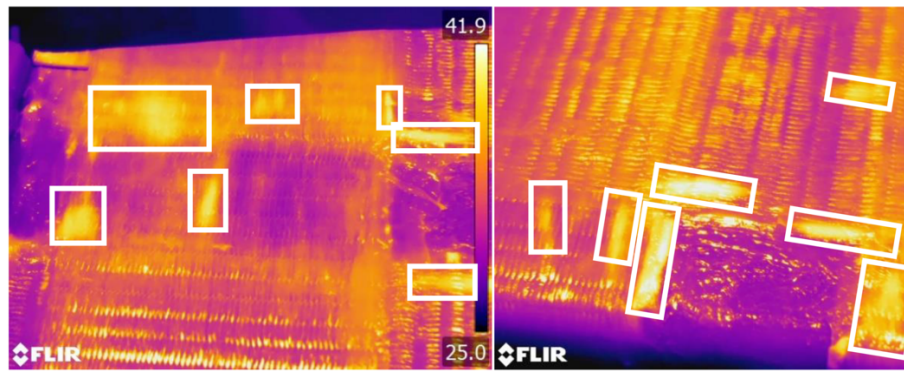


Fig. 1-1. Debonded areas detected in the anchorage strips by infrared thermography technique on the Champlain Bridge strengthened diaphragm (Fowai et al. 2023).

The presence of such defects and delamination necessitates the use of a combination of NDT techniques for accurate detection and characterization (Fowai et al. 2023). These defects pose potential risks to the overall efficiency and reliability of the repair system. Therefore, assessing the criticality of these defects is essential to evaluate bond performance and ensure the structural integrity of strengthened systems encountered in the field.

This thesis aims to evaluate the impact of interfacial defects on the bond performance of EB FRP applied to high strength concrete prisms using experimental and analytical approaches.

1.2. Research Objectives & Scope

The fundamental objective of this study is to evaluate the bond behaviour between CFRP sheets and concrete substrates, with a specific focus on the effects of interfacial defects on bond integrity. This research aims to:

1. Investigate the bond-slip behaviour of CFRP-strengthened concrete prisms using shear lap tests, with a focus on specimens of consistent stiffness and ply configurations.
2. Review and evaluate existing bond-slip models from the literature, and calibrate an analytical model based on a selected approach tailored to the needs of this study.
3. Contribute to the understanding of how interfacial imperfections impact the efficiency of CFRP strengthening in concrete structures with specific attention to the orientation and position of defects relative to the load transfer path.
4. Investigate defect-induced variations in strain distribution and debonding mechanisms using 3D DIC techniques.
5. Provide insights into limitations of the current state of the art and offer recommendations for future research.

1.3. Thesis Outline

The thesis consists of six chapters:

- Chapter 1 introduces the research scope and objectives.
- Chapter 2 provides a comprehensive literature review and background on CFRP-concrete bond slip behaviour, as well as the impact of interfacial defects.
- Chapter 3 details the experimental program and methodology employed in this study.

Chapter 1. Introduction

- Chapter 4 presents the experimental results, including material results and properties, shear lap testing, 3D DIC investigations, and the analytical work conducted to calibrate the bond slip models.
- Chapter 5 discusses the results, their implications, and the limitations encountered during the investigation.
- Chapter 6 concludes the study and offers recommendations for future research.

CHAPTER 2. LITERATURE REVIEW

2.1. General

Concrete is the most widely used building material globally, but reinforced concrete (RC) structures are susceptible to various forms of damage throughout their service life. These damages may arise from factors such as increased external loading, exposure to harsh weather conditions, durability challenges, or construction errors (Pan and Leung 2007; Dolati et al. 2023; Li et al. 2021).

Fibre-reinforced polymer (FRP) materials, originally used in naval, automotive, and industrial applications, have gained widespread popularity in structural engineering due to their innovative and resilient properties. In RC structures, FRPs have become prominent in both internal reinforcement, primarily as composite rebars, and as an external repair and strengthening material (NRC 2006; fib 2001; Camli and Binici 2007).

Externally bonded (EB) FRP systems are typically categorized into three types (NRC 2006; fib 2001):

- pre-cured pultruded plates applied directly to concrete elements with epoxy resin using the dry lay-up method.
- wet lay-up systems, where dry fabric is impregnated with saturant on-site; and composite rebars adhered to the substrate via grooves.
- or directly using epoxy in near-surface mounted (NSM) systems.

Chapter 2. Literature Review

While wet lay-up systems, which use flexible dry sheets, are widely used, they are prone to human error during installation, and in-situ conditions can introduce defects or irregularities that may affect the system's overall effectiveness (Karbhari et al. 2005).

A typical EB FRP material consists of composite fibres, which provide load-bearing capacity and stiffness, coupled with a polymer matrix that ensures proper stress distribution along the fibres (NRC 2006; Li et al. 2021; Pellegrino and Sena-Cruz 2016). Among FRP materials in the literature, carbon and glass fibre-reinforced polymers (CFRP and GFRP, respectively) are the most common (Vaculik et al. 2018). CFRP is the most widely used material for repair and strengthening as it offers superior tensile strength compared to GFRP and conventional steel (Sen 2015). Fig. 2-1 illustrates typical tensile stress-strain behaviour of various FRP materials compared to mild steel, demonstrating CFRP's significant advantage in tensile strength and stiffness which improves repair efficiency while reducing material usage (Sen 2015; Hollaway 2010).

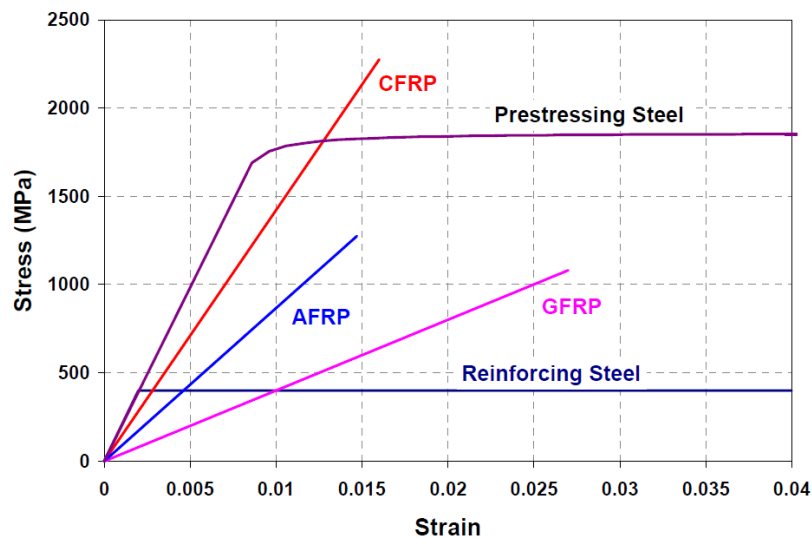


Fig. 2-1. Tensile stress-strain diagrams for FRPs and steel materials (ISIS 2008).

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Despite their advantages, the durability of EB FRP systems can pose some challenges. Exposure to acidic and alkaline environments, elevated temperatures, and UV radiation can degrade the strength and stiffness of these materials (Dolati et al. 2023). For instance, Al-Roussan et al. (2013) found that exposure to sulfate could degrade the bond strength and interfacial energy to a residual value of 63% and 36%, respectively.

Additionally, the organic matrix in FRPs is particularly sensitive to UV and temperature exposure, which can lead to a loss of mechanical properties (Pellegrino and Sena-Cruz 2016). Moisture penetration at the EB FRP interface is another vulnerability, exacerbated by temperature fluctuations (Sen 2015; Cabral-Fonseca et al. 2018).

Thus, despite their numerous benefits, there is a lack of in-field assessment of CFRP strengthened concrete structures and insufficient realistic durability evaluations of FRP repair systems. These limitations highlight the need for a comprehensive understanding of how environmental factors interact, as these systems are often exposed to multiple stressors simultaneously (Cabral-Fonseca et al. 2018), which could weaken the bond integrity, causing defects and deterioration in the interface and lead to a premature failure of the repair system.

This chapter begins with a review of the various bond mechanisms and parameters—such as material properties and anchorage length—and their influence on interface bond behaviour (Section 2.2). It will then explore both non-destructive and destructive bond evaluation methods used in the field and laboratory settings (Section 2.3). The bond-slip relationship will be examined (Section 2.4), with a comprehensive presentation and categorization of different bond-slip models available in the literature, followed by a concluding summary of the relevant parameters and considerations. A dedicated section on digital image correlation (Section 2.5)

will assess the technique in relation to the study's objectives, providing a solid foundation for the research. The final section (Section 2.6) will focus on defects in EB FRP-concrete systems, categorizing various types of deterioration, delamination, and imperfections and their impact on bond performance. The chapter will conclude with a summary of the identified research gaps (Section 2.7).

2.2. Interface Bond Behaviour

EB FRP systems involve adhering composite materials to a prepared and roughened concrete substrate using a two-part epoxy adhesive. The typical failure mode for these systems for normal strength concrete is delamination within the concrete substrate (Hadigheh et al. 2012; Pan and Leung 2007; Hao et al. 2024; Vaculik et al. 2018), as shown in Fig. 2-2. While bond failure is often dominated by pure shear stresses (or pulling) at the interface, flexural and shear cracks can lead to a mixed-mode failure, resulting in both pull and peel effects, as illustrated in Fig. 2-3.

The bond quality and performance of EB FRP in concrete structures are influenced by the interaction of three key components: the FRP material, the epoxy adhesive, and the concrete substrate. Additionally, multiple stress states, including peeling, pulling, and mixed-mode states, contribute to the bond's efficiency. In this section, the various bond parameters related to the properties of the concrete, FRP, and adhesive will be explored. Surface preparation, anchorage effects and bond length, as well as the influence of FRP bond width will also be examined, laying the groundwork for the subsequent sections of this Chapter.

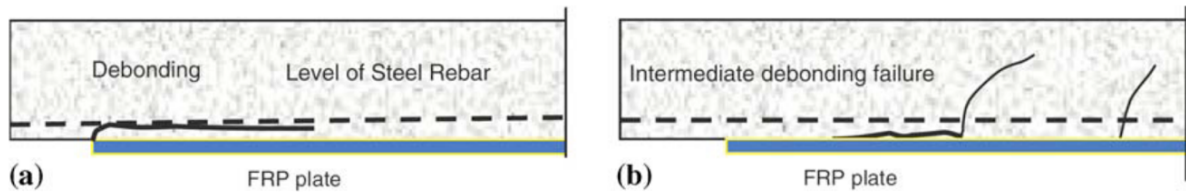


Fig. 2-2. Typical flexural failure of an EB FRP strengthened RC beam: (a) peel failure at the free end; (b) intermediate debonding failure from flexural/shear crack (Pan and Leung 2007).

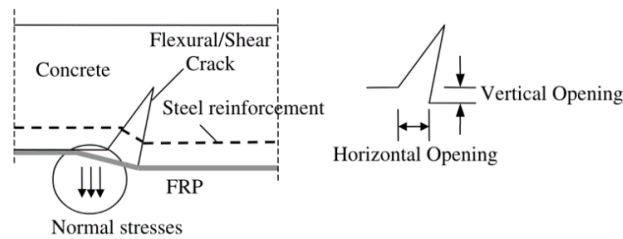


Fig. 2-3. Debonding induced by mixed mode loading from a combined pull-peel effect (Pan and Leung 2007).

2.2.1 Concrete Properties

The bond integrity between EB FRP and concrete in repaired RC structures is significantly influenced by the strength and composition of the concrete. When failure is governed by concrete debonding (i.e. concrete cohesive failure), the peak shear strength is directly proportional to the square root of the concrete's strength, highlighting the importance of concrete quality (Chajes et al. 1996).

Similarly, research has shown that peak bond stresses increase with the concrete's compressive capacity (Nakaba et al. 2001; Guo et al. 2005; Vaculik et al. 2018), although other factors play

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crucial roles as well (Mensah et al. 2020). Specifically, the bond resistance is more closely linked to the content and composition of coarse aggregates rather than just compressive or tensile split strengths (Pan et al. 2007). The bond also improves with a higher sand ratio in the concrete mix (Yang et al. 2022).

However, while higher compressive strengths generally increase bond stresses and ultimate slip, they may also lead to less effective bond performance, reduced strain, and no significant effect on the ultimate peak load (Garcez et al. 2021; Issa et al. 2016). Li et al. (2024) demonstrated that higher compressive strengths led to smaller strain gradients. High compressive strength can also diminish bond interlock, reducing both bond strength and interfacial fracture energy, which negatively affects bond quality and can change the debonding mode from mixed interfacial failure to cohesive adhesive failure (Shrestha et al. 2015). Conversely, it was found that specimens with poor or no surface treatments in single shear tests had a mixed mode of failure, combining both concrete shearing and cohesive adhesive failure, compared to pure concrete cohesive failure for the roughened specimens (Biscaia et al. 2015).

Additionally, the concrete strength is especially influential when the sheet width is smaller than the concrete element width, affecting overall bond performance (Wu and Jiang 2013).

2.2.2 FRP Properties

Axial stiffness is proportional to the cross-sectional area of the laminates, and its influence on bond performance remains a topic of discussion. Some researchers emphasize its crucial role in determining the bond integrity between EB FRP and the concrete substrate (Lorenzis et al.

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2001). Higher axial rigidity is typically associated with increased peak ultimate loads and enhanced overall bond capacity (Nakaba et al. 2001). Mensah et al. (2021) found that increasing both FRP thickness and axial rigidity significantly improved load capacity, further supporting the idea that axial stiffness plays a role in optimizing bond performance. Conversely, a reduction in axial stiffness can shift the failure mode from concrete shearing to interfacial debonding, compromising bond quality and potentially leading to premature failure (Garcez et al. 2021).

On the other hand, some studies suggest that higher axial rigidity does not necessarily improve bond performance in all cases. Czaderski et al. (2010) found that increasing FRP axial rigidity led to a decrease in both interfacial fracture energy and peak shear stress, indicating that greater stiffness may alter energy dissipation rather than uniformly strengthening the bond. While Pellegrino and Modena (2019) and Li et al. (2024) note that axial rigidity is often neglected, other researchers argue that bond strength and integrity are primarily governed by the substrate's capacity rather than the FRP stiffness itself (Nakaba et al. 2001; Lu et al. 2005; Verstrynge et al. 2016). As such, most bond-slip models in the literature do not incorporate FRP stiffness as a direct parameter, as will be discussed in subsection 2.4.

Additionally, Zhu et al. (2014) demonstrated that FRP material type also influences bond performance. Their double lap shear tests revealed that basalt FRP (BFRP) configurations, particularly when combined with CFRP (with BFRP applied first), delayed debonding, enhanced interfacial energy, and increased the effective bond length, leading to improved bond integrity (Zhu et al. 2014).

2.2.3 Adhesive Properties

Adhesive properties significantly influence bond performance and interfacial integrity in FRP-concrete repair systems (Diab and Farghal 2014; Sedlmair and Stempniewski 2019; Shi et al. 2019; Kwiecień et al. 2018) as it is considered the weak point at the FRP-concrete interface (Sen 2015; Shi et al. 2013). Studies suggest that low-stiffness adhesives yield conflicting results: while some report poorer bond quality and higher failure strains (Chajes et al. 1996), others argue that such adhesives enhance bond performance when sufficient joint length is provided (Dai and Ueda 2003).

The resin's properties, particularly its elastic modulus and thickness, play a crucial role in bond behaviour, though accurately measuring adhesive thickness remains challenging. Many validated bond-slip models fail to account for adhesive properties, despite their critical role in bond integrity (Diab and Farghal 2014). Flexible adhesives have been shown to increase both ultimate bond strength and effective bond length under proper conditions (Diab and Farghal 2014; Sedlmair and Stempniewski 2019). Conversely, stiffer adhesives with greater thickness can reduce peak shear stresses while improving the load capacity of the system (Ferrier et al. 2010; Shi et al. 2019). However, thicker adhesive layers tend to decrease interfacial fracture energy and initial bond-slip stiffness, leading to deeper concrete cracks during failure (Shi et al. 2019). Numerical simulations by Shi et al. (2019) further revealed that adhesive thickness variations between 0.5 mm and 1 mm had minimal influence on bond performance, suggesting that bond-slip models should instead emphasize the adhesive's elastic modulus.

Curing conditions also impact adhesive performance. Ferrier et al. (2010) observed that lower curing temperatures shifted failure modes from cohesive concrete failure to interfacial rupture

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due to incomplete curing, though ultimate load capacity and peak shear stresses remained largely unaffected. Conversely, adhesive stiffness also influences failure modes under freeze-thaw conditions. Shi et al. (2013) reported that stiffer epoxies shifted failure modes from cohesive concrete debonding to interfacial failure, while more flexible adhesives resulted in cohesive adhesive failure, indicating poorer bond quality and durability.

Vaculik et al. (2018) emphasized the adhesive's axial and shear deformability as critical for bond efficiency. Flexible adhesives enhance ultimate failure slip, interfacial fracture energy, and stress distribution, particularly in brittle substrates (Kwiecień et al. 2018). However, the lack of standardized reporting on adhesive properties, such as elastic modulus, poses a challenge, as approximately half of the studies involving FRP-concrete or FRP-masonry joints omit this information (Vaculik et al. 2018). Kwiecień et al. (2018) demonstrated that flexible adhesives improved tensile load capacity and stress distribution in single lap shear tests of CFRP-strengthened masonry blocks but recommended further studies to evaluate long-term durability under varying exposure conditions.

Alternative adhesives, such as cement-based options, have shown promise in improving both workability and mechanical performance in FRP repairs. Hashemi and Al-Mahaidi (2010) found cement adhesives compatible with CFRP applications in both shear lap and beam tests, enhancing load capacity and material compatibility. However, traditional epoxy adhesives remain the most widely used bonding material for FRP sheets and plates. While effective, epoxies pose limitations, including sensitivity to moisture, temperature degradation, and health risks due to toxic fumes during mixing and handling (Hashemi and Al-Mahaidi 2010).

2.2.4 Surface Preparation

In addition to ensuring surface cleanliness and the absence of debris, the preparation of the concrete substrate plays a critical role in the bond integrity of the repair. Research has consistently demonstrated a direct correlation between rougher surface preparations and improved bond performance (Lorenzis et al. 2001; Iovinella et al. 2013; Soares et al. 2019; Mazzoti et al. 2016). Typical surface preparation methods include brushing, grinding, scarifying, bush-hammering, steel shotblasting, sandblasting, etc. The ICRI guidelines specify a concrete surface profile (CSP) of 3 to allow for proper grip and adhesion performance (ICRI 2016; Mazzoti et al. 2016). Similar to these studies, Serbescu et al. (2013) argue that surface preparation greatly affects the bond integrity of the EB FRP systems. The authors encourage a proper roughening of the surface to achieve an optimal bond performance but warn against using high impact techniques (e.g. bush hammering) to avoid a potential weakening of the substrate (Serbescu et al. 2013). As shown in Fig. 2-4, sandblasting produces the most effective bond-slip profile by creating an optimal surface texture for mechanical interlocking and adhesive penetration (Iovinella et al. 2013).

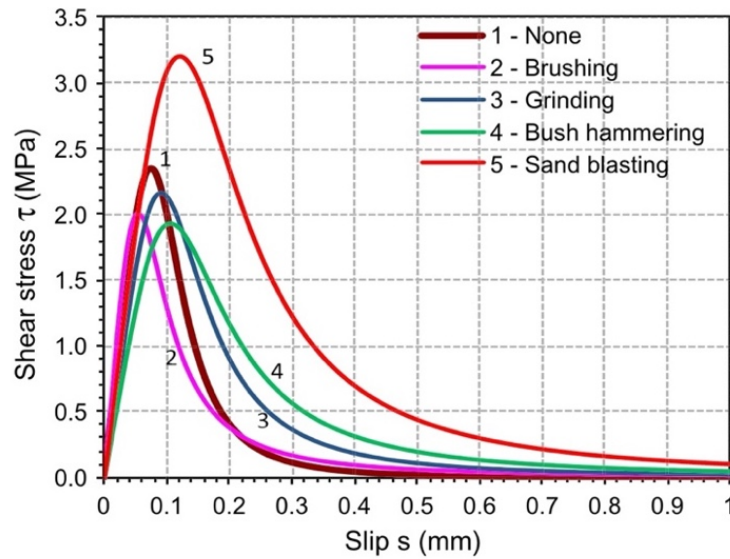


Fig. 2-4. Bond-slip curves derived from different surface preparation techniques (Iovinella et al. 2013).

Soares et al. (2019) further compared grinding and sandblasting—two widely recognized surface preparation methods in the field. Their results confirmed that sandblasting yields superior bond performance, characterized by higher shear force, greater stresses, increased interfacial fracture energy, and a thicker peeled concrete layer (1.8 mm compared to 1 mm for grinding). These findings align with those of Iovinella et al. (2013), highlighting the critical role of surface texture in enhancing the bond's mechanical properties.

2.2.5 Bond Length & Anchorage

As highlighted in the previous subsections, the bond integrity of EB FRP systems is critical and influenced by numerous parameters. One such key factor is the effective bond length (EBL), also referred to as the active bond length (Ben Ouezdou et al. 2009; Mazzotti et al. 2016; Pellegrino and Sena-Cruz 2016). The EBL is defined as the active zone along which

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shear stresses are transferred through the interface between the FRP laminate, epoxy resin, and concrete substrate. This length corresponds to the region where strains, which are maximal near the loaded end, progressively decrease in a non-linear pattern until they approach zero. The EBL may shift along the strengthened joint as the FRP laminate fails and sheets are peeled away, but its total value remains relatively constant (Ben Ouezdou et al. 2009; Mazzotti et al. 2016; Pellegrino and Sena-Cruz 2016).

The EBL is an essential parameter, prompting various modeling efforts aimed at accurately estimating bond performance. These models are categorized into six types, as detailed in Appendix B, with formulae that introduce bond length coefficients to estimate the load capacity of short joints, discussed further in Appendix C.

While standard code equations typically provide good estimates for anchorage length, some researchers argue that these equations do not fully account for variations in shear lap tests (Czaderski et al. 2010; Ouezdou et al. 2009). Ouezdou et al. (2009) particularly criticize North American codes (such as ACI 440.2R and CAN/CSA S806), which estimate bond length as proportional to the inverse of the axial stiffness of the FRP. Instead, they found that bond length is proportional to the FRP's stiffness itself, not its inverse (Ouezdou et al. 2009).

Several studies have also explored how bond length affects load capacity and failure behaviour. Mensah et al. (2020) found that increasing bond length resulted in higher load capacity, greater peak bond stress, and increased ultimate slip. Similarly, Nakaba et al. (2001) found that longer bond lengths tend to lead to a more uniform stress distribution, but this may also reduce bond strength over a larger area. On the other hand, Carrarra et al. (2011) demonstrated that failure mode and brittleness in single-lap shear tests are significantly

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influenced by bond length. Longer bond lengths were associated with snapbacks in load curves, while shorter lengths led to more pronounced softening behaviour. Shorter bond lengths also resulted in mixed-mode stress states, leading to complex failure mechanisms (Carrarra et al. 2011).

Al-Roussan et al. (2013) observed that increasing anchorage length resulted in a diminished peak shear stress, but also a larger slip at failure. Similarly, Li et al. (2024) showed that shorter bond lengths yielded more linear load-slip curves, whereas longer bond lengths exhibited a plateau after reaching the ultimate capacity, with a corresponding increase in slip at failure.

For joints with limited bond length or when aiming to improve bond performance, FRP anchors have been shown to offer substantial benefits. These anchors enhance joint deformability and reduce crack propagation (Zhang et al. 2017). Notable anchorage methods include FRP fan anchors (Fig. 2-5), U-wraps for beam strengthening, and mechanical fasteners, with metallic fasteners proving particularly effective for flexural anchorage (Kalfat et al. 2013).



Fig. 2-5. Handmade FRP anchor fan (Zhang et al. 2017).

2.2.6 Bond Width

Bond width is a critical parameter in the performance of EB FRP systems, as it influences the distribution of stresses at the interface between the FRP laminate and the concrete substrate (Czaderski et al. 2010, fib 2001). While bond width is typically considered in terms of specimen and laminate geometry, research has shown that laminate stiffness also plays a significant role in stress development and distribution, particularly in single lap shear tests (Czaderski et al. 2010).

In a study by Lorenzis et al. (2001), the bond performance of multiple CFRP-strengthened concrete beams was investigated, and it was found that the bonded width of the FRP did not significantly affect the bond strength. This suggests that other factors, such as the material properties or surface preparation, may have a more pronounced impact on bond performance than the width of the FRP laminate itself.

Conversely, Mensah et al. (2020) conducted a series of double lap shear tests using both GFRP and BFRP sheets and observed that increasing the laminate's width led to an increase in load capacity, peak bond stress, and ultimate slip. Their findings indicate that a greater laminate width improves bond distribution and stiffness, enhancing the overall performance of the FRP system.

However, Al-Roussan et al. (2013) reported an opposing trend, where an increase in laminate width resulted in a decrease in both peak stress and ultimate slip. This suggests that there may be an optimal laminate width beyond which the bond performance could be negatively affected, potentially due to the increased strain or stress concentrations at the interface.

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Finally, Camli and Binici (2007) highlighted that bond performance is more dependent on bond length and laminate width than on substrate strength. Their findings emphasize the importance of laminate dimensions in achieving effective bond strength, reinforcing the notion that both laminate length and width need to be carefully considered in the design of FRP-strengthened concrete members.

In conclusion, the relationship between bond width and bond performance is complex, with different studies presenting varying conclusions. While some suggest that a wider laminate enhances bond distribution and stiffness, others point to potential negative effects on bond strength and slip. These discrepancies highlight the need for further investigation to determine the optimal laminate width for different FRP systems and concrete applications.

2.2.7 Summary

In summary, this section examined the interplay of material factors affecting bond performance. While concrete strength and composition are important, adequate system capacity and proper substrate preparation are essential. Substrate cleaning and roughening must be done carefully to optimize adhesion and bond integrity, without excessively weakening the bond. The properties of the FRP and adhesive also play a critical role in bond stress development and deformability, facilitating increased slip. Most importantly, proper anchorage length must be provided to enhance bond capacity and integrity, allowing the full stress curve to develop and enabling the assessment of defects' impact on bond performance as per the objectives of this study.

2.3. Bond Evaluation & Assessment

2.3.1 Non-Destructive Testing (NDT)

Non-destructive testing (NDT), also referred to as non-destructive evaluation (NDE), encompasses various non-contact assessment techniques used to evaluate EB FRP systems in concrete infrastructure. These methods facilitate the inspection of repair quality, interfacial bond conditions, and long-term system performance without causing damage to the structure. They are widely employed in laboratory studies, field inspections, and structural health monitoring of in-service structures (Dolati et al. 2023; Sen 2015; Li et al. 2021). While NDTs provide valuable insights into the quality of strengthening systems, they cannot reliably predict failure. Instead, they serve as complementary tools to assess structural integrity, identify defects, and detect potential discontinuities in the bond (Karbhari et al. 2005).

NDT techniques can be broadly categorized into four groups. The first group consists of simple, low-cost methods that do not require specialized equipment, such as visual inspection and acoustic tap testing using a coin (Fig. 2-6) or hammer. These approaches, though subjective, provide a general assessment of structural integrity (Karbhari et al. 2005; Dolati et al. 2023; Dong and Ansari 2011; Fowai et al. 2023). The second category includes methods that utilize electromagnetic or radio waves, such as ground penetrating radar (GPR), microwave testing (MT) (Fig. 2-7), and radiography (Dolati et al. 2023; Li et al. 2021). The third category comprises mechanical wave-based techniques, including impact echo testing (IE) and ultrasonic testing (UT). The fourth category encompasses advanced methods such as acoustic emission (AE) and infrared thermography (IR), which involve the analysis of heat conduction or stress wave propagation to detect defects (Dolati et al. 2023).

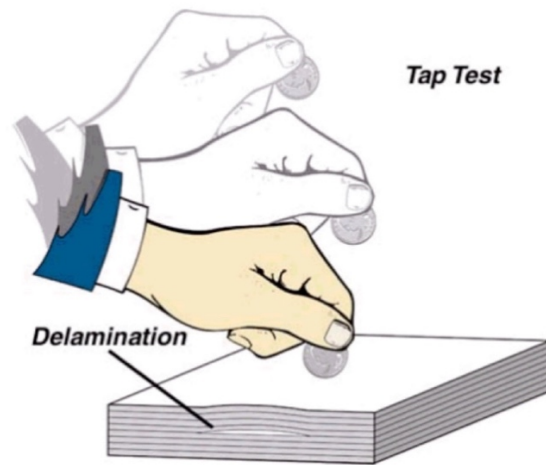


Fig. 2-6. Coin tap testing (Dolati et al. 2023).

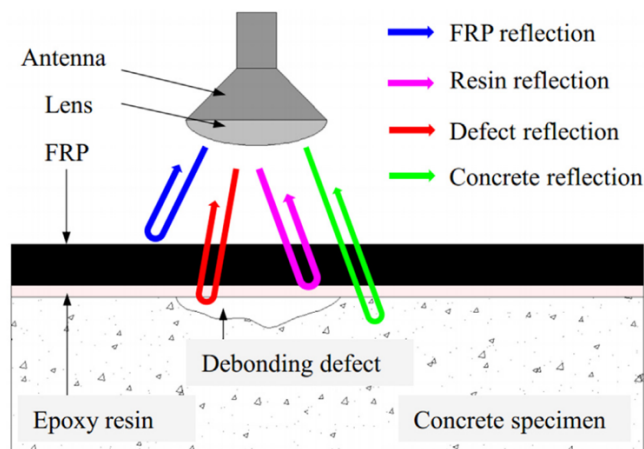


Fig. 2-7. Schematics of microwave testing (MT) in a typically defected FRP-strengthened concrete element (Li et al. 2021).

Multiple studies advocate for a combination of NDT methods depending on the type of damage being assessed (Dolati et al. 2023; Dong and Ansari 2011; Fowai et al. 2023). Composite damage within the CFRP laminate is best evaluated using UT, IR, and coin tap testing, whereas interfacial defects are more effectively identified through a combination of GPR, IR, and UT (Dolati et al. 2023).

A key challenge in NDT inspection of CFRP-strengthened members is its inability to accurately assess defect depth. Evaluating the criticality of delamination in the field often necessitates destructive testing to properly characterize bond integrity (Fowai et al. 2023).

2.3.2 Destructive Testing (DT)

Destructive testing (DT) methods are crucial for evaluating the bond performance of EB FRP systems, offering reliable quality control and parametric analysis without the high costs associated with full-scale structural testing (Serbescu et al. 2013). Nakaba et al. (2001) categorized DTs into four primary types: direct tensile tests (Fig. 2-8), shear lap tests, beam tests, and inserted tests, with the latter being less frequently employed. Each of these methods provides valuable insight into bond behaviour under different loading conditions.

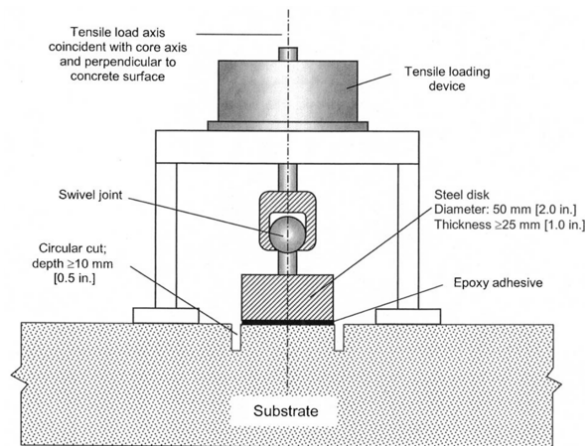


Fig. 2-8. Direct tension test/Pull-off test (ASTM 2004).

Notably, the direct tensile (pull-off) test subjects the material to pure tension (Mode I fracture mechanics Fig. 2-9), which is essential for evaluating the bond quality of repairs. Conversely, lap tests are intended to induce pure shear (Mode II, Fig. 2-9); however, even under ideal

conditions, mixed-mode loading occurs due to the presence of peeling and pulling stresses (Anderson 2005; Serbescu et al. 2013).

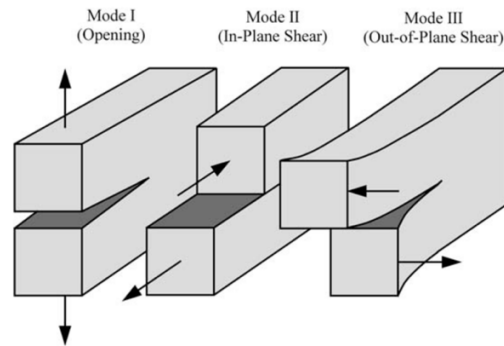


Fig. 2-9. Loading modes at a crack tip (Anderson 2005).

In contrast, shear lap tests, particularly single-lap shear tests (also known as pull-out tests), are widely used in laboratories to assess bond performance (Mode II fracture mechanics) by inducing direct shear along the bonded interface (Hadigheh et al. 2012; Vaculik et al. 2018). However, mixed-mode loading frequently occurs due to peeling effects and minor specimen alignment eccentricities (Serbescu et al. 2013). Proper specimen alignment is crucial for obtaining reliable results (Serbescu et al. 2013; Hadigheh et al. 2012). A schematic of a direct shear lap test is shown in Fig. 2-10.

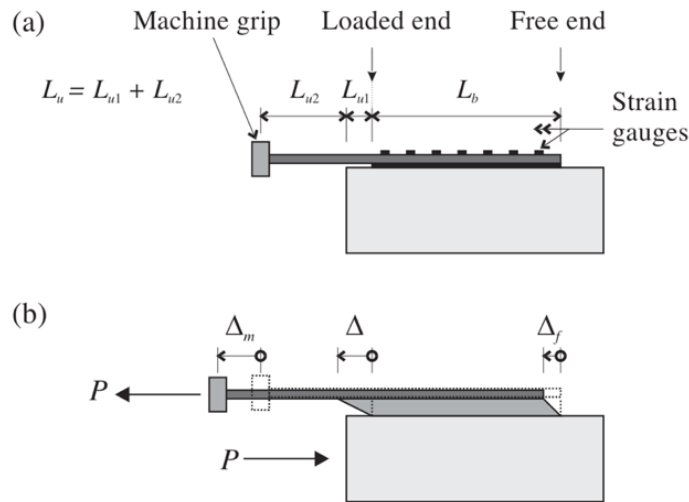


Fig. 2-10. Typical configuration of a single lap shear test (push-pull) (Vaculik et al. 2018).

An alternative is the double-lap shear test, where both specimen faces are strengthened to achieve more symmetric loading. This can be done using a single-block or double-block setup, with the latter being more commonly used (Vaculik et al. 2018). However, single-block tests can lead to asymmetric failure, causing specimen rotation and potential bias in results (Vaculik et al. 2018). The schematic of the double-lap shear test is shown in Fig. 2-11.

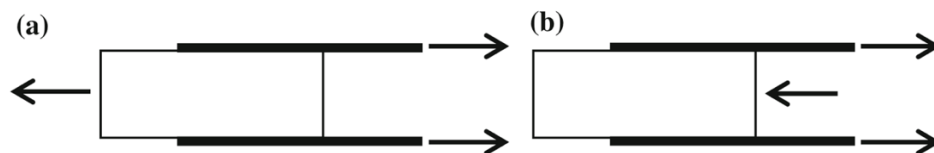


Fig. 2-11. Double shear lap tests: (a) pull-pull; (b) push-pull (Mazzotti et al. 2016).

While shear lap tests provide a practical approach for investigating bond integrity, they do not fully replicate the mixed-mode loading conditions experienced in RC beams, which involve

both Mode I (tensile peeling) and Mode II (shear) stresses (Serbescu et al. 2013). Thus, shear lap tests are best suited for comparative studies rather than direct application in beam behaviour assessments (Serbescu et al. 2013). Beam bending tests, which induce mixed-mode stresses, more accurately reflect real structural conditions, as shown in Fig. 2-12.

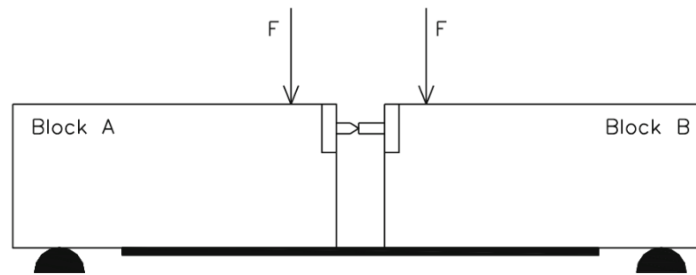


Fig. 2-12. Beam test (Mazzotti et al. 2016).

Despite ongoing debate regarding the optimal shear testing method, single-lap shear tests remain the preferred choice due to their simplicity and reduced bias from asymmetric failure (Vaculik et al. 2018). However, proper specimen alignment is essential to minimize eccentricities. Alternatively, Serbescu et al. (2013) recommend double-lap shear tests for their symmetry and straightforward setup. The choice of method ultimately depends on the specific research objectives and the bond performance aspects under investigation.

Therefore, considering the objectives of the present study and the simplicity and efficiency of the method, the single-lap shear test will be employed as the preferred testing technique for evaluating the bond performance of EB FRP systems in the presence of interfacial defects.

2.4. Bond Slip Relationship & Models

2.4.1 Rationale

The bond-slip relationship is a fundamental concept for understanding the interface behaviour between EB FRP and the concrete substrate. It governs the transfer of stresses and reflects the mechanisms of adhesion, friction, and mechanical interlocking. Numerous researchers have developed models to compute this relationship (Nelson et al. 2020; Ko et al. 2014; Biscaia et al. 2013). These bond-slip models vary (Fig. 2-13) but generally exhibit a characteristic trend: an initial increase in shear stresses as they reach a peak value, corresponding to a specific slip value, followed by a softening stage during which shear stresses gradually diminish to zero as the slip reaches its ultimate value (Nelson et al. 2020).

Different bond slip curves shapes from the literature are depicted in Fig. 2-14, typically ranging from bilinear to non-linear models. Some models (top right of the figure) also consider a linear-ascending (or linear-brittle) behaviour, from the sudden drop in shear stresses after reaching their peak stresses. Others (left top and left bottom) also consider a sudden increase in shear stresses, followed by a longer softening stage where slip increases.

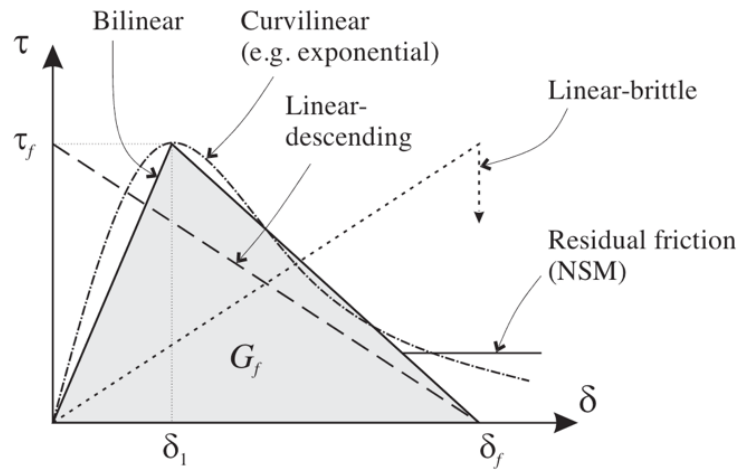


Fig. 2-13. Typical bond slip curves of EB FRP concrete or masonry joint (Vaculik et al. 2018).

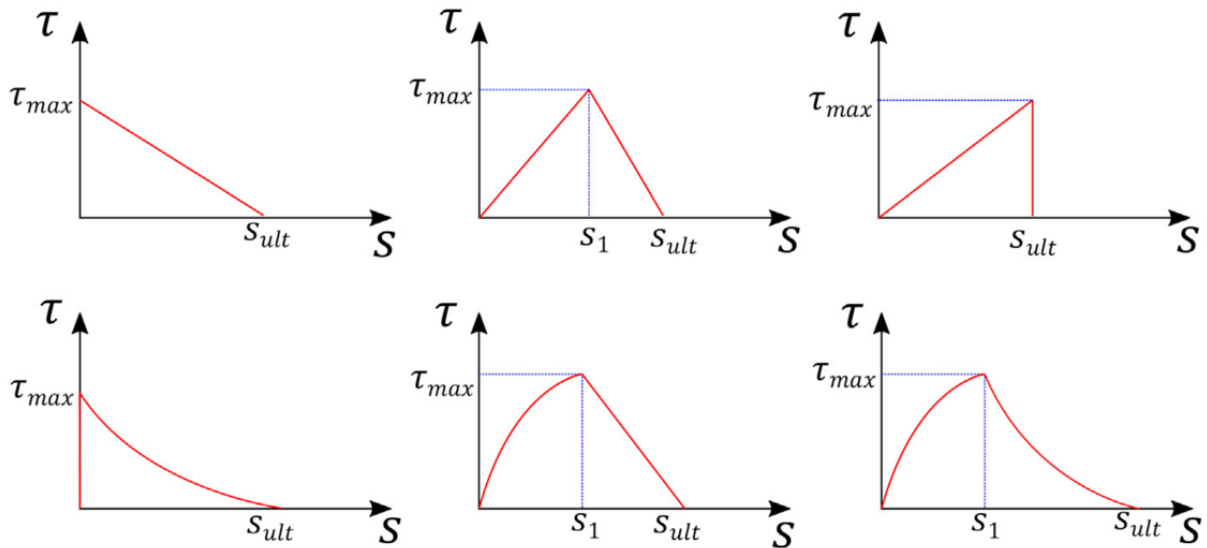


Fig. 2-14. Different bond slip models found in the literature (Nelson et al. 2020).

The relationship between bond stresses and slip values during a shear test is governed by the equilibrium and compatibility conditions of an infinitesimal segment within an EB FRP-concrete joint (Fig. 2-15), as expressed in Eq. 2-1. The strain and slip relationships are defined

in Eqs. 2-2 and 2-3. Substituting these expressions into Eq. 2-1 yields the second-order differential equation, presented in Eq. 2-4.

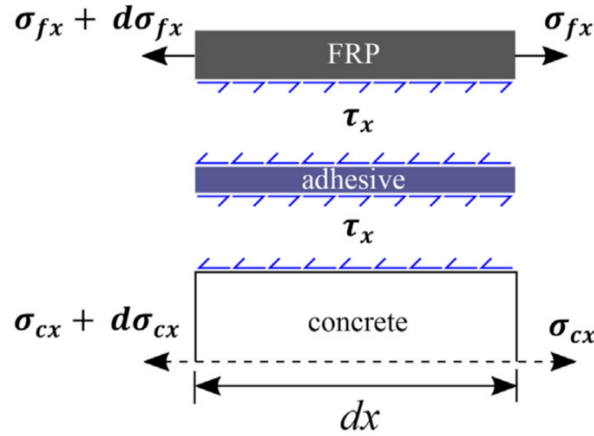


Fig. 2-15. Stresses acting on an infinitesimal portion of an EB FRP-concrete joint (Nelson et al. 2020).

$$\sigma_f t_f + \tau_f dy = \sigma_f t_f + d\sigma_f t_f \quad \text{Eq. 2-1}$$

$$\sigma_f = E_f \varepsilon_f \quad \text{Eq. 2-2}$$

$$\varepsilon_f = \frac{ds}{dy} \quad \text{Eq. 2-3}$$

$$\tau_f = E_f t_f \frac{d^2 s}{dy^2} \quad \text{Eq. 2-4}$$

On the other hand, the slip along the longitudinal axis of the bond length is defined as the relative displacement between the FRP and the concrete (Biscaia et al. 2013; Nelson et al. 2020; Ko et al. 2014).

2.4.2 Popovic Equation

In 1973, Sandor Popovics developed one of the most prominent concrete stress-strain models, widely recognized for its ability to represent the diverse behaviour of concrete materials based on their cementitious content, types, strengths, and other properties. The model is expressed using Eqs. 2-5 to 2-7 in imperial units, where f'_c represents the concrete compressive strength, and ε_0 represents the strain at peak stress. These values serve as reference points for strain (ε) and stress (f_c) variations throughout different phases of testing.

The model introduces the coefficient parameter n , an empirical value that accounts for the material properties and strength characteristics of the concrete. By adjusting the coefficient, the model accommodates variations in concrete composition, including differences in cement content, aggregate type, curing conditions, and strength levels.

$$f_c(\varepsilon) = f'_c \left(\frac{\varepsilon}{\varepsilon_0} \right) \frac{n}{(n-1) + \left(\frac{\varepsilon}{\varepsilon_0} \right)^n} \quad \text{Eq. 2-5}$$

$$n = 0.4 \times 10^{-3} f'_c + 1.0 \text{ (for a normal concrete)} \quad \text{Eq. 2-6}$$

$$\varepsilon_0 = 2.7 \times 10^{-4} (f'_c)^{\frac{1}{4}} \quad \text{Eq. 2-7}$$

Building on the same conceptual framework, many researchers have adapted Popovics' model to describe the shear-slip behaviour in bonded systems. By calibrating the model experimentally, they have proposed new bond-slip relationships that account for the material properties and parameters inherent to EB FRP systems for concrete strengthening. The generalized formula for the bond-slip model is presented in Eq. 2-8, where the interfacial bond

stresses (τ) at any slip point (s) are determined relative to an empirically calibrated peak shear stress (τ_m) and its corresponding slip (s_m).

$$\tau(s) = \tau_m \left(\frac{s}{s_m} \right)^{\frac{n}{(n-1) + \left(\frac{s}{s_m} \right)^n}} \quad \text{Eq. 2-8}$$

Some of these researchers and their respective calibrated parameters are summarized in Table 2-1. The parameter n primarily governs the softening branch of the bond-slip curve and should exceed 2, as noted by Mazzotti et al. (2016).

Table 2-1. Calibrated Parameters for Popovic's Model.

Source	$n > 2$	τ_m	s_m
Popovic (1973) (for $f'_c = 58.8$ MPa)	4.491	-	-
Nakaba et al. (2001)	3.0	$3.5f_c^{0.19}$	0.065 mm
Savoia et al. (2003)	2.86	$3.5f_c^{0.19}$	0.051 mm
Mazzotti and Savoia (2008)	3.64	6.63 MPa	0.038 mm
Mazzotti and Savoia (2009)	4.437	6.43 MPa	0.044 mm

2.4.3 Exponential Models

Similar to the Popovic models, exponential models are also non-linear formulations, as originally proposed by Dimande (2003) and Dai et al. (2005). However, unlike Popovic's models, these new approaches use the slip as the exponent. In this thesis, these will be referred

to as "exponential models." Overall, three comparable formulations can be expressed: a general form, one proposed by Dai et al. (2005), and another by Dimande (2003).

The overall similarity between the Popovic and exponential models can be illustrated in Fig. 2-17, where the non-linear bond slip models proposed by Ueda and Dai (2003), Dimande (2003), and the Popovic model with a coefficient of $n = 3$ are shown. These models exhibit the same peak shear stress and corresponding slip, but differ slightly in shape, particularly in the softening branch of the curves.

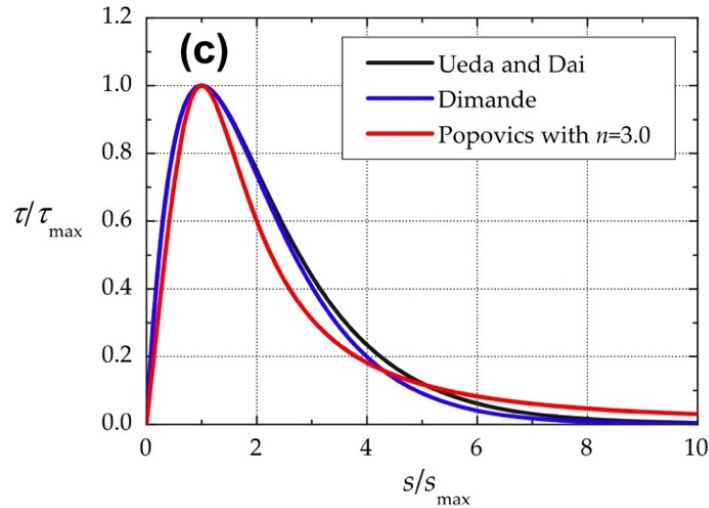


Fig. 2-16. Comparison of non-linear bond slip models: Ueda and Dai (2003), Dimande (2003) and Popovic with $n = 3$ (Biscaia et al. 2013).

2.4.3.1 General Shape

The general form of the bond shear-slip relationship, as presented in Eq. 2-9, expresses the bond shear at various slip points in relation to the axial stiffness of the laminates ($E_f t_f$) and

two calibration coefficients, denoted as α and β . These coefficients have been experimentally calibrated by various researchers, with some of their results presented in Table 2-2.

$$\tau(s) = \frac{E_f t_f}{1 + \rho} \frac{\alpha}{\beta^2} e^{-\frac{s}{\alpha}} \left(1 - e^{-\frac{s}{\alpha}}\right) \approx E_f t_f \frac{\alpha}{\beta^2} e^{-\frac{s}{\alpha}} \left(1 - e^{-\frac{s}{\alpha}}\right) \quad \text{Eq. 2-9}$$

Table 2-2. Calibration coefficients for the general shape exponential bond slip model.

Source	α (mm)	β (mm)
Zhou et al. (2010)	0.07926	14.66
Wu et al. (2012)	0.0474	21.30
Li et al. (2022)	0.1035	19.88

2.4.3.2 Dai et al. (2005)

In contrast, the model proposed by Dai et al. (2005) is represented in Eq. 2-10, where the bond shear stress is expressed as a function of two experimentally defined empirical parameters, A and B, the axial stiffness of the FRP ($E_f t_f$), and the slip values recorded throughout the shear test. The interfacial energy (G_f) is expressed in Eq. 2-11.

$$\tau(s) = A^2 B \times E_f t_f \times e^{-Bs} (1 - e^{-Bs}) \quad \text{Eq. 2-10}$$

$$G_f = \frac{1}{2} A^2 E_f t_f \quad \text{Eq. 2-11}$$

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When substituted into Eq. 2-6, it yields a simplified formulation of the model, as presented in Eq. 2-12.

$$\tau(s) = 2BG_f(e^{-Bs} - e^{-2Bs}) \quad \text{Eq. 2-12}$$

Although not explicitly included in the model formula in Eq. 2-12, the parameters such as peak bond shear and corresponding slip are defined in Eqs. 2-13 and 2-14, respectively.

$$\tau_m = \frac{1}{2}BG_f \quad \text{Eq. 2-13}$$

$$s_m = 0.693B \quad \text{Eq. 2-14}$$

The empirical parameters A and B were calibrated using experimental results from various studies. Shrestha et al. (2017) determined distinct values for A and B based on their findings for varying adhesive thicknesses, as summarized in Table 2-3. Their study revealed that increasing the adhesive thickness (ranging from 0.3 mm to 2 mm) generally led to a decrease in coefficient A and an increase in coefficient B. These results were obtained using a similar bond width of 50 mm for the laminate, comparable to this study, but with a shorter bond length of 200 mm, as opposed to 250 mm in this study.

Table 2-3. Different interpolated experimental empirical coefficients for the exponential model proposed by Dai et al. (2005) – obtained from Shrestha et al. (2017).

	t_f (mm)	A (mm)	B (mm)
b_f = 50 mm L_b = 200 mm	0.33	0.00336	14.40
	1	0.00261	16.3004
	1.4	0.00201	17.44
	2	0.00112	19.1495

2.4.3.3 Dimande (2003)

A distinct formulation is proposed by Dimande (2003), expressed in Eq. 2-15. This simplified model integrates the exponential slip function discussed earlier in this section, along with the ratio of peak shear to corresponding slip, which will be further defined for bilinear models in subsection 2.4.4.

The peak shear and corresponding slip are presented in Eqs. 2-16 and 2-17, respectively. In this model, Dimande (2003) makes a concerted effort to comprehensively address the interfacial parameters by introducing not only the calibrated coefficients α and β , but also the interfacial fracture energy G_f , alongside adhesive properties such as the shear modulus G_a and the epoxy thickness t_a .

$$\tau(s) = \tau_m \frac{s}{s_m} e^{1-\frac{s}{s_m}} \quad \text{Eq. 2-15}$$

$$\tau_m = \frac{1}{2\beta} \left(-1.575\alpha \frac{G_a}{t_a} + \sqrt{2.481 \alpha^2 \left(\frac{G_a}{t_a} \right)^2 + 6.3\alpha\beta^2 \frac{G_a}{t_a} G_f} \right) \quad \text{Eq. 2-16}$$

$$s_m = \frac{\tau_m}{\alpha \left(\frac{G_a}{t_a} \right)} \quad \text{Eq. 2-17}$$

Additionally, the coefficients were not provided as calibrated values but were instead expressed in general formulae, Eqs. 2-18 and 2-19, as functions of the axial stiffness of the laminate and the adhesive properties.

$$\alpha = 0.028 \left(\frac{E_f t_f}{1000} \right)^{0.254} \quad \text{Eq. 2-18}$$

$$\beta = 0.0035 \frac{G_a}{t_a} \left(\frac{E_f t_f}{1000} \right)^{0.34} \quad \text{Eq. 2-19}$$

Lastly, Dimande (2003) expresses the interfacial energy (Eq. 2-20) as a function of the adhesive shear modulus G_a , the epoxy thickness t_a , and the concrete strength f_c .

$$G_f = 7.554 \left(\frac{G_a}{t_a} \right)^{-0.449} f_c^{0.343} \quad \text{Eq. 2-20}$$

2.4.4 Bilinear Models

As indicated by their name, bilinear models are closed-form, two-part equations that consist of two stages: an initial linear elastic ascending stage, where stresses increase from zero to a maximum value and correspond to an equivalent slip, followed by a linear descending post-

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peak stage in which shear stresses decrease to zero, marking the onset of failure slip and the initiation of FRP debonding from the concrete substrate.

The typical bilinear model is expressed in Eq. 2-21, where τ_m represents the peak shear, s_m the corresponding slip value, and s_u the ultimate failure slip.

$$\tau(s) = \begin{cases} \tau_m \left(\frac{s}{s_m} \right); & 0 < s \leq s_m \\ \tau_m \left(\frac{s - s_u}{s_m - s_u} \right); & s_m < s \leq s_u \end{cases} \quad \text{Eq. 2-21}$$

Consequently, due to the model's simplicity, the interfacial fracture energy could be computed using Eq. 2-22.

$$G_f = \frac{1}{2} \tau_m s_f \quad \text{Eq. 2-22}$$

Table 2-4 presents the parameters for the bilinear bond-slip model, as proposed by six different studies – five sources with monotonic shear tests between EB FRP and concrete and one investigating the cyclic load effect. These studies provide variations in the key model parameters, including the peak bond shear stress (τ_m), corresponding slip (s_m), ultimate slip (s_u), bond width factor (β_w), and interfacial energy (G_f). The differences in these formulae reflect the diverse experimental setups, material properties, and test conditions used in each study. The table highlights how these parameters vary across different research efforts.

Table 2-4 (1). Bilinear model parameters and calibration from various references.

Source	Model Parameters
Brosens and Van Gemert (1999)	$\tau_m = 1.8\beta_w f_t$ $s_m = 2.5\tau_m \left(\frac{t_a}{E_a} + \frac{50}{E_c} \right) \text{ and } s_f = \frac{2G_f}{\tau_m}$ $\beta_w = \sqrt{1.5 \frac{2 - \frac{b_f}{b_c}}{1 + \frac{b_f}{100}}}$ $G_f = 0.3\beta_w^2 f_t$
Brosens (2001)	$\tau_m = \beta_c \beta_w \frac{f_t f_c}{f_t + f_c}$ $s_m = \tau_m \left[2.4 \frac{t_c}{E_c} + 2.5 \frac{t_m}{E_m} + n \frac{t_a}{E_a} \right] \text{ and } s_f = \frac{2G_f}{\tau_m}$ $\beta_w = \sqrt{1.5 \frac{2 - \frac{b_f}{b_c}}{1 + \frac{b_f}{100}}}$ $G_f = 0.3\beta_w^2 f_t$
Monti et al. (2003)	$\tau_m = 1.8\beta_w f_t$ $s_m = 2.5\tau_m \left(\frac{t_a}{E_a} + \frac{50}{E_c} \right) \text{ and } s_f = 0.33\beta_w$ $\beta_w = \sqrt{1.5 \frac{2 - \frac{b_f}{b_c}}{1 + \frac{b_f}{b_c}}}$

Table 2-5 (2). Bilinear model parameters and calibration from various references.

Lu et al. (2005)	$\tau_m = 1.5\beta_w f_t$ $s_m = 0.0195\beta_w f_t$ $s_f = \frac{2G_f}{\tau_m}$ $\beta_w = \sqrt{\frac{2.25 - \frac{b_f}{b_c}}{1.25 + \frac{b_f}{b_c}}}$ $G_f = 0.308\beta_w^2 \sqrt{f_t}$
Ko et al. (2014)	$\tau_m = 0.165f_c$ $s_m = -0.001f_c + 0.122$ $s_f = -0.002f_c + 0.302$
Li et al. (2018)	$\tau_m = 0.2233\beta_w f_c - 2.1433$ $s_m = 0.0195\beta_w f_t \leq 0.06$ $s_f = \frac{2G_f}{\tau_m}$ $\beta_w = \sqrt{\frac{2.25 - \frac{b_f}{b_c}}{1.25 + \frac{b_f}{b_c}}}$ $G_f = \beta_w^2 (0.029f_c - 0.2668)$

2.4.5 Linear Ascending Models

Similar to the bilinear models, Neubauer and Rostasy (1999) proposed a model characterized by a linear elastic ascending stage. However, this model assumes a more fragile behaviour, where the shear stresses drop abruptly after reaching their peak value, lacking a linear descending phase. While this representation may not accurately depict the full behaviour of bonded systems under stress, it is more conservative and computationally simpler. Additionally, this model is considered safer as it typically accounts for moderate loading conditions (Lorenzis et al. 2001). The model is expressed in Eqs. 2-23 to 2-26, where the peak shear stresses are correlated with the bond width factor and the tensile capacity of the concrete substrate.

$$\tau(s) = \begin{cases} \tau_m \left(\frac{s}{s_m} \right); & 0 < s \leq s_m \\ 0; & s_m < s \end{cases} \quad \text{Eq. 2-23}$$

$$\tau_m = 1.8\beta_w f_t \quad \text{Eq. 2-24}$$

$$s_m = 0.224\beta_w \quad \text{Eq. 2-25}$$

$$\beta_w = \sqrt{1.06 \frac{2 - \frac{b_f}{b_c}}{1 + \frac{b_f}{400}}} \quad \text{Eq. 2-26}$$

Another model worth mentioning is proposed by Lorenzis et al. (2001), which is expressed in Eqs. 2-27 to 2-30. In this model, the peak shear stress (τ_m) is defined as a function of the axial stiffness of the FRP ($E_f t_f$), while the corresponding slip (s_m) is determined as a function of the peak shear relative to the interfacial stiffness (K). This approach presents a more

comprehensive method for accounting for the interfacial shear properties by incorporating the shear modulus and thickness of both the surface primer and the resin/saturant, providing a more detailed representation of the bonding behaviour between the FRP and concrete.

$$\tau_m = 0.0184\sqrt{E_f t_f} \quad \text{Eq. 2-27}$$

$$s_m = \frac{\tau_m}{K} \quad \text{Eq. 2-28}$$

$$K = \frac{G_{\text{interface}}}{t_{\text{interface}}} \quad \text{Eq. 2-29}$$

$$G_{\text{in}} = \frac{G_{\text{resin}} G_{\text{primer}}}{t_{\text{resin}} G_{\text{resin}} + t_{\text{primer}} G_{\text{primer}}} \quad \text{Eq. 2-30}$$

2.4.6 Other Models

2.4.6.1 Dai and Ueda (2003)

Similar to the exponential models, Dai and Ueda (2003) have proposed a two-part non-linear model, which is expressed in Eq. 2-31. The peak bond shear and corresponding slip in this model are the same as defined for Dimande's (2003) model, expressed in Eqs. 2-16 and 2-17.

$$\tau(s) = \begin{cases} \tau_m \left(\frac{s}{s_m} \right)^{0.575} \\ \tau_m e^{-\beta(s-s_m)} \end{cases} \quad \text{Eq. 2-31}$$

2.4.6.2 Pellegrino and Modena (2009)

Pellegrino and Modena (2009) emphasize the significance of a model that prioritizes the axial rigidity of FRP. While they acknowledge limitations such as curve discontinuities and

inaccuracies, they accentuate its importance due to the lack of consideration for FRP stiffness in existing literature models (Pellegrino and Modena 2009). The model and its parameters are expressed in Eqs. 2-32 to 2-37.

$$\tau(s) = \begin{cases} \tau_m \left(\frac{s}{s_m} \right)^{0.575} & ; s \leq s_m \\ \tau_m \left(\frac{s_f^\alpha - s^\alpha}{s_f^\alpha - s_m^\alpha} \right) & ; s_m < s \leq s_f \end{cases} \quad \text{Eq. 2-32}$$

$$\alpha = \frac{-1.3162}{(n_f E_f t_f)^{0.187}} \quad \text{Eq. 2-33}$$

$$\tau_m = 3.1(n_f E_f t_f)^{0.32} \quad \text{Eq. 2-34}$$

$$s_m = \frac{0.075}{(n_f E_f t_f)^{0.2}} \quad \text{Eq. 2-35}$$

$$s_u = \frac{10.5}{(n_f E_f t_f)^{0.6}} \quad \text{Eq. 2-36}$$

$$G_f = \frac{\tau_m s_m}{1.575} + \frac{\tau_m}{(\alpha + 1)} \left[\frac{s_f^\alpha \times s_m - s_m^{\alpha+1}}{s_m^\alpha - s_f^\alpha} \right] \approx \frac{\tau_m s_m}{1.575} \quad \text{Eq. 2-37}$$

2.4.6.3 Lu et al. (2005) Simplified Model

Lu et al. (2005) proposed both a bilinear model, as detailed in subsection 2.4.4, and a simplified non-linear model, which is presented in Eqs. 2-38 and 2-39. The simplified model retains the same bond-slip parameters outlined in Table 2-4 but introduces a distinct curve shape that accounts for non-linear behaviour in both the initial and secondary stages of the bond-slip

relationship. This alternative model provides a more refined representation of the non-linearity of shear-slip response, particularly for systems exhibiting complex interfacial behaviour.

$$\tau(s) = \begin{cases} \tau_m \sqrt{\frac{s}{s_m}}; s \leq s_m \\ \tau_m e^{-\alpha(\frac{s}{s_m}-1)}; s_m < s \leq s_f \end{cases} \quad \text{Eq. 2-38}$$

$$\alpha = \frac{1}{\frac{G_f}{\tau_m s_m} - \frac{2}{3}} \quad \text{Eq. 2-39}$$

2.4.7 Summary

Table 2-5 presents a comprehensive summary of nearly thirty models from the literature, categorizing key parameters into three material groups: concrete properties (such as compressive and tensile strength, elastic modulus, and geometry of the prism), FRP properties (including axial stiffness, strength, and laminate width), and adhesive interfacial properties (focusing on the elastic and shear modulus, as well as the epoxy thickness). This classification facilitates a clear understanding of the variables influencing bond-slip behaviour in EB FRP-strengthened concrete systems.

The table highlights that some models, such as Popovic's and Ko et al.'s (2024) exponential model, focus exclusively on concrete compressive strength, disregarding FRP and adhesive properties. While exponential and bilinear models are generally more comprehensive, no single model incorporates all material properties. For example, Dai et al. (2005) and Yun et al. (2008) include FRP stiffness and adhesive interfacial properties but neglect certain concrete

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properties. Conversely, Pellegrino and Modena's (2009) model emphasizes FRP axial rigidity but overlooks concrete and adhesive properties.

Adhesive properties, particularly the shear modulus, are frequently overlooked. While elastic modulus and strength can be determined using ASTM D638 standards (ASTM 2014), measuring epoxy thickness is challenging due to its thin profile. Additionally, shear modulus values are often averaged, as the Poisson's ratio, a key factor in its calculation, is rarely provided by manufacturers and typically assumed to range between 0.3 and 0.4 (Bizindavyi et al. 1999; Barbieri et al. 2016; Carrara et al. 2011).

Table 2-6 (1). Comparison of bond-slip models from the literature: type, authors, and key parameters.

Model type	Adapted from	Authors	Comments	#	Concrete Properties					FRP Properties			Adhesive Properties		
					f _c	f _t	E _c	t _c	b _c	E _f	t _f	b _f	E _a	t _a	G _a
Popovic power curves	Nakaba et al. (2001)	Nakaba et al. (2001)	n= 3	1	x										
		Savioa et al. (2003)	n = 2.86	2	x										
		Mazzoti et al. (2008)	n = 4.1876	3	x										
		Mazzoti et al. (2009)	n = 4.253	4	x										
Exponential models	Dai et al. (2005)	Dai et al. (2005)		5	x					x	x		x	x	x
		Wu et al. (2013)	Cited in (Li et al. 2022)	6	x			x	x	x	x	x			
		Zhou (2009) (PhD thesis)		7			x	x	x	x	x	x			
		Zhou et al. (2010)		8			x	x	x	x	x	x			
		Yun et al. (2008)		9	x					x	x		x	x	x
		Chajes et al. (1996)		10	-	-	-	-	-	-	-	-	-	-	-
		Shrestha et al. (2017)		11						x	x				
		Li et al. (2022)		12			x	x	x	x	x	x			
	Dai and Ueda. (2003)	Dai and Ueda. (2003)		13	x					x	x			x	x
	Dimande (2003)	Dimande (2003)		14	x					x	x		x	x	x
	Lu et al. (simplified) (2005)	Lu et al. (2005)		15	x	x			x			x			

Table 2-7 (2). Comparison of bond-slip models from the literature: type, authors, and key parameters.

Model type	Adapted from	Authors	Comments	#	Concrete Properties					FRP Properties			Adhesive Properties		
					f_c	f_t	E_c	t_c	b_c	E_f	t_f	b_f	E_a	t_a	G_a
Bilinear models		Wang and Wu. (2018)	CFRP & Steel	16									x	x	x
		Lu et al. (2005)		17	x	x			x			x			
		Li et al. (2018)	Cyclic loading	18	x	x			x			x			
		Ko et al. (2014)		19	x										
		Brosens and Van Gemert (1999)		20		x	x		x			x	x	x	
		Brosens (2001)		21	x	x	x	x	x			x	x	x	
		Monti et al. 2003		22		x	x		x			x	x	x	
Linear ascending models		De Lorenzis et al. (2001)		23						x	x		x	x	x
		Neubauer and Rostasy (1999)		24		x			x			x			
Sinusoidal function		Zhang et al. (2013)	NSM & Concrete	25	x										
Others		Pellegrino and Modena (2009)		26						x	x				
		Nelson et al. (2020)		27	x					x	x				

2.5. Digital Image Correlation for Tracking Strain Distributions

2.5.1 General

Digital Image Correlation (DIC) is a novel optical measurement method first introduced in the 1950s and further developed in the 1980s. This non-contact technique quantifies displacement and strain values across the surface of a tested specimen by analyzing changes in speckle patterns in defined subsets or facets of acquired images during stress application (Sause 2016; Ab Ghani 2016; Pan et al. 2009). A simplified schematic of a subset tracking using 2D DIC is depicted in Fig. 2-17.

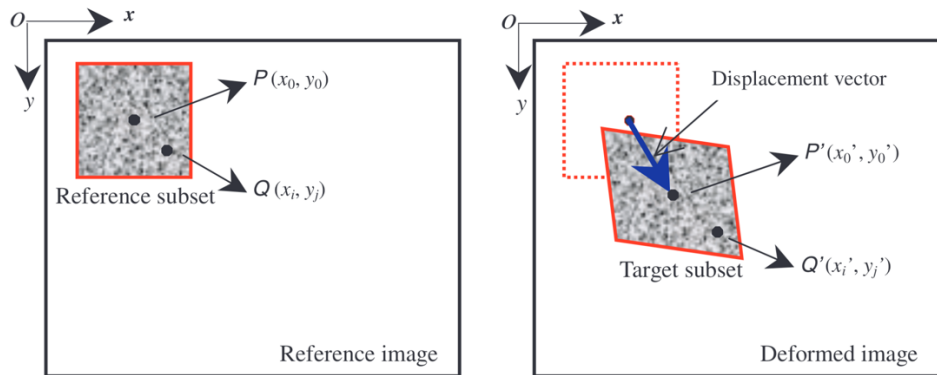


Fig. 2-17. Schematics of a subset tracking during 2D DIC analysis (Pan et al. 2009).

DIC offers a cost-effective, versatile, and scalable solution for various applications, ranging from laboratory experiments to structural health monitoring, enabling the capture of expansive monitoring fields equivalent to an almost infinite number of sensors or gauges (Ramos et al. 2015; Hoult et al. 2016; Woods et al. 2023; Kwiecień et al. 2018).

Studies have explored the efficiency and limitations of 2D DIC systems in structural health monitoring, particularly in bridge deflection measurements. For instance, Alipour et al. (2019)

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demonstrated that the method's accuracy is influenced by factors such as speckle size. Their findings indicated that smaller speckles (e.g., 0.5 mm) led to lower accuracy and blurry images, whereas larger speckles (e.g., 3 mm) hindered unique pattern identification for strain correlation. Thus, ensuring an appropriate speckle size is crucial for accurate readings (Alipour et al. 2019). While accuracy and noise can be enhanced with filtering and data interpolation, the outputs of DIC remain in strong agreement with conventional techniques like strain gauges or LVDTs (Ramos et al. 2015; Kwiecień et al. 2018).

The advancement to 3D DIC further improves the method's accuracy, particularly for applications involving curved surfaces, large structural elements, or cases with out-of-plane displacements. Studies have shown excellent agreement between 3D DIC and traditional methods, with deviations of less than 6% in scenarios involving CFRP and epoxy materials, affirming its reliability for engineering applications (Meng et al. 2006). Therefore, while 2D DIC is best suited for planar surfaces, 3D DIC provides superior accuracy and adaptability for more complex geometries or those involving out-of-plane behaviour (Meng et al. 2006; Pan et al. 2009).

2.5.2 2D DIC vs 3D DIC

The primary distinction between 2D and 3D DIC systems lies in their ability to handle displacement in multiple directions. A 2D DIC system, using a single camera, is susceptible to inaccuracies caused by out-of-plane displacements, which can corrupt in-plane measurements by introducing pseudo-displacements (Sause 2016; Hoult et al. 2013). Additionally, 2D systems are prone to lens distortion errors, particularly at the periphery of

the field of view, leading to non-uniform strain and displacement outputs. These errors must be accounted for and minimized to maintain accuracy (Pan et al. 2013).

In contrast, 3D DIC systems employ two cameras (Fig. 2-18), enabling the capture of a full three-dimensional range of motion and significantly reducing error magnitude in strain measurements (Sause 2016). For materials such as FRP, which are flexible and prone to out-of-plane motion during tensile testing, 3D systems are strongly recommended. The out-of-plane movements in FRP materials can induce substantial errors in 2D strain readings, emphasizing the importance of high-resolution cameras and finer speckle patterns for accurate strain capture (Sause 2016).

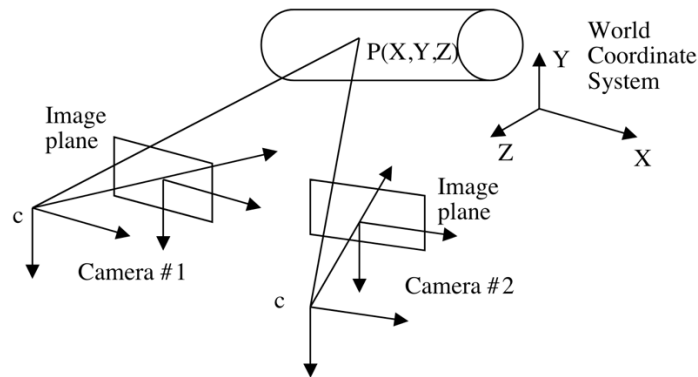


Fig. 2-18. Simplified principle of a 3D DIC system (Meng et al. 2006).

Despite their advantages, both 2D and 3D DIC systems are not without limitations. General challenges include noise introduction and errors arising from image acquisition, calibration, and camera resolution (Sause 2016). Specific environmental factors, such as vibrations or inadequate exposure conditions, can further compromise the accuracy of the system during initial calibration. Therefore, a stable base along with proper lighting conditions should be

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provided (Ramos et al. 2015; Kwiecień et al. 2018). Additionally, the quality of results in 2D DIC depends heavily on factors like speckle size and quality, target tilt, eccentricities, and noise during data acquisition and processing (Alipour et al. 2019). While 3D systems mitigate many of these issues, they are not immune to limitations, such as pixel shifts and interpolation errors in noisy regions (Sause 2016).

Another notable limitation of DIC is its inability to detect subsurface defects or strains beyond the first ply of materials, as its analysis is restricted to the exposed surface of the specimen (Ramos et al. 2015). However, DIC has demonstrated high accuracy in uniaxial testing of CFRP laminates by mitigating the effects of inter-fibre cracks that typically disrupt strain readings, thus offering a smoother optical analysis (Sause 2016). For fracture behaviour studies, 2D DIC has shown good agreement with full-field DIC when out-of-plane errors are accounted for or theoretically removed, as demonstrated in metal alloy tests by Agha (2022). Nevertheless, the inherent thinness and flexibility of CFRP materials and their tendency to exhibit out-of-plane motion during debonding highlight the superiority of 3D DIC systems for achieving accurate strain monitoring in this context.

2.5.3 The Use of DIC for EB FRP Applications

Zhu et al. (2014) investigated the performance of various optical methods for assessing bond slip in concrete specimens strengthened with CFRP and BFRP sheets. The study revealed that DIC provided superior accuracy and ease of setup, making it an ideal tool for full-field measurements, particularly when compared to traditional laboratory sensors and gauges.

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When FRP laminates debond from an externally strengthened concrete substrate, they tend to experience out-of-plane displacement, moving away perpendicularly from the concrete surface (Woods et al. 2023). This displacement further emphasizes the importance of employing 3D DIC systems to effectively capture bond performance in CFRP-strengthened concrete prisms, as illustrated in Fig. 2-19.

Czaderski et al. (2010) examined the bond performance of CFRP-concrete specimens through single lap shear tests using 3D DIC. Their findings contradicted the assumption that the prisms were subject to pure shear stresses (mode II), revealing the complexity of the stress state. The 3D DIC system proved to be highly effective in providing a full-field view of strain distribution, capturing all stages of the test, including crack initiation, debonding, and out-of-plane behaviour induced by mixed-mode loading (Fig. 2-19).

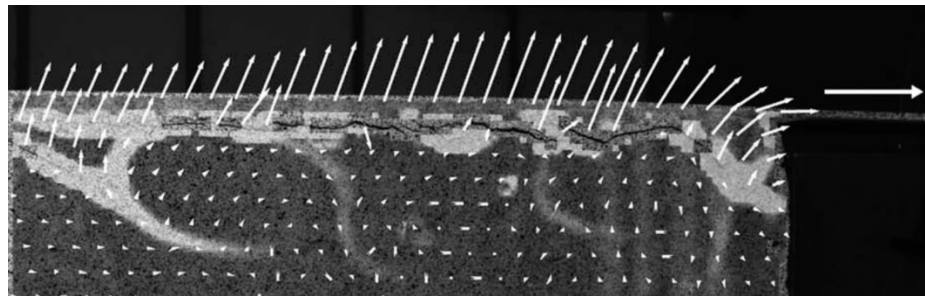


Fig. 2-19. Typical out-of-plane behaviour of a CFRP-concrete specimen prior to failure during a shear lap test (Czaderski et al. 2010).

Schorer and Sause (2015) assessed the ability of 3D DIC to accurately measure strain distributions in unidirectional CFRP fabrics with various laminate defects. The study demonstrated that 3D DIC performed well compared to conventional systems, effectively capturing the full-field strain in composite materials. While fibre cracks and delamination were

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easily detected and followed a predictable trend (larger defects leading to higher strain concentrations), inter-fibre cracks presented a challenge. These cracks did not exhibit a simple relationship between size and strain concentration; rather, their strain patterns were dependent on the orientation of the crack relative to the loading axis.

The use of DIC for bond performance and fracture analysis in direct shear tests of EB FRP with concrete prisms has been further validated by Ahmad et al. (2004), who highlighted the efficiency of DIC in mapping strain across CFRP-strengthened concrete specimens. Their study showed that DIC provides an accurate capture of bond parameters and fracture strength, along with precise monitoring of crack initiation and debonding propagation (Ahmad et al. 2004).

Conventional strain tracking methods, such as strain gauges, present several limitations when applied to composite materials like FRP. These limitations include inaccuracies due to the anisotropic nature of the laminate, the risk of overstraining, and potential failure of the gauge. Additionally, extensometers can introduce localized stress concentrations at their attachment points. In contrast, DIC offers a more efficient solution by enabling global strain capture, along with the ability to measure localized strain concentrations. DIC also allows for analysis over a broader range of fields, including aerial, linear, or single-point measurements (Sause 2016).

A separate study by Ab Ghani (2016) examined the effectiveness of DIC for capturing strain in both GFRP and CFRP tensile coupons. The study concluded that DIC is particularly suitable for FRP materials, providing reliable strain readings and tensile property measurements. This was further validated by conventional methods such as strain gauges, which typically capture only localized strains and require meticulous surface preparation.

Overall, several scholars (Carloni et al. 2012; Li et al. 2023; Li et al. 2024; Kwiecień et al. 2018) have successfully used DIC in investigating the bond performance of EB FRP concrete prisms. Typical strain contour maps at different load stages of a shear lap test are depicted in Fig. 2-20, illustrating how the DIC system could efficiently capture the strain propagation throughout the test.

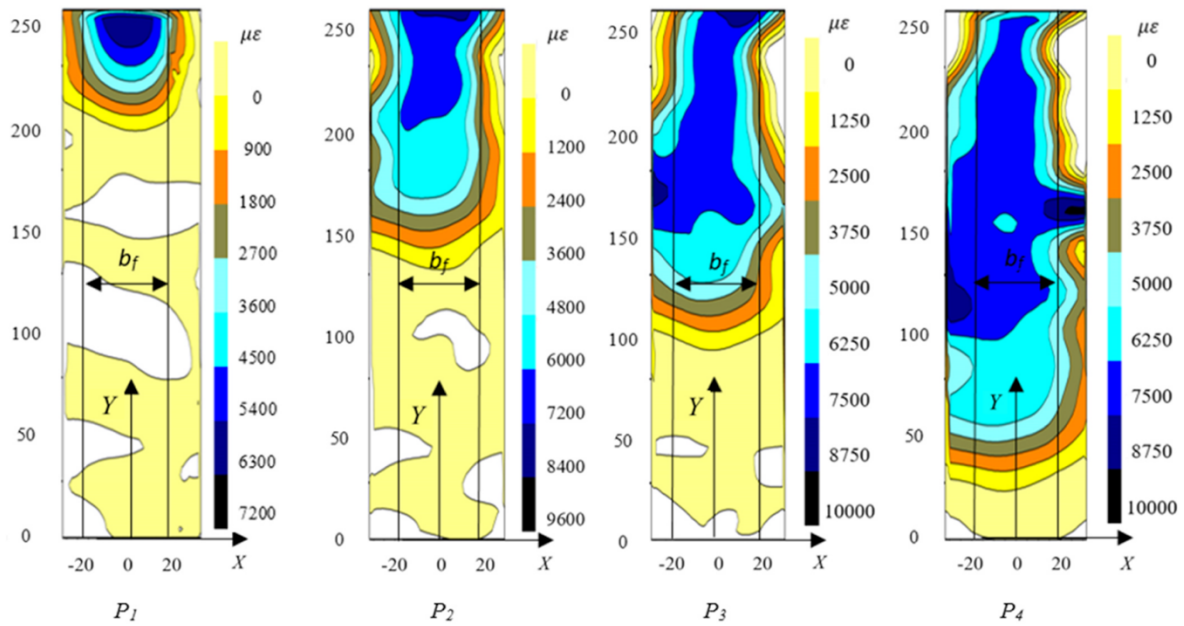


Fig. 2-20. Strain contour maps at different load stages (Li et al. 2024).

2.5.4 Summary

DIC is a highly effective tool as it enables full-field, accurate strain measurements, which are essential for evaluating the bond behaviour between CFRP sheets and concrete substrates, especially in the presence of interfacial defects. Unlike traditional strain gauges that are limited to discrete localized measurements, this technique provides a comprehensive view of strain distributions across the entire specimen, allowing for a more thorough investigation of the

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bond-slip behaviour of the CFRP-strengthened concrete prisms in this study. This is particularly important for capturing variations in strain due to defects such as delamination and other interfacial imperfections, directly contributing to the study's objective of understanding how such defects affect bond integrity and debonding mechanisms.

Additionally, 3D DIC is an efficient tool for analyzing complex loading conditions and mixed-mode debonding, which is crucial given that CFRP laminates often exhibit out-of-plane displacements during failure. The method's ability to detect both in-plane and out-of-plane displacements ensures that the study can accurately track the debonding process and provide insights into how defects influence the debonding initiation and propagation. With its ability to measure strain across the entire surface without requiring invasive surface preparation, 3D DIC offers superior reliability and ease of setup compared to conventional methods. This makes it an ideal tool for investigating the impact of interfacial defects on bond performance, supporting the overall goals of the research to improve CFRP strengthening applications and advance the understanding of bond-slip behaviour in concrete structures.

2.6. Presence of Defects in the Interface

2.6.1 General

There is currently a lack of monitoring standards for structures using FRP. FRP has unique properties compared to steel, contributing to greater non-homogeneity within the system. Consequently, due to varying loading and exposure conditions, the composite system can degrade over time, leading to the generation and propagation of defects at different levels depending on in-situ conditions (Dolati et al. 2023). These defects will generally lead to stress concentrations in the interface and cause further bond deterioration (Li et al. 2021)

These levels of deterioration can be categorized as material and interfacial, as illustrated in Fig. 2-21. Material defects involve degradation of the FRP fabric, resin, and concrete substrate, which may manifest as surface exposure, air voids, or scratches. Interfacial defects occur where these three materials interact and include issues such as cracks in the concrete elements, corrosion around steel rebar, or fissures in the concrete-adhesive zone (Dolati et al. 2023).

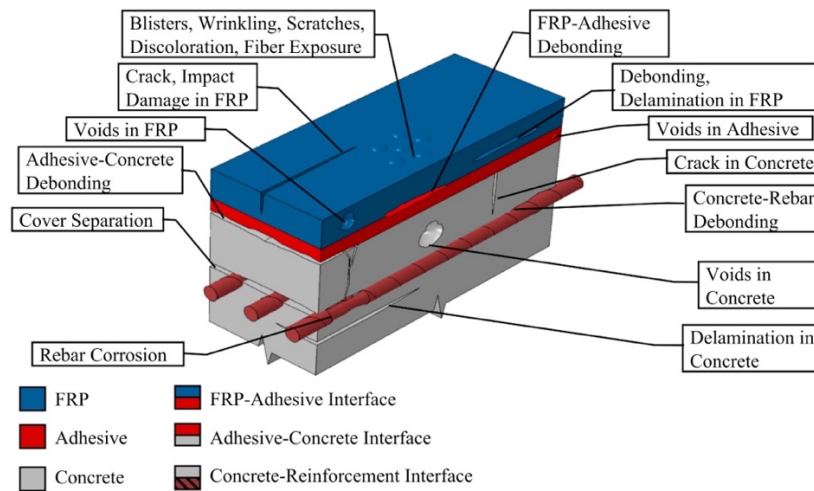


Fig. 2-21. Defects and deterioration in EB FRP systems in RC structures

(Khedmatgozar Dolati et al. 2023).

Overall, the presence of defects in the interface is a crucial aspect of EB FRP strengthening, as it can have several significant effects, including alteration of debonding mode and behaviour, reduction in load capacities, decrease in interfacial energy, decline in strains within the defect region, and increase in strains in the regions adjacent to the defect (Zou et al. 2023). As such, different modes of debonding failure such as FRP material rupture, adhesive or concrete cohesive fracture, or interfacial failure are illustrated in Fig. 2-22.

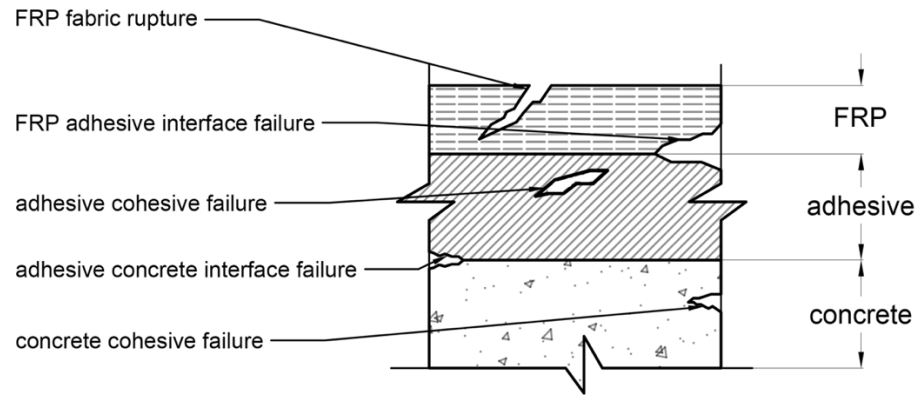


Fig. 2-22. EB FRP bond failure modes.

2.6.2 Origins

Kaiser and Karbhari (2001) classify defects in EB FRP systems into four levels: material, handling, application, and in-service.

At the material and handling level, FRP laminates or rods may suffer deterioration or damage during various phases such as production, storage, transportation, and preparation. This deterioration can result in the development of kinks, flaws, or misalignments in the fabric. Moreover, pulling, shearing, and wrinkling of the sheets can compromise their uniformity, significantly affecting their structural integrity. Similarly, the preparation and mixing of the resin are delicate processes, highly contingent upon factors such as the resin's strength, quality, viscosity, and the mixing environment. The introduction of air bubbles, improper mixing, or moisture infiltration can result in a deficient product, thereby compromising the entire repair process (Kaiser and Karbhari 2001).

The third classification level pertains to the application and installation processes. Multiple flaws and imperfections can arise at this stage, primarily due to unskilled workmanship and

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inadequate quality control. Even if the fibres' integrity is maintained, applying an incorrect amount of epoxy—whether too dry or excessive—failing to remove air bubbles after sheet application, or inadequately preparing the substrate can lead to a weak bond and an inefficient system (Kaiser and Karbhari 2001). Nevertheless, research studies have found that most defects are introduced at the material and handling level (Yazdani et al. 2019; Zou et al. 2023; He et al. 2022).

One of the most critical parameters is the preparation of the concrete substrate before repair. Ensuring an even surface free of debris, contaminants, and moisture, along with adequate priming, is essential for optimal bond performance (Iovinella et al. 2013; Soares et al. 2019; Mazzoti et al. 2016). Typical concrete-induced defects may include microcracks and spalled substrate, as illustrated in Fig. 2-23.

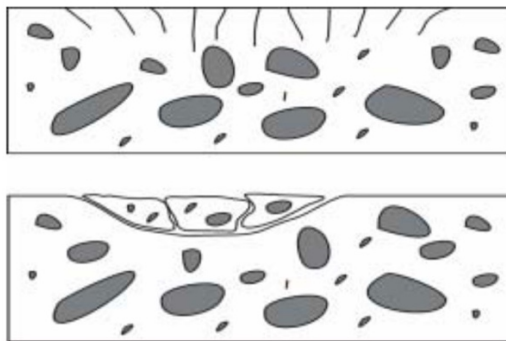


Fig. 2-23. Concrete substrate defects (Kaiser and Karbhari 2001).

Lastly, the in-service conditions also play an important role and could cause or propagate the damage affecting both the long-term performance and durability of the infrastructure. These defects and deterioration could be caused by a range of influencing factors and conditions (Fig. 2-24) including external exposure (e.g., hygrothermal fluctuations), internal influences (e.g.,

alkali aggregate and pH conditions), and interfacial condition (e.g., chemical exposure and moisture penetration) (fib 2001).

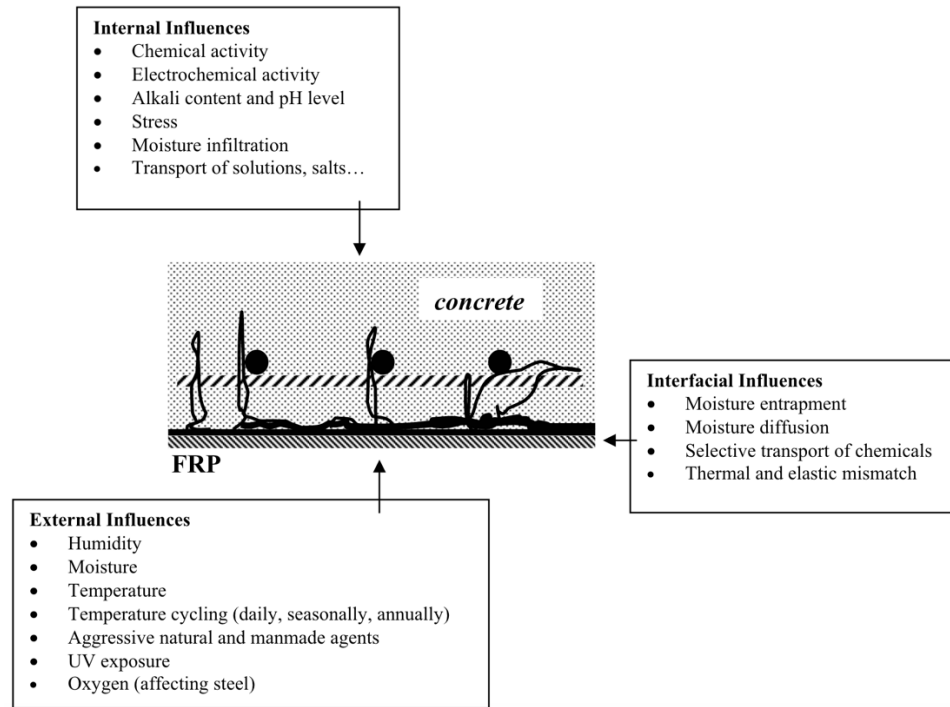


Fig. 2-24. Different influence levels in EB FRP systems causing in-service defects and deterioration (fib 2001).

2.6.3 Types & Classifications

There remains no clear consensus on the classification of defects that can occur at the FRP-concrete interface. In general, defects can be grouped into two primary types: (i) cracks/linear defects, as well as (ii) surface/area defects.

Karbhari and Nevada (2008) report that the only available guidelines for defect classification are found in the ACI 440.2R (2002) standards and the National Cooperative Highway Research Program Report (NCHRP) 514 (2004). These two classifications for area defects are detailed

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in Table 2-6. Despite these classifications, the authors noted a lack of field-representative guidelines and the high degree of conservatism inherent in those (now outdated) standards and codes for defect classification and provisions. They further contended that exposure conditions should be considered in the repair process to improve effectiveness, ultimately benefiting the long-term service life and efficiency of structural repairs (Karbhari and Nevada 2008).

Table 2-8. Comparison between the ACI 440.2R and NCHRP guidelines for defects classification (Karbhari and Nevada 2008).

	ACI 440.2R (2002)		NCHRP (2004)	
	Delamination classification	Recommended repair	Delamination classification	Recommended repair
Small (permissible)	smaller than 2 sq-in occurring in an area ratio less than 5% and with an occurrence of 10 maximum similar defects in a 10 sq-ft area	-	diameter less than 6.4 mm (or ¼ in)	-
Medium	between 2 and 25 sq-in	resin injection or fabric replacement	diameter between 6.4 mm (1/4 in) and 32 mm (1 ¼ in)	resin injection
Large (more detrimental)	greater than 25 sq-in	selective cutting and overlap patch repair	diameter between 6.4 mm (1/4 in) and 32 mm (1 ¼ in) and with an occurrence of 5 maximum similar defects per unit area of a width around 3 m (or 10 ft)	patching method
Very large (critical)	-	-	diameter greater than 32 mm (1 ¼ in)	section replacement

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Defect classifications have evolved in more recent editions of ACI 440.2 guidelines, including ACI 440.2R-17 and ACI 440.2R-23. The 2017 edition maintains a defect classification framework for wet lay-up applications, categorizing delaminations into three groups (ACI 2017):

- Small defects ($<2 \text{ in}^2$) may be ignored.
- Medium defects ($2\text{--}25 \text{ in}^2$) require resin injection and/or fabric replacement.
- Large defects ($>25 \text{ in}^2$) necessitate cutting and applying an overlapping patch.

These classifications align with the earlier versions (as summarized in Table 2-6), while also emphasizing that location, size, and scatter influence the final repair decision. The designer is encouraged to use NDT methods, manufacturer recommendations, and engineering judgment (ACI 2017). The latest edition, ACI 440.2R-23, removes this prescriptive classification, shifting responsibility for defect evaluation and repair to the licensed designer. The standard now prioritizes performance-based durability assessments, requiring engineers to evaluate defects based on their location, size, and overall distribution (ACI 2023).

In contrast, the Canadian FRP design standard for buildings (CAN/CSA S806-12), retains a quantifiable repair threshold, requiring that any void, delamination, or defect exceeding 25 mm (1 inch) in edge length or 600 mm^2 (0.93 in^2) in area be repaired. While this is more conservative than ACI 440.2R, it does not explicitly consider the total quantity or distribution of defects. The standard defers to manufacturer guidelines for selecting the appropriate repair method, such as resin injection or replacement (CSA 2012).

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The Canadian Highway Bridge Design Code (CAN/CSA S6-19) provides a stricter approach to FRP defect evaluation. Chapter 16, dedicated to FRP structures, mandates comprehensive inspection before, during, and after installation. The standard recommends NDT methods capable of detecting delaminations $\geq 1500 \text{ mm}^2$ (2.33 in^2) and leaves repair decisions to the designer, subject to owner approval. However, it specifically prescribes removal and reapplication for delaminations exceeding 1500 mm^2 or affecting more than 5% of the total laminate surface (CSA 2019).

Regarding bond performance, both ACI 440.2R and CAN/CSA S806 specify a minimum direct tensile bond strength of 1.4 MPa (ACI 2023; CSA 2012). In contrast, CAN/CSA S6-19 adopts a slightly stricter threshold of 1.5 MPa (CSA 2019).

Overall, while ACI 440.2, CAN/CSA S806, and CAN/CSA S6 emphasize thorough FRP inspections, CAN/CSA S6-19 imposes the strictest field evaluation requirements. All standards require verification of adhesion quality, delamination presence, sheet alignment, laminate thickness, curing conditions, and mechanical properties (ACI 2023; CSA 2019; CSA 2012).

Echoing those findings, a different study, conducted by Zhou et al. (2017), based itself upon the ACI 440.2R 2002 guidelines to further classify those interfacial imperfections through four-point bending tests of externally strengthened CFRP beams with multiple defect sizes and area ratios. Consequently, Zhou et al. (2017) explored the implications of these defects in flexural components, considering their location (middle span vs. shear span) and element depth (normal vs. deep beam). The authors established a direct correlation between span length and

capacity loss, demonstrating that interfacial defects are particularly critical in shear spans and deeper beams. The proposed classification diagram is presented in Fig. 2-25 (Zhou et al. 2017).

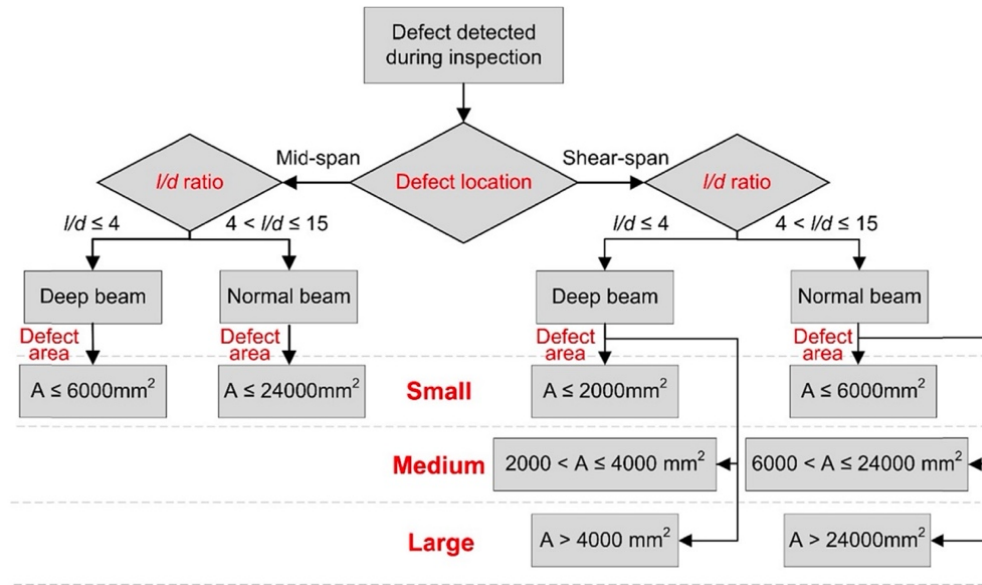


Fig. 2-25. Defect Classification Diagram (Zhou et al. 2017).

On the other hand, Wan et al. (2018) investigated the effect of linear defects by applying horizontal grooves on prisms and beams. Their findings indicate that, in shear lap tests, micro-interfacial cracks less than 1 mm in width contribute to stress redistribution and bond enhancement. Conversely, cracks exceeding 3 mm significantly impair capacity and interfacial energy (Wan et al. 2018).

Furthermore, a more recent study by Li et al. (2023) investigated the effects of middle-positioned defects in the adhesive layer through a series of single-lap shear tests. Concrete prisms were externally strengthened with two plies of high-strength unidirectional CFRP materials, and Teflon was used to simulate artificial imperfections at the interface. These

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defects were varied in shape (circular, square, vertical oval, horizontal oval, and offset oval), size (5%, 10%, 15%, and 25% area ratios), and distribution (0, 1, 4, and 9 defects scattered across the bond area) to assess their impact. Although all simulated imperfections were positioned centrally, the study found that defects delayed the bond slip response, with bond performance significantly deteriorating when the area ratio exceeded 5%. When defect area ratios ranged from 10% to 25%, strengthening efficiency sharply decreased due to the degradation of interfacial performance. In general, defects caused stress concentrations, increased strains in regions adjacent to the defects, and disrupted the bond interface, leading to less uniform bond transfer. Furthermore, the larger the defect, the more critical the reduction in load capacity (Li et al. 2023).

2.6.4 Overall Impact

The presence of even small defects or cracks in the interface of EB FRP systems can significantly diminish bond performance efficiency (Li et al. 2023). Li et al. (2023) found that interfacial imperfections in CFRP-concrete prisms during shear lap tests were particularly critical in three cases: (1) when aligned vertically (parallel to the loading direction) rather than horizontally (perpendicular to the loading direction), (2) when scattered across the interface rather than localized, even with the same total area ratio, and (3) when offset (near edges) rather than centrally located.

The influence of these defects primarily manifests during the debonding phase, where stress concentrations form at the edges of the defects, initiating failure at the corners (Li et al. 2023).

Chapter 2. Literature Review

Li et al. (2023) further demonstrated that square defects are more detrimental to bond distribution than circular ones due to their sharp corners, whereas circular defects are more critical regarding load capacity. However, the study only examined one square defect configuration. In all defects studied, the strain at the bottom edge of the defects was larger than that at the top edge, due to both the residual concrete and the CFRP detached zone moving downwards from the defect location (Li et al. 2023). The presence, size, and location of defects reduce the tensile capacity of the concrete, causing ineffective stress transmission and leading to premature failure (Li et al. 2023).

Additionally, it was found that substrate voids and irregularities significantly reduce interfacial energy, further compromising bond integrity (Karbhari and Nevada 2008). The presence of interfacial defects alters the debonding mode and behaviour, diminishes load capacities, decreases interfacial energy, reduces strains in the defect region, and increases strains in adjacent regions (Fig. 2-26) (Zou et al. 2023). Specifically, interfacial defects lead to strain increases in the regions adjacent to the defect (Schorer and Sause 2015). These defects cause bond discontinuities and stress concentrations, which hinder bond transfer and affect overall performance (Schorer and Sause 2015; Li et al. 2023).

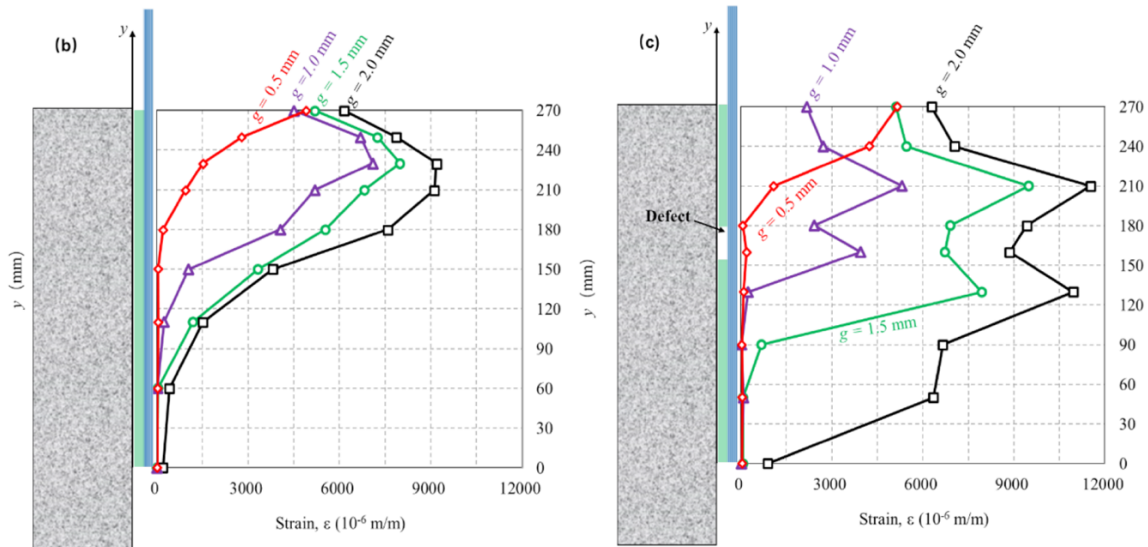


Fig. 2-26. Strain distribution along the bond length for a control specimen (left) and a defected specimen (right) (Zou et al. 2023).

Conversely, a different study by Wu et al. (2023) investigated bond line defects in CFRP-steel joints using double strap joints. Their study found that joints with defects greater than 20% of the bonded area experienced steel adhesive interfacial debonding, leading to a maximum load reduction of 38%. This alteration in failure mode changed both shear and normal interfacial stresses, further highlighting the detrimental effects of defects on bond performance.

Similarly, Zou et al. (2023) introduced middle-positioned defects into the CFRP-concrete interface by inserting thin plastic plates into concrete prisms. Their results showed a sharp load drop during loading (Fig. 2-27), followed by a recovery in the load after the drop, attributed to the bond length exceeding the effective bond length (EBL). Although the defected specimens demonstrated lower load capacity compared to control specimens, the study's findings align with those of Li et al. (2023), where defected specimens exhibited load drops (between 8% and 27%, with typical drops between 1% and 18% for a 15% defect area ratio) followed by a

recovery until failure. Li et al. (2023) also proposed a calibrated load reduction model, validated by experimental data, emphasizing the critical effect of defects on load capacity reduction.

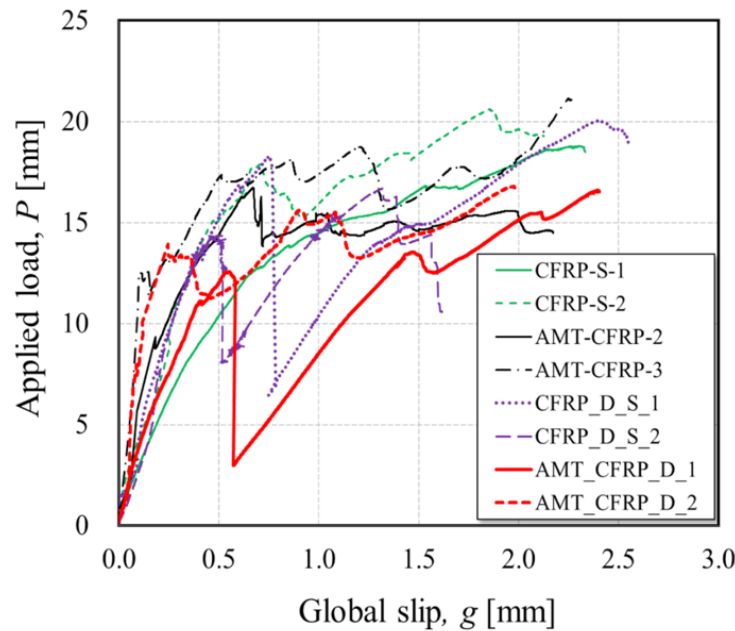


Fig. 2-27. Load slip curves of multiple tested CFRP-concrete prisms showing a load drop for defected specimens (Zou et al. 2023).

Larger defects generally result in a reduction of both capacity and deformability (Li et al. 2023). In terms of axial strains, defected specimens exhibit higher strain peaks in areas adjacent to the defect, with a decrease in strain at the defect location followed by an increase in strain in regions beyond the defect (Zou et al. 2023). In summary, the presence of interfacial defects can greatly affect the bond performance of CFRP-concrete joints by diminishing their overall performance and interfacial energy (Zou et al. 2023).

2.7. Summary & Research Gap

Numerous studies have examined the influence of interfacial defects on the bond integrity of EB FRP systems in concrete structures (Karbhari and Nevada 2008; Zhou et al. 2017; Wan et al. 2018; Li et al. 2023), but significant gaps remain in understanding the critical impact of such defects on bond performance. Li et al. (2023) introduced a novel framework to predict CFRP-concrete repair performance in the presence of defects, providing valuable insights into factors such as defect size, shape, and placement. However, several limitations in this study highlight the need for further experimental research to validate and expand on these findings.

Li et al. (2023) classified defects based on their orientation, size, shape, and position, focusing solely on middle-positioned defects with specific shapes (elliptical, circular, and square). However, the randomness and variability of defects, as observed in field conditions—such as delamination or deterioration—need to be addressed. Fowai et al. (2023) demonstrated in their NDT testing of the Champlain Bridge’s CFRP-strengthened diaphragm that defects occur with varying depths and criticality, further emphasizing the need for studies examining defects in different locations, such as the loaded end, bottom end, or other areas along the bonded length.

While Li et al. (2023) investigated the effect of sharp corners and edge placed defects on shear transfer, their study did not fully explore the phenomenon, suggesting the need for additional experiments to investigate the effect of rectangular defects on bond performance. Additionally, although Li et al. (2023) focused on the load-slip behaviour of middle-positioned defects, they did not comprehensively analyze the impact of defects on bond-slip curves, an area requiring further experimental investigation.

Chapter 2. Literature Review

In summary, while Li et al. (2023) provided a solid theoretical foundation in the study of EB FRP-concrete structures with interfacial defects, several critical research gaps persist, necessitating further experimental exploration. Key areas for future research include:

- Confirmation and validation of Li et al.'s findings (2023).
- Investigation of the impact of load-end defects, which remains unexplored in the current literature.
- Examination of combined defects and their cumulative effects on bond performance.
- Study of defects in high-strength concrete, an area not yet investigated.
- Exploration of the development of strain and shear stresses in the presence of various defect configurations.
- Comprehensive assessment of how defects influence bond performance, integrity, and bond-slip curves.

Addressing these research gaps is essential to enhancing the understanding of CFRP-strengthened concrete structures with interfacial defects, ultimately leading to more reliable and durable infrastructure solutions.

CHAPTER 3. EXPERIMENTAL WORK

3.1. Overview

The experimental program involved the fabrication and testing of ten CFRP-concrete specimens under single lap shear conditions using a Universal Testing Machine (UTM) to investigate the bond behaviour and the impact of interfacial defects. Specimens were tested until complete debonding and failure occurred. Strain distribution during testing was captured and analyzed using 3D Digital Image Correlation (DIC) with a high-resolution Aramis system, following specimen preparation with a white base coat and black speckling for optimal contrast.

Each specimen comprised a 150 mm × 150 mm × 300 mm high-strength concrete prism externally strengthened with unidirectional CFRP laminates applied in single- and double-layer configurations using a two-part structural epoxy. The CFRP laminates had a width of 50 mm and a bonded length of 250 mm, with a 35 mm unbonded region at the loaded end to mitigate stress concentrations.

To investigate the influence of interfacial imperfections, defects were introduced into the adhesive layer using 0.11 mm thick Teflon inserts. These defects varied in position, orientation, and configuration and represented a cumulative area ratio of 15.2% of the bonded area.

3.2. Materials

3.2.1 Concrete Mix

A mix design was developed according to CAN/CSA A23.1 & 2 standards (CSA 2019) with a target strength of 45 MPa and a total batch volume of 112 L. The concrete mix consisted of general use Portland Limestone Cement (PLC), limestone coarse aggregates with a maximum size of 19.5 mm, fine river sand aggregates with a fineness modulus of 2.38 mm, 110 mL superplasticiser and water. The mix proportion with respect to cement weight was (1:1.09:1.99:0.40) presented in the order of (Cement : Fine : Coarse : Water).

3.2.2 Blocks & Cylinders Casting

The different components were combined in a 200 L capacity concrete mixer and mixed until a homogeneous concrete paste was obtained. After the molds and the formwork were lubricated, the concrete mixture was poured and vibrated. A total of 10 blocks with dimensions of 150 mm x 150 mm x 300 mm, as well as 16 cylinders with a diameter of 100 mm and a length of 200 mm were cast, as shown in Fig. 3-1.

Once cast, the blocks were covered with wet burlap fabric and wrapped in plastic sheets to prevent moisture loss. The samples were then allowed to cure for 7 days, after which the formwork was removed, and the samples were stored in dry plastic bins. The cylinders were demolded after 24 hours then grinded on both sides. They were placed in the same curing conditions for 7 days, then stored in covered buckets in dry conditions.



Fig. 3-1. Cast blocks and cylinders.

3.2.3 Cylinder Testing

Compressive and tensile split tests were conducted according to ASTM C39 (ASTM 2021) and ASTM C496 standards (ASTM 2017), respectively, at ages of 7 days, 28 days, and at the time of the shear lap tests. The setups for both tests are shown in Fig. 3-2, while the number of replicate samples for each test is presented in Table 3-1.

3.3. FRP & Resin

3.3.1 Material Properties

In this research, high-strength unidirectional CFRP fabric, supplied in dry rolls (Fig. 3-3), was utilized in combination with a two-part structural epoxy adhesive comprising a resin and a hardener. The epoxy adhesive served as both a primer for surface preparation and a saturant

Chapter 3. Experimental Work

for the CFRP fabric during specimen fabrication. According to the manufacturer, the cured epoxy has a composite tensile strength of 36 MPa and an elastic modulus of 2.22 GPa. The cured CFRP laminates had a reported tensile strength of 880 MPa, an elastic modulus of 98 GPa, and a nominal thickness of 1 mm per ply.

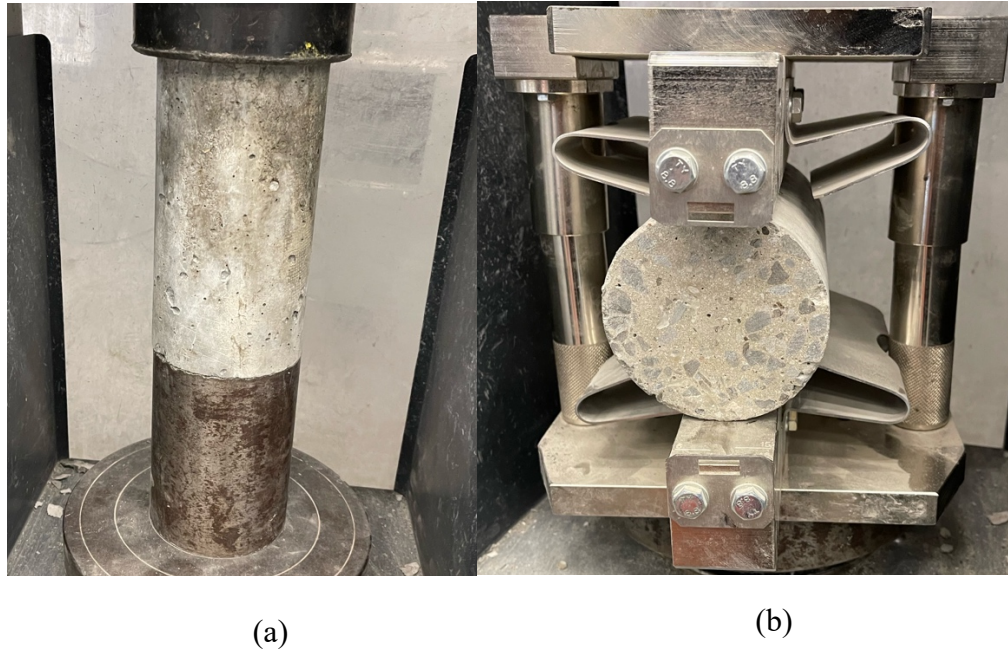


Fig. 3-2. Setup of (a) compressive and (b) tensile split tests.

Table 3-1. Concrete cylinder tests.

Phase	Compressive Tests	Tensile Split Tests
7 days	2	-
28 days	3	-
Test Day	3	2

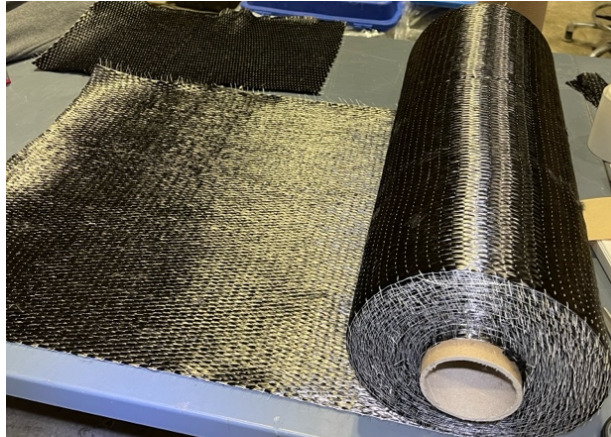


Fig. 3-3. CFRP fabric roll.

3.3.2 Coupons Fabrication & Test

In order to evaluate the tensile properties of the CFRP sheets, composite coupons were designed and tested as per ASTM D3039 standards (ASTM 2014). The coupons had a width of 25 mm and a length of 300 mm. Once the fabric was cured, two 25 mm x 70 mm aluminum tabs were installed at both ends of each coupon to facilitate gripping by the Universal Testing Machine (UTM). The detailing is shown in Fig. 3-4 and a picture of four CFRP coupons is presented in Fig. 3-5. Tensile testing was carried out with a controlled displacement rate of 2 mm/min, as recommended by ASTM D3039 (ASTM 2014). An extensometer was placed in the middle of the coupon to measure the strain during loading.

3.4. Shear Lap Test

3.4.1 Specimen Fabrication

Mechanical shot blasting was applied on two faces of each concrete block to roughen the surface and remove the external mortar layer until a concrete surface profile (CSP) of 3 was

Chapter 3. Experimental Work

obtained according to the International Concrete Repair Institute (ICRI) and manufacturer guidelines (ICRI 2016). As shown in Fig. 3-6, a properly roughened surface should expose the aggregates in the concrete to allow for proper bond with the CFRP sheet.

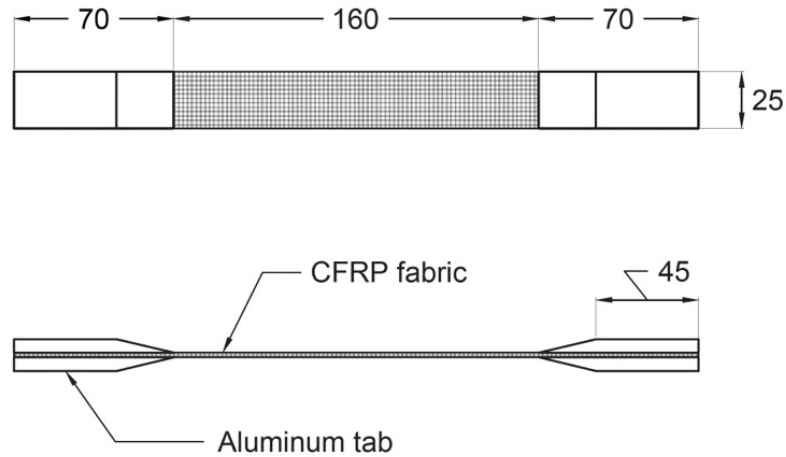


Fig. 3-4. CFRP coupon detailing (dimensions in mm).



Fig. 3-5. CFRP coupons.

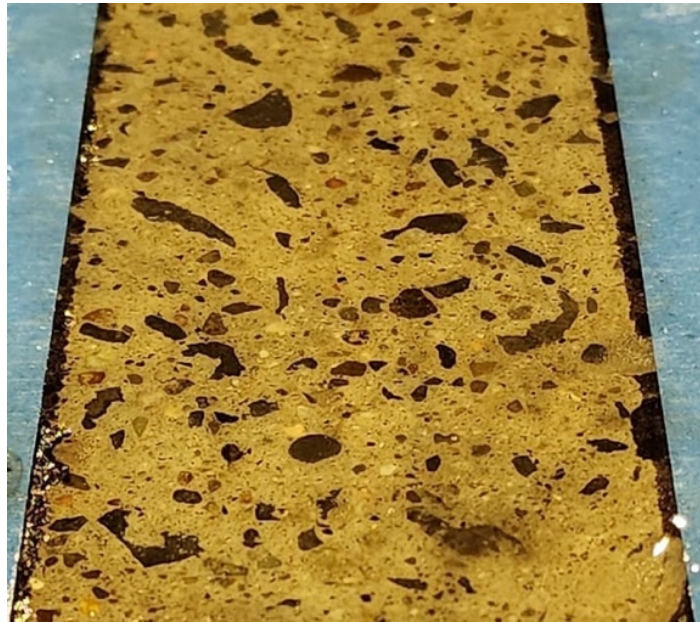


Fig. 3-6. Exposed aggregates in concrete surface after shotblast (CSP 3).

As shown in Fig. 3-7, the bonded area for each block was marked for accurate placement of the laminates. Wood stands were built for each specimen to maintain the alignment of the sheet overhangs during curing and minimize eccentricities during testing. The fabric was applied using the wet lay-up method consisting of mixing the epoxy, priming the concrete surface, and saturating the fabric, as illustrated in Fig. 3-8. Initially, the two parts of the resin were thoroughly mixed using a slow speed drill to avoid air entrapment. A brush was then used to apply a layer of the primer on the prepared concrete surface to cover any possible void or discontinuity. The CFRP was then impregnated in the remaining resin, ensuring proper saturation of all portions of the fabric. The excess epoxy was removed by hand and the fabric was applied to the designated area on the concrete block. After the CFRP sheets were placed, an aluminum roller was used in the direction of the fibres to remove any entrapped air and

Chapter 3. Experimental Work

ensure the uniformity of the application. The specimens were then left to cure for 7 days under ambient temperature conditions.

For the defect specimens, a 0.11 mm Teflon fabric was used to simulate an interfacial imperfection. These defects were positioned atop the primed surface prior to placing the saturated CFRP sheets, as portrayed in Fig. 3-9.



Fig. 3-7. Marked block with wood stand.

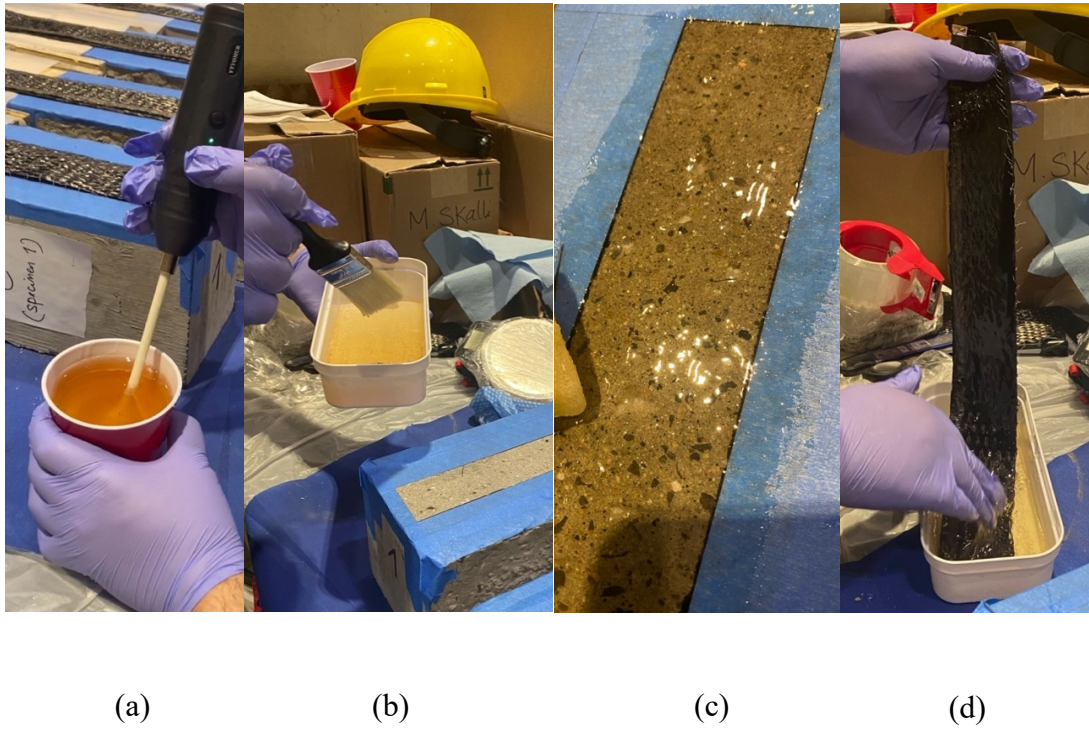


Fig. 3-8. Wet lay-up application: (a) mixing, (b) & (c) priming, and (d) saturating.



Fig. 3-9. Defect placement during fabrication.

Once the specimens were cured, the wood stands were removed, and the edges of the cured CFRP sheet were cut to the desired width using a grinder, as depicted in Fig. 3-10. This process ensured that any frayed or loose fibres at the edges of the sheet were removed prior to testing.

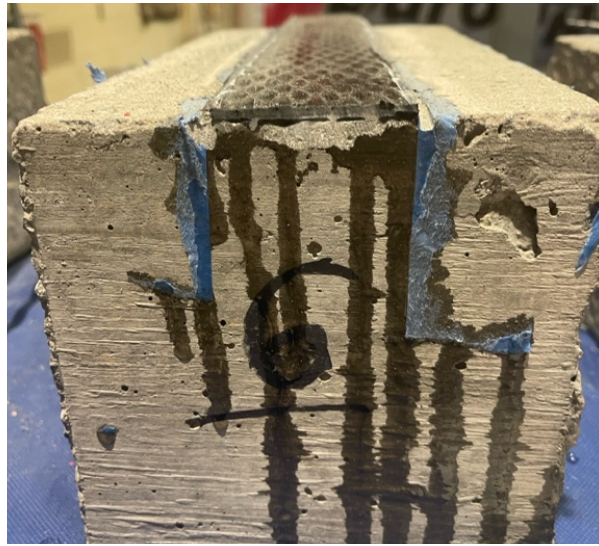


Fig. 3-10. Post-cutting of the cured CFRP sheets.

Finally, the specimens were coated with a layer of white paint, followed by the application of a black speckle pattern using spray paint for DIC acquisition during testing, as detailed in subsection 3.5. Fig 3-11 displays three specimens after the speckle pattern application.



Fig. 3-11. Specimens prepared with white paint and black speckle patterns for 3D DIC acquisition.

3.4.2 Test Set Up and Geometry

The experiments involved a series of single-lap shear tests (SLST), in which the fabricated samples were positioned to apply a tensile monotonic load on the CFRP laminates through the UTM's top grips, in accordance with ASTM D8337 (ASTM 2021). This loading induced shear stresses at the bonded interfaces between the laminates, adhesive layer, and concrete substrate. A constant displacement rate was maintained, with the first two specimens tested at 0.20 mm/min, while the remaining eight specimens were tested at 0.25 mm/min.

The concrete blocks were braced in a steel set up consisting of two 1" thick top and bottom steel plates, and a $\frac{3}{4}$ " thick base plate bolted to the base of the UTM through a system of threaded steel rods and bolt nuts. Additionally, a 1.5 mm thick high strength neoprene sheet was placed between the top plate and the top face of the concrete block to distribute the load uniformly during the experiments. The test fixture components are illustrated in Fig. 3-12.

The concrete and CFRP widths were 150 mm and 50 mm, respectively. The bonded length was defined as 250 mm, while a 35 mm portion was left unbonded at the loaded end of the block to avoid stress concentrations during testing. Moreover, a 200 mm overhang portion of the CFRP sheets extended beyond the concrete face to be gripped by the UTM. The total fabric length consisting of the overhang, the unbonded portion, and the bonded part of the sheets was 485 mm.

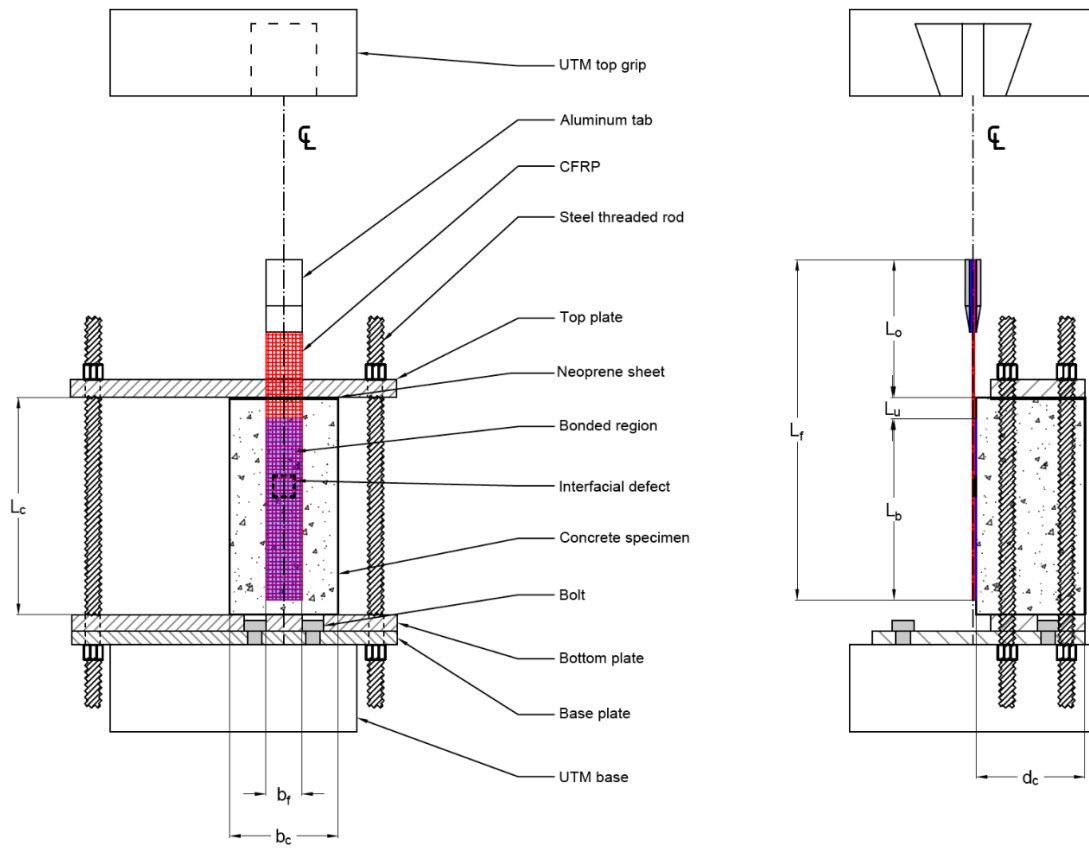


Fig. 3-12. Fixture and test setup.

3.4.3 Experimental Matrix

A total of ten specimens were designed for this study, including two control specimens, five single defect specimens, and three dual defect specimens, as detailed in Table 3-2. The control specimens consist of one-layer and two-layer CFRP sheet configurations, labeled as C-1 and C-2, respectively.

Singular defect specimens are labeled as XY-Z, where X denotes defect orientation (V-vertical or H-horizontal), Y the defect position (either at the L-load end or the M-middle of the bonded length), and Z the number of layers (single- or two-sheet configurations).

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Dual defect specimens include two defects positioned at both the load end and the middle of the sheet. These are labeled XY-XY-Z, where X denotes the defect orientation (V or H), Y their location relative to the sheet's centerline (C-center, R-right edge, or L-left edge), and Z the number of sheets (single- or two-sheet configurations).

Table 3-2. Experimental matrix.

Specimen ID	Number of Defects	Defect Size (mm x mm)	Defect Orientation	Vertical Position*	Horizontal Position**	Number of Layers
C-1	0	-	-	-	-	1
C-2	0	-	-	-	-	2
VL-2	1	(38 x 50)	Vertical	L	C	2
VM-2	1	(38 x 50)	Vertical	M	C	2
VM-1	1	(38 x 50)	Vertical	M	C	1
HL-2	1	(50 x 38)	Horizontal	L	C	2
HM-2	1	(50 x 38)	Horizontal	M	C	2
VC-VC-2	2	(50 x 19) & (50 x 19)	Vertical & Vertical	L & M	C & C	2
VR-VR-2	2	(50 x 19) & (50 x 19)	Vertical & Vertical	L & M	R & R	2
VR-VL-2	2	(50 x 19) & (50 x 19)	Vertical & Vertical	L & M	R & L	2

*L – Loaded end; M – Middle

**C – Center; R – Right edge; L – Left edge

Interfacial imperfections were introduced with a 0.11 mm Teflon sheet placed over the epoxy primer before installing the CFRP sheets. The total defective area for each specimen comprised 15.2% of the total bonded surface for both singular and dual defect specimens. The defect size

was 38 mm x 50 mm for singular defects and 19 mm x 50 mm each for the dual defects. The defect configurations are further illustrated in Fig. 3-13.

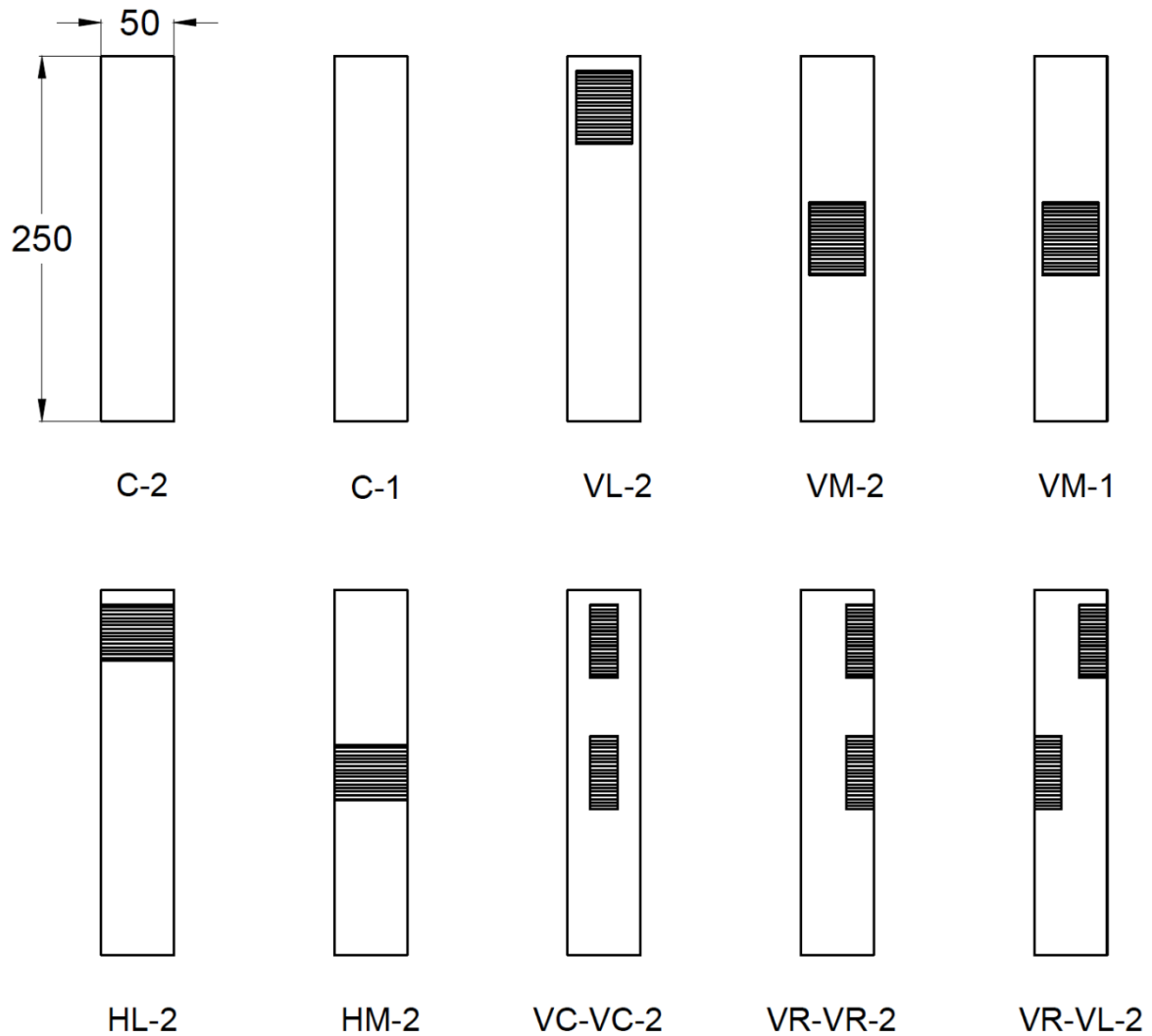


Fig. 3-13. Configuration layout of control and defect specimens (dimensions in mm).

3.5. 3D Digital Image Correlation

To comprehensively investigate strain distribution along the bonded area of each specimen, a 3D Digital Image Correlation (DIC) system was employed for strain acquisition during testing,

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along with the UTM loading data. Fig. 3-14 shows the DIC setup comprising a 3D Aramis system equipped with two high-resolution cameras and a blue light source, ensuring optimal exposure. This system effectively captures the entire specimen behaviour throughout testing as shear stresses propagate, progressively inducing debonding and ultimate failure of the specimens. The 3-dimensional capability of the DIC allows detailed tracking across all axes, including any out-of-plane translations occurring during testing, as depicted in Fig. 3-15.

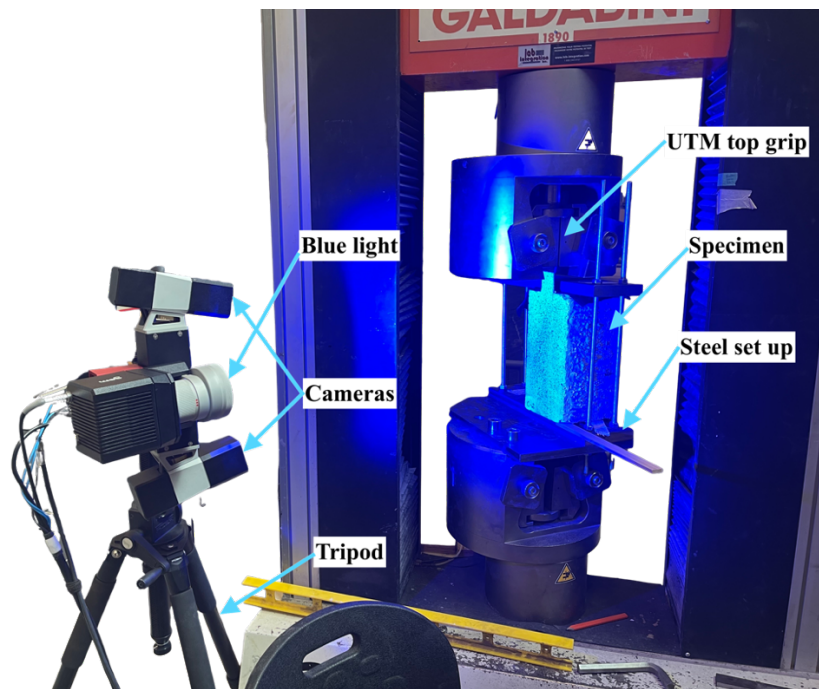


Fig. 3-14. 3D DIC and UTM system setup for strain and displacement tracking.

As mentioned in subsection 3.4.1, a black-speckle pattern was applied to the white-coated specimens to enhance the system's optical tracking by allowing full-field strain and displacement correlation across the surface.

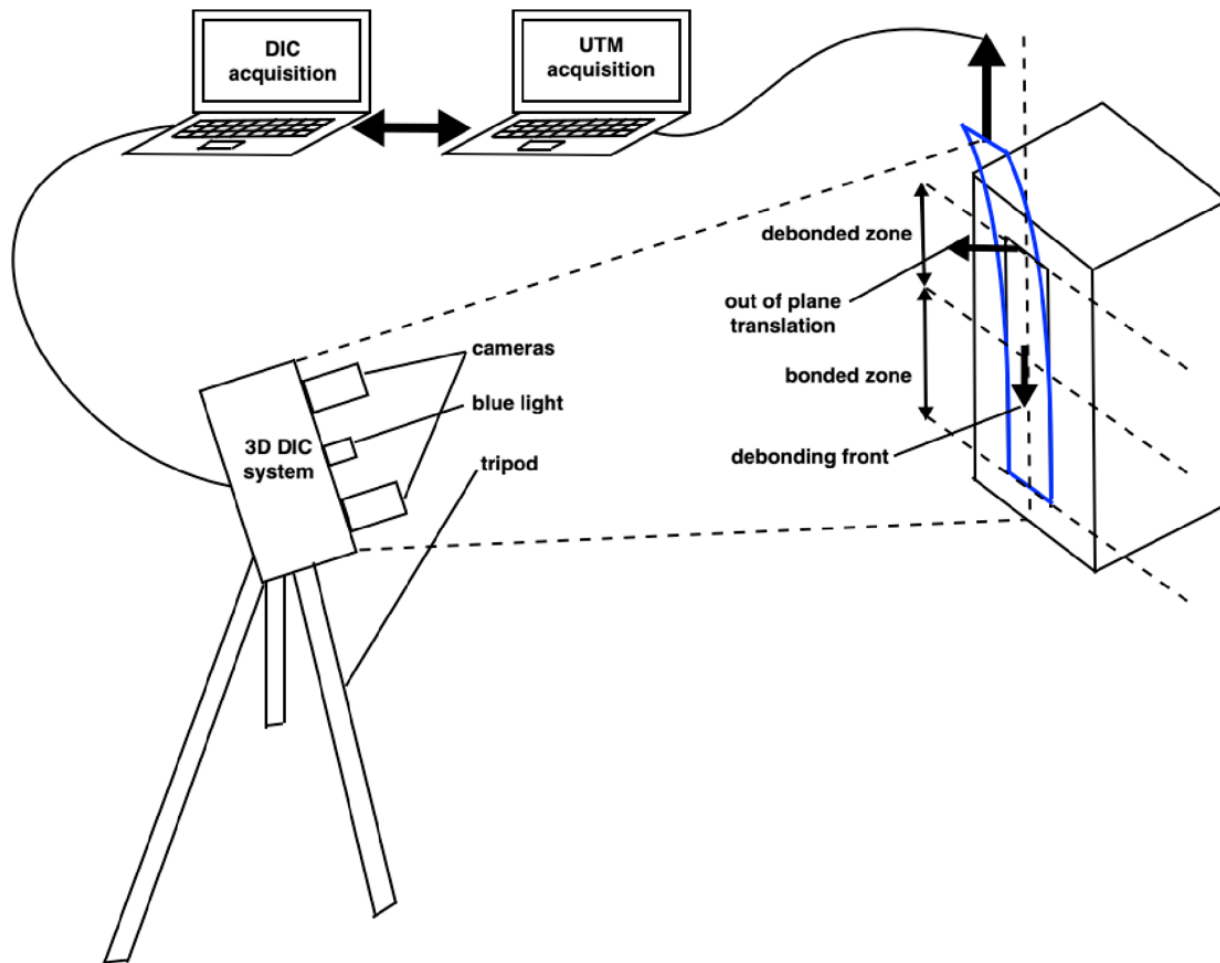


Fig. 3-15. 3D DIC and UTM acquisition during debonding and out-of-plane translations.

CHAPTER 4. EXPERIMENTAL RESULTS & DATA ANALYSIS

4.1. Material Properties

4.1.1 Concrete

The concrete had a fresh density of 2405 kg/m³. The compressive strength of the concrete at 7 days reached 35.4 MPa with a coefficient of variation (CV) of 4.15%. By 28 days, the strength increased to 54.6 MPa with a CV of 11.34%. On the day of the shear lap test, the average compressive strength was 58.8 MPa with a CV of 4.13%. In contrast, the tensile capacity was only tested on the day of the shear lap test, where the tensile split test yielded a result of 2.8 MPa, with a CV of 5.11%.

4.1.2 FRP Coupons

Four CFRP coupons were tested, yielding a mean ultimate tensile strength of 938.6 MPa, an average elastic modulus of 102.3 GPa, and an ultimate rupture strain of 0.92%. A summary of the test results is presented in Table 4-1, while the stress-strain curves for the four coupons are shown in Fig. 4-1.

Table 4-1. Summary of FRP coupon test results.

	f_{tu} (MPa)	E_f (GPa)	ε_{fu} (%)
Coupon 1	1033	102.71	1.01%
Coupon 2	840.5	100.37	0.84%
Coupon 3	984	104.32	0.94%
Coupon 4	897	101.62	0.88%
Mean	938.6 MPa	102.3 GPa	0.92%
CV (%)	9.19%	1.64%	7.98%

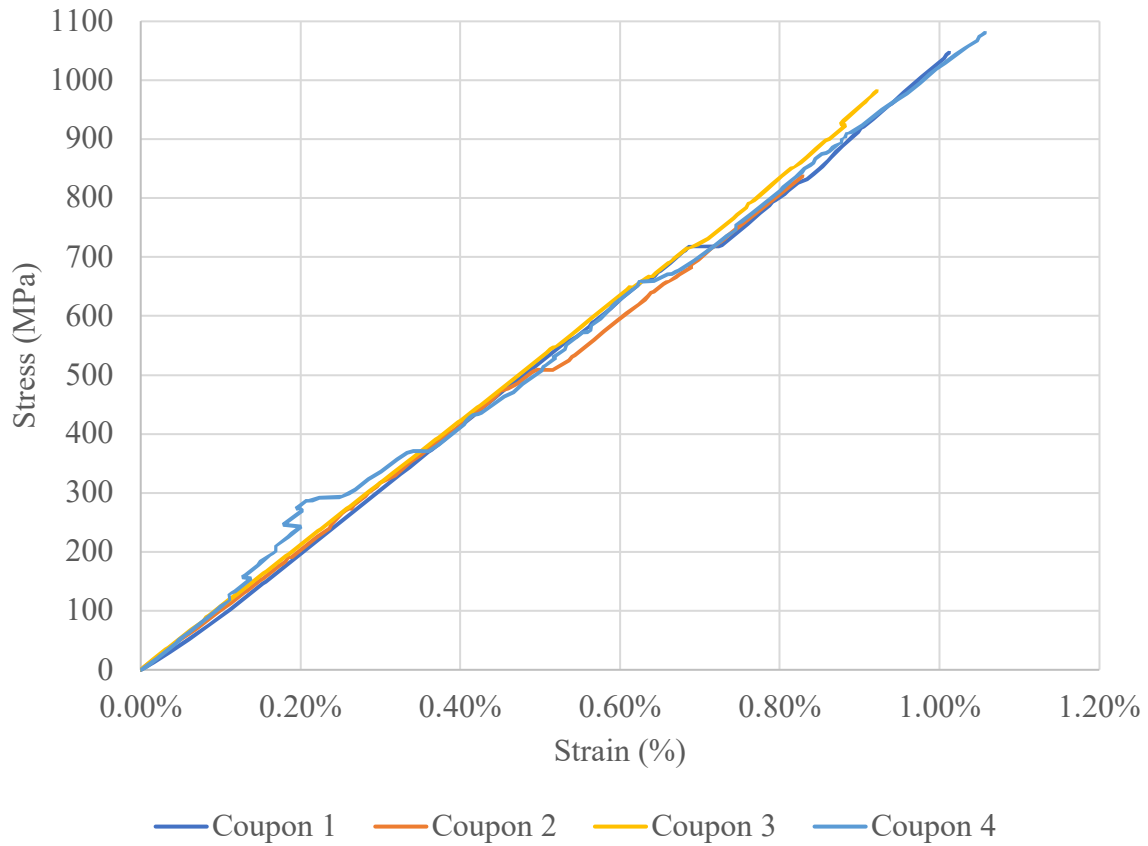


Fig. 4-1. CFRP coupon stress-strain curves.

4.2. Failure Mode

4.2.1 Ultimate Loads

The ultimate loads for the specimens ranged between 6.7 kN and 13.9 kN. The different peak loads along with maximum strains, peak shear stresses, as well as ultimate slips for the different specimen are presented in Table 4-2.

Chapter 4. Experimental Results & Data Analysis

Table 4-2. Summary of peak failure loads, maximum strains, maximum shear stresses, and ultimate slips for all specimens.

Category	Specimen ID	P_u (kN)	$\epsilon_{f,max}$ (%)	τ_{max} (MPa)	s_u (mm)
Control specimens	C-1	12.7	0.36%	5.4	0.39
	C-2	13.2	0.14%	5.1	0.19
Single defect specimens	VL-2	11.7	0.17%	8.6	0.18
	VM-2	10.1	0.10%	5.3	0.11
	VM-1	6.7	0.26%	5.9	0.23
	HL-2	11.5	0.22%	8.5	0.17
	HM-2	11.8	0.26%	17.5	0.23
Dual defect specimens	VC-VC-2	13.9	0.21%	7.2	0.22
	VR-VR-2 (RE)	8.9	0.11%	3.9	0.14
	VR-VR-2 (LE)	8.9	0.03%	3.8	0.07
	VR-VL-2 (RE)	13.2	0.15%	4.8	0.13
	VR-VL-2 (LE)	13.2	0.17%	12.7	0.14

4.2.2 Control Specimens

Specimen C-1 exhibited an ultimate load of 12.7 kN, while Specimen C-2 reached a peak failure load of 13.2 kN. Both control specimens demonstrated a dominant cohesive failure at the adhesive interface that extended over most of the bond length with some observed localized concrete peeling in some area of the laminate, particularly near the load end, where stress concentrations were highest. Post-failure observations, as depicted in Fig. 4-2, illustrate these failure modes and provide visual confirmation of the observed mechanisms.

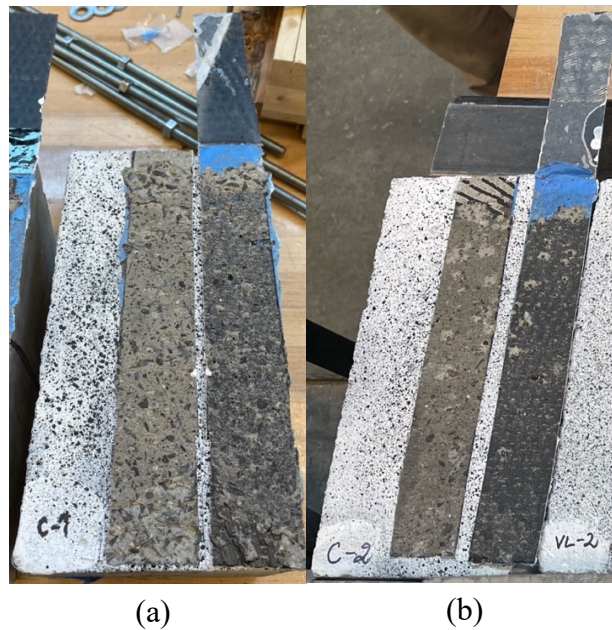


Fig. 4-2. Post-failure condition of the control specimens: (a) C-1 & (b) C-2

4.2.3 Single Defect Specimens

The single-defect specimens exhibited varying behaviours and ultimate load capacities, influenced by their defect configurations. Specimen VL-2 demonstrated a cohesive failure of the adhesive with some concrete peeling in localized areas, with an ultimate load capacity of 11.7 kN.

Similarly, VM-2 experienced interfacial failure, marked by adhesive rupture along the bonded length and thin concrete chunks peeling near the loaded end and predominantly near the bottom of the sheet beyond the defect vicinity. This suggests stress concentrations below the defect region, resulting in a reduced ultimate load of 10.1 kN. Additionally, specimen VM-1, which shares the same defect orientation and placement as VM-2 but features a single sheet of CFRP, exhibited an adhesive rupture, consistent with VM-2, but with a significantly lower ultimate load of 6.7 kN.

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For specimen HL-2, a similar failure pattern occurred, with a cohesive adhesive rupture and some localized concrete peeling and an ultimate load capacity of 11.5 kN.

Lastly, specimen HM-2 followed the same mixed-mode failure trend, with adhesive rupture and localized concrete shearing near the defect area, reflecting the stress concentration pattern observed in VM-2, achieving an ultimate load capacity of 11.8 kN. Fig. 4-3 presents images of the specimens after failure.

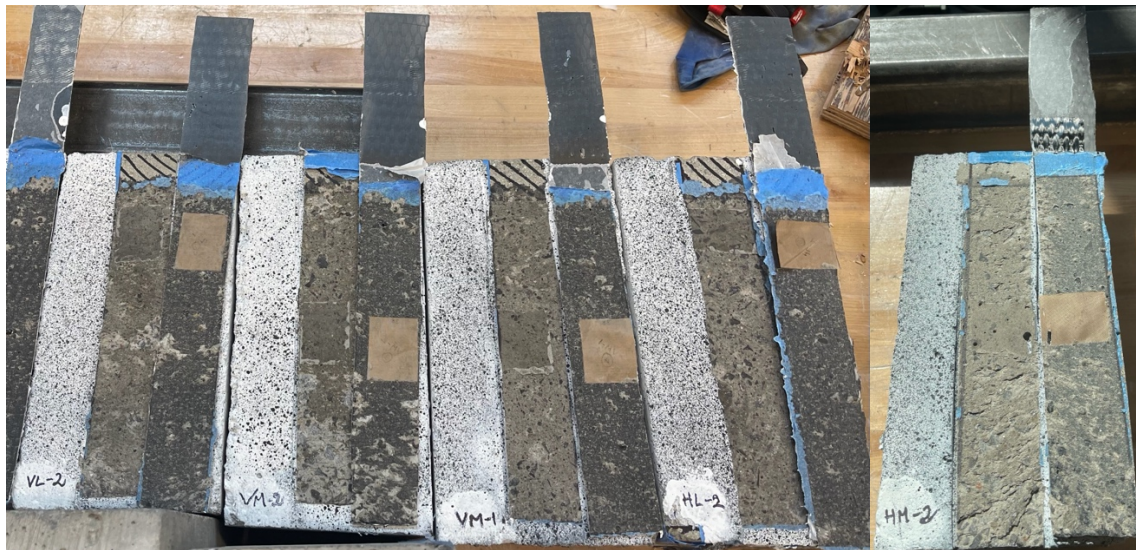


Fig. 4-3. Post-failure condition of single defect specimens from left to right: Specimen VL-2, Specimen VM-2, Specimen VM-1, Specimen HL-2, and Specimen HM-2

4.2.4 Dual Defect Specimens

The three dual-defect specimens displayed the same mode of failures as the single-defect specimens with mainly cohesive adhesive rupture along most of the bond length and very thin concrete shearing along the CFRP laminates, as shown in Fig. 4-4.

Chapter 4. Experimental Results & Data Analysis

For specimens VC-VC-2 and VR-VL-2, thin concrete chunks were distributed somewhat randomly, with peak ultimate loads of 13.9 kN and 13.2 kN, respectively. In contrast, specimen VR-VR-2 exhibited thin concrete peeling primarily along the opposite edges of the two defects and at the bottom of the bonded area, suggesting stress concentrations along the edges and bottom regions beneath the defects. This specimen had a lower ultimate load of 9.0 kN.

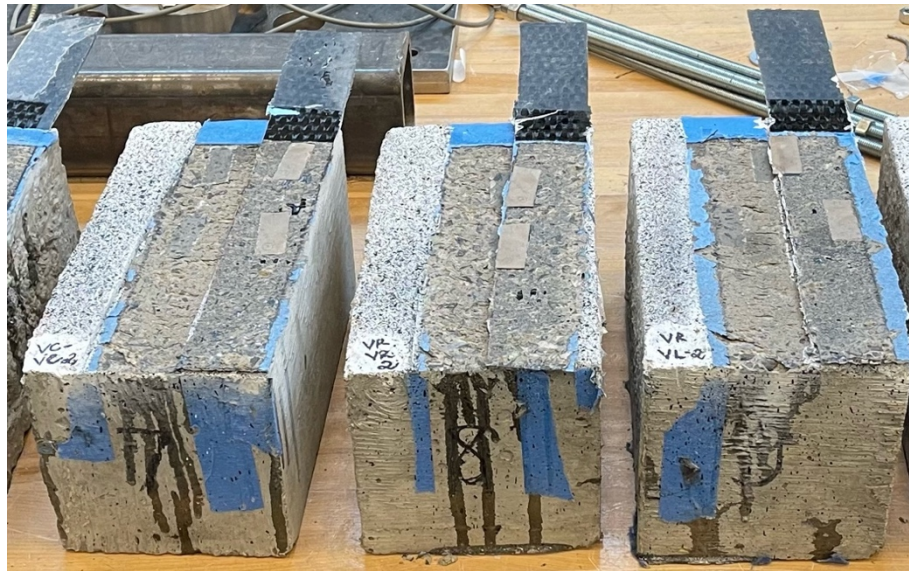


Fig. 4-4. Post-failure condition of dual defect specimens from left to right: Specimen VC-VC-2, Specimen VR-VR-2, and Specimen VR-VL-2

4.3. Strain Distribution

Longitudinal strain contour maps and distributions along the bonded length of the laminates are presented for five selected load stages (A-E). The length is measured from the load end ($y = 0$ mm at the top of the bonded region) to the bottom of the laminates ($y = 250$ mm). To reduce noise, strain measurements were averaged over 20 mm intervals along the length for all specimens.

4.3.1 Control Specimens

4.3.1.1 Specimen C-1

Strain contour maps and strain distribution for specimen C-1 are shown in Fig. 4-5 and Fig. 4-6, respectively. The specimen exhibited minimal strain values during the initial load stages, progressively increasing to a peak strain of 0.36% at stage E ($P = 12.7$ kN). This maximum strain occurred approximately 30 mm below the top of the bonded region.

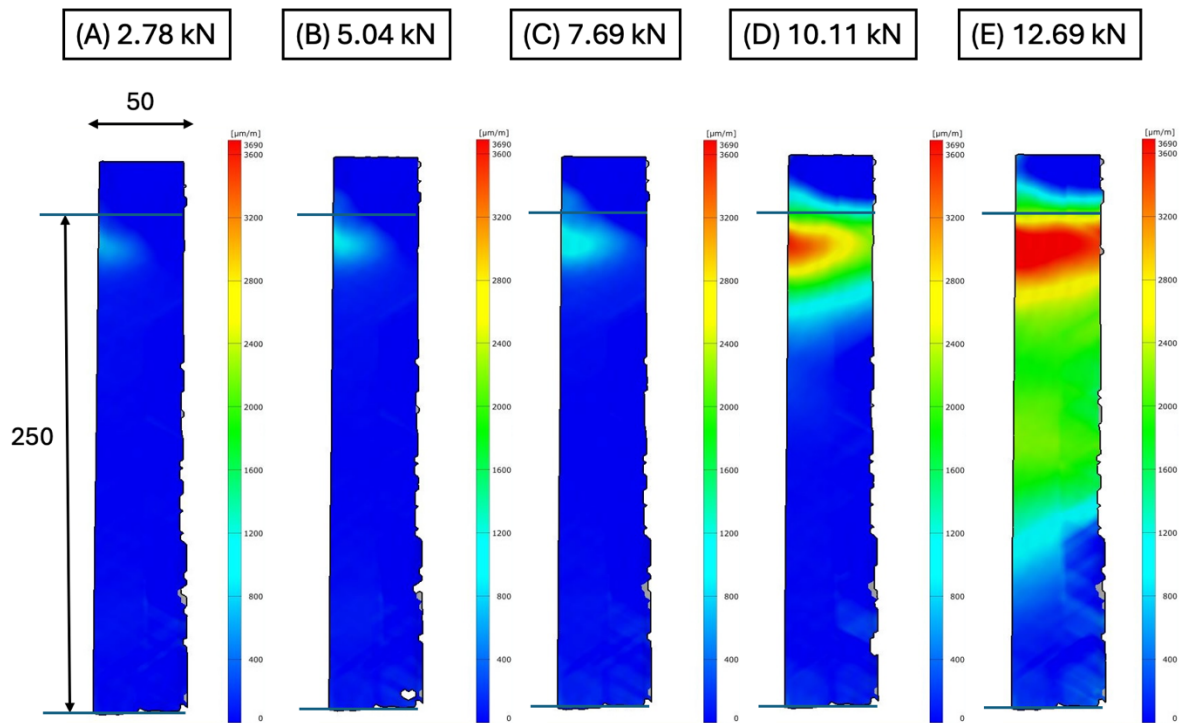


Fig. 4-5. Strain contour maps at various load stages for Specimen C-1.

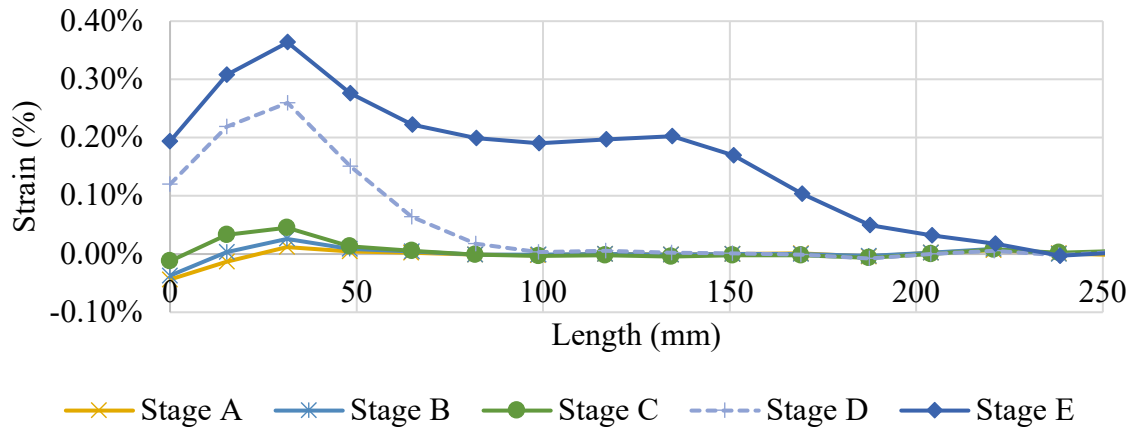


Fig. 4-6. Strain distribution across different load stages for Specimen C-1.

4.3.1.2 Specimen C-2

Strain contour maps for specimen C-2 are shown in Fig. 4-7. At the ultimate stage ($P = 13.2$ kN), the longitudinal strain profile is generally uniform, without any pronounced local strain concentrations. However, there is some variation across the width of the laminate, with slightly higher strain values observed along the right edge compared to the left edge, particularly in the top portion of the laminate.

Strain distributions along a section line in the middle of the bond area are presented in Fig. 4-8. Negative strains are observed at the top portion of the bond length during the first two load stages (7.9 kN and 10.2 kN), suggesting out-of-plane bending of the unbonded portion. As the load increases, the strains progressively rise to a maximum of 0.14% at the top of the bond length, then gradually decrease in a nonlinear gradient, approaching zero at the end of the laminates.

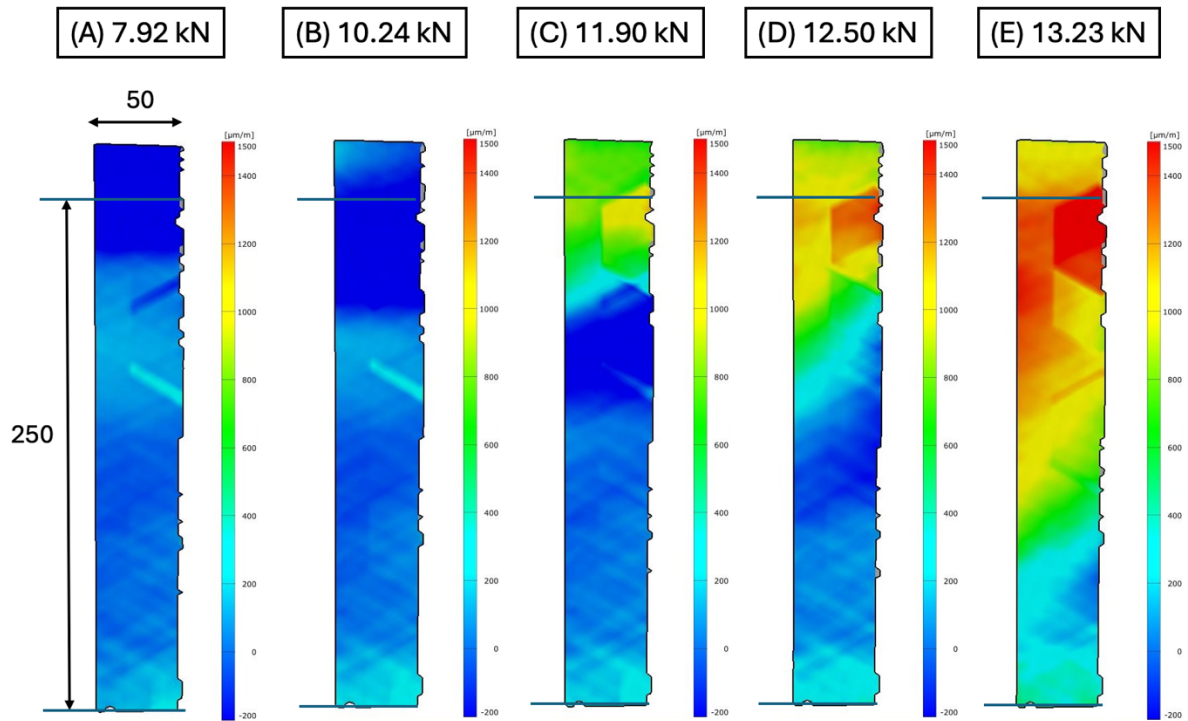


Fig. 4-7. Strain contour maps at various load stages for Specimen C-2.

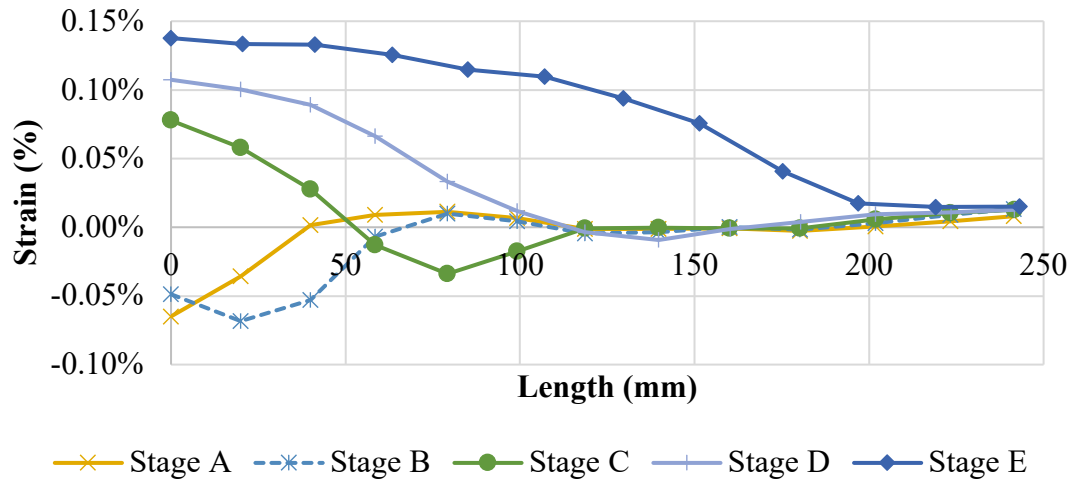


Fig. 4-8. Strain distribution across different load stages for Specimen C-2.

4.3.2 Single Defect Specimens

4.3.2.1 Specimen VL-2

For specimen VL-2, which had a vertically placed defect near the load end of the bond area, the strain contour maps indicate a progressive strain distribution with increasing load. As shown in Fig. 4-9, the highest strains were observed in the defect region in load stage D, close to specimen failure ($P = 11.7$ kN), reaching a maximum value of 0.16%.

The strain distributions presented in Fig. 4-10 indicate that the peak strain of approximately 0.17% occurred at load stage C, about 60 mm below the start of the bonded length, directly beneath the defect. Additionally, Fig. 4-10 suggests a decrease in strain near the defect during the first load stage (A, $P = 6.1$ kN), followed by an increase over the subsequent stages (B–E).

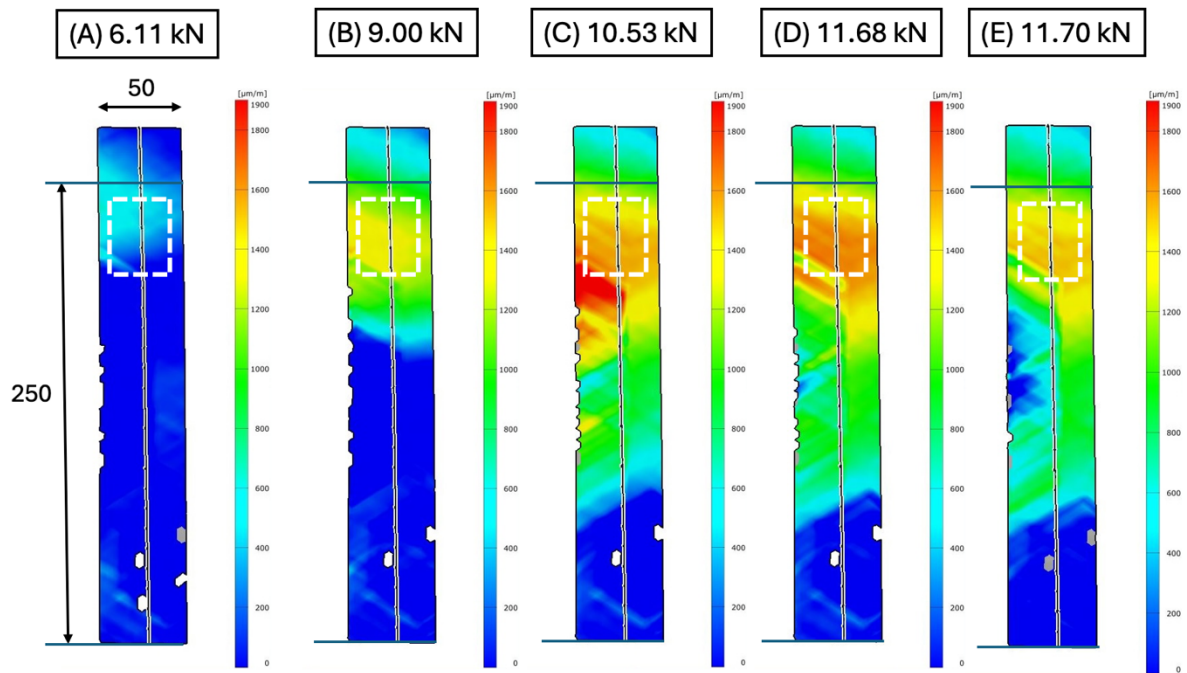


Fig. 4-9. Strain contour maps at various load stages for Specimen VL-2.

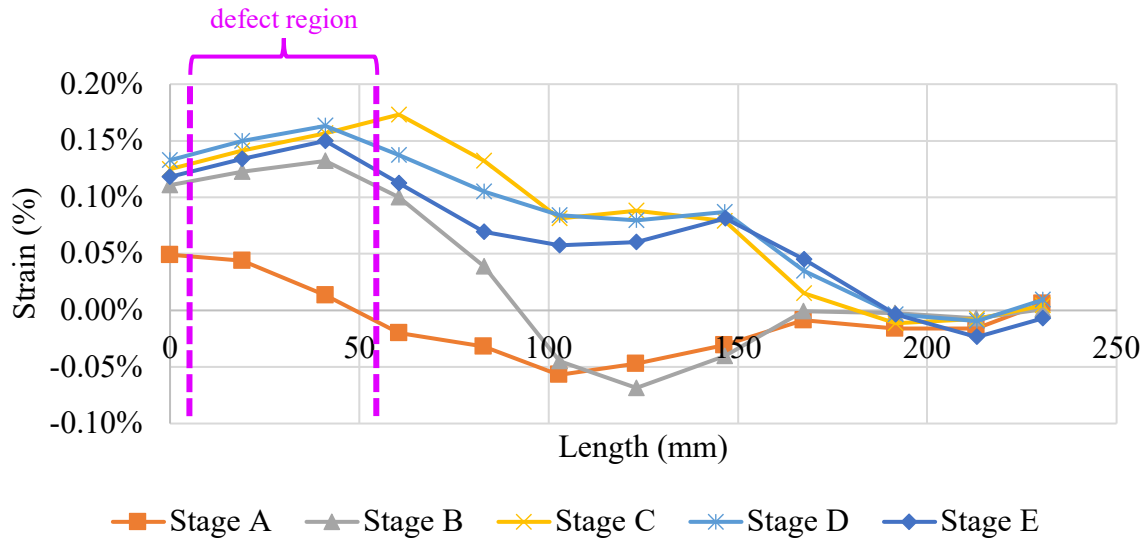


Fig. 4-10. Strain distribution across different load stages for Specimen VL-2.

4.3.2.2 Specimen VM-2

For specimen VM-2, with the defect vertically placed in the middle of the bonded area, the strain contour maps depicted in Fig. 4-11 show progressive strain development until specimen failure. The strain colour gradients reveal a non-uniform strain distribution, with notable eccentricities as strains were predominantly concentrated along the left edge of the laminates. The highest strains were observed during the fourth load stage ($P = 9.03$ kN). The strain distributions, presented in Fig. 4-12, indicate that the peak strain value of 0.10% was reached at load stage D (the fourth stage) at the beginning of the bonded area, potentially due to localized strain concentrations and eccentric load transfer.

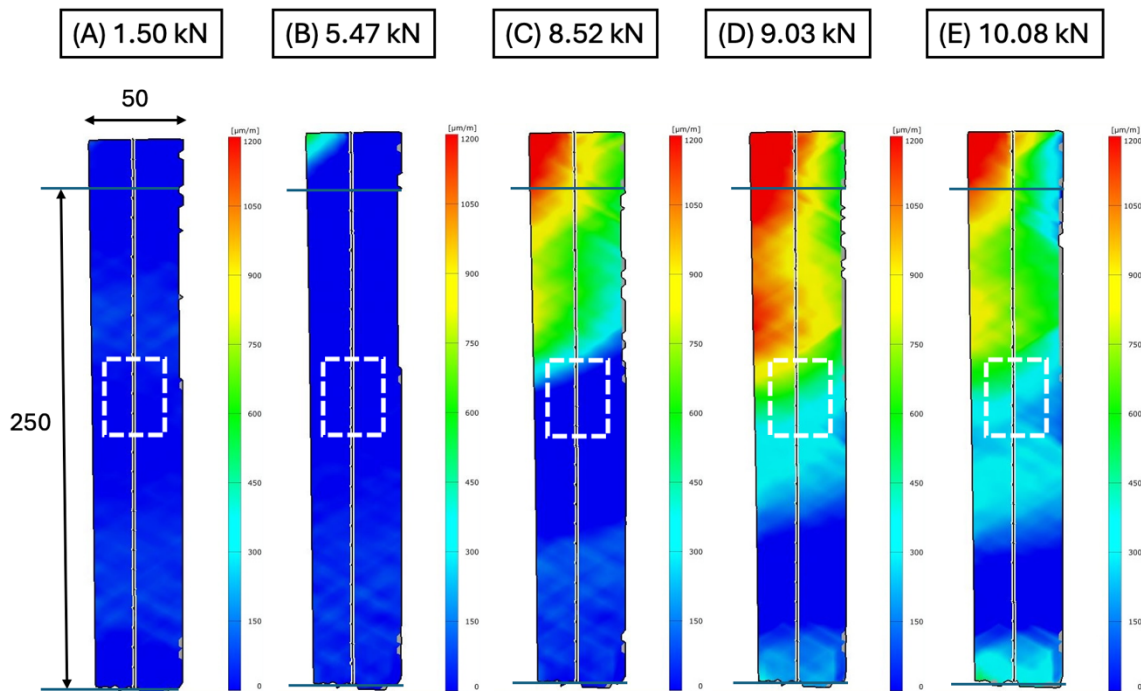


Fig. 4-11. Strain contour maps at various load stages for Specimen VM-2.

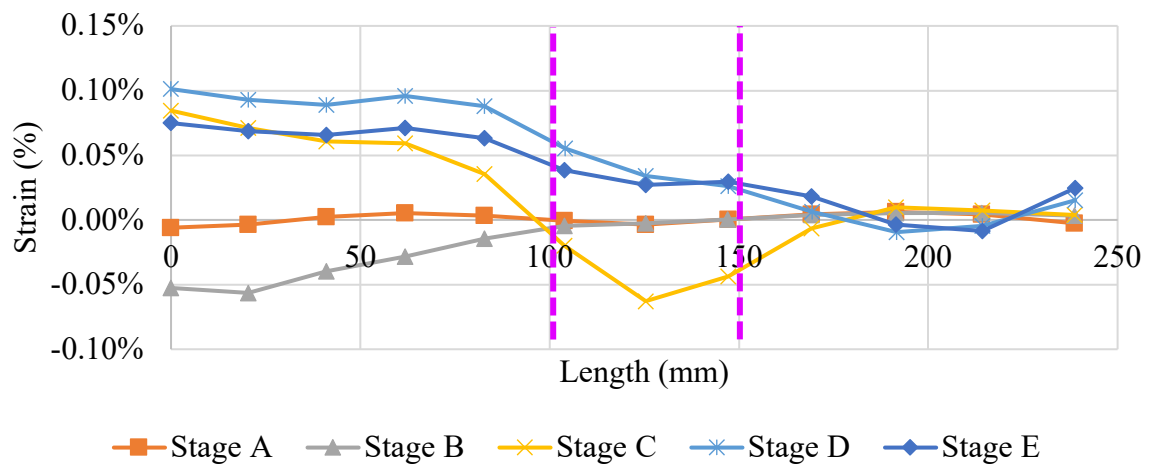


Fig. 4-12. Strain distribution across different load stages for Specimen VM-2.

4.3.2.3 Specimen VM-1

Similar to specimen VM-2, specimen VM-1 has a vertical defect placed in the middle of the bonded area, though it features only one sheet of CFRP. The strain contour maps presented in Fig. 4-13 depict a similar behaviour, with strains progressively developing. While eccentricities are present, they appear less pronounced than in VM-2, as the strain peaks seem more evenly distributed across the specimen's width.

Fig. 4-14 shows the strain distributions along the bond length, indicating a progressive increase in strains with each load stage. For stages A and B, with loads of approximately 2.4 kN and 4.3 kN, respectively, the strains initially take on small negative values just before the defect region. At the ultimate stage (stage E, $P = 6.65$ kN), the maximum strain reaches 0.26% at the beginning of the bonded area, while the strain in the defect vicinity peaks at 0.12%.

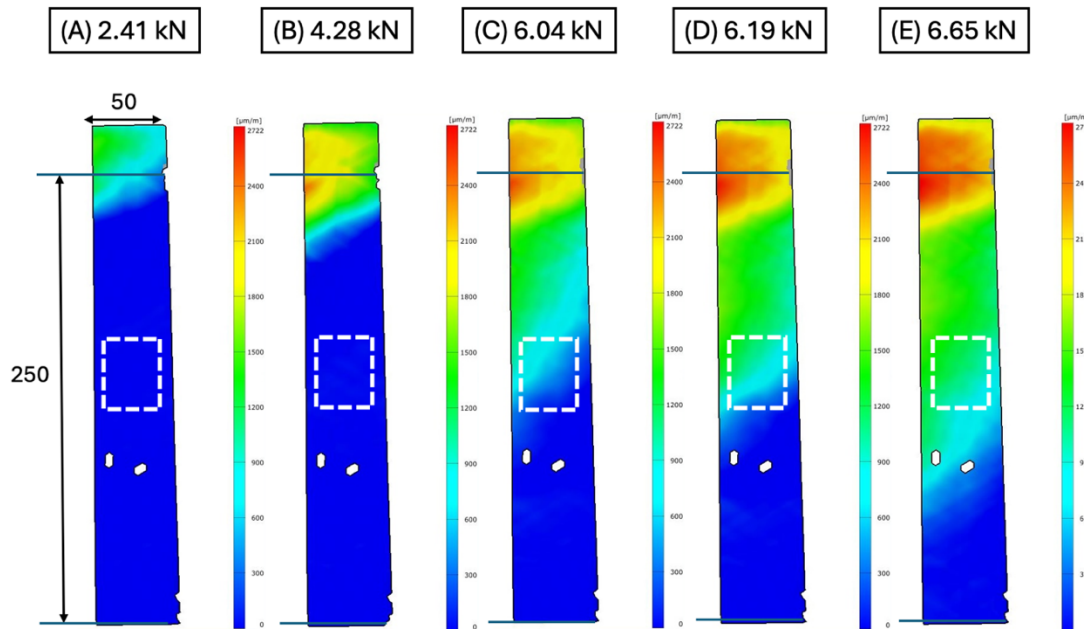


Fig. 4-13. Strain contour maps at various load stages for Specimen VM-1.

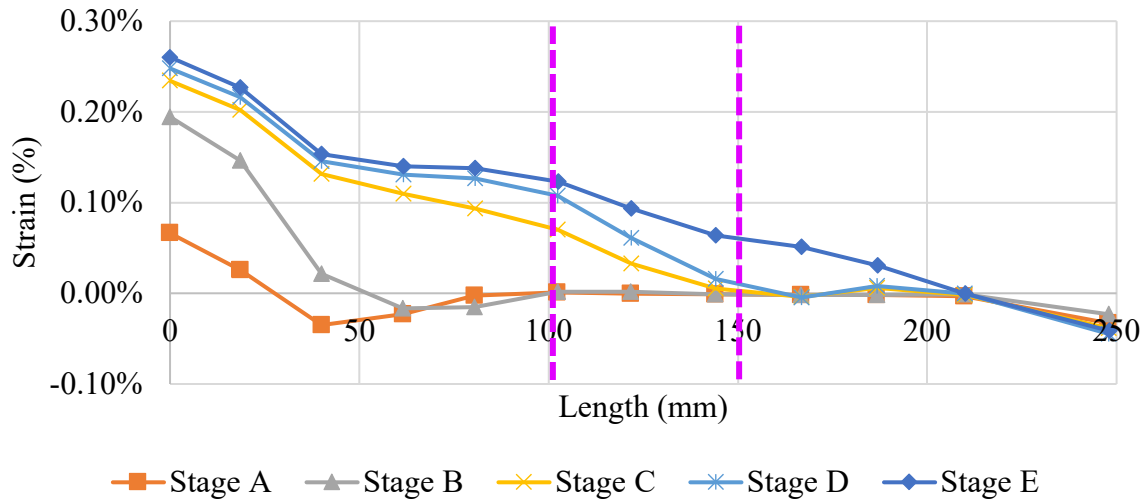


Fig. 4-14. Strain distribution across different load stages for Specimen VM-1.

4.3.2.4 Specimen HL-2

For specimen HL-2, the defect was placed horizontally at the loaded end of the bond area, spanning the entire width of the laminate. The strain contour maps, presented in Fig. 4-15, reveal eccentricities, particularly at the top right edge of the sheets where strains are higher compared to the top left edge. Stress concentrations were also observed in the last three load stages below the defect area, particularly along the left edge of the laminates.

Strain distributions in Fig. 4-16 show a decrease in strain values within the defect region (stages B to E), followed by a recovery beyond the defect, with a peak strain of 0.15% observed at stage D, approximately 80 mm below the start of the bonded length. The maximum strain of 0.22% occurred at stage D ($P = 10.3$ kN) at the start of the bond area ($y = 0$ mm).

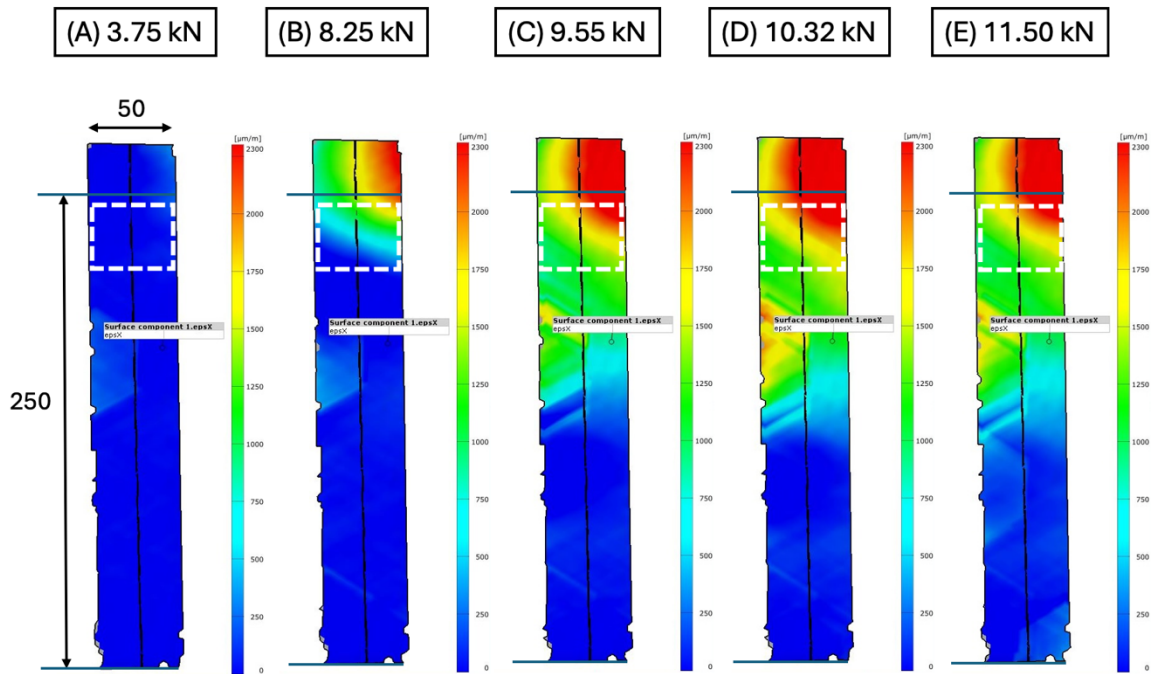


Fig. 4-15. Strain contour maps at various load stages for Specimen HL-2.

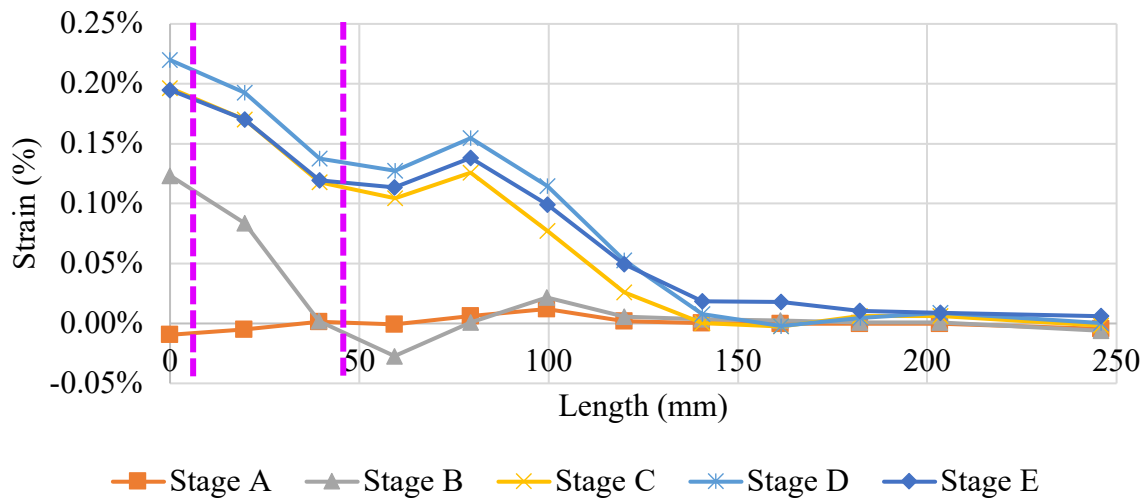


Fig. 4-16. Strain distribution across different load stages for Specimen HL-2.

4.3.2.5 Specimen HM-2

Specimen HM-2 features a horizontal defect placed in the middle of the bond area, spanning the width of the laminates. The strain contour maps for this specimen (Fig. 4-17) show high strains in the unbonded region of the sheets prior to the bond area, as well as inconsistent strain development as the load progresses. Fig. 4-18 illustrates the strain distribution along the bonded length, with negative strains observed before the defect region for load stages A and B, where the loads are 3.3 kN and 6.9 kN, respectively. The strains decrease in the defect vicinity for all load stages, only to increase again after this region. Interestingly, the highest strains of 0.26% are observed at stage E ($P = 12.0$ kN), located below the defect region (about 193 mm from the start of the bond length). The strain contour maps suggest stress concentrations in this area, particularly along the center-right edge of the sheets.

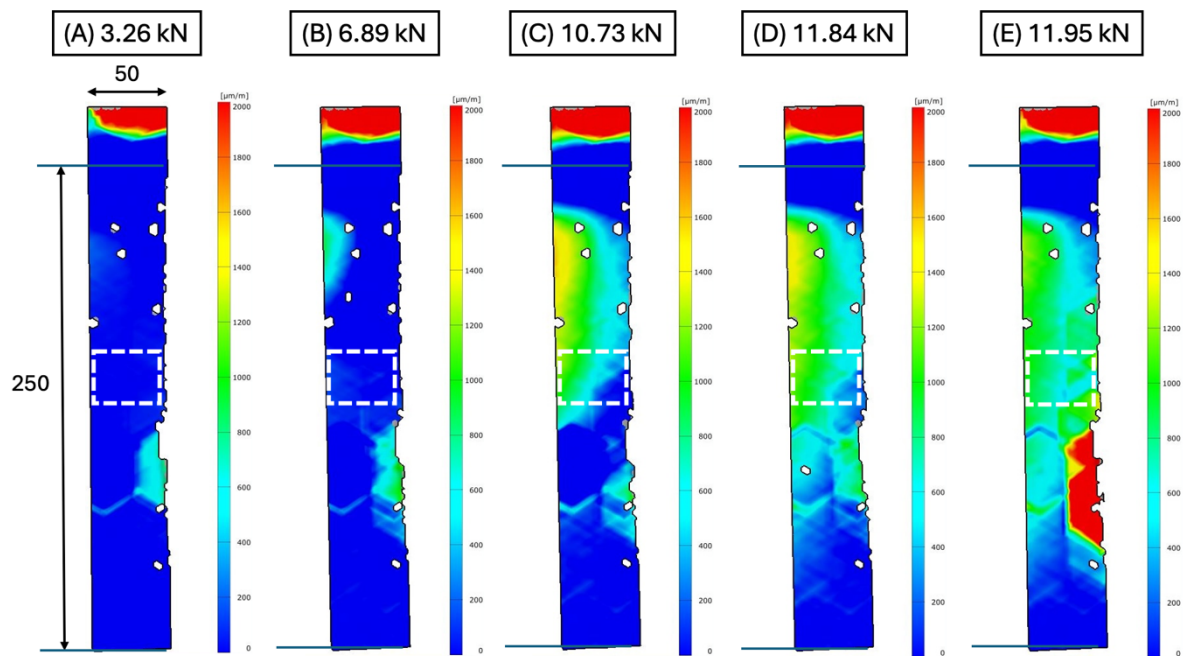


Fig. 4-17. Strain contour maps at various load stages for Specimen HM-2.

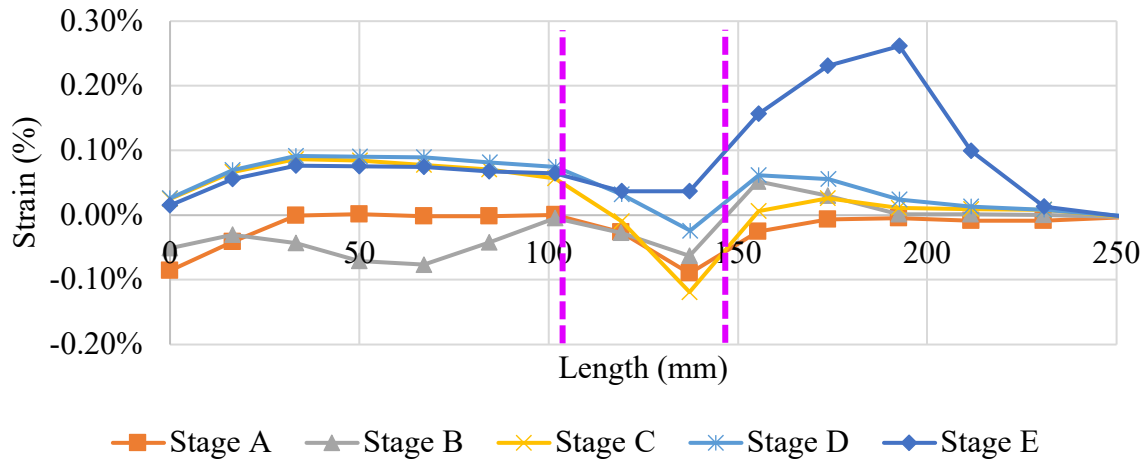


Fig. 4-18. Strain distribution across different load stages for Specimen HM-2.

4.3.3 Dual Defect Specimens

While the strain, shear and slip distribution for the control and single defect specimens were presented along a longitudinal section line at the middle of the laminates, the analysis for the dual-defect specimens differs slightly as follows:

- For specimen VC-VC-2, the distribution will be evaluated along a section line at the laminate centerline (CL).
- For specimen VR-VR-2, where both defects are located at the right edge of the bonded area, and VR-VL-2, where defects are placed at the right and left edges (load-end and middle, respectively), two section lines will be analyzed: one along the left edge (LE) and another along the right edge (RE) of the laminates.

As with the other specimens, the y-coordinate is measured from the load end of the bonded area ($y = 0$ mm) and extends to the bottom end of the laminates ($y = 250$ mm). The section lines for the dual-defect specimens are illustrated in Fig. 4-19.

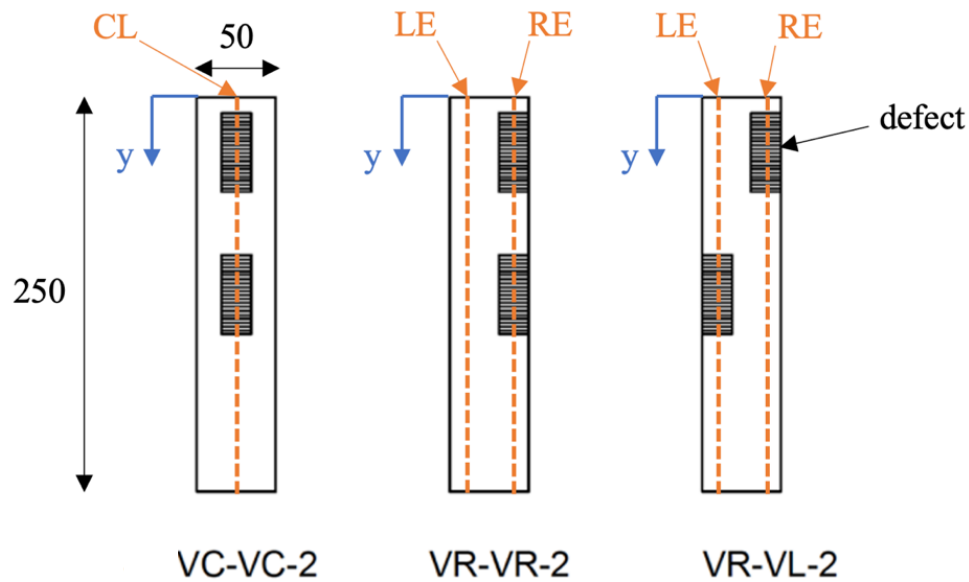


Fig. 4-19. Section line distribution across dual-defect specimens (dimensions in mm).

4.3.3.1 Specimen VC-VC-2

For specimen VC-VC-2, two equally sized defects (19 mm x 50 mm) were oriented vertically and placed along the centerline of the interfacial region, one near the load end and the other in the middle of the bond area. Similar to the control and single-defect specimens, contour maps for various load stages (A-E) are presented in Fig. 4-20. These maps show progressive strain development with some eccentricities along the right edge of the laminates as the load increases. The highest strains were observed at the load end of the sheets.

Fig. 4-21 illustrates the strain distributions along the centerline of the bond area. At the start of the first defect vicinity, during load stage A ($P = 3.3$ kN), the strains are negative (-0.04%) and progressively increase as the load increases. Strain drops are observed along the first defect's region for the last four load stages (between 9.1 kN and 13.9 kN). Conversely, for the second defect placed in the middle of the bond area, the strains are near zero at stage A ($P =$

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3.3 kN), become negative at stage B, and decrease further during stages C and D (with loads of 11.1 kN and 11.9 kN, respectively). For the final load stage E ($P = 13.9$ kN), the strains remain somewhat constant in the first half of the second defect vicinity, then drop again in the latter half.

Overall, the maximum strains are reached during the final load stage E, where they reach approximately 0.21% at the start of the bond area ($y = 0$ mm).

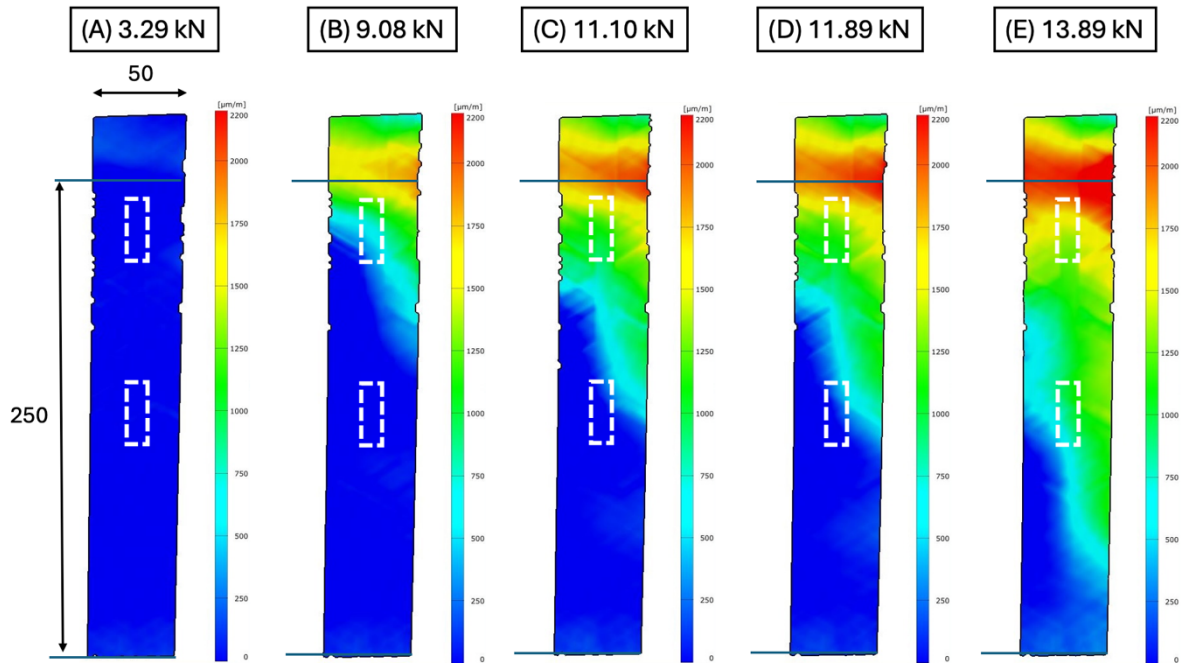


Fig. 4-20. Strain contour maps at various load stages for Specimen VC-VC-2.

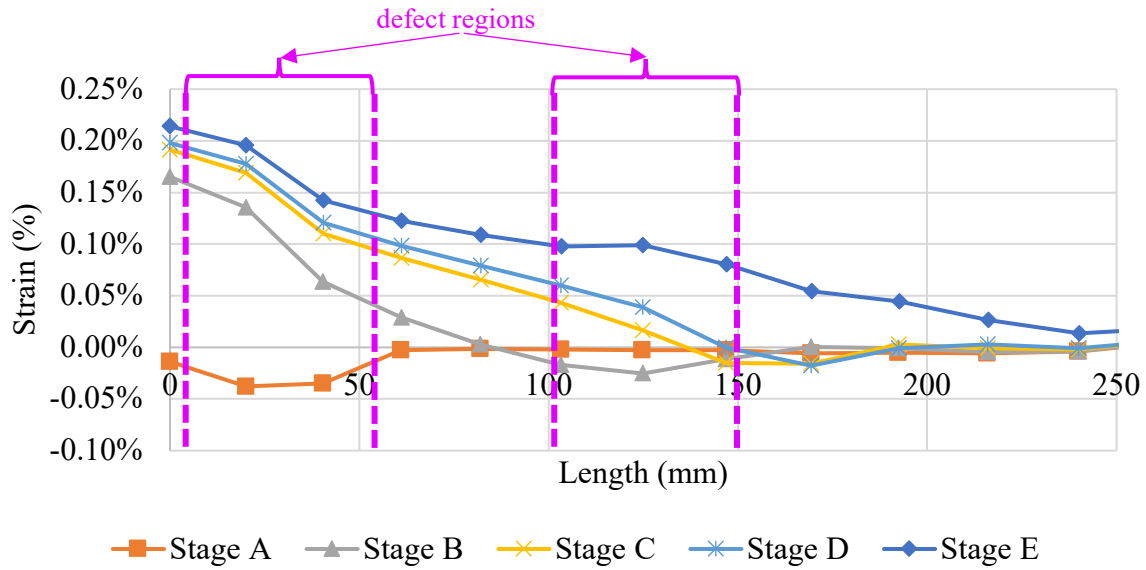


Fig. 4-21. Strain distribution across different load stages for Specimen VC-VC-2.

4.3.3.2 Specimen VR-VR-2

Similar to specimen VC-VC-2, specimen VR-VR-2 features two equally sized defects, placed vertically at the load end and the middle of the bond area, respectively. However, for VR-VR-2, these imperfections are located along the right edge of the laminates.

Fig. 4-22 presents the strain contour maps for various load stages (A-E). The strain development is predominantly observed on the right edge of the sheets. In the final load stage ($P = 8.1$ kN), the strains near the middle defect reach an average of 0.06%, while strains at the same distance on the left edge are nearly zero. The highest strains, depicted in red on the maps, occur in the load-end region of the laminates, near the start of the bond area and the first defect.

For strain distribution, two section lines are analyzed: one along the right edge (RE), crossing both defects, and one along the left edge (LE) of the laminates. The strain distributions along the bond length for the RE and LE are shown in Figs. 4-23 and 4-24, respectively. While the

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contour maps display zero strains (due to the legend being fixed to display positive values), the LE distribution shows significant negative strains at the top of the bond area, mainly for the first three load stages (A-C, 4.3 kN to 7.7 kN), and at the bottom of the bond area during stages D and E (7.8 kN and 8.1 kN, respectively). These data suggest bending of the left portions, with the highest positive strain (0.03%) occurring in stage D ($P = 7.8$ kN).

In contrast, the RE distribution shows negative strains in the first 100 mm of the bond length during stages A and E (4.3 kN and 6.5 kN, respectively). The strains become positive in the vicinity of the first defect during the last three load stages (C-E). In stage C ($P = 7.7$ kN), the strains decrease within the defect region.

Conversely, the strains remain relatively constant in the first half of the defect region, then decrease in the second half during stages D ($P = 7.8$ kN) and E ($P = 8.1$ kN).

Ultimately, the maximum strains were observed during the fourth load stage (D), averaging 0.11% at the beginning of the bond length.

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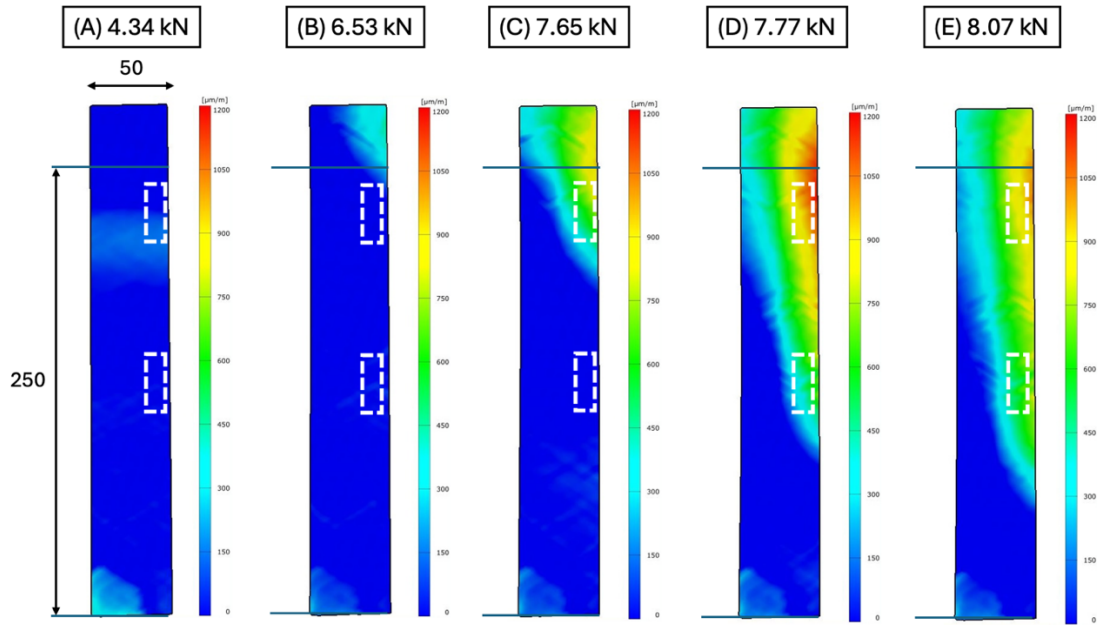


Fig. 4-22. Strain contour maps at various load stages for Specimen VR-VR-2.

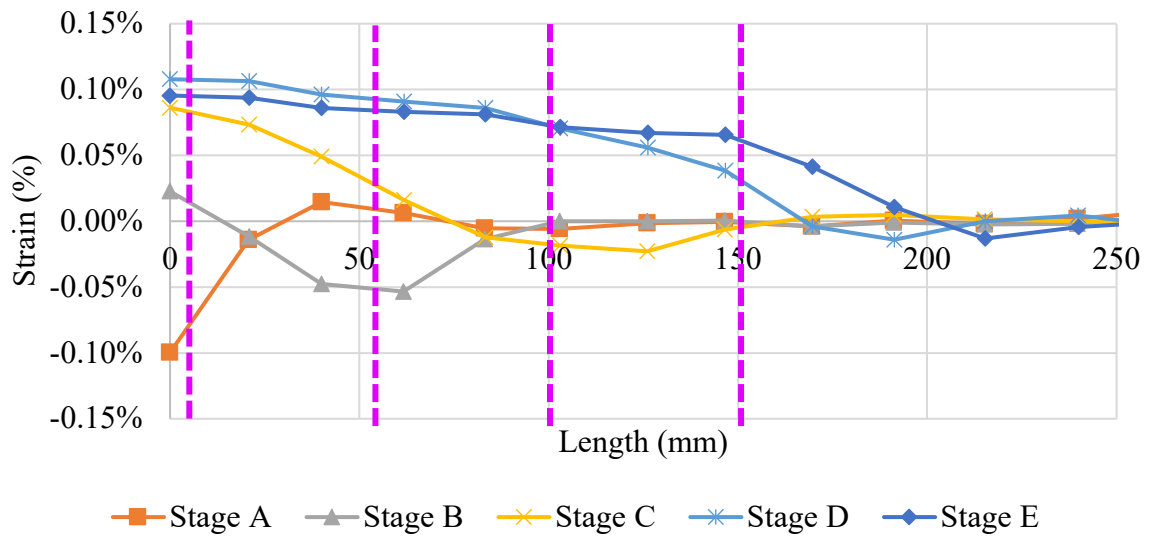


Fig. 4-23. Strain distribution across different load stages along the right edge section line (RE) for Specimen VR-VR-2.

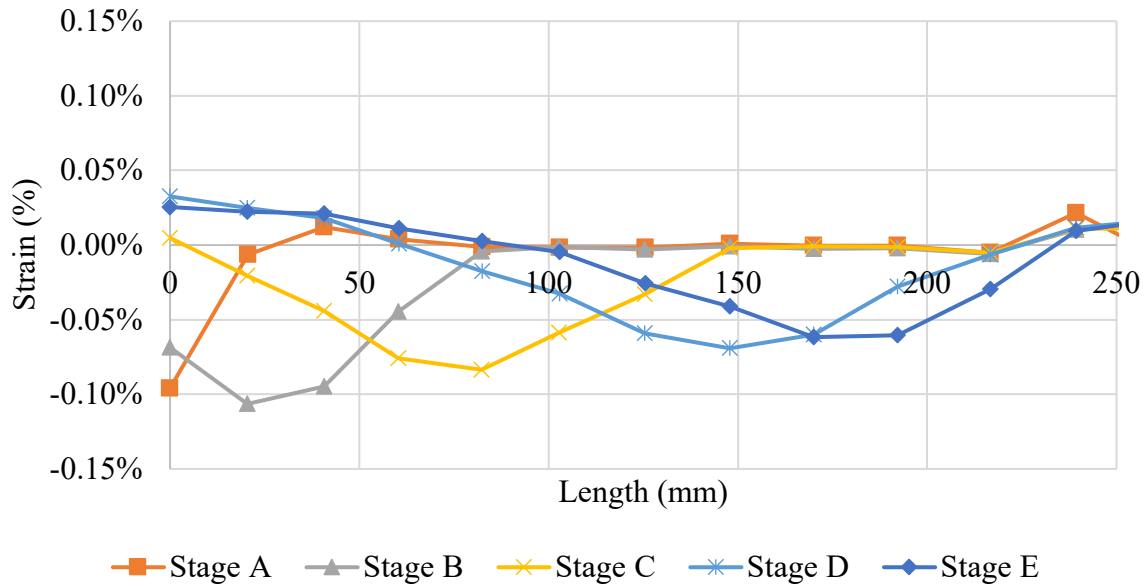


Fig. 4-24. Strain distribution across different load stages along the left edge section line (LE) for Specimen VR-VR-2.

4.3.3.3 Specimen VR-VL-2

Specimen VR-VL-2 features two vertically oriented defects located at the load end and the middle of the bond area, respectively. The strain contours, as shown in Fig. 4-25, exhibit a progressive distribution pattern similar to that of specimen VC-VC-2, with strain concentrations predominantly observed at the load end of the laminates. Strains appear more concentrated along the right edge (RE) of the sheets as the load increases.

Strain distributions were obtained along two section lines: one at the RE of the bond area (Fig. 4-26), crossing the load-end defect, and the other at the LE of the bond area (Fig. 4-27), crossing the middle-placed defect.

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For the RE, strains near the load-end defect decrease linearly across the last four load stages (B–E), corresponding to loads between 5.9 kN and 12.9 kN. Negative strain values are observed at stage B ($P = 5.9$ kN) beyond the defect vicinity (approximately $y = 60$ mm). Beyond the middle defect, strain values decline more steeply, approaching zero near the end of the bond area (beyond $y = 150$ mm).

The LE strain distribution exhibits similar initial strain values at $y = 0$ for stages B–E. Along the bond length, strains decrease sharply across the load-end defect, reaching negative values before and through the vicinity of the middle defect. Beyond $y = 150$ mm, strain values approach zero, consistent with the RE distribution.

The ultimate strain for this specimen was obtained at the start of the bond area as 0.15% for the RE and 0.17% for the LE.

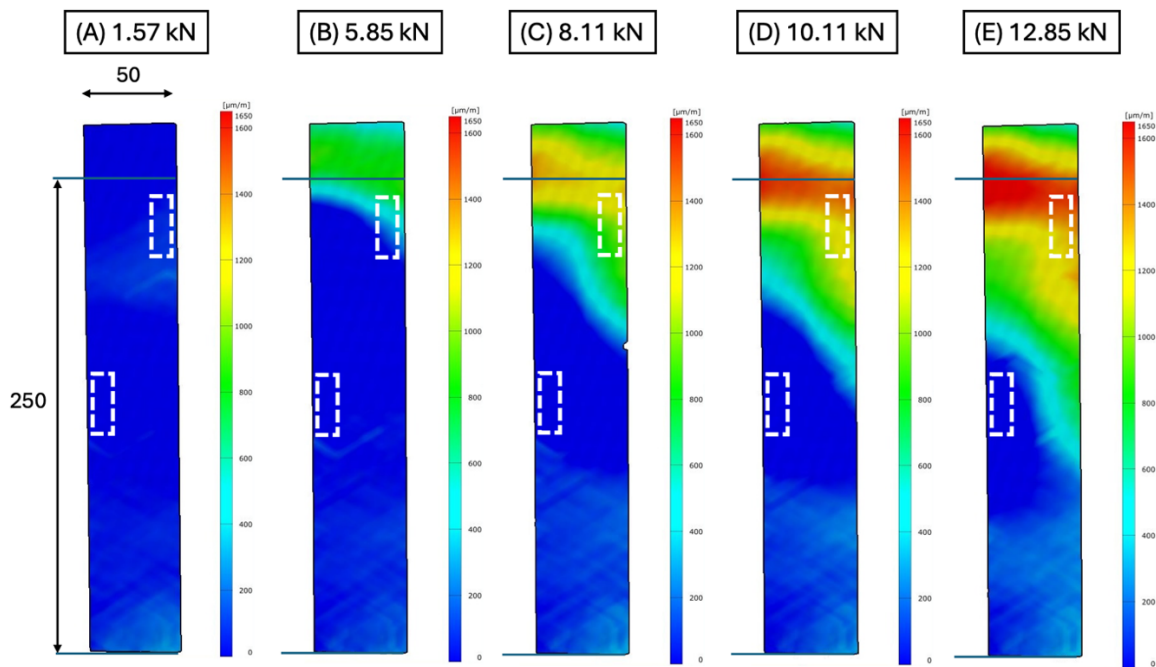


Fig. 4-25. Strain contour maps at various load stages for Specimen VR-VL-2.

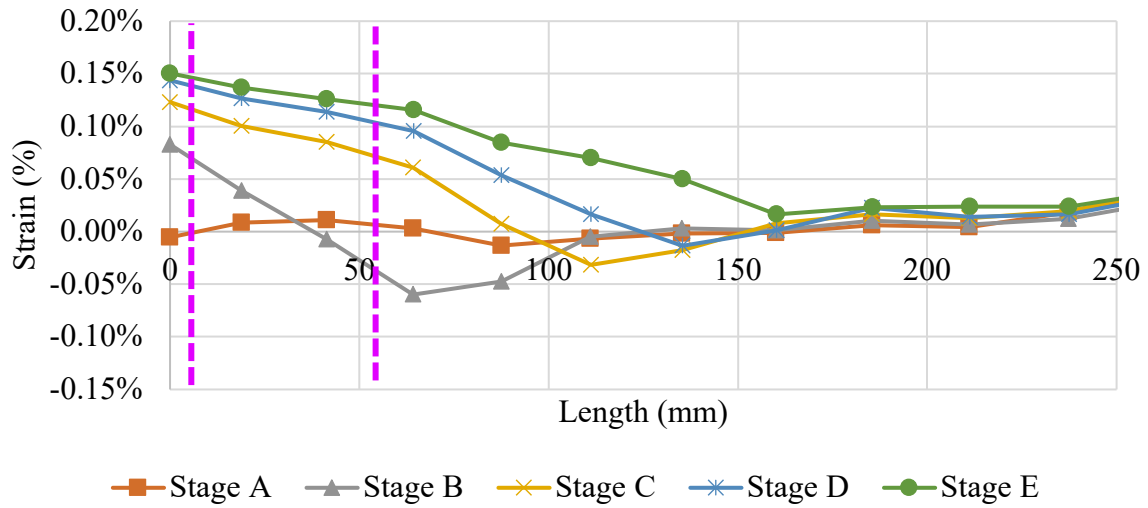


Fig. 4-26. Strain distribution across different load stages along the right edge section line (RE) for Specimen VR-VL-2.

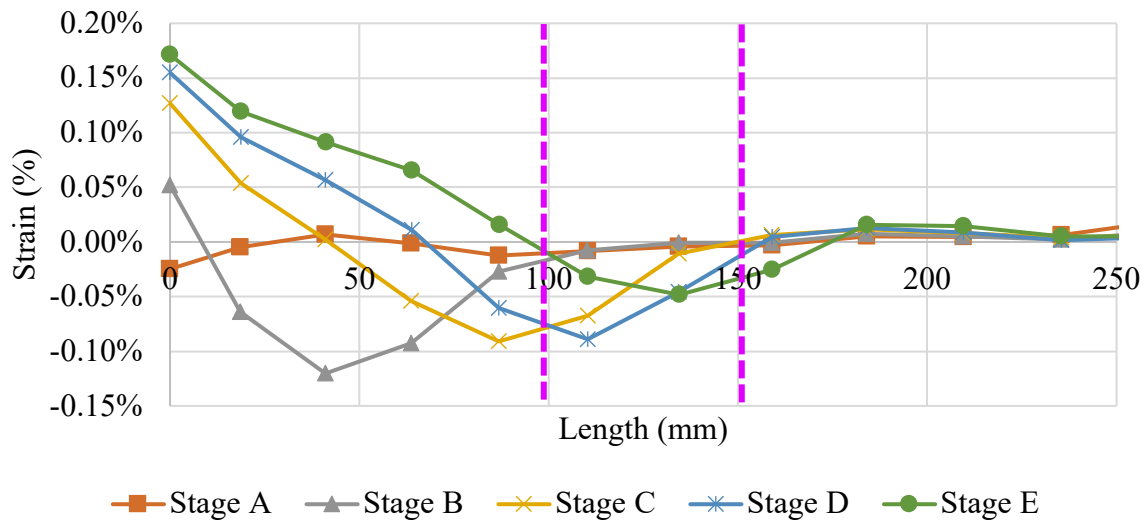


Fig. 4-27. Strain distribution across different load stages along the left edge section line (LE) for Specimen VR-VL-2.

4.4. Shear Stress & Slip Distribution

4.4.1 Rationale

Strains and displacement values were obtained through the 3D DIC analysis. From these, shear stresses along the bond length (τ_i) were derived by calculating average strain differences ($d\varepsilon_f$) at $dy = 20$ mm increments using Eq. 4-1, where E_f and t_f are the elastic modulus and thickness of the CFRP laminate, respectively. The choice of 20 mm increments was made to reduce noise and avoid large fluctuations in the shear stress distribution caused by local strain variations, which do not reflect the overall bond behaviour. This approach provided a smoother strain curve and bond stress distribution while maintaining computational efficiency. Smaller increments were initially tested but resulted in a noisy distribution, which led to inaccurate results.

$$\tau_i = E_f t_f \left(\frac{d\varepsilon_f}{dy} \right)_i \quad \text{Eq. 4-1}$$

Conversely, the slip distribution (s_i) along the bond length was calculated as the difference between the displacement values of the FRP (δ_{FRP}) and the concrete (δ_c) at the same height along the specimen, as expressed in Eq. 4-2.

$$s_i = \delta_{FRP} - \delta_c \quad \text{Eq. 4-2}$$

4.4.2 Control Specimens

4.4.2.1 Specimen C-1

The bond shear stress, influenced by the stiffness of the laminates and the strain gradient, was derived from the strain distribution for specimen C-1 and is presented in Fig. 4-28. Theoretically, the absolute strain is expected to be nonlinear and decreasing, with maximal strains at the beginning of the bond region and minimal or zero strain at the bottom of the sheet. For specimen C-1, the calculated shear stresses in two regions (0–30 mm and 110–130 mm) resulted in negative bond stresses, which violates the theoretical assumptions of the model. Consequently, the bond stress was set to zero in these regions. The underlying mechanisms influencing these irregularities will be discussed further in Chapter 5.

The peak shear stresses for specimen C-1 increased progressively with the applied load, reaching a maximum of 6.6 MPa at approximately $y = 50$ mm during load stage D ($P = 10.1$ kN). Notably, in the initial stages (A–D), shear stresses were localized at $y = 50$ mm. By stage E ($P = 12.7$ kN), the shear stress at this point decreased to approximately 5.3 MPa, while additional bond stresses developed in the lower region of the bonded area, forming a second peak of 3.7 MPa at $y = 170$ mm.

The slip distribution across the five load stages (A–E) is illustrated in Fig. 4-29. Slip values were maximal at the start of the bond length and gradually decreased to zero at the bottom of the laminates. In the initial load stages, slip values remained low, reaching approximately 0.12 mm at stage D. The ultimate slip was recorded as 0.39 mm during stage E prior to failure.

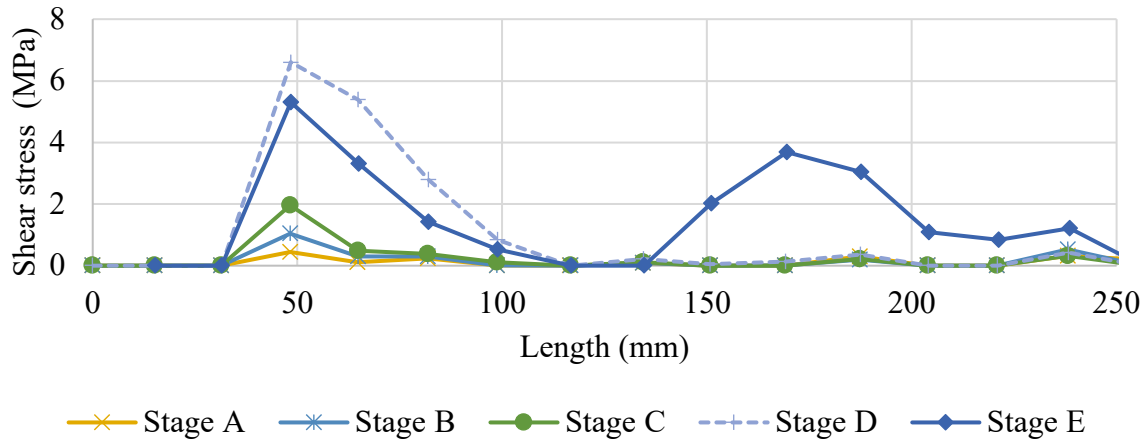


Fig. 4-28. Shear stress distribution across different load stages for Specimen C-1.

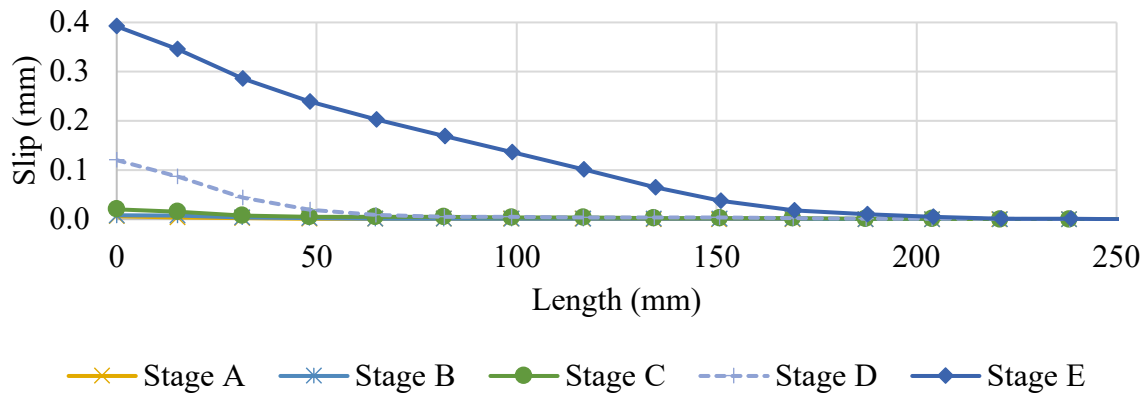


Fig. 4-29. Slip distribution across different load stages for Specimen C-1.

4.4.2.2 Specimen C-2

Similar to specimen C-1, the shear and slip distributions for Specimen C-2 are shown in Figs. 4-30 and 4-31, respectively. The shear distribution progressively developed until reaching a peak of 5.1 MPa at $y = 60$ mm during load stage B ($P = 10.2$ kN). At this load stage, the slip at the beginning of the bond length was recorded as 0.07 mm.

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As the load increased, the shear curve evolved, with shear values at the beginning of the bond length decreasing gradually until reaching zero at the ultimate stage E ($P = 13.2\text{kN}$). At this stage, the maximum shear, 3.0 MPa at $y = 175\text{ mm}$, was lower than the peak shear of 5.1 MPa observed during the second load stage. The ultimate slip at stage E was measured as 0.19 mm .

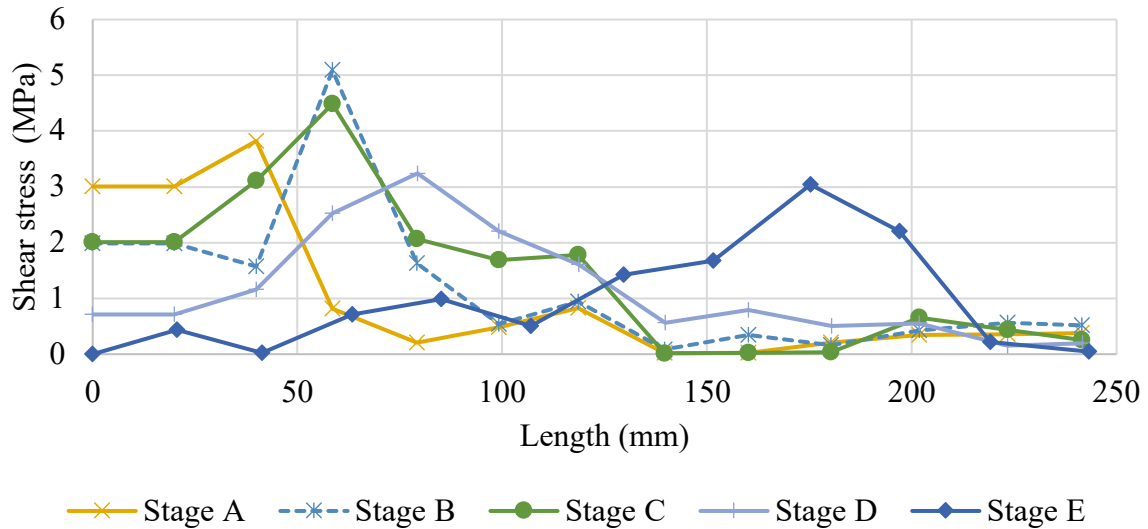


Fig. 4-30. Shear stress distribution across different load stages for Specimen C-2.

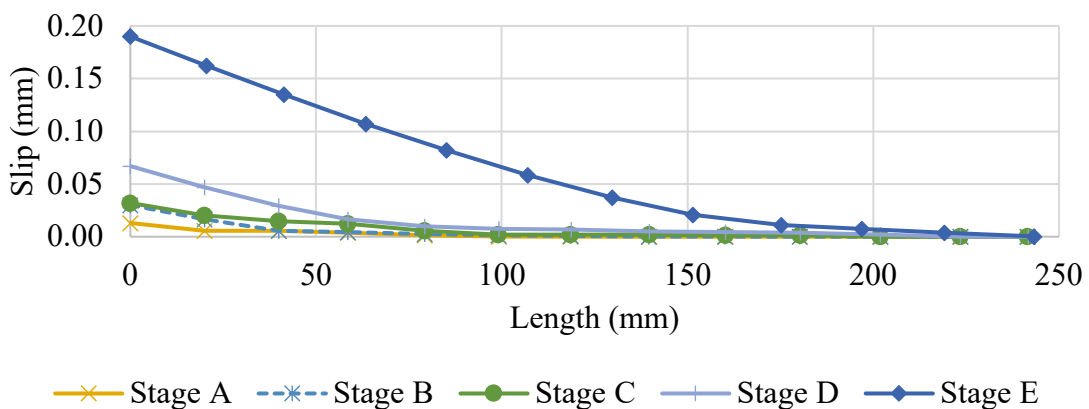


Fig. 4-31. Slip distribution across different load stages for Specimen C-2.

4.4.3 Single Defect Specimens

4.4.3.1 Specimen VL-2

Specimen VL-2, featuring a vertical defect located at the load end of the bond area, exhibited a maximum shear value of 8.6 MPa at $y = 103$ mm during load stage B ($P = 9.0$ kN), as shown in Fig. 4-32.

Within the defect region, the shear values remained low and relatively constant across all load stages, followed by a localized drop before increasing again near the end of the region for the first four load stages (6.1 kN to 11.7 kN). Beyond the defect vicinity, shear stresses increased up to approximately $y = 100$ mm, decreased between $y = 120$ mm and $y = 145$ mm, and increased again between $y = 170$ mm and $y = 190$ mm.

The slip distributions, illustrated in Fig. 4-33, show a progressive increase at the beginning of the bond length as the loads progressed, culminating in an ultimate slip of 0.18 mm during stage E ($P = 11.7$ kN). Notably, calculated slip values along the centerline of the CFRP sheet within the defect vicinity were higher during stage A ($P = 6.1$ kN) compared to stage B ($P = 9.0$ kN), despite the lower load in stage A. Beyond the defect region, however, slip values for stage B exceeded those of stage A.

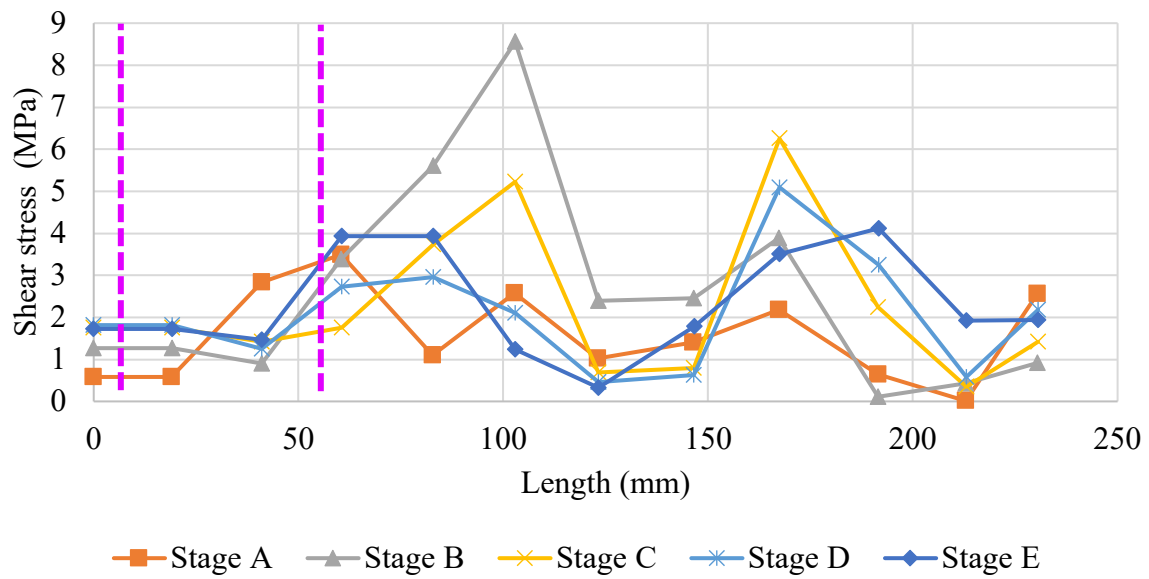


Fig. 4-32. Shear stress distribution across different load stages for Specimen VL-2.

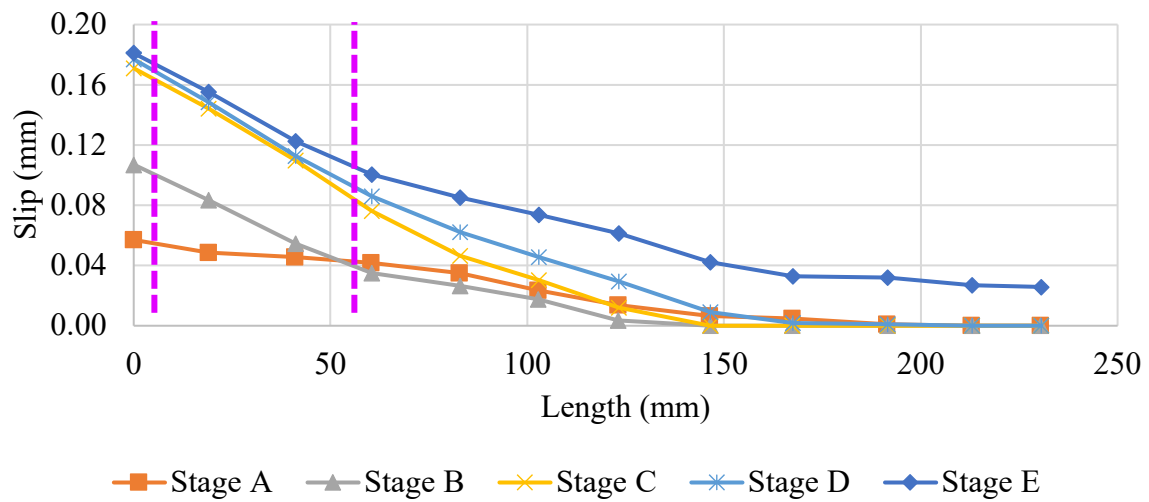


Fig. 4-33. Slip distribution across different load stages for Specimen VL-2.

4.4.3.2 Specimen VM-2

Specimen VM-2 features a vertical defect located in the middle of the bond area. The shear stress distribution presented in Fig. 4-34 shows that the peak shear stresses for the last three load stages (C-E, between 8.5 kN and 10.1 kN) occur at the beginning of the defect vicinity, approximately 100 mm below the start of the bond length. The maximum recorded shear stress was 5.3 MPa for stage C ($P = 8.5$ kN). In the defect vicinity, the shear stresses drop slightly for stages B-E (5.5 kN to 10.1 kN) before increasing again beyond the defect region, around $y = 170$ mm.

The slip distribution, depicted in Fig. 4-35, shows a progressive increase in slip at $y = 0$ as the load progresses, with the maximum slip value of 0.11 mm recorded at stage D ($P = 9.03$ kN). Notably, the slip values for stage A ($P = 1.5$ kN) remain higher than those of stage B ($P = 5.5$ kN) along the bond length. Within the defect vicinity, the slip at stage E ($P = 10.1$ kN) is consistently the highest. Additionally, the slip at stage C exceeds that of stage D in the first half of the defect region but becomes lower in the second half.

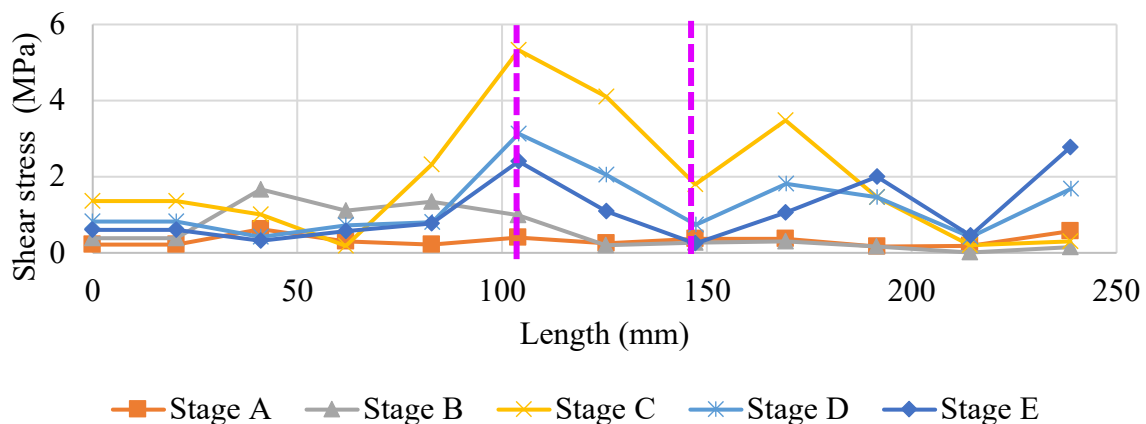


Fig. 4-34. Shear stress distribution across different load stages for Specimen VM-2.

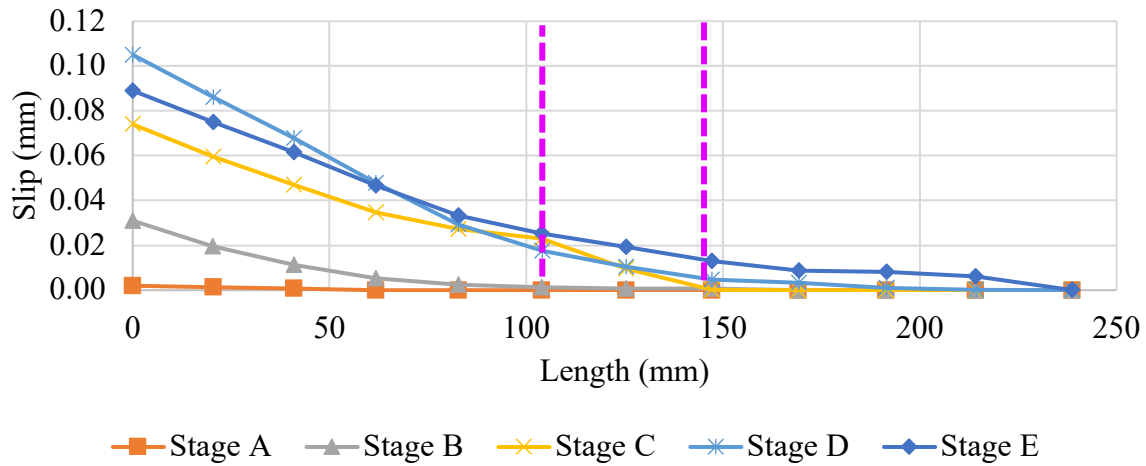


Fig. 4-35. Slip distribution across different load stages for Specimen VM-2.

4.4.3.3 Specimen VM-1

Similar to specimen VM-2, specimen VM-1 had a vertical defect positioned in the middle of the bond area. However, this specimen features only one ply of CFRP, resulting in lower stiffness.

The shear and slip distributions are presented in Figs. 4-36 and 4-37, respectively. The shear stresses increase progressively before the defect region for all load stages, peaking at approximately $y = 40$ mm, where the maximum shear stress of 5.9 MPa was recorded during stage B ($P = 4.3$ kN). For the last four load stages (B-E, ranging from 4.3 kN to 6.7 kN), the shear stresses decrease progressively before reaching the defect vicinity. Within the defect region, stresses increase for stages B, C, and E ($P = 6.0$ kN, 6.2 kN, and 6.7 kN, respectively) before decreasing again beyond the defect, up to approximately 165 mm below the start of the bond length.

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The slip distribution shows the highest values at the beginning of the bond length, decreasing progressively towards the bottom of the CFRP sheet. The ultimate slip was recorded as 0.23 mm during stage E ($P = 6.7$ kN).

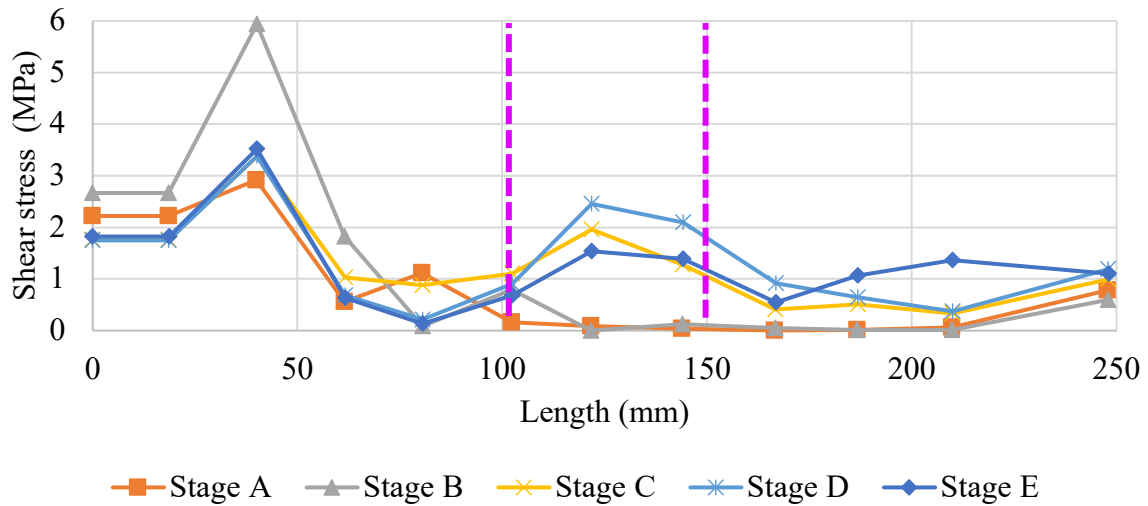


Fig. 4-36. Shear stress distribution across different load stages for Specimen VM-1.

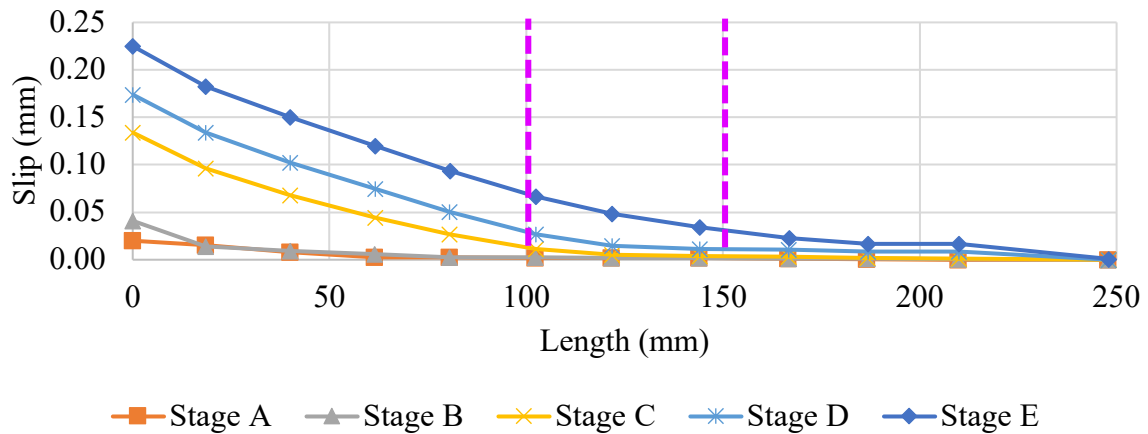


Fig. 4-37. Slip distribution across different load stages for Specimen VM-1.

4.4.3.4 Specimen HL-2

The defect in specimen HL-2 was horizontally oriented, spanning the entire bond width. It was located near the load end of the bond area and features two plies of CFRP, similar to VL-2.

The shear distribution, illustrated in Fig. 4-38, demonstrates the bond stress development as the applied loads increase during the test. The shear stresses reach their peak at the end of the defect vicinity ($y = 40$ mm) for the last four load stages (B–E, ranging from 8.3 kN to 11.5 kN). The maximum shear stress of 8.5 MPa was recorded during the second load stage B ($P = 8.3$ kN).

Beyond the defect region, the shear stresses drop near $y = 60$ mm across all load stages. For stage B, the bond stresses continue to decline, eventually reaching zero near the end of the bond length. However, for stages C–E, the stresses rise to form a second smaller peak at approximately $y = 120$ mm, potentially due to stress concentrations as discussed in subsection 4.3.2.4, before declining again.

The slip distributions for specimen HL-2 are depicted in Fig. 4-39. Similar to other specimens, slip values at the beginning of the bond length progressively increase with higher loads. The ultimate slip value was recorded as 0.17 mm during stage E ($P = 11.5$ kN).

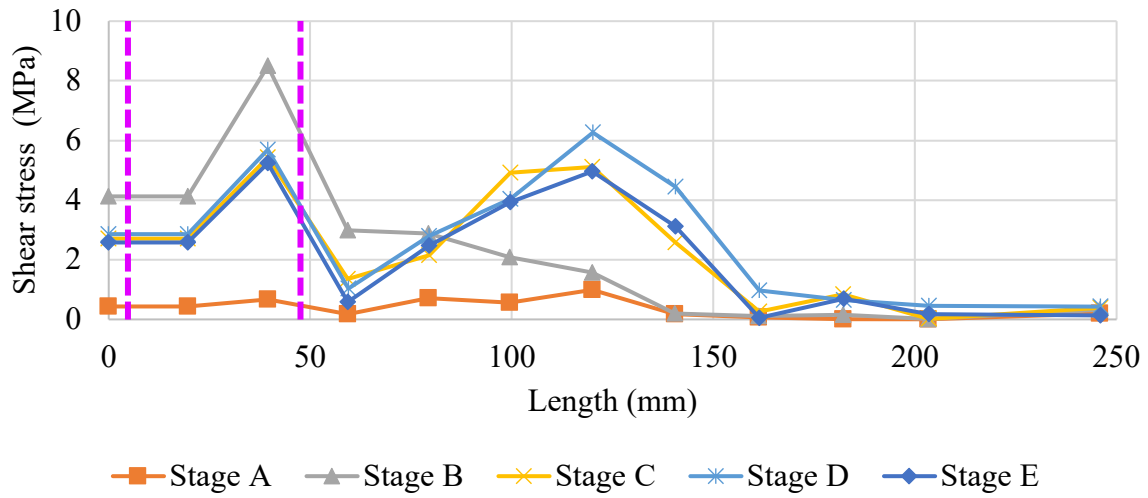


Fig. 4-38. Shear stress distribution across different load stages for Specimen HL-2.

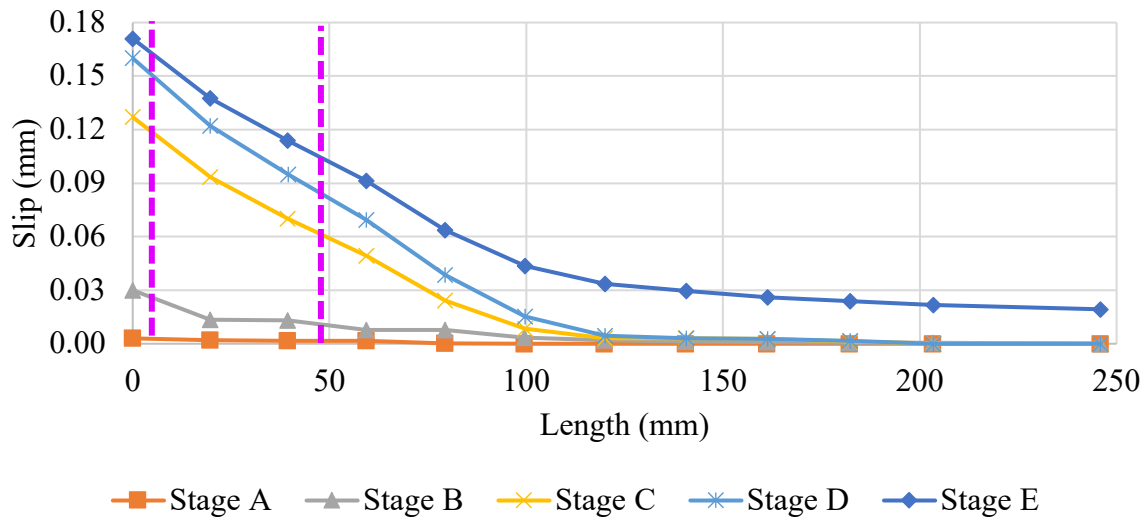


Fig. 4-39. Slip distribution across different load stages for Specimen HL-2.

4.4.3.5 Specimen HM-2

Specimen HM-2 features a defect similar to that of HL-2, spanning the entire bond width but located in the middle of the bond area.

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The shear stress distribution for the five load stages (A–E, ranging from 3.3 kN to 12.0 kN) is illustrated in Fig. 4-40. The shear stresses exhibit an unusual behaviour across all load stages. Initial shear stresses develop at the beginning of the bond length but decrease in the region between $y = 50$ mm and $y = 100$ mm. Beyond this point, the stresses rise again through the defect vicinity, reaching their peak at the bottom of the laminates. The maximum shear stress of 17.5 MPa was recorded at approximately $y = 210$ mm during the final load stage (E, $P = 12.0$ kN).

Similarly, the slip distributions, shown in Fig. 4-41, indicate an increase in slip at $y = 0$ as the loads progress. The highest recorded slip was 0.23 mm during stage E. As expected, slip values decrease with increasing distance from the load end. However, for stages D and E (11.8 kN and 12.0 kN, respectively), the slope of the slip distribution transitions through three distinct behaviours: initially linear before the defect vicinity, more horizontal (steady) within the defect region, and steep (indicating a higher rate of decrease) beyond the defect area.

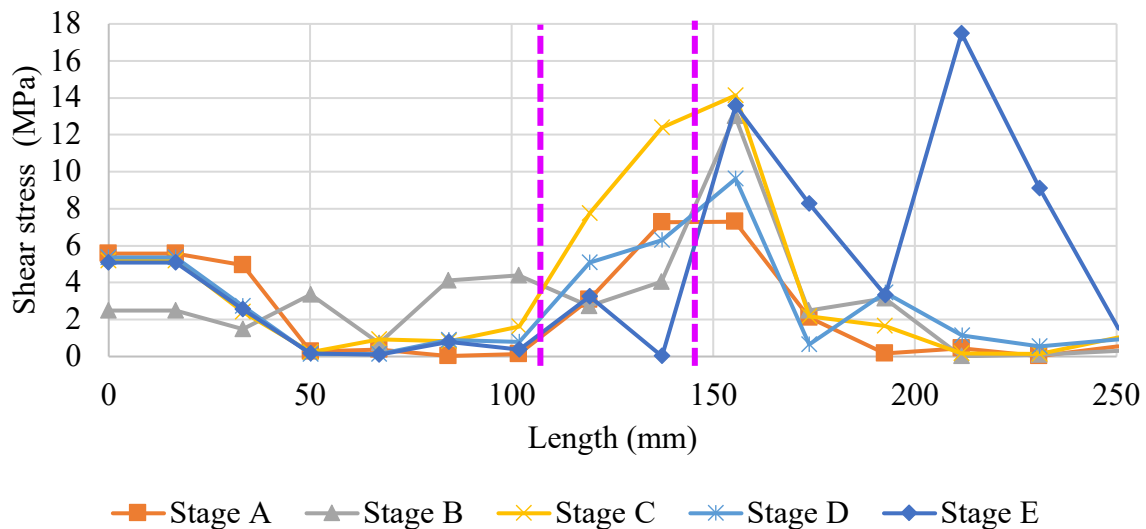


Fig. 4-40. Shear stress distribution across different load stages for Specimen HM-2.

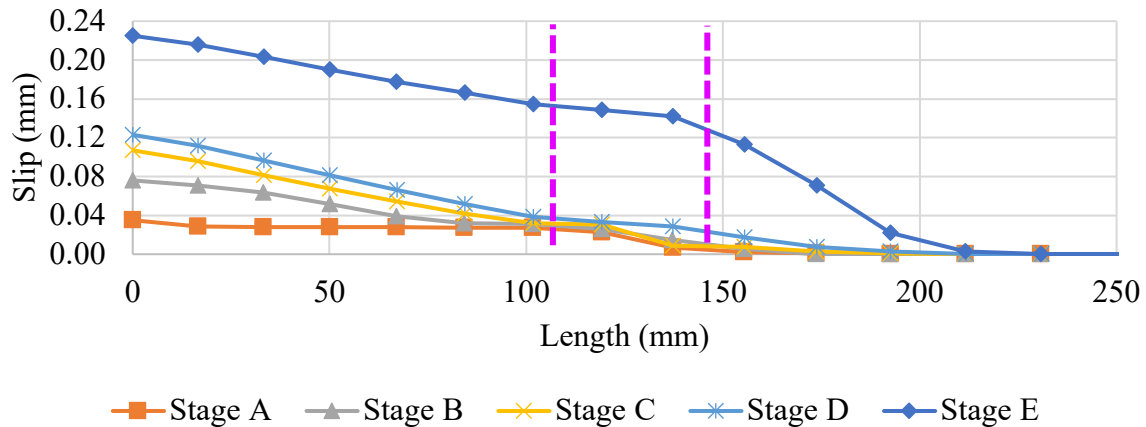


Fig. 4-41. Slip distribution across different load stages for Specimen HM-2.

4.4.4 Dual Defect Specimens

4.4.4.1 Specimen VC-VC-2

Specimen VC-VC-2 featured two equally sized (19 mm x 50 mm) vertical defects placed along the center of the bond area. The shear stress and slip distributions along the centerline at different load stages are shown in Fig. 4-42 and Fig. 4-43, respectively.

The shear stress distribution progressively increases as the load is applied, with stress peaks observed near the end of the load end defect region and then start to progressively decrease. A second, smaller peak appears at the end of the second defect's vicinity (located at the middle of the bond area), after which the stress drops to near zero values at the free end of the laminate. The maximum shear stress, 7.2 MPa, occurs at the end of the load-end defect region (around $y = 40$ mm) during load stage B ($P = 9.1$ kN).

As with the other specimens, the slip values at the load end increase with the applied load. The slip distribution starts with a maximum value at the load end ($y = 0$ mm) and gradually

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decreases, reaching near-zero values at the free end of the laminate. An ultimate slip of 0.22 mm is achieved at load stage E ($P = 13.9$ kN).

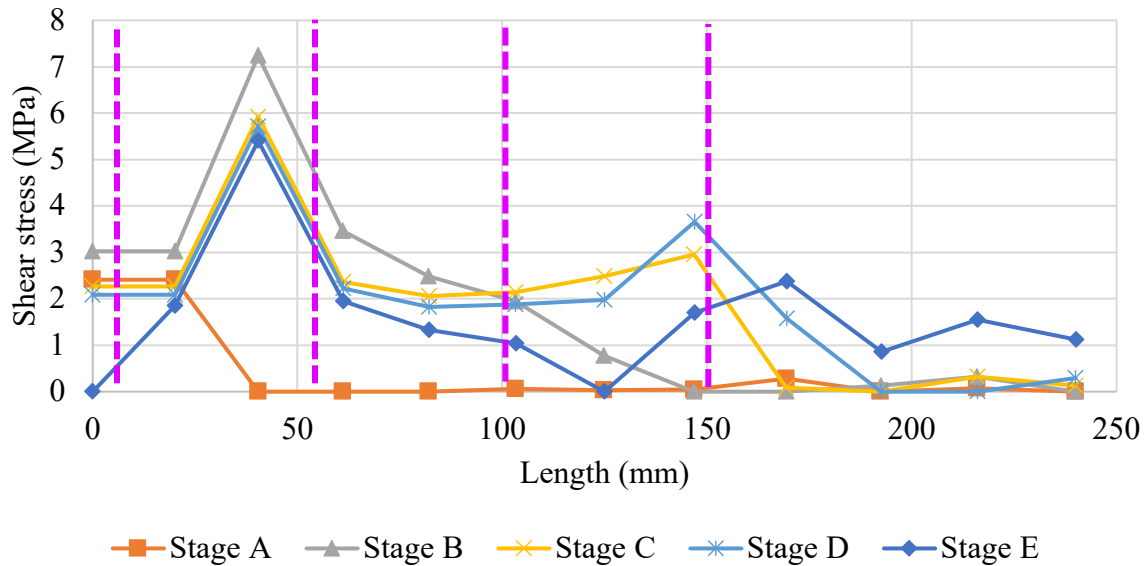


Fig. 4-42. Shear stress distribution across different load stages for Specimen VC-VC-2.

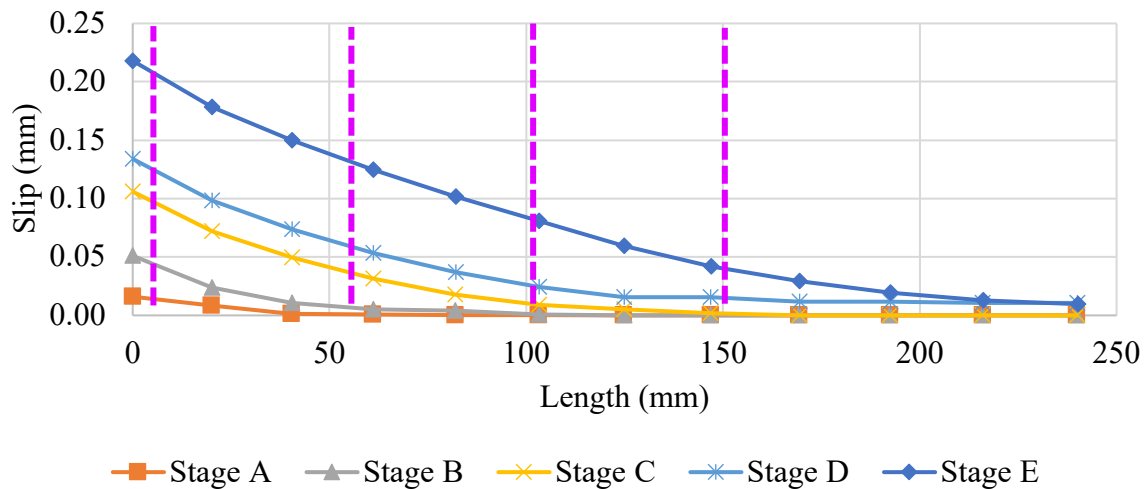


Fig. 4-43. Slip distribution across different load stages for Specimen VC-VC-2.

4.4.4.2 Specimen VR-VR-2

Specimen VR-VR-2 features two equally sized vertical defects placed at the load end and the middle of the bond area, respectively. Unlike Specimen VC-VC-2, the defects in this specimen are placed along the right edge (RE) of the bond area rather than along its centreline. The shear stress distributions along the RE are shown in Fig. 4-44, while those along the left edge (LE) are presented in Fig. 4-45.

The overall shear distribution is similar along both edges in the vicinity of the load-end defect, where stress concentrations are observed. For the first four load stages, shear stresses are slightly more pronounced along the RE near the load-end defect, with stress concentrations occurring just below the defect vicinity. In contrast, higher stresses are observed along the RE in the middle defect region, with a second peak appearing just below this vicinity during load stages D ($P = 7.8$ kN) and E ($P = 8.1$ kN). On the LE, smaller second peaks are observed parallel to the defect vicinity, averaging 2.4 MPa at load stage D. Overall, higher shear stress distributions are observed along the RE where the defects are placed, and stresses decrease more gradually along the RE below the middle defect region, reaching the free end of the laminate. On the LE, the shear stresses decrease more rapidly, reaching near zero values at about $y = 192$ mm.

The slip distributions for both the RE and LE are shown in Fig. 4-46 and Fig. 4-47, respectively. Notably, slip values are higher along the RE, and they decrease more steeply. On the LE, a slip plateau is observed up to the second defect vicinity (at about $y = 100$ mm) for load stages D (7.8 kN) and E (8.1 kN). After this point, the slip distribution decreases in a concave manner. The ultimate slip for this specimen was reached during the last load stage E,

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with values of 0.14 mm on the RE and 0.07 mm on the LE (the ultimate slip on the RE is double that on the LE).

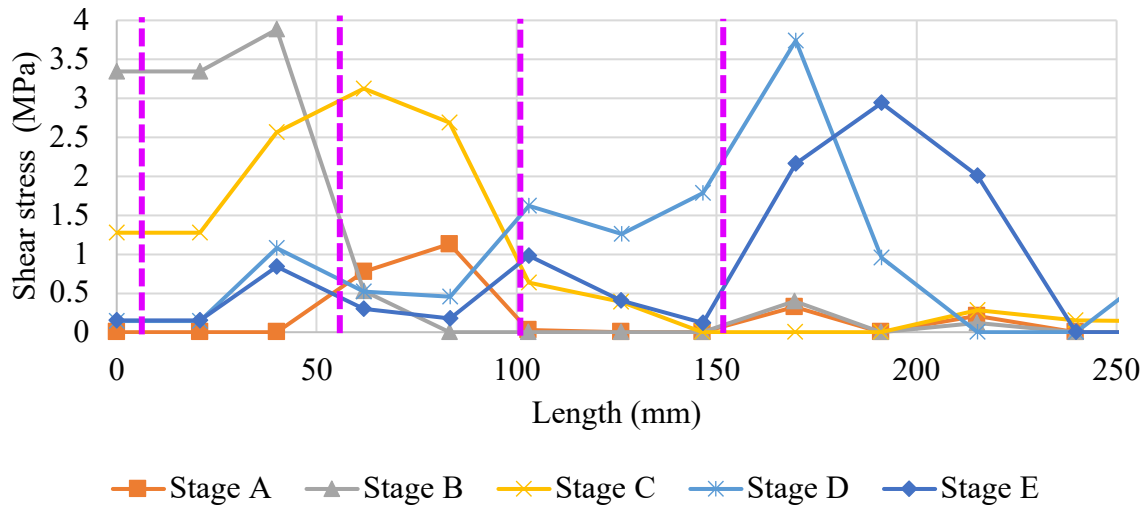


Fig. 4-44. Shear stress distribution across different load stages along the right edge section line (RE) for Specimen VR-VR-2.

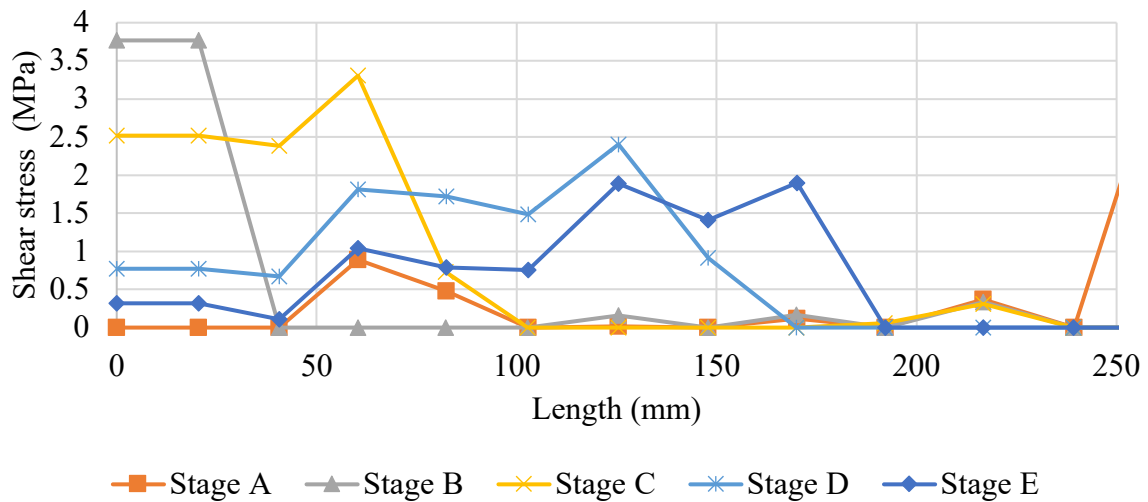


Fig. 4-45. Shear stress distribution across different load stages along the left edge section line (LE) for Specimen VR-VR-2.

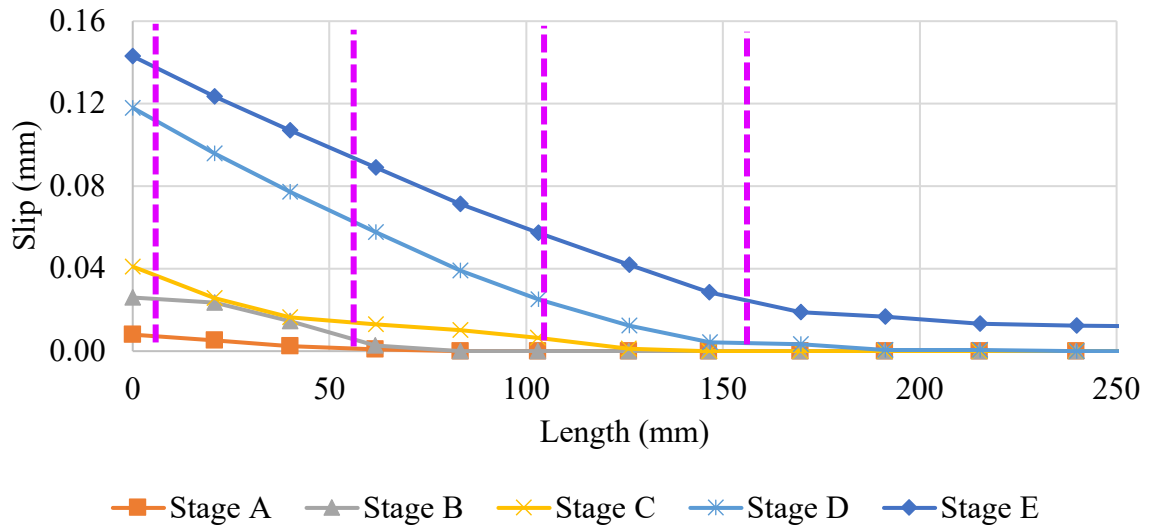


Fig. 4-46. Slip distribution across different load stages along the right edge section line (RE) for Specimen VR-VR-2.

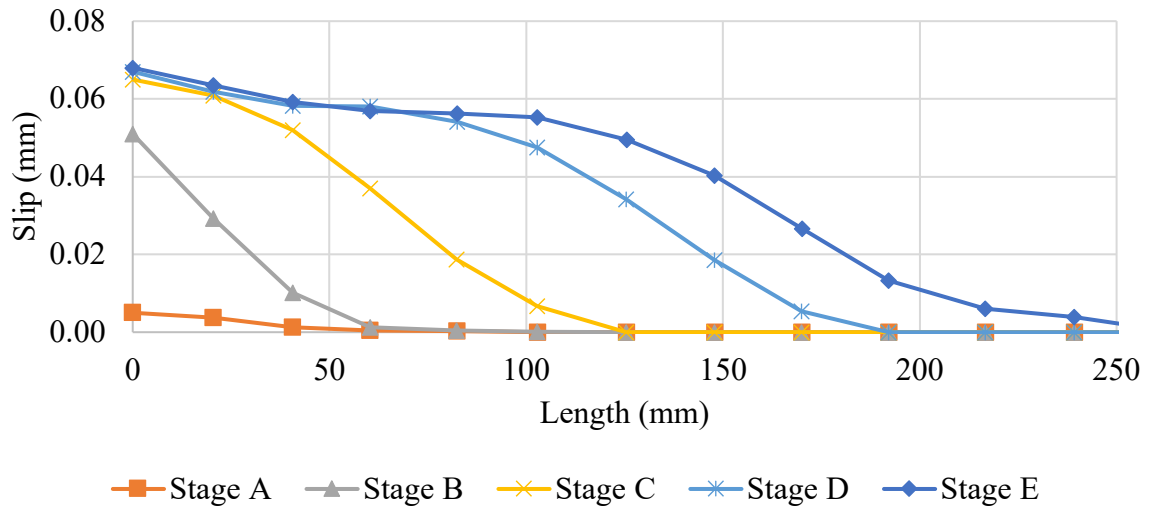


Fig. 4-47. Slip distribution across different load stages along the left edge section line (LE) for Specimen VR-VR-2.

4.4.4.3 Specimen VR-VL-2

Similar to specimen VR-VR-2, shear and slip values for specimen VR-VL-2 were analyzed along two section lines: the right edge (RE) and left edge (LE) of the laminates.

Bond shear stress values along the RE (Fig. 4-48) were lower than those along the LE (Fig. 4-49), with maximum values occurring at stage B ($P = 5.9$ kN) at $y = 0$: 4.78 MPa for the RE and 12.74 MPa for the LE. Despite differences in magnitude, the bond shear distributions display similar behaviour near the load-end defect.

At the start of the bond length ($y = 0$ mm), initial shear stress values are zero at stage A ($P = 1.6$ kN). Shear stress values peak at stage B and progressively decline with increasing load (stages C–E, $P = 8.1$ – 12.9 kN).

Within the defect region, shear stress values initially remain constant before beginning to drop. Along the RE, this drop is relative, with bond stresses recovering near the end of the defect region and forming a second peak due to stress concentrations. In contrast, along the LE, shear values decrease steadily across the defect region, forming a smaller second peak before reaching the middle-placed defect at approximately $y = 100$ mm.

Near the middle defect, shear stress values decrease for all load stages. Along the RE, the decline occurs at a slower rate compared to the LE, where shear values drop to zero near the defect vicinity for stages D and E (where the loads are 10.1 and 12.9 kN, respectively). For the LE, shear values reach zero within the defect region at stages A ($P = 1.6$ kN), C ($P = 8.1$ kN), and D ($P = 10.1$ kN). For stage E ($P = 12.9$ kN), stresses drop to zero just beyond the defect

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region (approximately $y = 160$ mm). At stage B, shear stress values for both distributions reach zero before the second defect region.

Slip distributions for load stages A–E are presented in Figs. 4-50 and 4-51 for the RE and LE, respectively. Maximum slip values occur at $y = 0$ for all load stages and increase with higher loads. At the ultimate failure load (beyond stage E, $P = 13.2$ kN), the ultimate slip values were 0.13 mm and 0.14 mm at the start of the RE and LE section lines, respectively.

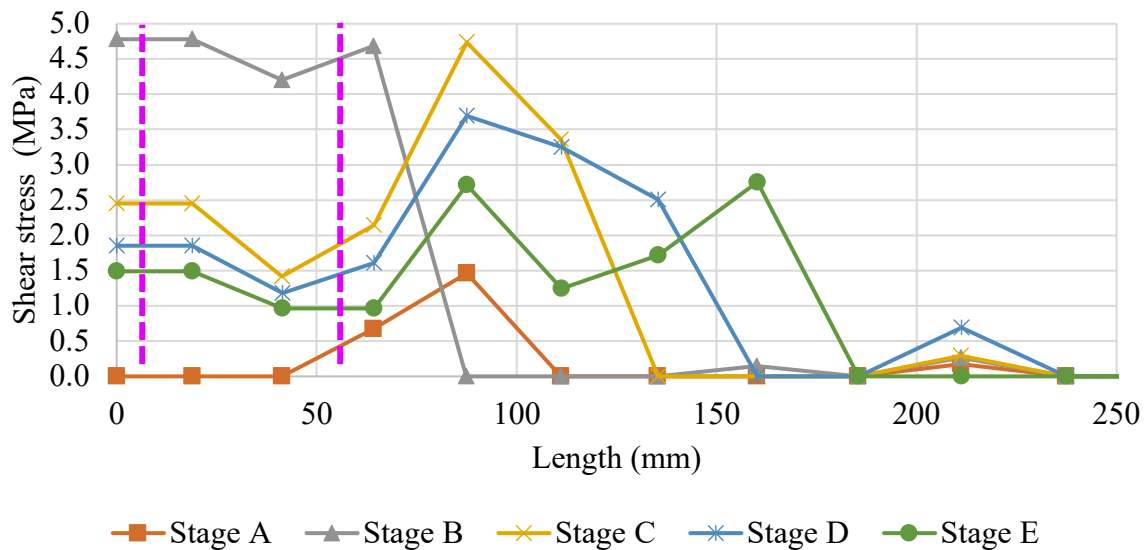


Fig. 4-48. Shear stress distribution across different load stages along the right edge section line (RE) for Specimen VR-VL-2.

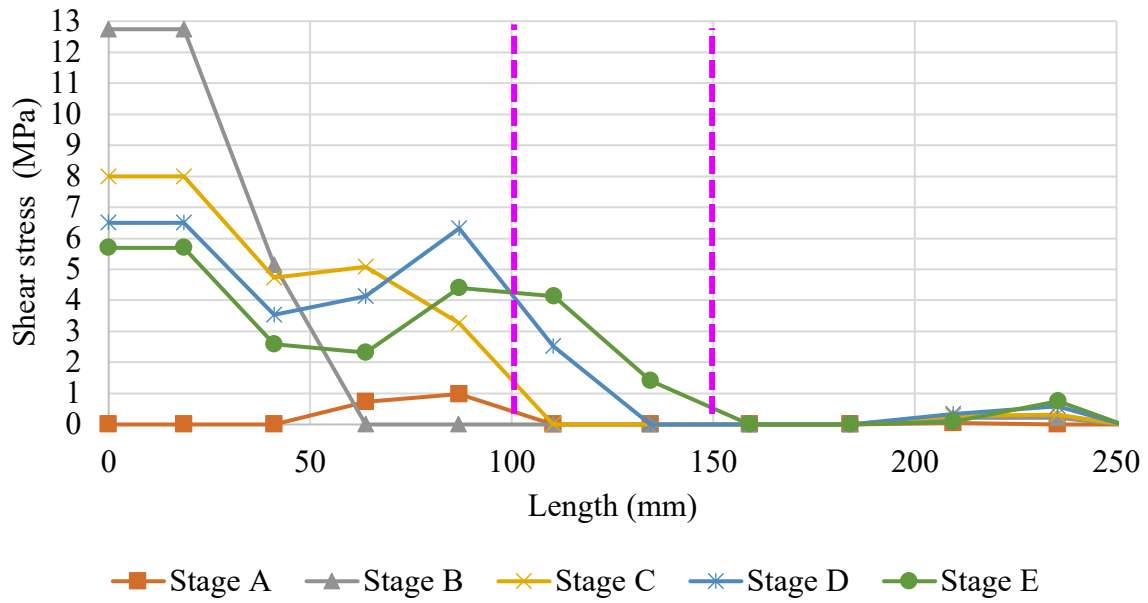


Fig. 4-49. Shear stress distribution across different load stages along the left edge section line (LE) for Specimen VR-VL-2.

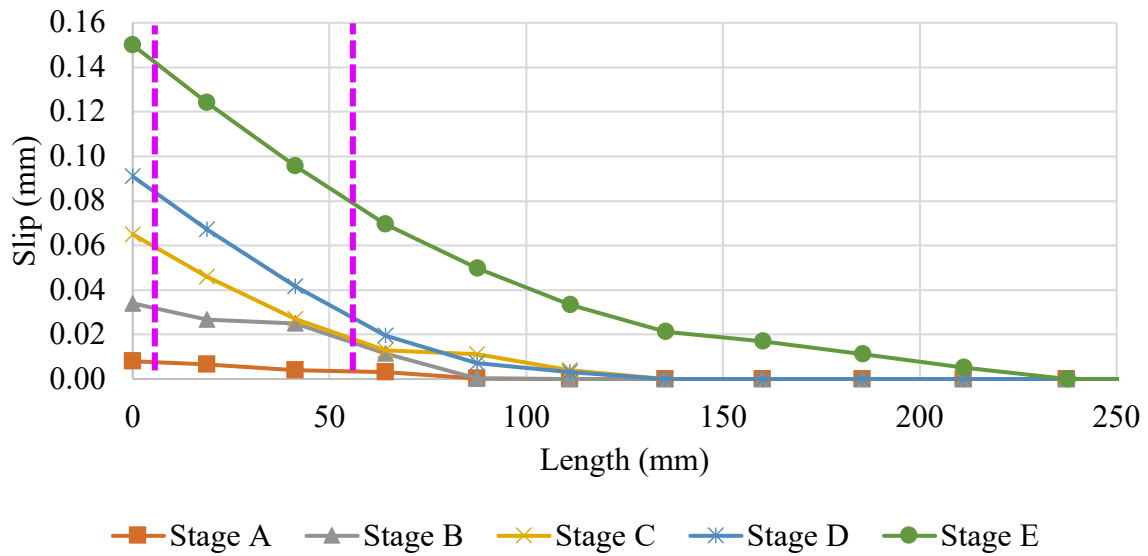


Fig. 4-50. Slip distribution across different load stages along the right edge section line (RE) for Specimen VR-VL-2.

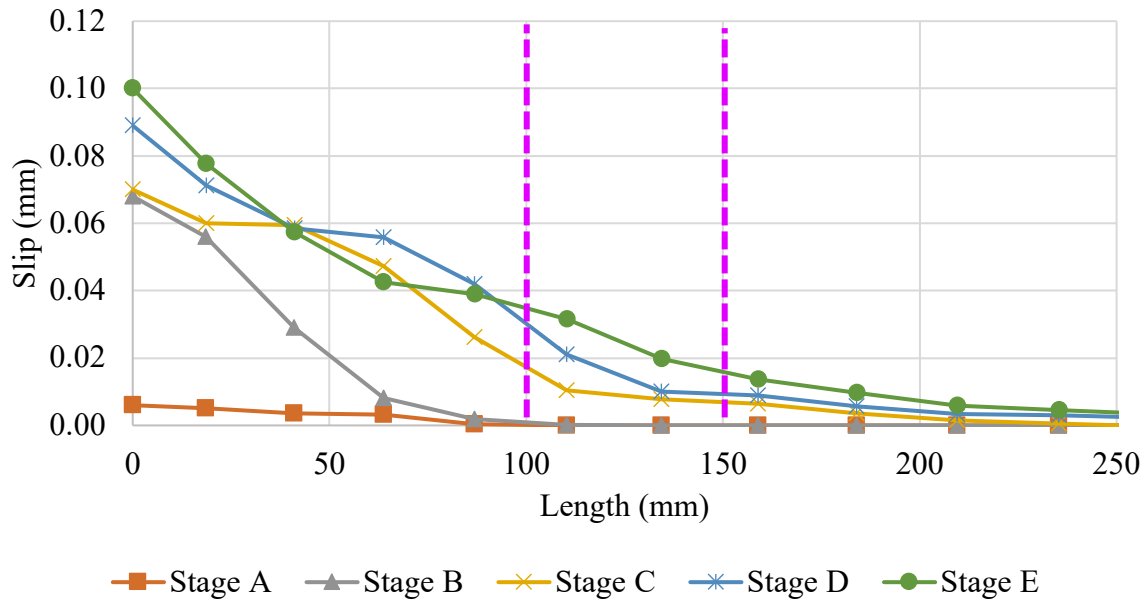


Fig. 4-51. Slip distribution across different load stages along the left edge section line (LE) for Specimen VR-VL-2.

4.5. Load Slip Curves

In this subsection, load-slip curves are presented for the control specimens, single-defect specimens, and dual-defect specimens. The load-slip curves were constructed using data points from seven load stages for each specimen.

4.5.1 Control Specimens

For both control specimens, the load-slip curves, presented in Fig. 4-52, illustrate a three-stage behaviour. Initially, there is an elastic region characterized by a linear increase in load with small values of slip. This is followed by a second stage where the load continues to increase at a reduced rate. Finally, a plateau stage is reached, marking the attainment of the ultimate capacity.

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The load-slip behaviour of specimen C-2, strengthened with two plies of CFRP, demonstrates greater stiffness in both the elastic and secondary stages compared to specimen C-1, which features only a single ply. Specimen C-2 achieved an ultimate load of 13.1 kN at an ultimate slip of 0.19 mm. In contrast, specimen C-1 attained a slightly lower ultimate load of 12.7 kN but exhibited a higher ultimate slip of 0.39 mm.

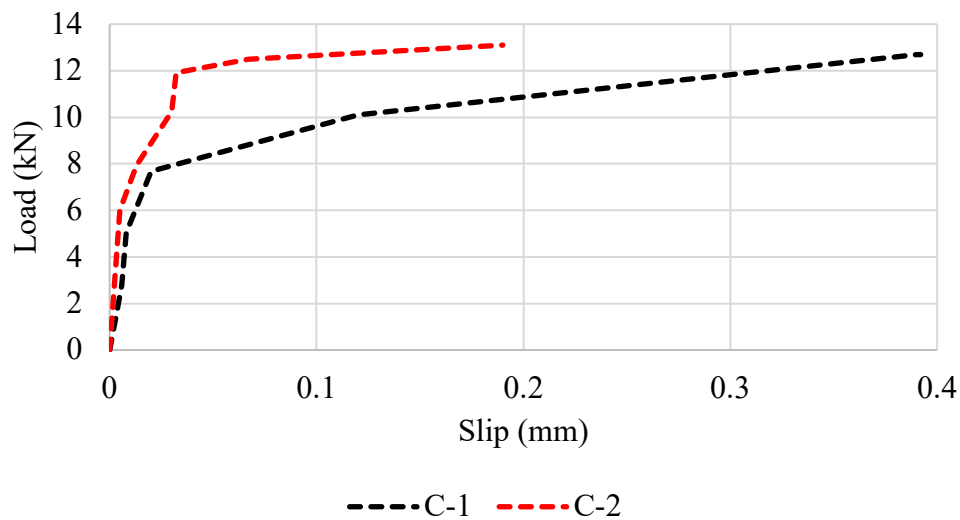


Fig. 4-52. Comparison of load-slip curves for Specimens C-1 and C-2.

4.5.2 Single Defect Specimens

The load-slip curves of the specimens with single defects are presented in Fig. 4-53, along with the curves of specimens C-1 and C-2 for comparison. Overall, all curves exhibit behaviour consistent with the control specimens, featuring two distinct phases of load increase, the second occurring at a reduced rate.

Specimens VL-2 and HL-2 share similarities in the position of the defect at the load end of the bond area and the number of CFRP plies. However, VL-2 features a vertically oriented defect

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covering only part of the bond width, whereas HL-2 has a horizontally oriented defect spanning the entire bond width. Both specimens achieved comparable ultimate load and slip values: HL-2 reached an ultimate load of 11.5 kN at a slip of 0.17 mm, while VL-2 achieved an ultimate load of 11.7 kN at a slip of 0.18 mm. Despite their similarities, HL-2 demonstrated higher stiffness during the first phase of load increase but lower stiffness during the second phase compared to VL-2. Additionally, the loads for HL-2 increased at lower slip values. Both specimens exhibited lower ultimate loads and failed at lower slip values compared to C-2, reflecting the influence of the defects on bond behaviour and performance.

Specimens VM-2, VM-1, and HM-2 all feature a middle-placed defect, with VM-2 and VM-1 differing in the number of CFRP plies. This variation in axial stiffness allowed VM-2 to reach its peak load faster than VM-1, demonstrating higher stiffness during the initial load stages. Consequently, VM-2 achieved a higher ultimate load (10.2 kN) compared to VM-1 (6.7 kN), though VM-1 attained a greater ultimate slip (0.23 mm versus 0.11 mm for VM-2). Both specimens exhibited lower ultimate loads than those of C-2 (13.1 kN) and C-1 (12.7 kN). However, VM-2 failed at a lower slip (0.11 mm) compared to C-2 (0.19 mm), while VM-1 (0.23 mm) exhibited a lower ultimate slip compared to C-1 (0.39 mm).

Both VM-2 and VM-1 exhibited a distinct load-slip response, reaching their ultimate capacity before experiencing a load drop, followed by a recovery to their ultimate capacity, and eventually failing. However, in VM-2, the load drop and subsequent recovery occurred at a lower slip value compared to VM-1.

In contrast, HM-2 demonstrated a different load-slip behaviour compared to VM-2 and VM-1. It exhibited lower stiffness during the first stage, followed by a shorter second stage with

minimal slip progression. Subsequently, the load plateaued until failure. Unlike VM-2 and VM-1, HM-2 did not display the characteristic load drop and recovery, further distinguishing its behaviour.

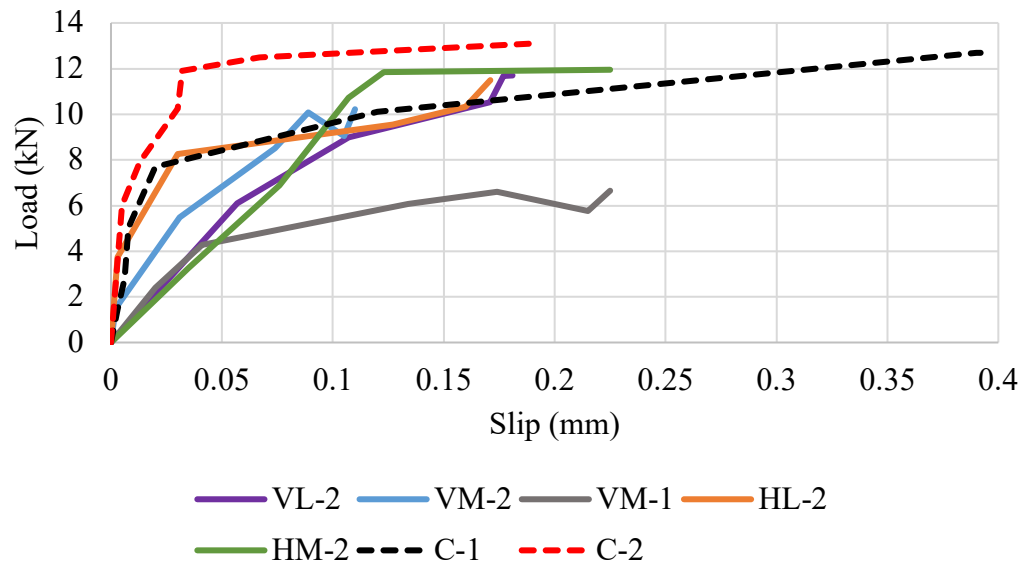


Fig. 4-53. Load-slip curves comparison for control specimens (C-1, C-2) and single defect specimens (VL-2, VM-2, VM-1, HL-2, HM-2).

4.5.3 Dual Defect Specimens

The load-slip curves for dual-defect specimens (VC-VC-2, VR-VR-2, and VR-VL-2) and the two-sheet control specimen (C-2) are presented in Fig. 4-54.

Specimen VC-VC-2 was analyzed using a single line distribution along the center of the bond area. In contrast, two section lines—on the right edge (RE) and left edge (LE)—were used for slip distribution in VR-VR-2 and VR-VL-2. For these two specimens, the average (avg.) slip values from the RE and LE were considered for comparison.

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The control specimen (C-2) exhibited the highest initial stiffness among all specimens. The dual-defect specimens showed an overall behaviour similar to C-2, characterized by an initial elastic stage, a second stage with reduced stiffness, and a plateau where load increased gradually with higher slip.

Specimen VR-VR-2, with defects on the right edge of the bond area, displayed a higher initial stiffness than VC-VC-2 and VR-VL-2. However, the specimen failed at the lowest load (8.1 kN) and exhibited the lowest ultimate slip (0.11 mm). Its load-slip curve shows a load drop to 7.65 kN at the end of the second stage, followed by a recovery with increased slip until failure. This behaviour emphasizes the critical impact of defect placement along the edge of the bond area.

Specimen VR-VL-2, with one defect at the load end on the right edge and another in the middle on the left edge, exhibited the lowest initial stiffness. It failed at a load of 13.2 kN, slightly higher than the control specimen (13.1 kN) but lower than VC-VC-2 (13.9 kN). Its ultimate slip (0.14 mm) was also lower than those of C-2 (0.19 mm) and VC-VC-2 (0.22 mm).

Specimen VC-VC-2 showed behaviour similar to VR-VR-2, with a slower initial load progression, reaching 8.5 kN before dropping to 7.9 kN at 0.03 mm of slip. Afterward, the load increased, surpassing the ultimate load of C-2 (13.1 kN), and the specimen failed at 13.9 kN with an ultimate slip of 0.22 mm.

Overall, specimens VC-VC-2 and VR-VL-2 demonstrated behaviour consistent with a combination of single load-end and middle-placed defects, characterized by slower initial progression and a load drop and recovery near the plateau region. In contrast, VR-VR-2

exhibited a significant reduction in load capacity, stiffness, and slip compared to the control specimen, highlighting the critical influence of defect positioning.

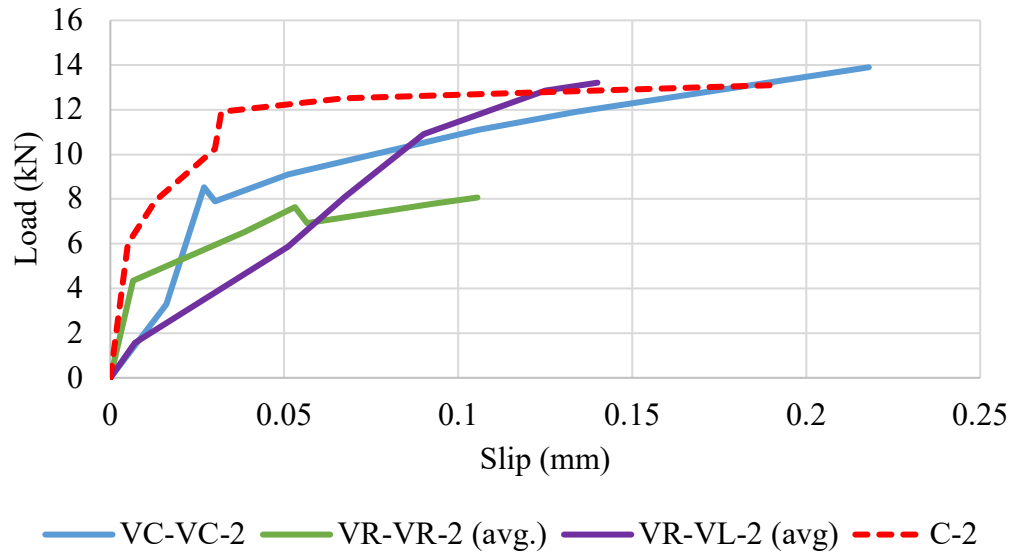


Fig. 4-54. Load-slip curves comparison for control specimen (C-2) and dual defect specimens (VC-VC-2, VR-VR-2, and VR-VL-2).

4.6. Experimental Bond Slip Curves of Control Specimens

The bond shear stress and slip values across different load stages were presented in subsection 4.4 using a 20 mm length interval along the bond length. By plotting the bond stress as a function of slip at each position along the bond length, multiple bond-slip curves were generated for each specimen. Subsequently, the bond shear stress values corresponding to different slip values were averaged, and the minimum and maximum values were identified to define the range of bond behaviour over the length of the bonded interface.

In this subsection, the experimental bond-slip relationship is analyzed for both control specimens (C-1 and C-2), which serve as benchmarks for the bond-slip behaviour of the

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specimens with defects. The average curve will serve as a reference for validating analytical models in subsection 4.8.

4.6.1 Specimen C-1

The experimental bond-slip curves at various locations along the bond length as well as the envelope curve for specimen C-1 are presented in Fig. 4-55.

To construct these curves, five load stages were selected across the test. Shear stress and slip values were distributed along the bond length for each load stage, allowing the progression of these distributions to be examined. The data was then reorganized by combining shear and slip values for each 20 mm increment (e.g., $y = 0, 20, 40, \dots, 240$ mm) across all load stages, enabling the plotting of bond-slip curves at different locations. A unified slip axis was created, and shear stress values were interpolated for each slip value to calculate average, maximum, and minimum bond-slip curves, which are depicted in Fig. 4-56.

The curves capture key aspects of the bond-slip behaviour: the peak stresses (6.6 MPa) were reached at a local slip of 0.02 mm. Although the failure progressed quickly, it was characterized by a significant slip of 0.39 mm at the loaded end at the ultimate stage. The bond-slip behaviour varied along the bond length, with regions ($y = 0, 20, 40, 140, 220$ and 240 mm) showing zero or very low bond stress for the entire test, consistent with the shear distribution findings in subsection 4.4.2.1. (Note that shear values were set to zero in regions where the calculated values were found to be negative.) To maintain clarity, Fig. 4-56 only presents curves for locations with appreciable shear stresses, where slips mostly exceeded 0.02 mm. However, a more detailed analysis of the omitted curves is provided in subsection 5.5.1.

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To calculate the average bond slip curve, only non-zero values were used as a means to neglect the inactive (zero-shear) regions. The minimum stress values were zero for all slip levels, hence the lower bound of the bond stress envelope coincides with the horizontal axis. While a fully zero-stress response across the entire bond line is theoretically implausible under load, this minimum envelope curve illustrates the observed behaviour in some localized regions found to have a negligible contribution to the overall response. In practice, minor localized stresses inevitably develop throughout the bond line during progressive loading, ultimately contributing to laminate deformation and interface peeling observed at failure.

The limited number of data points used to construct each curve partially explains the sudden transition from small slip (<0.03 mm) corresponding to peak stress levels to higher slip values (>0.1 mm). For example, at $y = 80$ mm, the slip increases sharply from a small value (0.01 mm at 5.4 MPa) to 0.2 mm for a shear value of 3.3 MPa. Consequently, while the curves provide valuable insights into the general bond-slip behaviour, the data does not conclusively capture the complete behaviour at some locations, particularly at the loaded and free ends of the bond area.

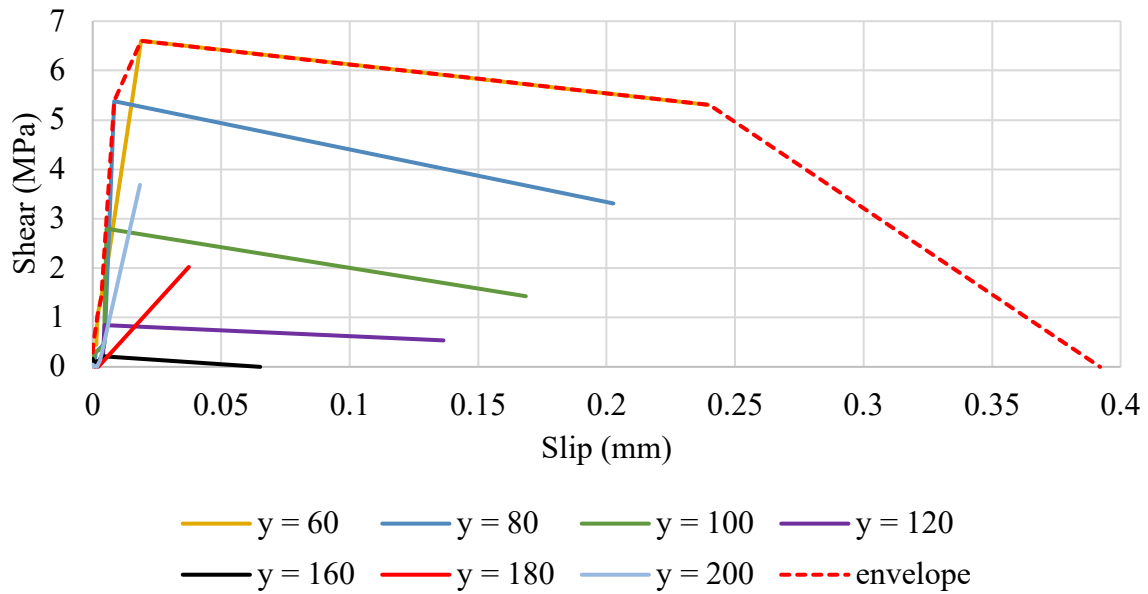


Fig. 4-55. Experimental bond-slip curves at various distances from the loaded end for Specimen C-1 where significant shear and slip was observed.

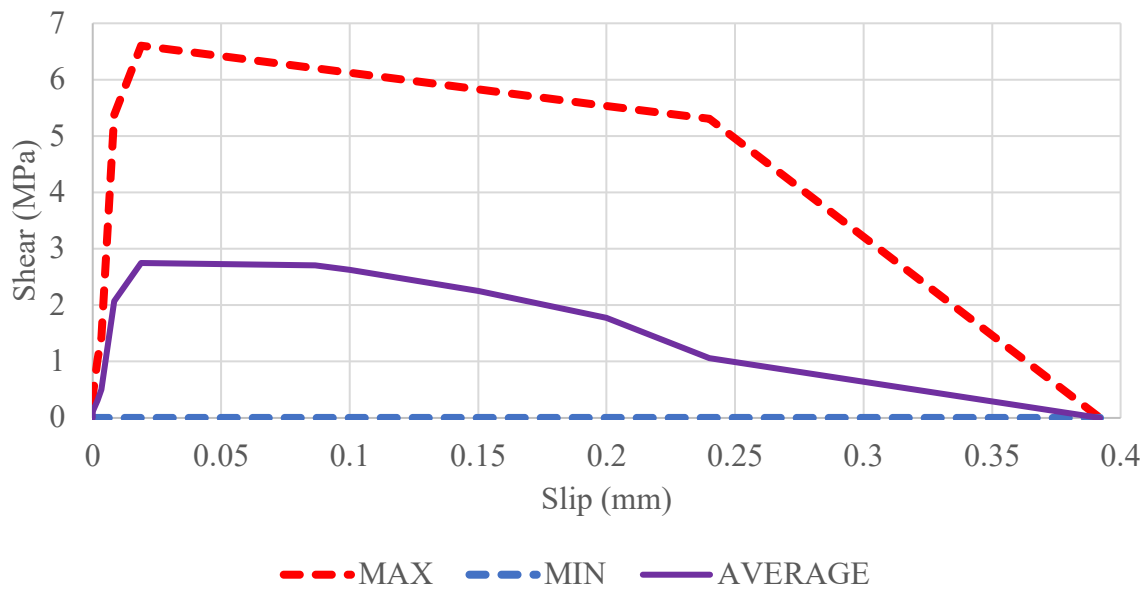


Fig. 4-56. Experimental bond-slip average, envelope, and minimum curves for Specimen C-1.

4.6.2 Specimen C-2

Similar to specimen C-1, the construction of experimental bond-slip curves for specimen C-2 involved combining shear stress and slip values at each bond location, using a 20 mm distance increment across all selected load stages. The resulting curves at various distances from the loaded end, along with an envelope curve, are presented in Fig. 4-57.

Unlike specimen C-1, most locations along the bond length exhibited consistent behaviour, characterized by an initial elastic stage, followed by a decrease in shear stress values with increasing slip. The peak shear stress reached 5.1 MPa at a slip value of 0.005 mm. The ultimate slip, measuring 0.19 mm, occurred at the top of the bonded region ($y = 0$) coinciding with the specimen's failure. Subsequently, interpolation of shear values along a unified slip axis allowed for the computation of average and minimal bond stresses at each slip value. The envelope, minimum, and average bond-slip curves are illustrated in Fig. 4-58.

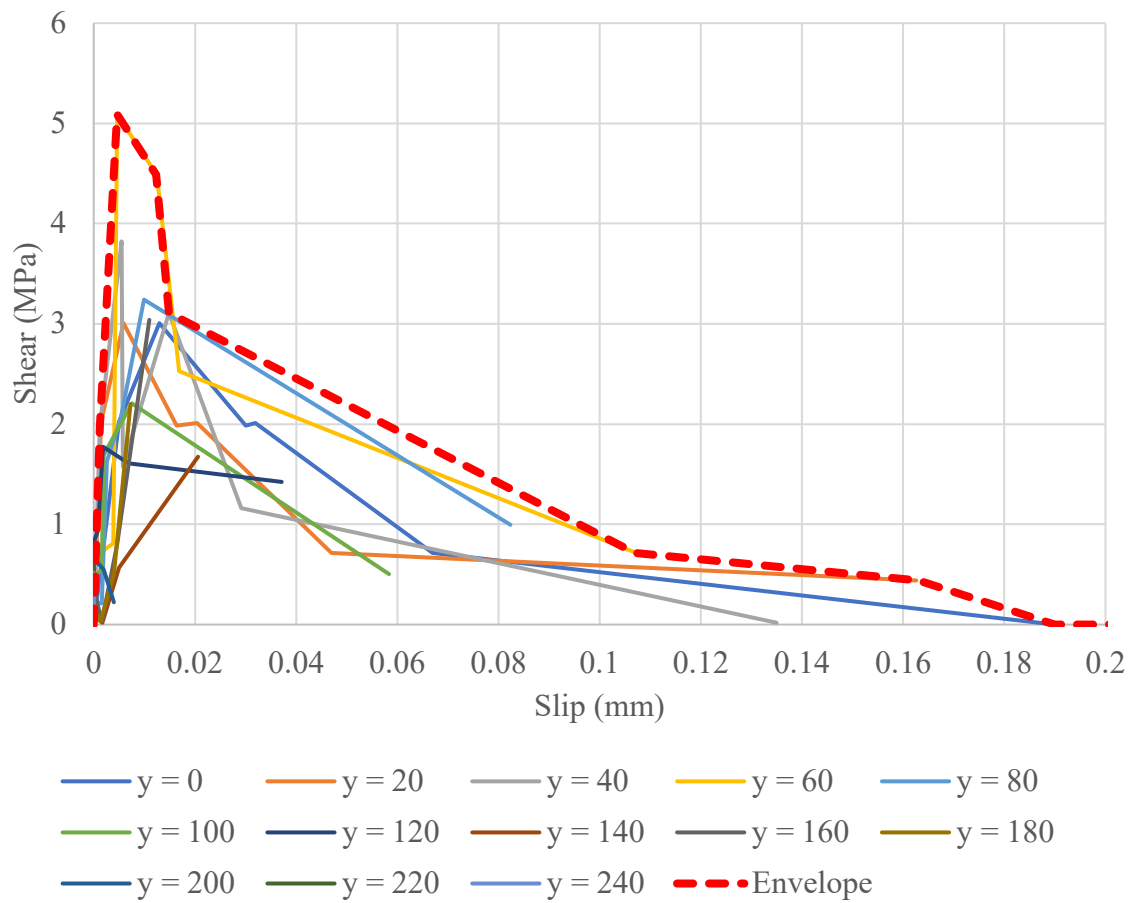


Fig. 4-57. Experimental bond-slip curves at various distances from the loaded end for Specimen C-2.

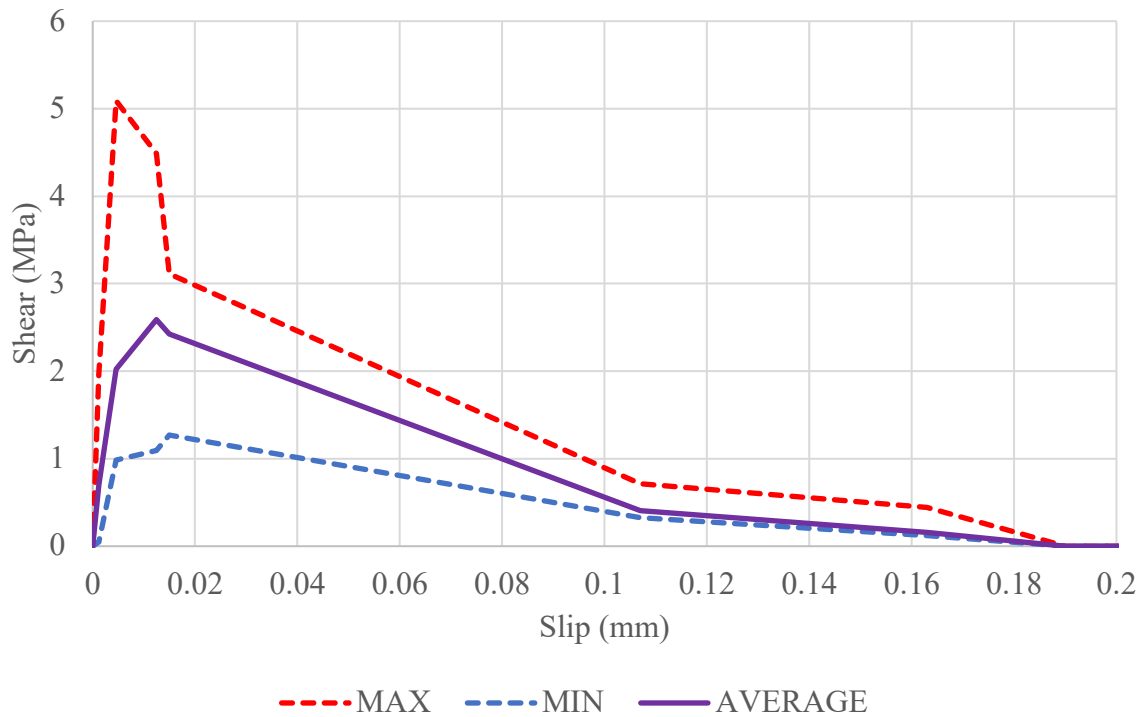


Fig. 4-58. Experimental bond-slip average, envelope, and minimum curves for Specimen C-2.

4.7. Analytical Bond Slip Model Adaptation for Control Specimens

4.7.1 Specimen C-1

Ten models from the literature (Section 2.4) were selected to predict the bond-slip response in this study. These models, labeled A to J, represent a diverse range of existing approaches, including Popovic curves, exponential models, bilinear models, a linear ascending curve, and a simplified model by Lu et al. (2005). Table 4.3 summarizes their peak shear stress and corresponding slip for specimen C-1 material properties, while Fig. 4-59 provides a graphical representation.

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Despite variations in shape, the models predicted peak shear stresses between 3.4 MPa and 8.1 MPa, with corresponding slip values ranging from 0.01 mm to 0.26 mm. Most models (A, B, I, H, and J) considered only concrete strength as a primary variable. In contrast, three bilinear models (E, F, and G) incorporated adhesive properties (thickness and stiffness) alongside concrete capacity but omitted the influence of FRP axial rigidity. Only the exponential models (C and D) accounted for all three factors: concrete strength, adhesive properties, and FRP stiffness (ply thickness and elastic modulus).

Table 4-3. Summary of selected bond-slip models and their predicted peak shear stress and slip for specimen C-1.

	Model	Type	$\tau_{\max}$ (MPa)	s_m (mm)
A	Nakaba et al (2001)	Popovic	7.6	0.07
B	Savioa et al. (2003)	Popovic	7.6	0.05
C	Dai et al. (2005)	Exponential	6.0	0.09
D	Dai and Ueda (2003)	Exponential	8.1	0.12
E	Brosens (2001)	Bilinear	3.4	0.01
F	Brosens and Van Gemert (1999)	Bilinear	6.4	0.03
G	Monti et al. (2003)	Bilinear	6.4	0.03
H	Lu et al. (2005) bilinear	Bilinear	4.0	0.05
I	Neubauer and Rostasy (1997)	Linear ascending	5.0	0.26
J	Lu et al. (2005) simplified	Other	4.0	0.05

The experimental average bond-slip model for specimen C-1 demonstrated a peak bond stress of 2.8 MPa at a corresponding slip of 0.02 mm and an ultimate slip of 0.39 mm. Conversely, specimen C-2 indicated an average peak bond stress of 2.6 MPa at a corresponding slip of 0.01 mm and an ultimate slip of 0.19 mm.

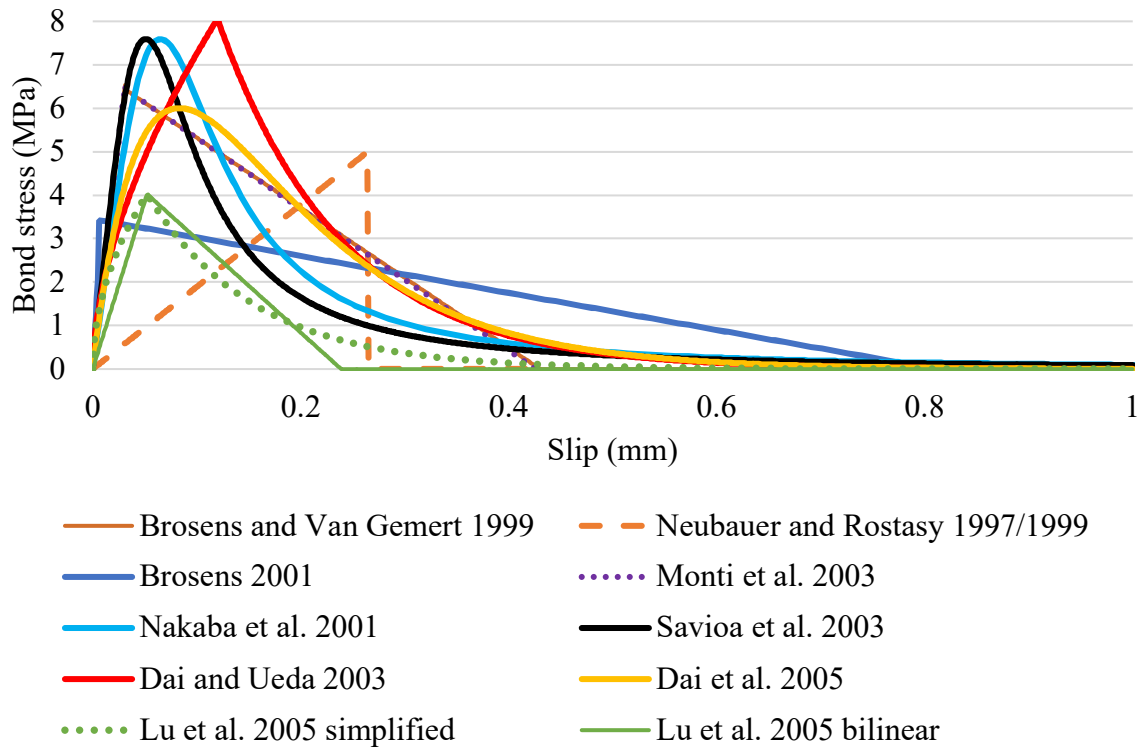


Fig. 4-59. Bond-slip curves from 10 literature models using experimental material properties for Specimen C-1.

Among the models, Lu et al. (2005)'s bilinear model, commonly adopted for its simplicity, was qualitatively found to capture the overall average specimen behaviour observed in this study. This model features two distinct stages: an initial elastic stage, with a peak stress of 3.9 MPa at a slip of 0.06 mm, followed by a descending stage, culminating in an ultimate slip of 0.24 mm. Given these characteristics, this model was selected for further calibration in this study. Since the chosen model did not incorporate the FRP stiffness and thickness, the FRP properties are not considered for the calibration.

The experimental average, maximum, and minimum bond-slip curves for specimen C-1, along with the original (pre-calibration) Lu et al. (2005) bilinear model, are presented in Fig. 4-60.

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The maximum and minimum curves are included for visual reference only, as the average experimental bond-slip curve will exclusively be used for validating the adapted and calibrated analytical model in this subsection.

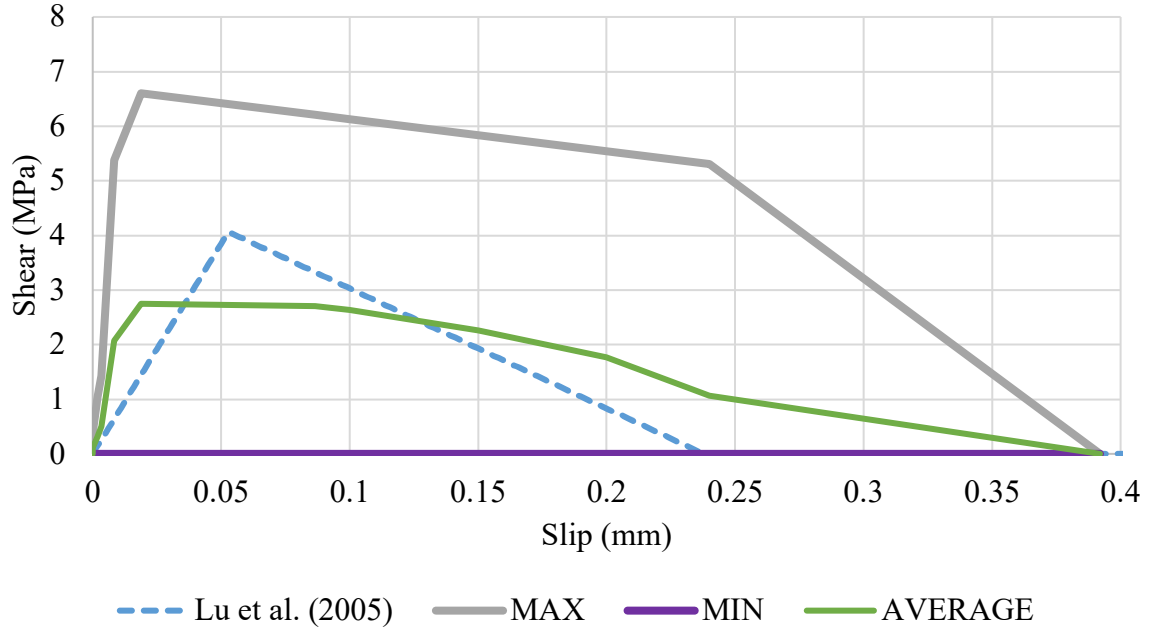


Fig. 4-60. Bond-slip curves: Lu et al. (2005) bilinear model, experimental average, envelope, and minimum curves for Specimen C-1.

The mathematical formulation of Lu et al. (2005)'s model is provided in Eqs. 4-1 to 4-6.

$$\tau(s) = \begin{cases} \tau_m \left(\frac{s}{s_m} \right); & 0 < s \leq s_m \\ \tau_m \left(\frac{s - s_f}{s_m - s_f} \right); & s_m < s \leq s_f \end{cases} \quad \text{Eq. 4-1}$$

$$\tau_m = \alpha_1 \beta_w f_t \quad \text{Eq. 4-2}$$

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$$s_m = \alpha_2 \beta_w f_t \quad \text{Eq. 4-3}$$

$$s_u = \frac{2G_f}{\tau_m} \quad \text{Eq. 4-4}$$

$$\beta_w = \sqrt{\frac{2.25 - \frac{b_f}{b_c}}{1.25 + \frac{b_f}{b_c}}} \quad \text{Eq. 4-5}$$

$$G_f = \alpha_3 \beta_w^2 \sqrt{f_t} \quad \text{Eq. 4-6}$$

As proposed by Lu et al. (2005), three coefficients (α_1 , α_2 , α_3) define the bond slip relationship and are initially given as $\alpha_1 = 1.5$, $\alpha_2 = 0.0195$, and $\alpha_3 = 0.308$, as shown in Eqs. 4-1, 4-2, and 4-3, respectively. Altering these coefficients directly influences the predicted bond stress at each slip value.

To calibrate the model, four specific errors were evaluated for the predicted peak stress, the corresponding slip, the ultimate slip, and the interfacial fracture energy. The three coefficients were then optimized using Excel's solver function to minimize the total error, defined as the sum of these individual error components.

The calibration process resulted in new coefficient values: $\alpha_1 = 1.15$, $\alpha_2 = 0.007$, and $\alpha_3 = 0.39$. Following calibration, the error was reduced to 14% for peak stress, 0% for the corresponding slip, 0% for the ultimate slip, and 0% for the interfacial energy. The adapted analytical model, alongside the experimental bond-slip curve, is presented in Fig. 4-61.

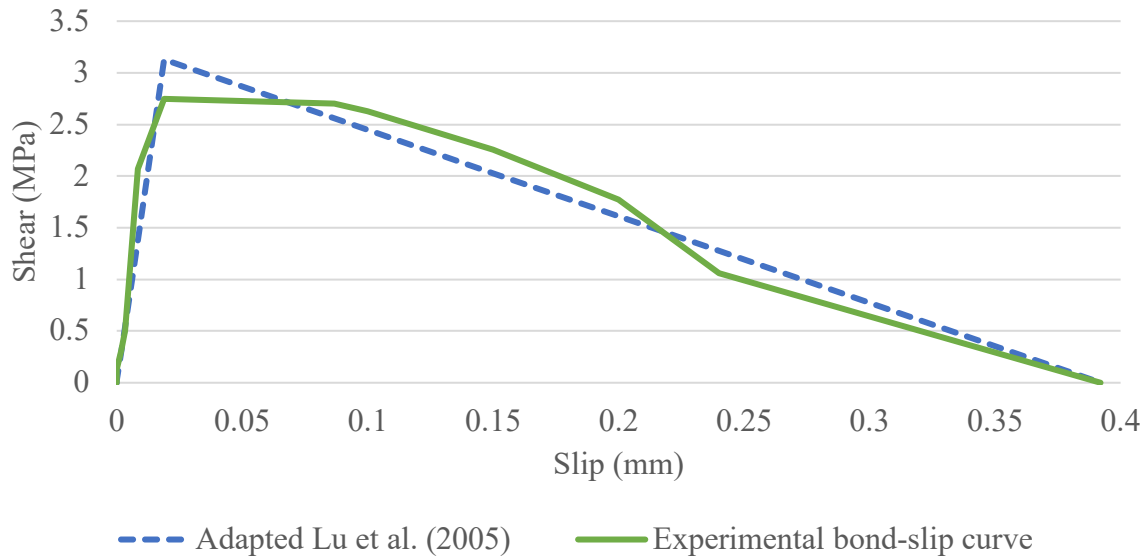


Fig. 4-61. Lu et al. (2005) bilinear adapted model and experimental average bond-slip curve for Specimen C-1.

4.7.2 Specimen C-2

Specimen C-2 incorporated two layers of CFRP, increasing stiffness and thickness compared to C-1, while the concrete strength and adhesive properties remained unchanged. The ten models (graphically presented in Fig. 4-62) produced similar outputs, with some predicting slightly higher peak bond stresses. As with specimen C-1, the ten selected models, labeled A to J, and their analytical outputs (predicted peak shear stress and corresponding slip) are summarized in Table 4-4, alongside those of specimen C-1 for comparison.

The predicted peak bond stresses ranged from 3.4 MPa to 9.1 MPa, with corresponding slip values between 0.01 mm and 0.26 mm. In contrast, experimental data for specimen C-2 indicated an average peak bond stress of 2.6 MPa at a corresponding slip of 0.01 mm, with an ultimate slip of 0.19 mm. Overall, the predicted peak stresses were similar to those of specimen

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C-1, with higher peak shear values in the exponential models C and D. The highest prediction came from Dai and Ueda (2003), which estimated a peak stress of 9.1 MPa for specimen C-2 compared to 8.1 MPa for C-1. These two models were the only ones that incorporated FRP properties, leading to greater predicted bond capacity due to the increased FRP thickness (double-ply vs. single-ply). For all other models, where only concrete and adhesive properties were considered, the predicted shear performance remained unchanged.

Table 4-4. Summary of selected Bond-Slip Models and their predicted peak shear stress and slip for specimens C-2 and C-1.

	Model	Type	C-2		C-1	
			$\tau_{\max}$ (MPa)	s_m (mm)	$\tau_{\max}$ (MPa)	s_m (mm)
A	Nakaba et al (2001)	Popovic	7.6	0.07	7.6	0.07
B	Savioa et al. (2003)	Popovic	7.6	0.05	7.6	0.05
C	Dai et al. (2005)	Exponential	6.6	0.08	6.0	0.09
D	Dai and Ueda (2003)	Exponential	9.1	0.11	8.1	0.12
E	Brosens (2001)	Bilinear	3.4	0.01	3.4	0.01
F	Brosens and Van Gemert (1999)	Bilinear	6.4	0.03	6.4	0.03
G	Monti et al. (2003)	Bilinear	6.4	0.03	6.4	0.03
H	Lu et al. (2005) bilinear	Bilinear	4.0	0.05	4.0	0.05
I	Neubauer and Rostasy (1997)	Linear ascending	5.0	0.26	5.0	0.26
J	Lu et al. (2005) simplified	Other	4.0	0.05	4.0	0.05

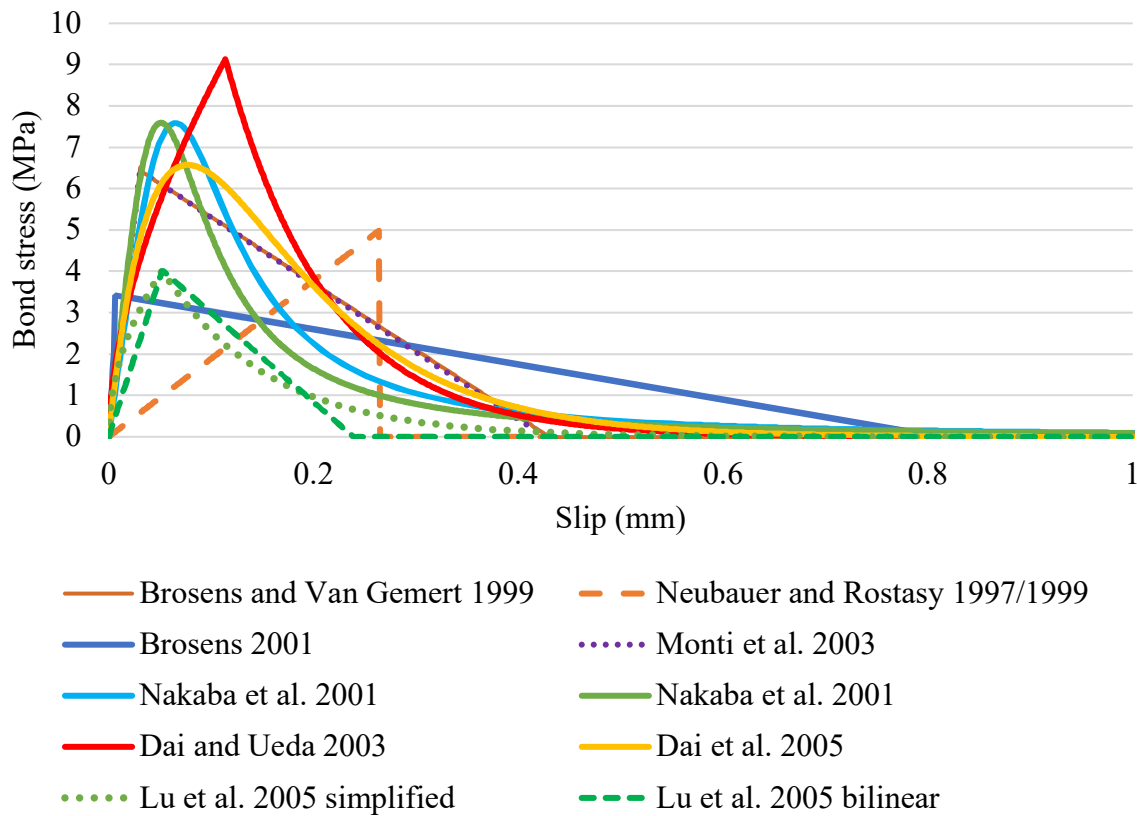


Fig. 4-62. Bond-slip curves from 10 literature models using experimental material properties for Specimen C-2.

Similar to Specimen C-1, the experimental bond-slip curve for Specimen C-2 closely aligns, in a qualitative sense, with the bilinear model proposed by Lu et al. (2005). According to the bilinear model, the predicted values for peak bond stress were 3.9 MPa at a slip of 0.06 mm, with an ultimate slip of 0.24 mm. These values are identical to those for specimen C-1 as the model's parameters are solely based on the concrete's strength, as outlined in Eqs. 4-2, 4-3, and 4-6, and do not account for variations in FRP stiffness.

The original (non-adapted) Lu et al. (2005) bilinear model, along with the experimental envelope, average, and minimum curves, is depicted in Fig. 4-63.

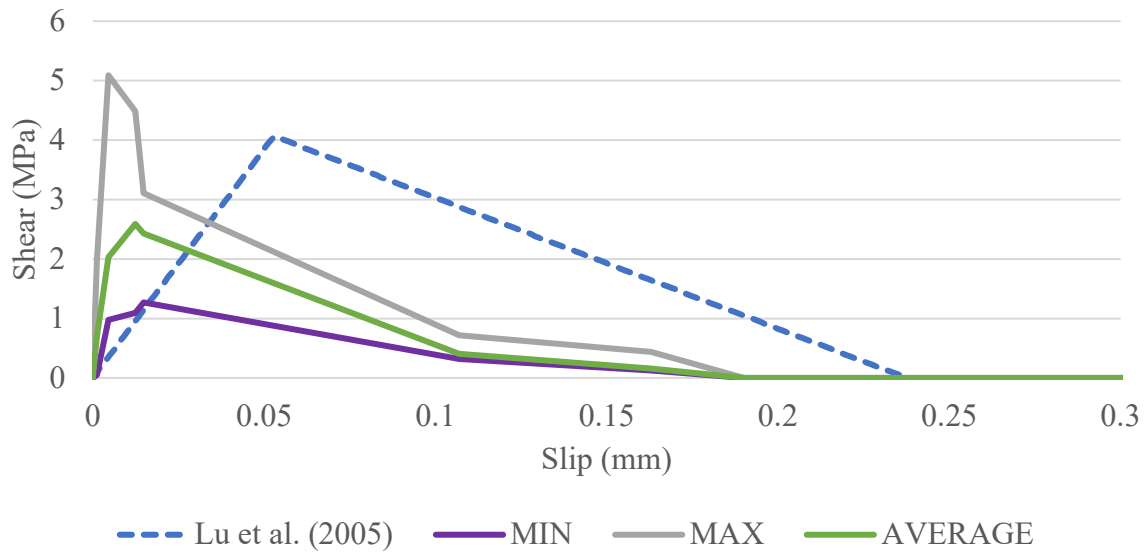


Fig. 4-63. Bond-slip curves: Lu et al. (2005) bilinear model, experimental average, envelope, and minimum curves for Specimen C-2.

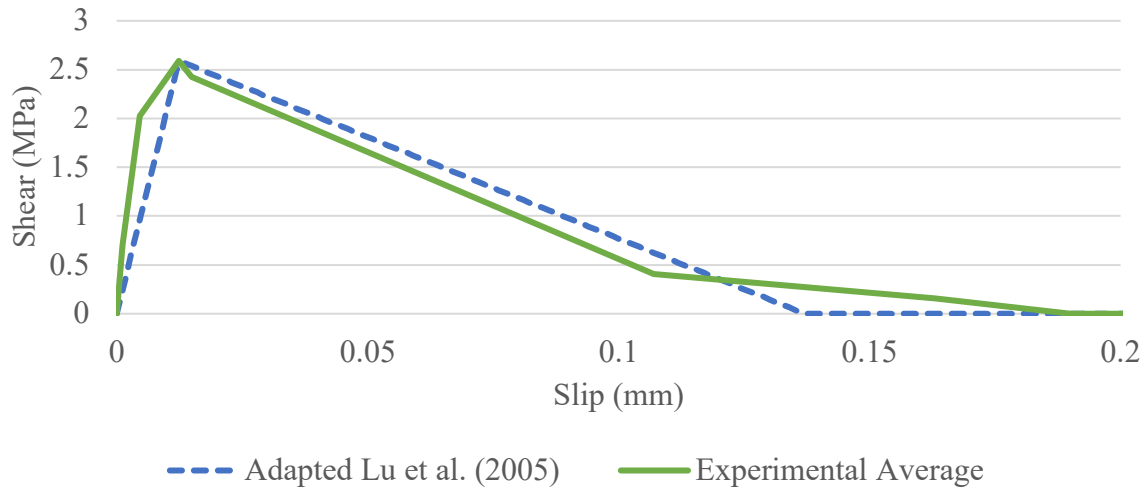


Fig. 4-64. Lu et al. (2005) adapted bilinear model and experimental average bond-slip curves for Specimen C-2

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Similar to specimen C-1, the calibration of the analytical model for specimen C-2 omitted to consider the FRP axial rigidity following the model's approach and resulted in new coefficient values: $\alpha_1 = 0.96$, $\alpha_2 = 0.005$, and $\alpha_3 = 0.11$. Following calibration, the error was found to be 0% for peak stress, 0% for the corresponding slip, 28% for the ultimate slip, and 0% for the interfacial energy. Both the adapted analytical model and the experimental bond-slip curve are presented in Fig. 4-64.

4.7.3 Unified Model

In this subsection, a unified bond-slip model is introduced. After generating adapted models for specimens C-1 and C-2 individually and determining their respective calibration coefficients, the coefficients (α_1 , α_2 , and α_3) were averaged to develop a new unified adapted model. This unified model aims to represent the average bond-slip behaviour of both specimens. Since eight of the ten analytical models did not consider the FRP properties in their formulae and considering the smallness of plies difference (nominal difference of 1 mm between a single-ply and a double-ply configuration), the thickness and stiffness of the FRP sheets are not considered for the unified models for simplicity purposes (Nakaba et al. 2001; Monti et al. 2003; Lu et al. 2005).

The comparison of model outputs and calibration coefficients for the original Lu et al. (2005) bilinear model, the adapted models for specimens C-1 and C-2, their experimental average models, and the unified model is presented in Table 4-4. The calibration coefficients for the unified model are $\alpha_1 = 1.06$, $\alpha_2 = 0.006$, and $\alpha_3 = 0.25$.

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Fig. 4-65 illustrates the experimental average bond-slip curves for specimens C-1 and C-2 alongside the unified model's curve, highlighting its capacity to approximately capture the average behaviour of both specimens with a single set of parameters.

The unified model lies between the two experimental average bond-slip curves. It exhibits a higher ultimate slip (0.27 mm) and interfacial energy (0.39 N/mm) compared to specimen C-2 (0.19 mm and 0.18 N/mm), while having less ultimate slip and less interfacial energy compared to specimen C-1 (0.39 mm and 0.61 N/mm). The model's peak shear value of 2.9 MPa closely aligns with the peak shear stresses of specimens C-1 and C-2, which are 2.7 MPa and 2.6 MPa, respectively.

Table 4-5. Comparison of model outputs and calibration coefficients for Lu et al. (2005) original and adapted bilinear models, experimental average models of specimens C-1 and C-2, and unified model.

	τ_m (mm)	s_m (mm)	s_u (mm)	G_f (N/mm)	α_1	α_2	α_3
Original	4.1	0.05	0.24	0.483	1.5	0.020	0.31
C-1	2.7	0.02	0.39	0.613	-	-	-
Adapted C-1	3.1	0.02	0.39	0.613	1.15	0.007	0.39
C-2	2.6	0.01	0.19	0.177	-	-	-
Adapted C-2	2.6	0.01	0.14	0.177	0.96	0.005	0.11
Unified Model	2.9	0.02	0.27	0.393	1.06	0.006	0.25

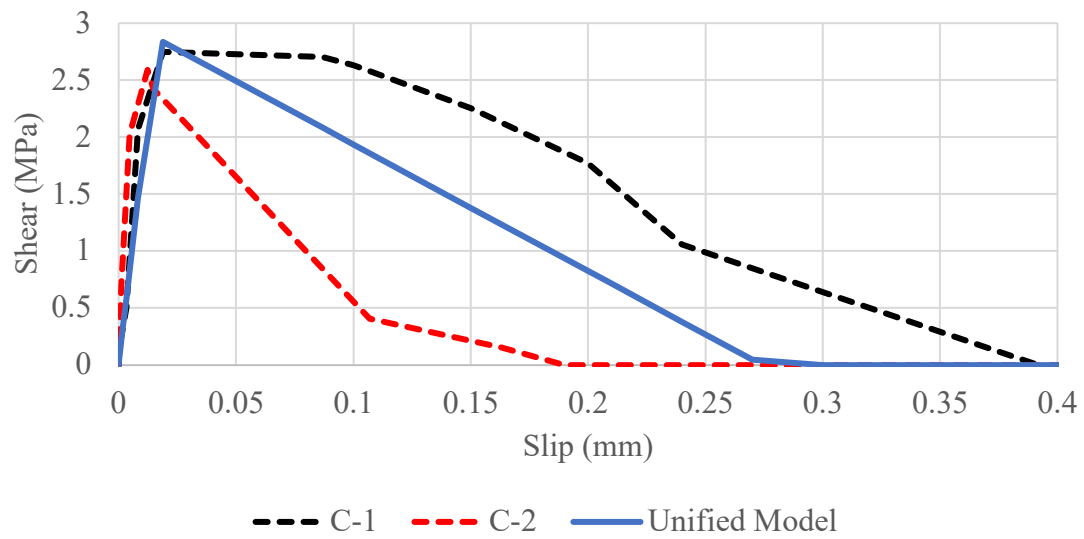


Fig. 4-65. Comparison of experimental average bond-slip curves for specimens C-1 and C-2 with the unified adapted bond-slip model.

CHAPTER 5. DISCUSSION OF RESULTS

5.1. Introduction

Externally bonded CFRP sheets are widely recognized as effective repair materials for strengthening deficient concrete structures. Their high stiffness-to-weight ratio and exceptional tensile strength make them particularly attractive alternatives to conventional materials such as steel plates. However, the effectiveness of CFRP strengthening systems is entirely dependent on the quality of the bond between the composite material and the concrete substrate. Limited research has been conducted to evaluate the integrity of this bond in the presence of interfacial defects and their implications for the service life of rehabilitated structures.

The primary objective of this study was to investigate the bond performance between CFRP sheets and high-strength concrete substrates in a series of ten prism specimens containing interfacial defects in various configurations. Destructive shear lap testing was performed using a universal testing machine (UTM) in conjunction with a 3D DIC system for data acquisition and strain analysis.

The experimental program provided valuable insights into the mechanical properties of the materials and the progressive bond behaviour of the specimens. Strain contour maps, strain distributions, and bond shear-slip profiles were generated along the bond length of the laminates. Load-slip curves were derived independently from both UTM and DIC outputs, and experimental bond-slip models were developed for control specimens. Furthermore, an analytical investigation reviewed existing models from the literature, selecting one for calibration and validation against the experimental results.

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Despite the comprehensive approach, certain limitations and discrepancies were encountered during the study. These aspects, along with their potential impact on the findings, will be explored in the following subsections, alongside a detailed discussion of key observations, behaviours, and insights derived from the experimental and analytical results.

5.2. Failure Mechanisms

All specimens exhibited a mixed-mode failure, with adhesive cohesive failure being the dominant mode. Thin localized concrete peeling along the laminates was observed and classified into four categories:

- For most specimens, the concrete peeling was thin and uniformly adhered to the laminate area.
- Some specimens exhibited more concentrated thin peeling in the bottom regions of the sheets (e.g., specimens VL-2, VM-2, and HM-2).
- Specimen C-1 displayed a significant chunk of peeling at the loaded end of the bond area, suggesting the presence of stress concentrations, as well as a thin detached layer observed in the bottom end of the laminate.
- Specimen VR-VR-2 showed most concrete peeling concentrated on the left edge of the sheet, opposite to where the defects were placed.

The ultimate loads observed in the experimental study were significantly lower than the theoretically expected values. One widely cited load prediction model, proposed by Wu and Jiang (2013), is described by Eq. 5-1 and Eq. 5-2.

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$$P_u = 0.703\beta_w b_f (E_f t_f)^{0.5} f'_c{}^{0.108} \quad \text{Eq. 5-1}$$

$$\beta_w = 1 + f'_c{}^{0.385} \left(8(E_f t_f)^{-0.438} + 0.001 \right) \frac{\left(1 - \frac{b_f}{b_c} \right)}{\left(1 + b_f^{1.7} \right)} \quad \text{Eq. 5-2}$$

Where: P_u is the ultimate tensile load;

β_w is the bond width factor;

$E_f t_f$ represents the axial stiffness of the laminate;

f'_c is the concrete compressive strength;

b_f and b_c are the width of the FRP laminate and concrete prism, respectively.

Similarly, Chen and Teng (2001) developed another influential model for estimating the load capacity of FRP-strengthened concrete joints, as expressed in Eq. 5-3 through Eq. 5-6, where β_L is a bond length factor, and L_e represents the effective bond length (EBL).

$$P_u = 0.427\beta_w \beta_L \sqrt{f'_c} b_f L_e \quad \text{Eq. 5-3}$$

$$\beta_w = \sqrt{\frac{2 - \frac{b_f}{b_c}}{1 + \frac{b_f}{b_c}}} \quad \text{Eq. 5-4}$$

$$\beta_L = \begin{cases} 1, & \text{if } L_b \geq L_e \\ \sin\left(\frac{\pi L_b}{2L_e}\right), & \text{if } L_b < L_e \end{cases} \quad \text{Eq. 5-5}$$

$$L_e = \sqrt{\frac{E_f t_f}{\sqrt{f'_c}}} \quad \text{Eq. 5-6}$$

Additionally, one prominent ultimate load model (Eq. 5-7) was proposed by Lu et al. (2005) whose bond slip model was used for the analytical work of this study. The interfacial fracture energy (G_f) is taken from Eq. 2-24 from Chapter 2 for the bilinear bond slip model, whereas the bond length factor (β_l) is expressed in Eq. C-2 in Appendix C.

$$P_u = \beta_l b_f \sqrt{2E_f t_f G_f} \quad \text{Eq. 5-7}$$

The theoretical and experimental load values are summarized in Table 5-1. For both control specimens, only a portion of the predicted load was reached (44% to 81% of the theoretical predicted load) suggesting a premature failure.

Table 5-1. Predicted and experimental load values with load proportions for control specimens C-1 & C-2.

Specimen	Model	$P_{u, \text{theoretical}}$ (kN)	$P_{u, \text{experimental}}$ (kN)	Load Portion
C-1	Lu and Wang (2013)	17.5	12.7	73%
	Chen and Teng (2001)	21.1	12.7	60%
	Lu et al. (2005)	15.7	12.7	81%
C-2	Lu and Wang (2013)	24.7	13.2	53%
	Chen and Teng (2001)	29.9	13.2	44%
	Lu et al. (2005)	22.2	13.2	60%

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Experimental ultimate loads and maximum shear values for all specimens are presented in Fig. 5-1. The peak failure loads ranged from 6.7 kN to 13.9 kN, with the lowest loads observed for specimens VM-1 and VR-VR-2 (6.7 kN and 8.9 kN, respectively). This highlights the significant role of CFRP stiffness and the critical impact of imperfections at the bond area edges on load capacity. In contrast, the maximum shear stress values ranged from 3.8 MPa for specimen VR-VR-2 to 17.5 MPa for specimen HM-2.

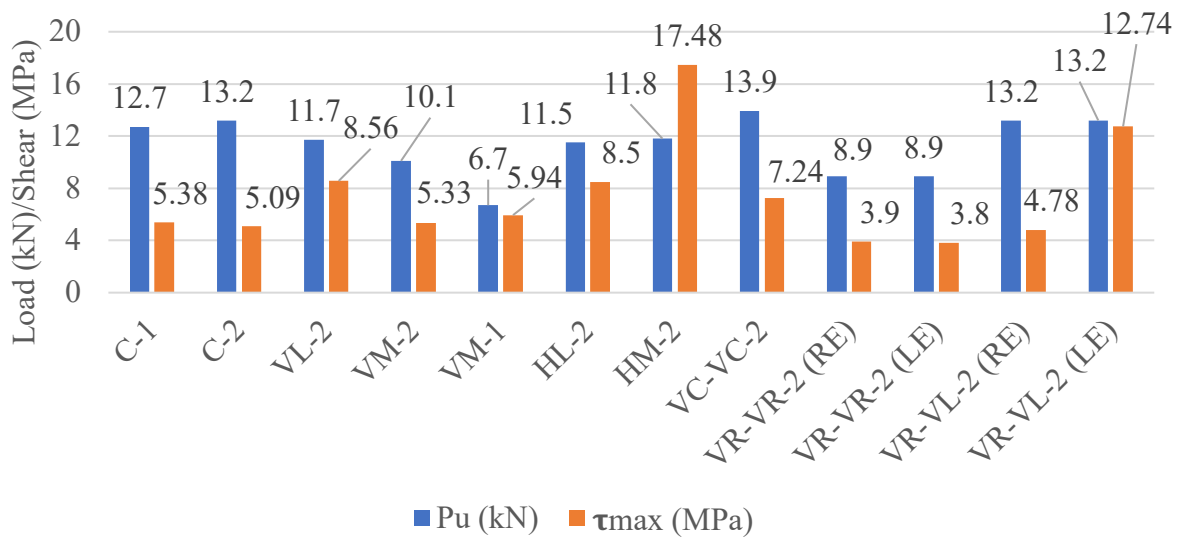


Fig. 5-1. Experimental ultimate loads and maximum shear values for all specimens.

Specimens VR-VR-2 and VR-VL-2, analyzed at two different section lines along the right and left edges of their bond areas, exhibited varying properties. For instance, VR-VR-2 showed minimal shear variation, with a maximum stress of 3.9 MPa along the right edge (RE) and 3.8 MPa along the left edge (LE) at a failure load of 8.9 kN. However, significant strain variations were recorded, with the ultimate strain at the top of the RE (0.11%) being notably higher than that of the LE (0.03%). In contrast, VR-VL-2 exhibited a significant shear variation, with maximum stresses of 4.8 MPa along the RE and 12.7 MPa along the LE, where the stresses on the section opposite the

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load-end defect were four times higher. The strains, however, showed less variation, with values of 0.15% for the RE and 0.17% for the LE. This suggests that a partial defect at the bond area edge may redistribute stress to the opposite edge, leading to stress concentrations and potentially weakening the bond.

The reduced load capacity of VR-VR-2 (8.9 kN) compared to C-2 (13.2 kN) and VR-VL-2 (13.2 kN) further emphasizes the critical influence of defects at the bond area edges. This aligns with the conclusions of Li et al. (2023), which highlight the detrimental effects of offset defects on bond strength and load capacity.

The ultimate strains recorded for the different specimens are shown in Fig. 5-2. The strain values range from 0.03% for VR-VR-2 at its LE to 0.36% for specimen C-1. Specimens C-1 (0.36%) and VM-1 (0.26%) exhibit higher strains than their two-ply counterparts, C-2 (0.14%) and VM-2 (0.10%), even though the latter achieve higher ultimate loads. This could be attributed to the higher axial stiffness of the two-ply fabric, which allows for higher load capacity and less deformability. The lower stiffness in single-ply specimens likely results in less effective stress distribution due to a reduced transverse area, potentially causing localized debonding. This explains the concrete shearing observed at the load-end of the single-ply control specimen C-1 and the lower capacity exhibited by VM-1 (6.7 kN).

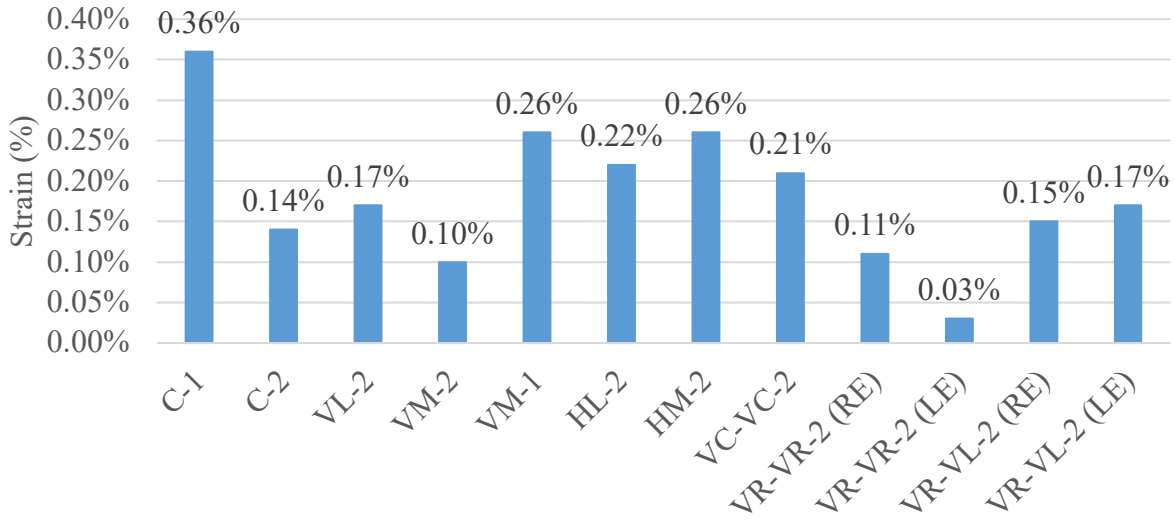


Fig. 5-2. Maximum strains for all specimens.

The different strains shown in Fig. 5-2 represent the maximum strains recorded by the DIC, influenced by inspection conditions and eccentricities. To evaluate deviations from the expected ultimate strains (ϵ_{fu}) – assumed average along the laminate width – corresponding to the peak loads (P_u) obtained from the UTM, Eq. 5-7 can be applied, where $E_f t_f$ represents the FRP axial stiffness and b_f the FRP width.

$$\epsilon_{fu} = \frac{P_u}{E_f t_f b_f} \quad \text{Eq. 5-7}$$

Additionally, various North American and international codes offer design equations to estimate debonding strains for strengthening of sound concrete (i.e., without defects). These equations (Eqs. A-1, A-2, A-3, and A-4) are provided in Appendix A. Table 5-2 presents the calculated ultimate strain as well as the debonding design strain for many codes and standards and highlights their deviations with the maximum strains measured by the DIC.

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Table 5-2. Comparison of experimental maximum strains (DIC analysis), calculated strains from experimental ultimate load, and code-predicted debonding strains (North American, Chinese, Japanese, and Italian standards).

Specimen ID	DIC Reading		Calculated Ultimate		North American Codes		Chinese Codes		Japanese Code		Italian Code	
					ACI 440.2R, CAN/CSA S806 & CAN/CSA S6		GB 50367 & CECS 146		JSCE		CNR DT200	
	P_u (kN)	$\epsilon_{f,max}$ (%)	ϵ_{fu} (%)	%error	ϵ_d (%)	%error	ϵ_d (%)	%error	ϵ_d (%)	%error	ϵ_d (%)	%error
C-1	12.7	0.36%	0.25%	-31%	0.98%	173%	0.23%	-35%	0.31%	-13%	0.57%	59%
C-2	13.2	0.14%	0.13%	-8%	0.70%	396%	0.23%	67%	0.22%	58%	0.41%	189%
VL-2	11.7	0.17%	0.11%	-33%	0.70%	309%	0.23%	37%	0.22%	30%	0.41%	138%
VM-2	10.1	0.10%	0.10%	-1%	0.70%	595%	0.23%	133%	0.22%	121%	0.41%	305%
VM-1	6.7	0.26%	0.13%	-50%	0.98%	278%	0.23%	-10%	0.31%	20%	0.57%	120%
HL-2	11.5	0.22%	0.11%	-49%	0.70%	216%	0.23%	6%	0.22%	0%	0.41%	84%
HM-2	11.8	0.26%	0.12%	-56%	0.70%	167%	0.23%	-10%	0.22%	-15%	0.41%	56%
VC-VC-2	13.9	0.21%	0.14%	-35%	0.70%	231%	0.23%	11%	0.22%	5%	0.41%	93%
VR-VR-2 (RE)	8.9	0.11%	0.09%	-21%	0.70%	532%	0.23%	112%	0.22%	101%	0.41%	268%
VR-VR-2 (LE)	8.9	0.03%	0.09%	190%	0.70%	2217%	0.23%	678%	0.22%	637%	0.41%	1251%
VR-VL-2 (RE)	13.2	0.15%	0.13%	-14%	0.70%	363%	0.23%	56%	0.22%	47%	0.41%	170%
VR-VL-2 (LE)	13.2	0.17%	0.13%	-24%	0.70%	309%	0.23%	37%	0.22%	30%	0.41%	138%
			average:	-11%	average:	482%	average:	90%	average:	85%	average:	239%

*ACI 440.2R & CAN/CSA S806 standards have an upper bound limit on the debonding strain of 0.7%.

** CAN/CSA S6 code has an upper bound limit on the debonding strain of 0.6%.

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The deviations between the calculated ultimate strains and the maximum DIC strains ranged from -56% for specimen HM-2 to 190% for specimen VR-VL-2 on the left edge (LE) section line, with an average error of 11%. Overall, the DIC maximum strains were consistently higher than the predicted average ultimate strains calculated using failure loads. Single-ply CFRP specimens, such as C-1 and VM-1, exhibited larger deviations (-31% and -50%, respectively), likely due to localized stress concentrations, greater deformability, and premature debonding, stemming from reduced stiffness and cross-sectional area compared to two-ply specimens. In contrast, two-ply CFRP specimens, such as C-2 and VM-2, demonstrated smaller deviations (-8% and -1%, respectively), suggesting that increased axial stiffness facilitates more uniform stress transfer and strain development along the bond width, leading to better alignment between experimental and theoretical predicted values. However, specimens HL-2 and HM-2 exhibited errors exceeding -49%, likely due to imperfections in the bond interface or uneven load transfer, highlighting the sensitivity of strain behaviour to interface quality and defect concentration.

For specimens VR-VR-2 and VR-VL-2, significant strain variations were observed between the right edge (RE) and left edge (LE), with specimen VR-VR-2 showing a deviation of 190% on its LE compared to just -20% on the RE. This indicates that bond defects can lead to localized strain variations and increased errors in stress redistribution.

One key factor contributing to the overall discrepancies is the variation in strain across the laminate width, as illustrated in Fig. 5-3. While longitudinal strains are typically highest at the center of the laminates and decrease toward the edges, they are not uniformly distributed (Li et al. 2023). This behaviour contrasts with theoretical calculations that assume a uniform strain across the width.

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Furthermore, DIC strains were measured along only one or two section lines across the laminates, representing just a single or dual points at the load end as the ultimate strain value. This limitation likely accounts for many strain readings exceeding theoretical values.

In contrast, while shear strains were not investigated in this study, they tend to be nearly zero along the centerline but increase near the edges, showing a higher shear gradient at the boundaries adjacent to the concrete regions (Li et al. 2023; Li et al. 2024).

Additionally, Table 5-2 compares experimental ultimate strains with design debonding strains based on various code equations, including North American, Chinese, Japanese, and Italian standards. Overall, while all codes exhibited a positive average error value relative to the experimental DIC strains (i.e. overall higher debonding design strains), the North American codes (ACI 440.2R, CAN/CSA S802, and CAN/CSA S6) and the Italian guidelines (CNR DT200) showed the largest deviations, with average errors of 482% and 239%, respectively. Conversely, the Chinese codes (GB 50367 & CECS 146) showed an average error of 90%, while the Japanese (JSCE) standards demonstrated slightly better accuracy with the experimental data, with an average error of 85%, aligning closely with the CECS 146 predictions.

These errors suggest the observed strains are overall significantly smaller than calculated debonding strains from existing standards, likely due to premature failure during testing. Additional factors, discussed in sections 5.6 and 5.7, such as out-of-plane and mixed-mode behaviour, misalignment, eccentricities, and material properties (e.g., the brittle nature of high-strength concrete), may have further contributed to these discrepancies. While design codes prioritize simplicity and conservatism, no definitive conclusion can be drawn regarding their reliability from this comparison.

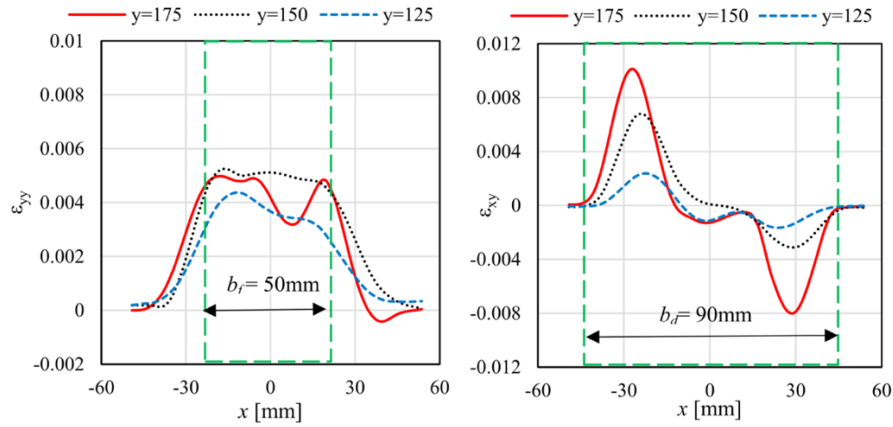


Fig. 5-3. Comparison of longitudinal strain (left) and shear strain (right) distributions across the width at different length locations (Li et al. 2023).

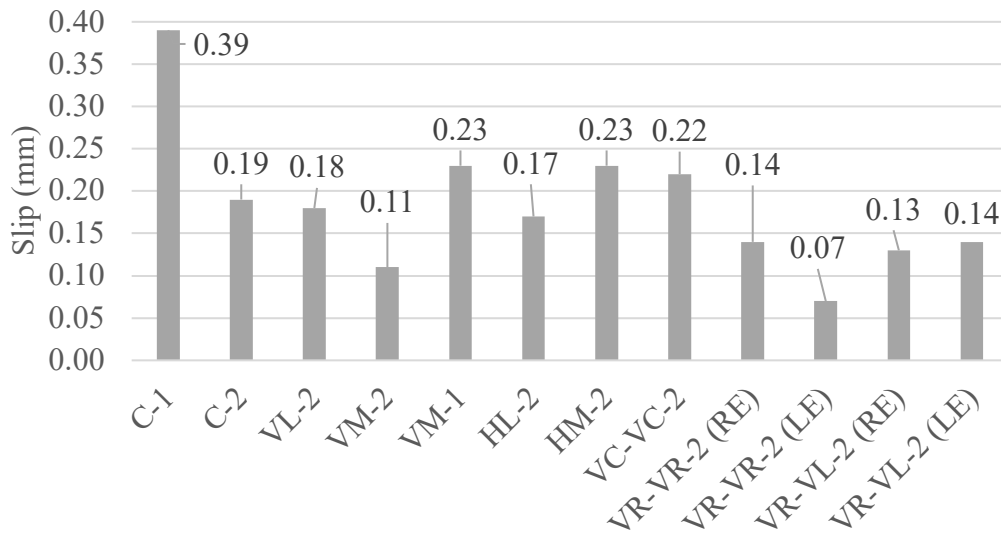


Fig. 5-4. Ultimate slip values for all specimens.

A variation in ultimate slip was observed across different specimens, as illustrated in Fig. 5-4. These values range from 0.07 mm on the left edge (LE) of specimen VR-VR-2 to 0.39 mm for specimen C-1. The results suggest that ultimate slip increases with a decrease in CFRP stiffness, as specimens with fewer plies of CFRP laminate exhibited higher slip values. For example, C-1

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(0.39 mm) and VM-1 (0.23 mm) show higher slip compared to VM-2 (0.11 mm) and C-2 (0.19 mm). This trend supports the idea that reduced CFRP stiffness leads to increased bond interface displacement.

However, specimen HM-2 exhibited an ultimate slip of 0.23 mm, which is the same as VM-1, despite presenting a unique strain profile. Specifically, the strain values for HM-2 peaked at the bottom of the CFRP laminates, which suggests an unusual strain distribution. This behaviour will be further discussed in subsection 5.7.3.

Interestingly, specimen VL-2 showed an ultimate slip of 0.18 mm, similar to HL-2 (0.17 mm), despite having half the ultimate strain (0.10% vs 0.22%), with both specimens carrying an average ultimate load of 11.6 kN. This discrepancy between strain and slip will be analyzed further in section 5.4.

Furthermore, specimen VR-VL-2 exhibited nearly identical ultimate slip values of 0.13 mm for the RE and 0.14 mm for the LE, which is consistent with the uniform strain distribution observed on both edges in the vicinity of the load-end defect (although the strain values decreased more steeply on the LE, with some values reaching negative strains opposite to where the first defect was placed). However, the shear stresses on the RE were approximately three times lower than those on the LE (4.8 MPa vs 12.7 MPa), with the maximum stresses occurring near the defect at the load-end of the specimen. This suggests that the load-end defect created a stress concentration that caused a redistribution of stresses toward the opposite edge (i.e., the LE), resulting in higher stresses and localized strain concentrations on the LE. The near-zero shear stresses along the LE, in the region of the middle defect, further support this hypothesis. This behaviour is consistent with the strain contour maps (Fig. 4-25), which indicate that as the load progresses, strain develops

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in an "S" shape, transferring from the LE at the load-end to the RE further along the bond area. It should also be noted that negative strains could indicate bending of the LE of the laminates, potentially skewing the strain readings and affecting the accuracy of the strain distribution at that edge. The presence of negative strains may suggest that the bond behaviour is not purely shear-driven but may also involve flexural effects at the load-end, leading to a more complex stress state.

In contrast, specimen VR-VR-2 exhibited mixed-mode failure, with noticeable concrete peeling concentrated on the LE, opposite to the location of the defect. Despite having nearly equal peak bond stresses on both edges (around 3.8 MPa), the LE exhibited smaller strain and slip values compared to the RE (0.07 mm for the LE vs 0.14 mm for the RE). The presence of concrete peeling suggests that stress redistribution on the LE caused the bond stresses to exceed the tensile capacity of the concrete (2.8 MPa), leading to shallow peeling. This resulted in a significant loss of load capacity (33%) compared to C-2 (8.9 kN vs 13.2 kN), highlighting the sensitivity of bond performance to edge conditions and interface quality. Asymmetry in bond performance, particularly near imperfections or defects, can lead to uneven stress redistribution and premature failure on the opposite side.

Finally, specimen VC-VC-2 exhibited an ultimate slip of 0.22 mm, greater than C-2 (0.19 mm). Despite a slight increase in slip, the strain contour map (Fig. 4-20) reveals that while strain is relatively uniformly distributed at the load end of the laminates, it becomes more concentrated on the RE as the distance from the bond area increases. Nevertheless, VC-VC-2 demonstrated an ultimate load (13.9 kN) similar to C-2 (13.2 kN), suggesting that the centrally located defects in VC-VC-2 had less impact on the specimen's load capacity compared to the defect-induced behaviour observed in VR-VR-2.

5.3. Bond Stress Distribution

The distribution of strain, shear, and slip along the bond interface has been widely studied in the literature. During a shear test, as the load increases, shear stresses are induced in the interface. At the loaded end, strains are at their maximum and decrease non-linearly – in an exponential decay – along the bond length until reaching zero at the bottom of the laminates. Strain values increase progressively with the load. Shear values are influenced by the product of axial stiffness and the strain variations along the length of the CFRP. Bond stresses initially build up during the primary elastic phase, where the interface remains uncracked, until reaching a peak at the loaded end. The secondary softening phase then begins, causing the shear distribution to shift downward along the sheets until a full distribution is formed across the effective length, at which point the shear is near zero at the loaded end and reach their peak near the bottom end of the bond area, as the laminates begin to peel from the substrate. Slip, defined as the integral of strain, is maximum at the loaded end. It continues to increase in the loaded end region with load until it reaches the value corresponding to peak bond stresses. In the secondary stage, slip reaches its ultimate value while the corresponding stress decreases. The slip distribution decreases non-linearly with increasing distance from the loaded end (Nelson et al. 2020). The general behaviour of slip, shear, and strain at the ultimate stage is illustrated in Fig. 5-5.

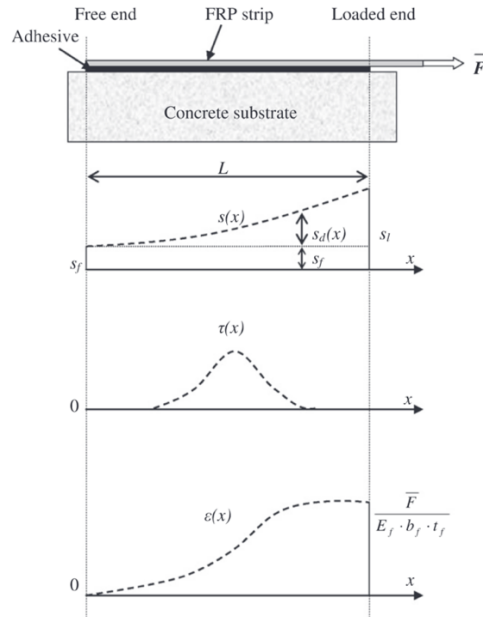


Fig. 5-5. Bond parameter distribution at the ultimate stage for a single lap shear test (Liu and Wu 2012).

The effective bond length (EBL) plays a crucial role in the development of the full shear distribution across externally strengthened CFRP-concrete joints. To assess whether the bonded length of the CFRP laminates is sufficient for the complete development of bond stresses, the EBL can be predicted for the specimens in this study. This will help determine if the joint has an adequately long bonded length or if it is considered short.

Detailed expressions for the various EBL models are provided in Appendix B. Table 5-3 presents the predicted EBL values for both single- and double-ply specimens, following each model type and their corresponding references. Based on the observations of this study, the zone along which shear stresses are completely transferred was estimated to be approximately 200-220 mm and is taken as the experimental EBL.

Table 5-3. Predicted EBL for single- and double-ply specimens based on different models from the literature.

Reference	Single-ply specimen	Double-ply specimen
fib 14 (2001); Neubauer and Rostasy (1997)	135 mm	191 mm
TR55 – Arya et al. (2002)	134 mm	189 mm
Niedermeier (2000)	96 mm	135 mm
Chen and Teng (2001); ACI 440.2R (2023); CAN/S806 (2012)	116 mm	163 mm
CAN/CSA S6 (2019)	160 mm	226 mm
Sato et al. (2001)	191 mm	252 mm
ISO model - JCI (2003)	90 mm	133 mm
Kanakubo et al. (2003)	149 mm	211 mm
Wu et al. (2009)	139 mm	202 mm
Zhou (2009)	139 mm	196 mm
Yang et al. (2001)	100 mm	100 mm
Average	132 mm	182 mm

For single-ply specimens, predicted EBL values ranged from 90 mm to 160 mm, with an average of 132 mm. For double-ply specimens, the predicted values ranged from 100 mm to 252 mm, with an average of 182 mm. Except for Sato et al. (2001), the overall bond lengths in this study exceed these predicted EBL values, suggesting that sufficient length is provided for shear and load transfer to reach full capacity.

However, for defected specimens, the bond length is locally reduced and varies across the laminates. Although single vertical defects – with a width of 38 mm – cover 76% of the bond width, they reduce the total undisturbed bond length to 200 mm. In contrast, single horizontal defects – with a width of 50 mm – cover the full bond width (100%), but their orientation

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minimizes the bond length reduction to 212 mm, suggesting better bond performance despite a greater obstruction. While specimen VL-2 and specimen HL-2 had very close failure loads (11.7 kN vs 11.5 kN), specimens VM-2 (10.1 kN) and HM-2 (11.8 kN) failed with a load difference of 1.7 kN. Overall, VM-2 exhibited the most critical behaviour among the four specimens, as both defect orientation and location significantly influenced its bond performance. The vertical orientation of the defect extended along the bond length, further reducing its effectiveness, while its placement in the mid-span divided the bonded region into two shorter segments, limiting bond development.

For specimens with dual defects, featuring two defects of 19 mm width and 50 mm length, the bond length and failure loads vary depending on the defect placement. For specimen VR-VL-2, the bond length remains 250 mm along the center of the sheets but is reduced to 200 mm at both the right and left edges. This specimen exhibited a failure load of 13.2 kN, slightly lower than specimens with fewer or less critical defects. As for specimen VC-VC-2, the bond length is reduced to 150 mm along the middle section line passing through the aligned vertical defects, while maintaining 250 mm along the left and right edges. This specimen displayed a failure load of 13.9 kN, indicating a more robust performance despite the localized reduction in bond length. Lastly, for specimen VR-VR-2, despite its similarity with VC-VC-2 in maintaining the bond length along some edges of the bond area, the alignment of both defects on the right edge significantly impacts performance. This configuration proved the most critical, with the ultimate load reduced to 8.9 kN.

In terms of ultimate strains, the overall behaviour matches the theoretical predictions, with the strains being maximal in the load end region and decreasing in a nonlinear gradient until reaching

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zero in the bottom end of the laminate. Shear stresses develop along the length as the loads progress, and slips at $y = 0$ increase until reaching their ultimate value at the failure stage of the test. For specimen C-1, the maximum strains are obtained at 30 mm below the start of the bond length.

The distribution of the bond parameters varies between defected specimens. Strain measurements across the specimens show a consistent pattern of variation, particularly in regions around defects. In most specimens, strain values initially increase and reach a peak at the beginning of the bonded length. As the load progresses, strains tend to decrease near the defect vicinity, often reaching a minimum or negative strain value. Notably, some specimens exhibit a second peak in strain after the defect region, which then decreases gradually to near zero values at the end of the bonded length.

This pattern reflects the localized stress redistribution caused by the defects. In specimens such as VL-2, VC-VC-2, and VR-VL-2, strains decrease sharply at the defect vicinity, suggesting that the bond is less effective in transferring load in these regions. The second peak observed after the defect can be attributed to the residual load transfer after the initial disruption once the defect's impact diminishes. The observed negative strains, particularly in specimens like VR-VL-2, indicate local bending in the CFRP likely due to the thinness of the laminates combined with the influence of defects, which disrupt the uniform distribution of stresses along the bonded length.

The shear stress distribution across specimens exhibits a pattern of initial peak formation at the defect region, followed by a decrease and a subsequent rebound in shear values as the load increases. The initial shear peak at the defect vicinity is indicative of the local shear transfer concentration, where the defect induces localized stress concentrations. These values decrease in

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the middle of the defect region, reflecting the reduced bond integrity caused by the defect. As load continues to increase, shear stresses begin to rebound beyond the defect region, suggesting that the bond area further away from the defect is less influenced by the defect and can resume load transfer more effectively.

In specimens such as VL-2, VM-2, and VC-VC-2, this trend of shear stress recovery beyond the defect is clear. However, specimens like VR-VL-2 demonstrate more complex shear behaviour, with two shear peaks observed: one at the loaded end of the defect region and another after the defect is passed. This may reflect complex interactions between shear and strain fields across defects, where shear transfer is disrupted in the defect region but re-engages once the defect is bypassed.

The shear stress concentration and recovery observed in all specimens highlight the critical role of defects in weakening the CFRP-concrete bond. The ability of the system to regain shear capacity beyond the defect region indicates that the CFRP laminate can still provide load-bearing performance, although with reduced efficiency compared to an ideal, defect-free bond.

Slip measurements provide a clear indication of the bond's shear resistance and its ability to transfer load between the CFRP laminate and the concrete substrate. In most specimens, the slip increases with load, particularly in the defect regions. This is consistent with the observation that defects reduce the bond's ability to effectively resist shear, leading to increased relative movement between the CFRP and concrete.

The steep decrease in slip within the defect region, particularly noted in specimens such as VL-2 and HL-2, suggests that defects create significant localized weakening. This could be due to the

microstructural damage in the bond interface, which hinders the effective transfer of shear forces and thus accelerates slip. Beyond the defect regions, the slip behaviour becomes more gradual, indicating that the bond is recovering its resistance to relative movement once the defect's influence is minimized.

In specimens like VC-VC-2 and VR-VL-2, the behaviour of slip is more complex, with decreasing slip in the defect region followed by a relatively constant or gradually decreasing slip after the defect. This suggests that defects do not solely impair the bond but also influence how the bond behaves in terms of load transfer. The decrease in slip at certain stages, particularly for specimens like VC-VC-2, implies that the bond is initially weakened but recovers as the defect's effect diminishes or as the CFRP laminate engages more effectively after passing the defect.

5.4. Load-Slip & Bond Slip-Behaviour

5.4.1 General Behaviour

The load-slip curve for a CFRP-strengthened concrete prism can typically be divided into three stages (Fig. 5-6):

1. **Elastic Stage:** In the initial stage, the load increases elastically, and the bond stresses reach their peak values at the start of the laminate's load-end. This stage ends when the first load peak is attained.
2. **Secondary Stage (Softening Phase):** Following the peak, the curve softens as microcracks and initial plastic deformations occur. During this stage, the shear peak gradually moves downward along the bond length, and the stress value at the load end decreases.

3. Plateau Stage: In the final stage, a plateau is observed where the slip reaches its ultimate value. At this stage, the stresses at the beginning of the bond length become zero, and the shear stress distribution curve develops over the effective bond length.

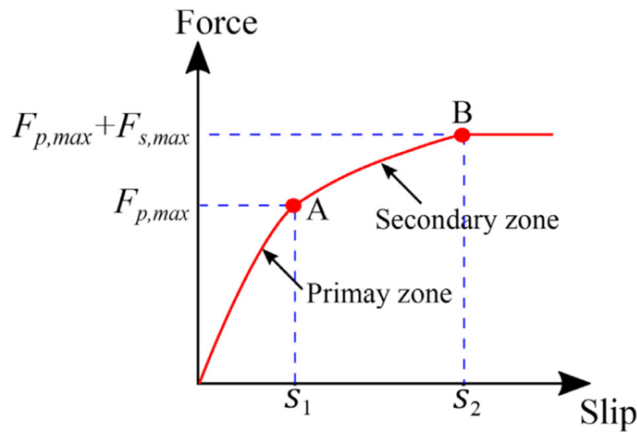


Fig. 5-6. Typical load-slip curve (Nelson et al. 2020).

Both control specimens, C-1 and C-2, followed a similar trend, as illustrated in Fig. 4.52. Specimen C-2 exhibited a higher ultimate load (13.1 kN) at a lower slip (0.19 mm) compared to C-1 (12.7 kN at 0.39 mm). This further attest that increased laminate stiffness could result in a higher ultimate capacity and a lower slip value, potentially facilitating a more uniform stress distribution by mitigating stress concentrations due to the fabric's stiffness.

5.4.2 Single Defects in the Load End Region

Figs. 4.53 and 4.54 present the load-slip curves for the single and dual-defected specimens, respectively, along with the control specimens for comparison.

For the specimens with a defect placed at the load-end of the laminates (VL-2 and HL-2), a reduction in stiffness is observed compared to C-2, likely due to the presence of the obstruction at

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the load end of the bond. Both specimens exhibited similar peak loads (~ 11.5 kN) and ultimate slips (0.18 mm), which were lower than C-2's values.

Specimen HL-2 exhibited higher stiffness than VL-2, reaching its first load peak more rapidly. HL-2 also displayed a longer plateau, suggesting that its full-width defect (50 mm) promoted uniform strain distribution but led to earlier inelastic behaviour and increased deformability after crack initiation beneath the defect at the loaded end. Stress concentrations observed at the defect's edge in HL-2 support this finding, indicating early crack formation in that region.

In contrast, VL-2, with a partial-width defect (36 mm out of 50 mm), demonstrated improved stress distribution along the bond length. This configuration reduced stiffness at the load end but delayed plastic deformations, showing a more gradual transition from elastic to inelastic stages. The partial-width defect allowed for more efficient stress redistribution, leading to initial higher deformation during the elastic stage but a quicker softening thereafter.

These results suggest that full-width defects are more critical in redistributing stress and maintaining elasticity, inducing earlier plastic deformations and potentially reducing the service life of the structure more than partial-width covering defects.

During the second stage of loading, both specimens reached their ultimate load and slip values simultaneously, with HL-2 exhibiting more localized deformation after losing its initial stiffness. This behaviour implies that bond failure is not solely determined by the width or orientation of the defect, but by how the defect influences the progression of bond damage and stress redistribution along the bond length.

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To further investigate the influence of defects on bond performance, two additional figures (Fig. 5-7 and Fig. 5-8) are presented, showing bond-slip curves at different distances from the defect for specimen VL-2. The first plot (Fig. 5-7) focuses on the bond-slip behaviour in the vicinity of the defect, providing a detailed view of how the defect affects the stress and slip distribution near its location. This figure highlights the localized bond damage and stress redistribution that occurs as the defect influences the bond capacity.

The second plot (Fig. 5-8) presents the bond-slip curve further away from the defect, enabling a comparison of bond behaviour at a distance from the defect region. This allows a better understanding how the defect affects the overall bond performance across the length of the CFRP laminate, providing insights into the extent of stress redistribution and its effect on the final load and slip characteristics.

In the vicinity of the defect, the bond exhibits lower shear values (3.9 MPa) and higher slip values (0.18 mm), indicating increased deformability around the defect region. This suggests that the presence of the defect introduces localized weakening, which allows for more slip and a reduction in bond stiffness. The defect likely disrupts the uniform stress transfer, leading to greater deformation in this region.

Further from the defect, stress concentrations are observed at around 80–100 mm from the start of the bond length, where shear stresses rise to 8.6 MPa, but the slip is much smaller (0.09 mm). This behaviour suggests that, although the shear stress is higher at these points, the bond still exhibits relatively stiff behaviour with limited slip. These regions, further from the defect, may act as compensation zones for stress redistribution caused by the defect, leading to higher shear values but restrained slip due to the more effective bond performance.

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The bond-slip relationship appears linear from 0 to 20 mm, indicating a gradual increase in bond stress with increasing slip. This linear behaviour potentially indicates that, in this initial region, the bond is behaving elastically, with shear stresses rising proportionally to the applied slip. However, beyond 20 mm, shear stresses increase at a higher rate before displaying a relative drop. This non-linear behaviour can be attributed to bond damage or microcracking in the interface as the stress transfer becomes less efficient, particularly around the defect.

The same pattern is observed away from the defect (at $y = 80$ mm and $y = 100$ mm), where the bond behaves similarly, with a steep increase in shear stress followed by a decrease, suggesting that the bond's overall integrity is affected by damage propagation from the defect region. At distances greater than 120 mm, shear values remain relatively low (below 4 MPa), and slip values are smaller, likely indicating that these regions are not significantly impacted by the defect and are closer to behaving in an ideal, undamaged state.

This progression highlights that the bond failure is not uniform along the bond length. The defect leads to a redistribution of stress and strain along the bond length, with localized increases in shear stress and slip at certain points, particularly near the defect. The performance suggests that the defect region is a critical area for bond failure initiation, and the performance of the bond in these regions is influenced not only by the defect's proximity but also by the way the defect alters the distribution of stresses along the entire bond length.

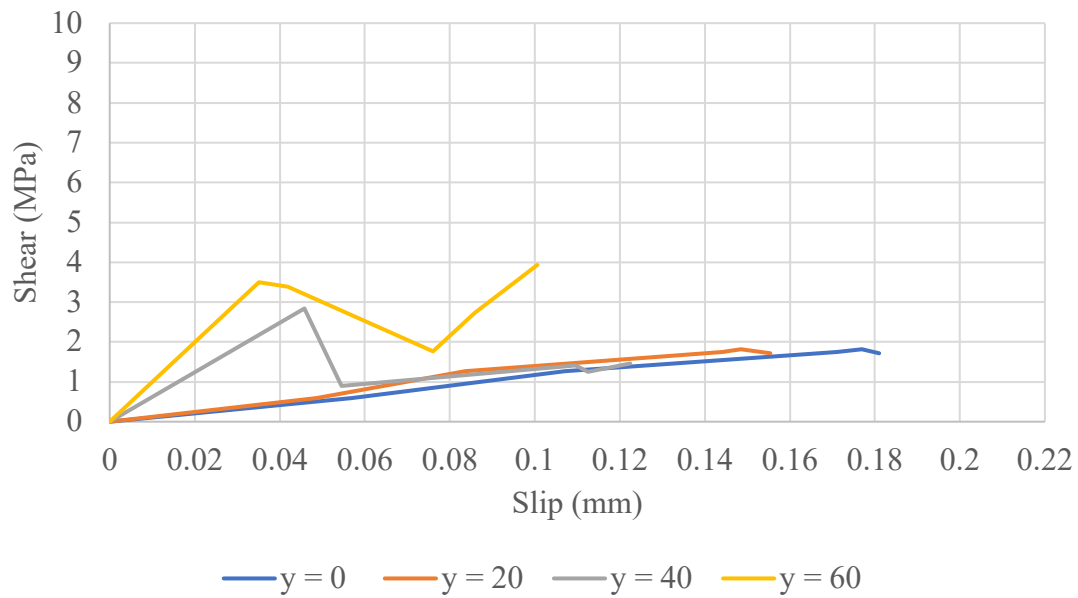


Fig. 5-7. Bond-slip curves at various distances near the defect vicinity for Specimen VL-2.

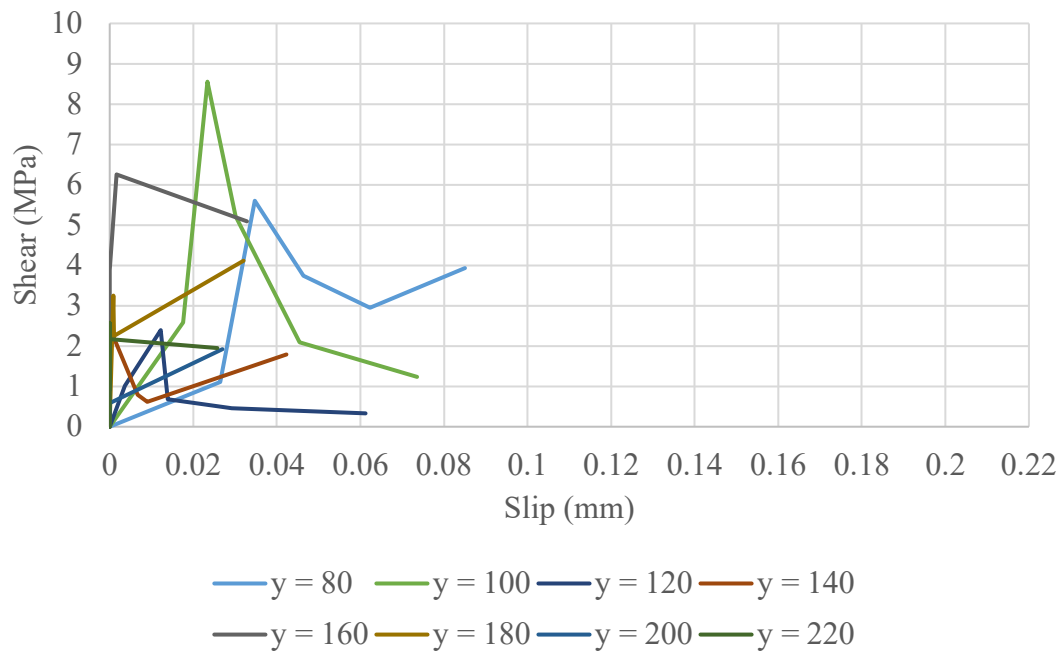


Fig. 5-8. Bond-slip curves at varying distances away from the defect region for Specimen VL-2.

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For comparison purposes, bond-slip curves at different distances from the defect for specimen HL-2 are depicted in Figs. 5-9 and 5-10, respectively.

The bond-slip curves around the defect vicinity for specimen HL-2 exhibit a behaviour similar to that of specimen VL-2, characterized by higher slip values compared to the bond-slip curves observed at distances away from the defect. This consistent trend highlights the increased deformability within the defect region, likely due to localized weakening of the bond. Such weakening could be attributed to factors such as microcracking, debonding initiation, or a reduction in shear transfer efficiency at the defect site.

However, the highest stress values (8.5 MPa) are recorded within the defect vicinity, while lower shear stresses are observed at distances away from the imperfection (6.3 MPa). This behaviour may result from the initial high stiffness of the bond-slip curve, indicating increased shear transfer at the boundary of the defect region and leading to localized stress concentrations. As hypothesized, the absence of a linear bond-slip relationship within the defect vicinity suggests that the bond no longer behaves elastically in this region. This implies possible plastic deformations and degradation of the bond interface, which explains the more gradual softening observed in specimen HL-2 compared to VL-2, as well as the transition to more complex failure mechanisms.

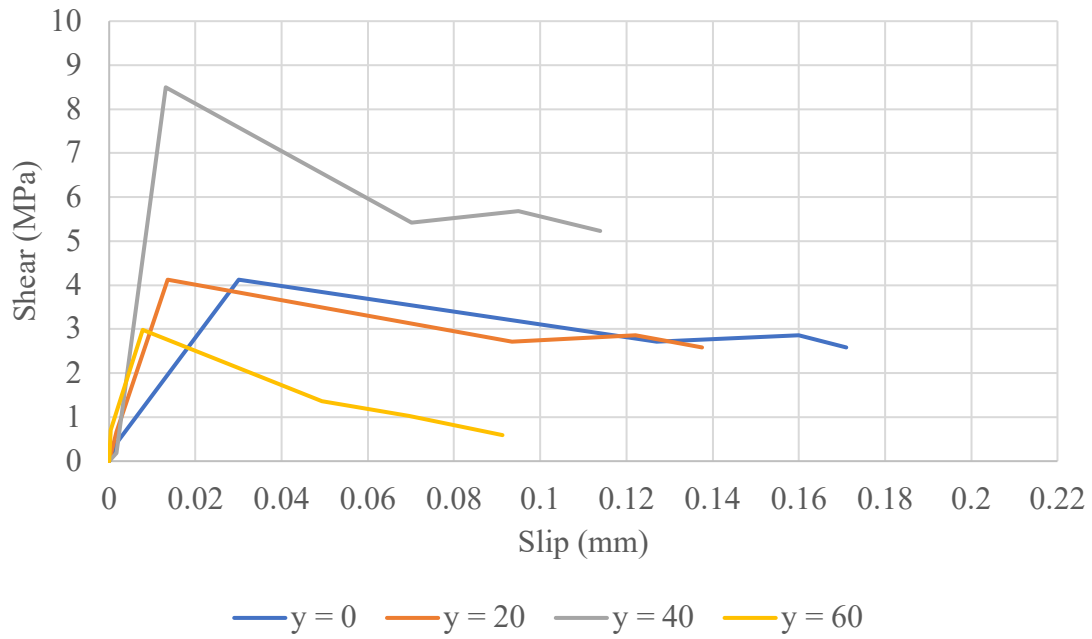


Fig. 5-9. Bond-slip curves at various distances near the defect vicinity for Specimen HL-2.

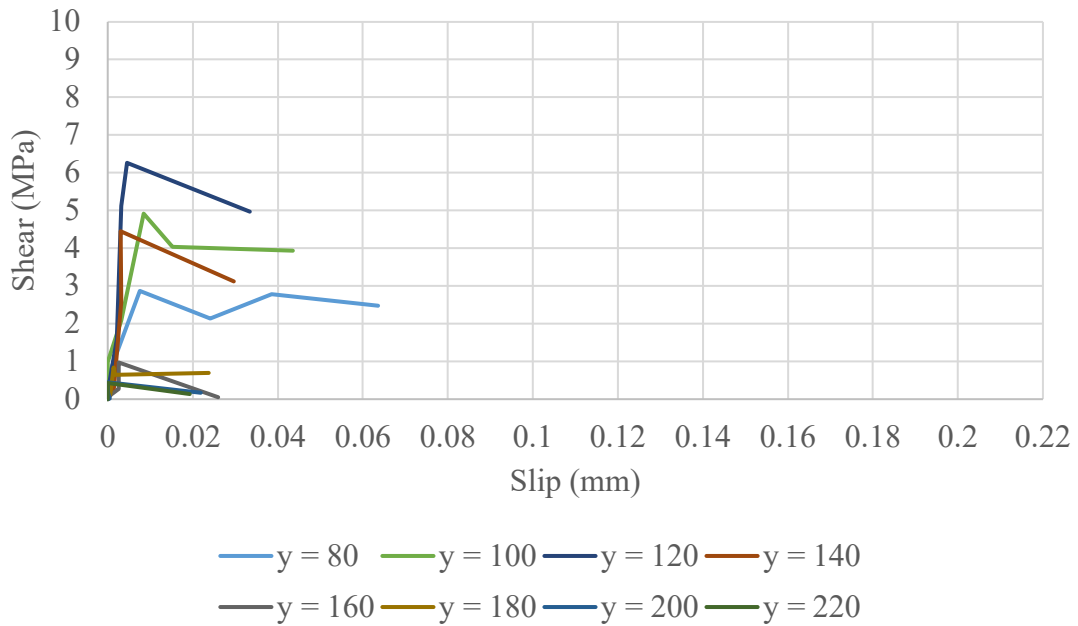


Fig. 5-10. Bond-slip curves at varying distances away from the defect region for Specimen HL-2.

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By taking the maximum bond for every slip value for both specimens, an envelope curve can be obtained to summarize the overall bond slip behaviour, as depicted in Fig. 5-11. Despite HL-2 showing a stiffer response at the initial stage of the load-slip curve, specimen VL-2 exhibits a higher bond-slip stiffness at lower slip values, reaching its initial peak at a smaller slip compared to HL-2. However, both specimens reach the same maximum stress, but the peak for VL-2 occurs at a higher slip compared to HL-2.

The bond-slip curves for both specimens show a similar trend, starting with an increase in shear, followed by a peak stress, then a softening phase with a decrease in bond strength at the center of the bond length, and a subsequent slower recovery before final failure. Notably, VL-2 exhibits a more gradual stress distribution, with the peak stress occurring further from the defect and a more transitional softening phase. This suggests that the stresses in VL-2 redistribute more gradually along the bond length, leading to a slower failure progression. On the other hand, HL-2 shows a higher bond stress post-peak, suggesting greater interfacial energy and indicating a more abrupt failure after the peak stress.

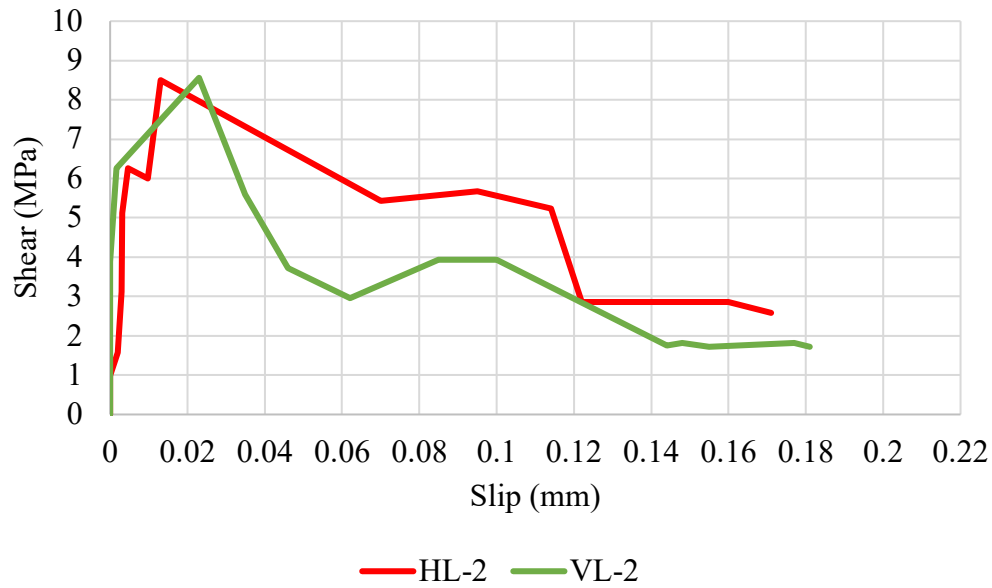


Fig. 5-11. Comparison of experimental bond slip envelope curves for Specimens VL-2 and HL-2.

5.4.3 Single Defects Placed in the Middle of the Bond Area

Specimens VM-2, VM-1, and HM-2 featured mid-length defects with variations in orientation (vertical vs horizontal) and CFRP ply configuration (single-ply vs two plies). Similar to the load-slip curves observed for the control specimens, VM-1, with a single ply of CFRP (and consequently lower stiffness), demonstrated a lower peak failure load and higher ultimate slip compared to VM-2, which was strengthened with two plies of CFRP fabric. While VM-2 exhibited a lower ultimate load and slip than C-2, VM-1 had failure parameters (load and slip) lower than both VM-2 and C-1.

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Both VM-1 and VM-2 experienced a distinct load drop after reaching values near their ultimate capacity, followed by a recovery phase and eventual failure. Due to its higher stiffness, VM-2 exhibited this load drop at a smaller slip compared to VM-1.

Specimen HM-2 displayed irregular strain behaviour, with unusual strain contour maps and peak strain values concentrated at the bond's end. Despite these discrepancies, the data is presented for completeness. The load-slip curve of HM-2 resembles that of a control specimen with no defect, lacking the expected load drop behaviour. Additionally, it showed the lowest initial stiffness among all specimens, possibly attributable to the need to stop the test to realign the specimen and reload it. Further limitations were identified and are discussed in sections 5.6 and 5.7. Additionally, specimen HM-2 showed a strange strain behaviour (subsection 4.3.2.5) with an atypical strain development below the defect region and peaking further at the bottom end of the laminates. For these reasons, the envelope bond slip curve for this specimen is not included in this Chapter.

The load-drop behaviour associated with mid-length defects has been discussed by Li et al. (2023) and is illustrated in Fig. 5-12 to provide a simplified understanding of this phenomenon.

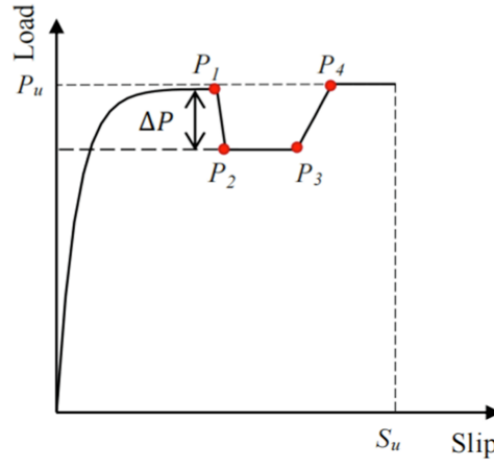


Fig. 5-12. Simplified load-slip behaviour of a CFRP-concrete specimen with a middle-placed defect (Li et al. 2023).

Li et al. (2023) further introduced a model to account for the influence of such imperfections on load capacity (P_u), expressed in Eq. 5-8 as a function of their surface area ratio (k_s). The authors proposed coefficient values of $a = 0.607$ and $b = 0.67$ to better estimate the reduction in bond performance (ΔP) due to these defects.

$$\Delta P = a \times (k_s)^b \times P_u \quad \text{Eq. 5-8}$$

Table 5-4 compares the experimental data obtained in this study with similar specimens investigated by Li et al. (2023), which featured square, middle-placed defects with an area ratio of 15%. The table includes specimen names, defect area ratios, experimental ultimate loads, experimental load drops, and the corresponding drop proportions relative to the ultimate load for each specimen. Additionally, three predicted load drops are computed based on different sets of coefficients (a , b). The first set uses the values proposed by Li et al. (2023), while the second set

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is calibrated to incorporate all data points, including both this study's experimental results and Li et al.'s data. Lastly, the third set is calibrated exclusively for this study's data.

For each adaptation, a mean absolute percentage error (MAPE) is calculated to provide an overall error estimate. The calibrated coefficients were determined using Excel's solver function to minimize the average error. Although specimens VC-VC-2 and VR-VR-2 featured dual defects, they exhibited a load drop behaviour similar to single-defect specimens with middle-placed imperfections. This allows their data to be included alongside VM-1 and VM-2 for calibrating the coefficients.

Table 5-4. Comparison of experimental and predicted load drops for specimens with middle-placed defects.

Reference	Experimental data					a =	0.607	a =	2.95	a =	0.53
						b =	0.67	b =	1.64	b =	0.81
						Li et al. (2023)		Adapted (all points)		Adapted (only this study)	
	Specimen	k_s (mm ²)	P_u (kN)	ΔP (kN)	$\Delta P/P_u$ (kN)	ΔP (kN)	MAPE	ΔP (kN)	MAPE	ΔP (kN)	MAPE
Li et al. (2023)	S1-1	15%	16.7	2.2	13.3%	2.8	29.3%	2.2	0.0%	1.9	13.9%
	S1-2	15%	19.0	3.4	18.2%	3.2	4.8%	2.5	26.4%	2.2	36.6%
	S1-3	15%	21.2	2.8	12.9%	3.6	28.9%	2.8	0.3%	2.4	14.1%
This study	VM-1	15.2%	6.7	0.9	13.4%	1.2	27.9%	0.9	0.2%	0.8	14.6%
	VM-2	15.2%	10.2	1.2	11.5%	1.8	49.8%	1.4	17.4%	1.2	0.0%
	HM-2	15.2%	12.0	-	-	2.1	-	1.6	-	1.4	-
	VC-VC-2	7.6%	13.9	0.6	4.3%	1.5	150.1%	0.6	0.2%	0.9	51.7%
	VR-VR-2	7.6%	8.1	0.7	8.6%	0.9	24.9%	0.4	50.0%	0.5	24.2%
Error:						45.1%	Error:	13.5%	Error:	22.6%	

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The analysis indicates that the second set of coefficients, calibrated using all data points (Li et al.'s and this study's experimental results), provided the best fit. The optimized coefficients, $\mathbf{a} = 2.95$ and $\mathbf{b} = 1.64$, achieved an average error (MAPE) of 13.5%. This adaptation aligned well with Li et al.'s results, achieving near-perfect predictions for two load drops (0% MAPE error) and a moderate error of 26% for one data point. For this study's specimens, the MAPE error ranged between 0.2% and 50%, indicating a reasonably good fit across a small dataset.

While specimen HM-2's experimental data is unreliable, the newly calibrated model predicts that, based on its ultimate load of 12.0 kN, a load drop of approximately 1.6 kN should have been evident in the load-slip curve.

On the other hand, bond slip curves for both VM-2 and VM-1 are presented both near the defect vicinity and further from that region. For specimen VM-2, the bond slip curves near the defect region (Fig. 5-13) showed a higher peak stress (5.3 MPa at 100 mm) compared to those away from the defect vicinity (Fig. 5-14), where the peak stress was 2.8 MPa (at $y = 0$ mm). Conversely, the slip values in the defect region were greater than those away from the defect vicinity. This could be potentially explained by localized weakening near the defect region, leading to the increased slip and lower shear values at larger distances from the defect.

Similarly, the bond slip curves for specimen VM-1 both near the defect vicinity and further from the defect length are depicted in Fig. 5-15 and Fig. 5-16, respectively. In contrast, specimen VM-1 displays a different trend, with both higher peak shear (5.9 MPa at $y = 40$ mm) and slip values away from the defect vicinity, compared to the defect region (2.5 MPa at $y = 120$ mm). The data suggests that the specimen's bond near the defect seems to lose stiffness quickly, which could

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explain the lower shear values observed in the defect region. Away from the defect, there seems to be higher shear transfer efficiency, leading to greater peak stresses and a stiffer bond-slip curve.

All presented curves (for both VM-2 and VM-1) display a non-linear pattern, potentially representing plastic behaviour as a result of the defects. The greater slip near the defect in VM-2 could indicate early initiation of bond degradation, while VM-1's behaviour away from the defect region reflects a more gradual transition to failure. Notably, specimen VM-1's behaviour is consistent with specimen VL-2 discussed in subsection 5.4.2, as VM-1 shows a reduction in bond strength near the defect and an increase in shear values away from the defect.

Interestingly, specimen VM-2's unique behaviour is relatively similar to both of that of specimen HL-2 and VL-2. VM-2 exhibited higher bond stresses near the defect vicinity, similar to specimen HL-2, but the slip values are still relatively higher, attesting of a greater deformability. However, VM-2's slip values remain smaller than those of specimen VL-2. This is potentially explained by localized stress concentrations near the defect vicinity for specimen VM-2 with more controlled slip compared to specimen HL-2.

Overall, both specimens undergo inelastic shear slip behaviour, indicating that the defect in the middle of the bond area caused a softening behaviour eventually leading to the failure of the specimens.

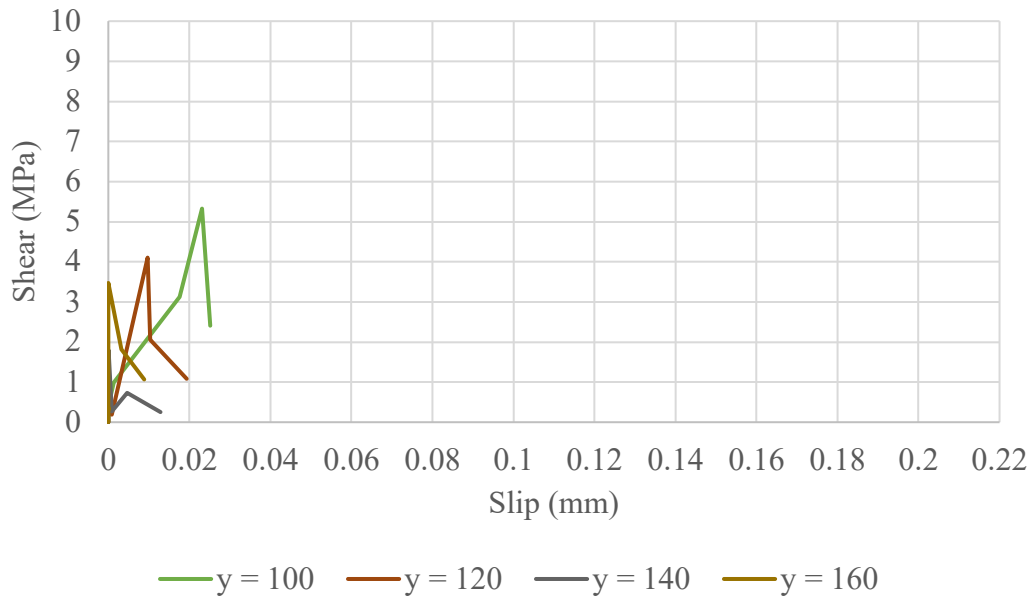


Fig. 5-13. Bond-slip curves at various distances near the defect vicinity for Specimen VM-2.

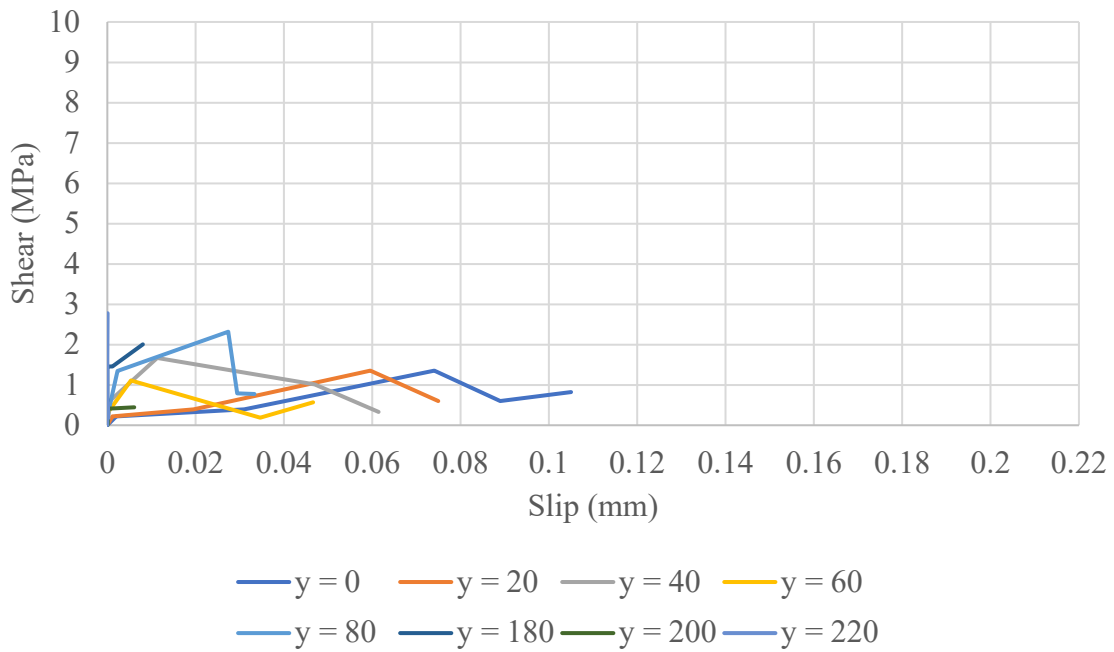


Fig. 5-14. Bond-slip curves at various distances away from the defect vicinity for Specimen VM-2.

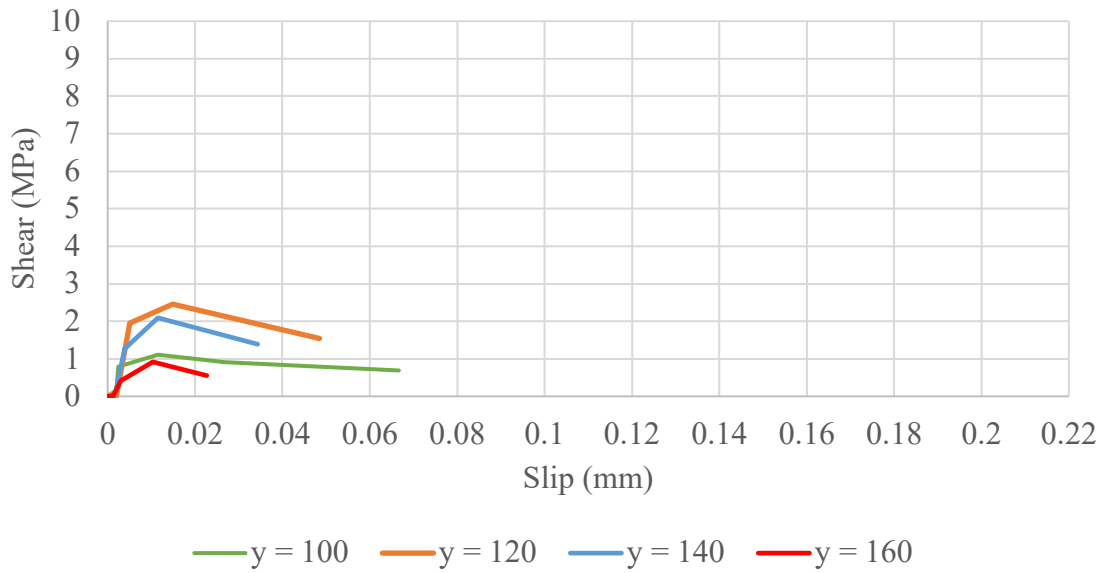


Fig. 5-15. Bond-slip curves at various distances near the defect vicinity for Specimen VM-1.

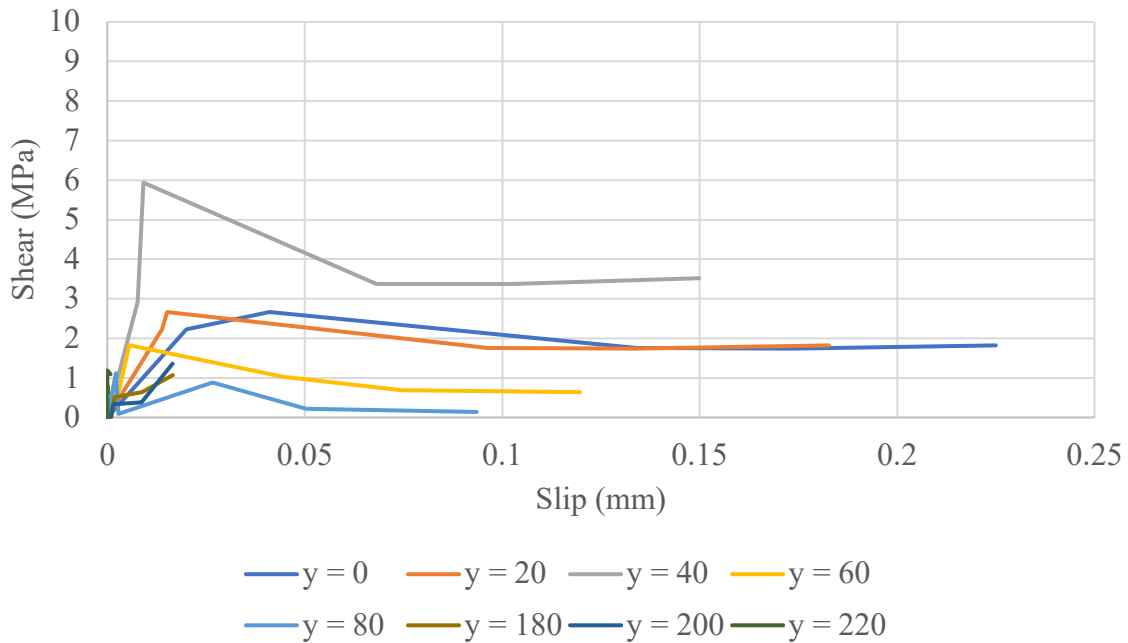


Fig. 5-16. Bond-slip curves at various distances away from the defect vicinity for Specimen VM-1.

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Similar to subsection 5.4.2, the envelope curves of the bond slip data points are plotted and presented in Fig. 5-17. The bond slip envelope of specimen VM-1 shows lower initial stiffness, likely due to its single ply of CFRP, after which a higher peak is reached at a lower slip value compared to VM-2.

In contrast, specimen VM-2's bond slip curve exhibits a significant drop right after reaching its peak stress, followed by a decrease in bond stresses as slip values progress. Despite the higher initial stiffness, the overall behaviour of VM-2 indicates a smaller interfacial energy, with an earlier softening of the curve. This suggests that the presence of dual plies in VM-2 leads to a more brittle failure, reflecting a less resilient bond interface. Furthermore, the peak shear and slip values of VM-2 are smaller than those of VM-1, pointing to a reduced bond strength in VM-2 despite its higher stiffness. Additionally, the fact that the peak shear and slip values of VM-2 are smaller than those of VM-1, despite VM-2's higher initial stiffness, indicates that the additional ply does not necessarily translate into a stronger bond. Instead, the data suggests that the two plies may result in a less stable bond interface in the long term, with quicker degradation once the peak stress is reached. This could be due to factors like fabric misalignment, delamination between plies, or a less efficient transfer of stresses across the bond.

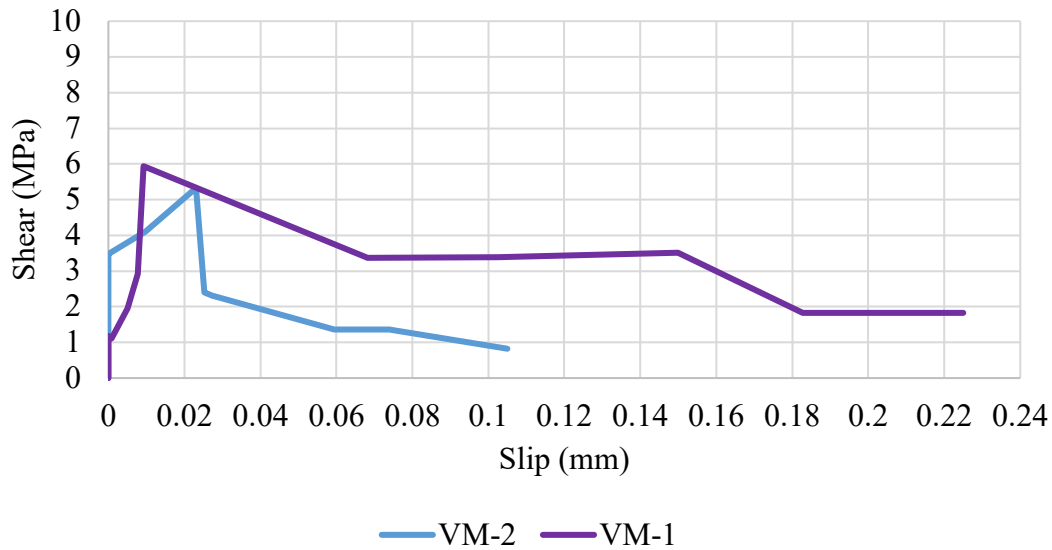


Fig. 5-17. Comparison of experimental bond slip envelope curves for Specimens VM-2 and VM-1.

5.4.4 Dual Defected Specimens

The bond-slip curves for specimen VC-VC-2 reveal significant variations in bond performance near and away from the defect regions (Figs. 5-18 and 5-19, respectively). Regions near defects (e.g., at $y = 40$ mm, within the vicinity of the load-end defect) exhibited higher maximum shear stresses (7.2 MPa) and greater slips compared to regions away from defects ($y = 0$ mm), where peak stresses were notably lower (3.0 MPa). These findings suggest localized stress concentrations and enhanced deformability near the defects.

The bond-slip performance near defects demonstrated an extended second-stage plateau, characterized by reduced stiffness and increased slip values, indicating a higher capacity for deformation. In contrast, regions farther from defects, particularly near the bottom of the laminate

($y = 200$ and 220 mm), exhibited minimal stresses and slips (< 0.02 mm), which may highlight the rapid progression of debonding and failure in the test setup.

Furthermore, while the bond-slip curve at $y = 0$ displayed higher slip values (0.22 mm), the interfacial energy was lower than at $y = 40$ mm. This discrepancy highlights the critical influence of defect-induced stress concentrations and their ability to redistribute bond energy unevenly along the laminate.

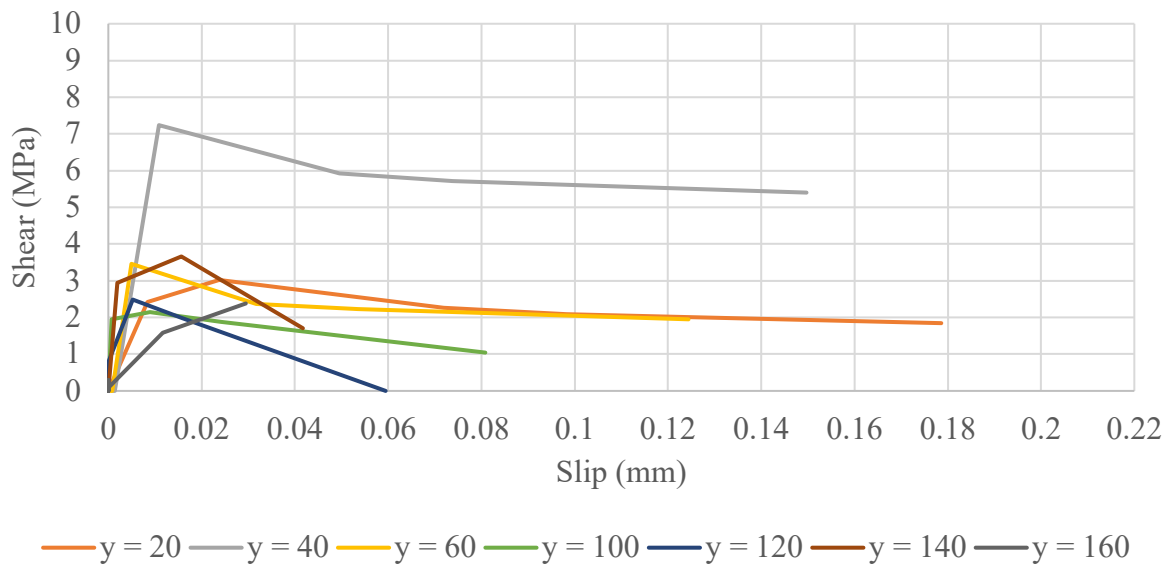


Fig. 5-18. Bond-slip curves at various distances near the defects' vicinity for Specimen VC-VC-2.

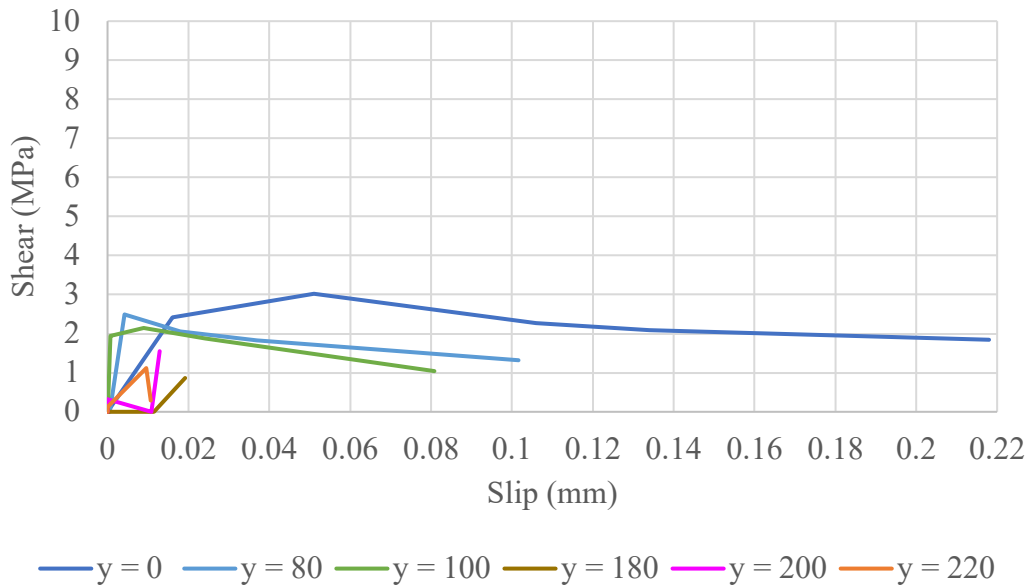


Fig. 5-19. Bond-slip curves at various distances away from the defects' vicinity for Specimen VC-VC-2.

The bond-slip curves for specimen VR-VR-2 along the right edge (RE) – crossing both defects – are shown for areas near defects (Fig. 5-20) and away from the defects (Fig. 5-21). Overall, a somewhat similar behaviour is observed, with most curves exhibiting an initial elastic stage, followed by a steep loss of stiffness post-peak, and a plateau region where slip increases while shear values slowly decrease.

The plateau regions are more pronounced near the defects, showing multiple shear peaks at varying slip values. This behaviour indicates higher deformability and a significant loss of stiffness in the defect regions, consistent with stress redistribution around defects. Notably, for some regions beneath the middle-placed defect ($y = 180$ and 200 mm), linear curves are observed for low slip values (<0.02 mm). This suggests that these regions likely failed prematurely without experiencing

substantial deformability, possibly due to localized stress concentrations accelerating debonding propagation.

In contrast, the bond-slip curves along the left edge (LE) for regions near and away from the defects' vicinities are shown in Figs. 5-22 and 5-23, respectively. Compared to the RE, the LE exhibited similar shear peaks for lower slip values and therefore smaller interfacial energies. Regions parallel to where the defects were placed displayed a similar behaviour, characterized by a lower initial stiffness, a post-peak drop in shear stresses accompanied by increased slips (though smaller slips compared to the RE), and a sudden drop in shear stresses. These observations align with the stress redistribution evident in the strain contour map for the specimen (discussed in Section 5.2).

The behaviours observed highlight the critical role of edge-aligned defects in influencing stress redistribution and bond performance. Edge-aligned defects concentrated stresses on the opposite edge (LE), leading to reduced stiffness, lower interfacial energy, and premature failure. The asymmetric stress distribution caused by these defects impaired the capacity for uniform stress redistribution, particularly in the LE, intensifying localized stress concentrations and accelerating failure. In contrast, regions near defects on the RE exhibited higher deformability, with extended plateau regions and multiple shear peaks. This suggests that stress redistribution was more effective on the RE, although at the cost of significant stiffness loss. Additionally, Li et al. (2023) reported that the shear strain gradient is near zero at the defect location and increases at farther edges away from the imperfections. This observation corroborates the failure mechanism in specimen VR-VR-2, where stress concentrations and concrete peeling occurred on the LE—

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opposite the defect placement. The high induced shear gradient likely caused stresses to exceed the concrete's tensile capacity, resulting in the specimen's premature failure.

Mechanistically, the presence of defects altered the interplay of mode I and mode II stresses. Defect regions exhibited complex interactions, manifesting as higher shear stresses and deformability. The stress redistribution near defects facilitated the formation of extended plateau regions but also localized shear peaks, emphasizing the critical impact of defect geometry and placement on bond-slip behaviour.

Overall, specimen VR-VR-2 highlights the criticality of defect placement and alignment in bond performance. The findings reveal that edge-aligned defects not only induce stress concentrations but also reduce load capacity, interfacial energy, and stiffness, highlighting their detrimental impact on structural integrity. Practical implications of these findings are significant. Mitigation strategies, such as avoiding edge-aligned defects, ensuring uniform adhesive application, or increasing laminate width to promote better stress redistribution, are recommended.

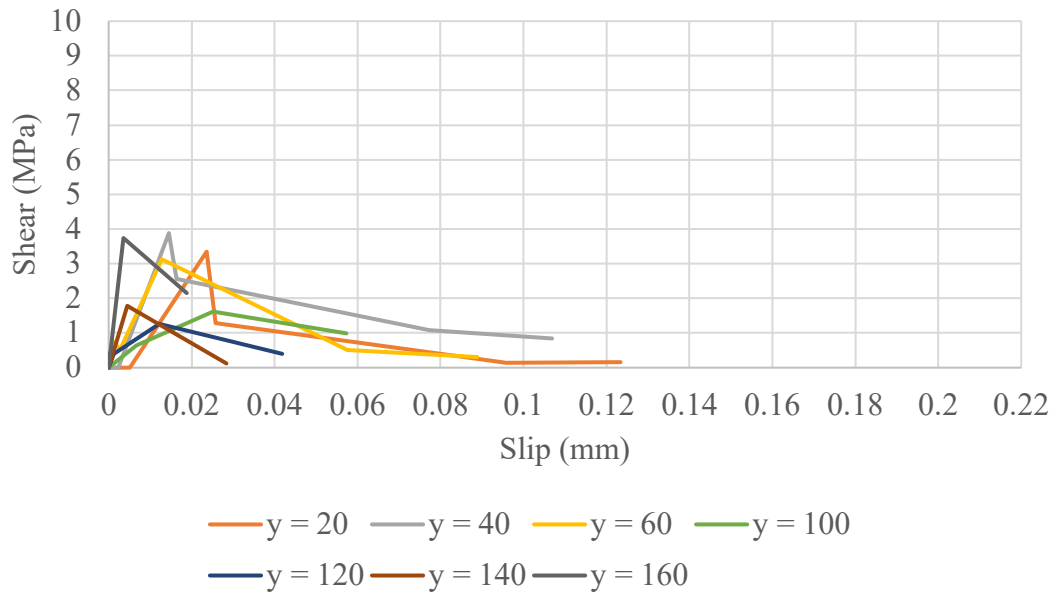


Fig. 5-20. Bond-slip curves at various distances near the defects vicinity along the right edge section line (RE) for Specimen VR-VR-2.

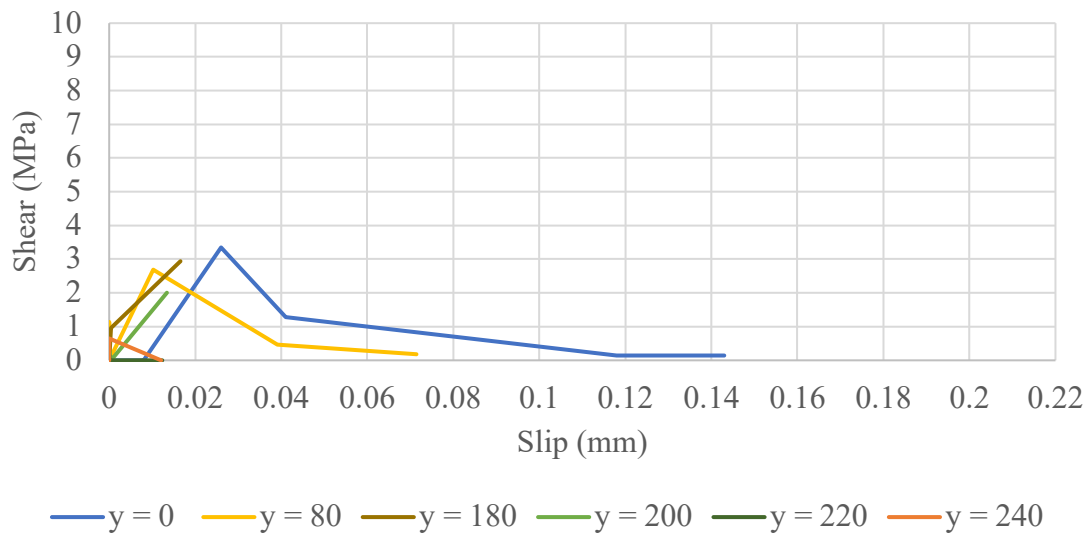


Fig. 5-21. Bond-slip curves at various distances away from the defects region along the right edge section line (RE) for Specimen VR-VR-2.

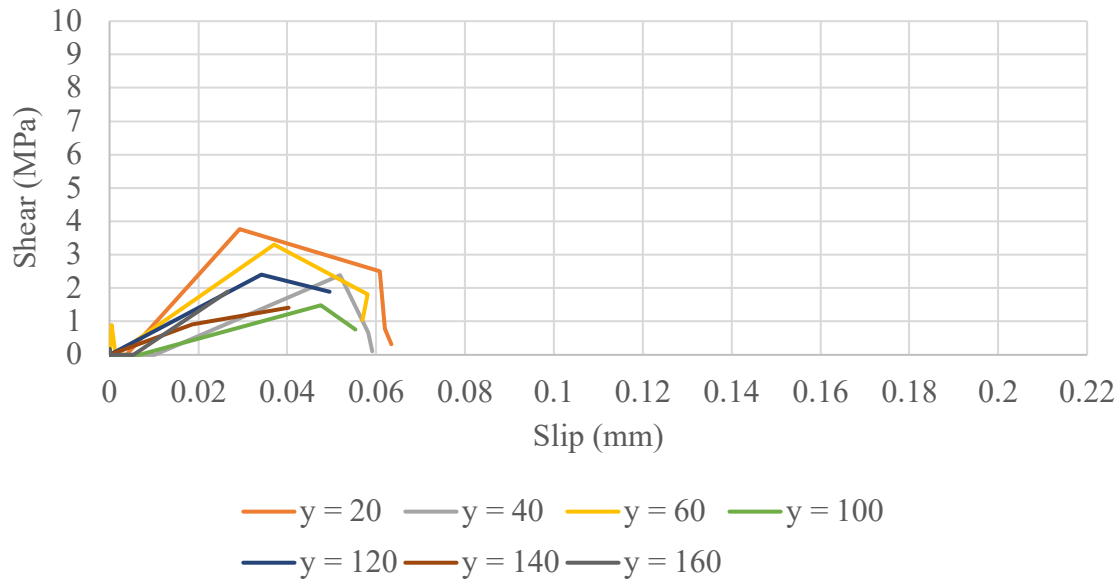


Fig. 5-22. Bond-slip curves at various distances near the defects vicinity along the left edge section line (LE) for Specimen VR-VR-2.

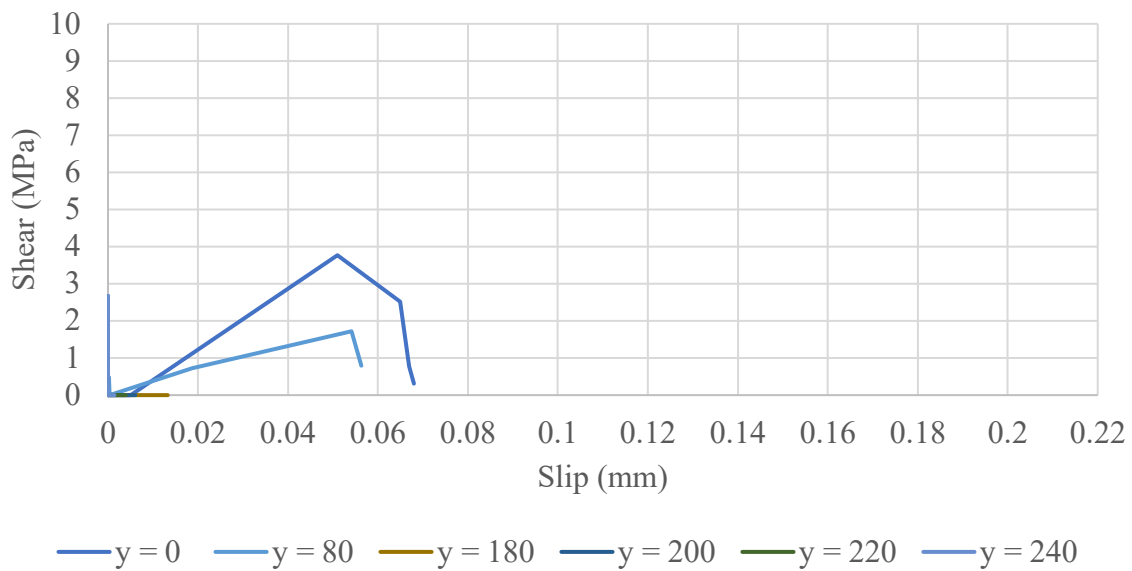


Fig. 5-23. Bond-slip curves at various distances away from the defects region along the left edge section line (LE) for Specimen VR-VR-2.

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The bond-slip curves for specimen VR-VL-2 provide critical insights into the influence of defects on bond performance. Along the right edge (RE), which crosses the load-end defect but is opposite to the middle-placed defect, the behaviour varies depending on proximity to the defects. Near the load-end defect, the curves (Fig. 5-24) demonstrate notable stress concentrations and higher slips. This contrasts with regions near the middle defect (Fig. 5-25) and away from both defects along the RE (Fig. 5-26), where lower stresses and smaller slips are observed. Interestingly, although the slips near the load-end defect along the RE are somewhat similar compared to those along the left edge (LE), the shear stresses in the load end defect region on the LE (Fig. 5-27) are nearly triple those along the RE, further reinforcing the criticality of defects in altering bond behaviour. However, the corresponding slips are smaller, suggesting that while the stress redistribution is significant, the deformability is lower. This disparity shows how the presence of defects results in a loss of stiffness, a decrease of interfacial energy, and an increase in deformability in the imperfection's vicinity.

For some locations near the middle-placed defect along the LE (Fig. 5-28), the curves reveal similar stresses, but lower slips compared to the RE near the load-end defect. Conversely, other regions in that vicinity (e.g., $y = 140$ and 160 mm) show zero stresses. Additionally, more plastic behaviour is observed in the middle-placed defect vicinity along the LE compared to the RE (Fig. 5-25), where curves are linear. This indicates a cumulative effect of stiffness loss induced by the load-end defect and likely early crack formations reducing the deformability of those regions, potentially inducing premature debonding. The previously identified S-shaped stress redistribution pattern further supports this conclusion, highlighting the complex interaction between defect placement, stress transfer, and bond performance. Despite these effects, the criticality of edge-

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aligned defects was mitigated by their small width, as the failure load (13.2 kN) remained comparable to that of the control specimen C-2, indicating that capacity loss was limited.

Certain localized behaviours are noteworthy, especially near the middle-placed defect along the LE. In some regions, stresses remain zero, while in others (e.g., $y = 100$ and 120 mm), they increase linearly before failure. This linear trend, coupled with premature failure, suggests localized stress concentrations and insufficient stress redistribution. A similar trend is observed at $y = 80$ mm, just before the middle defect region, where stresses rise linearly up to 4.1 MPa before failure. These behaviours underline the sensitivity of defect-induced stress redistribution and its influence on premature debonding in localized areas.

When compared to VR-VR-2, VR-VL-2 exhibits similar overall behaviour, with defects causing significant stress concentrations and stiffness reduction on opposite edges to where defects were placed. However, the stress redistribution patterns in VR-VL-2 are less abrupt due to the smaller width of the defects, allowing smoother stress transfer across the bond length. This distinction emphasizes the role of defect geometry in determining bond performance. In both specimens, the presence of defects along edges highlights the vulnerability of these regions to stress redistribution and premature failure, a critical consideration in bonded repairs.

From a repair perspective, these findings highlight the importance of understanding the placement and geometry of defects. Repairs with defects near the load-end or along edges are particularly prone to stress concentrations, reduced stiffness, and lower interfacial energy, compromising the effectiveness of the bond. Mitigating these effects requires careful defect characterization and consideration of stress redistribution during design. Furthermore, the relatively preserved failure load in VR-VL-2 demonstrates that even with defects, repair performance can remain adequate if

stress redistribution is managed effectively. These insights are crucial for optimizing repair strategies and ensuring the durability and performance of bonded systems in the presence of defects.

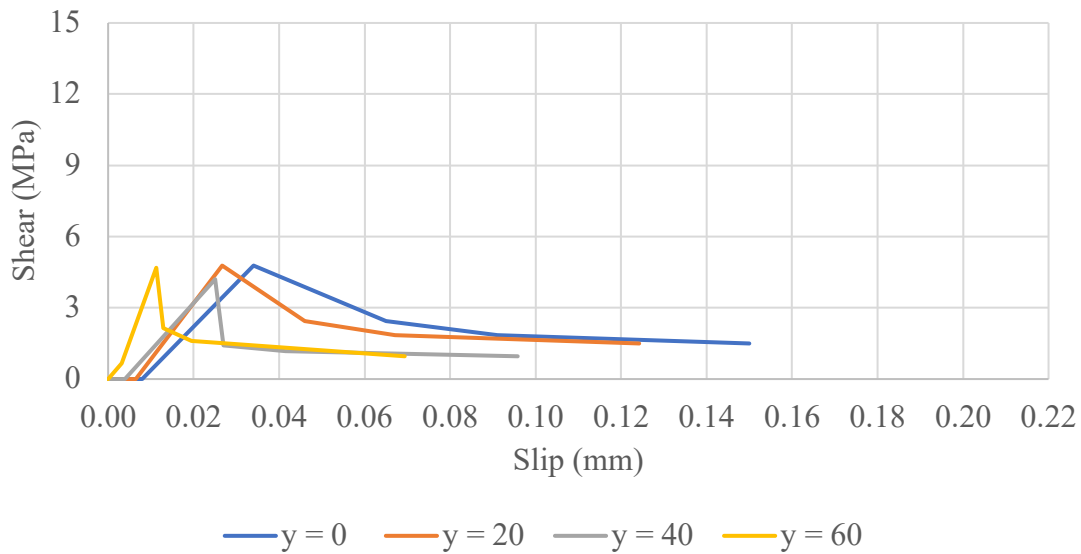


Fig. 5-24. Bond-slip curves at various distances near the load end defect region along the right edge section line (RE) for Specimen VR-VL-2.

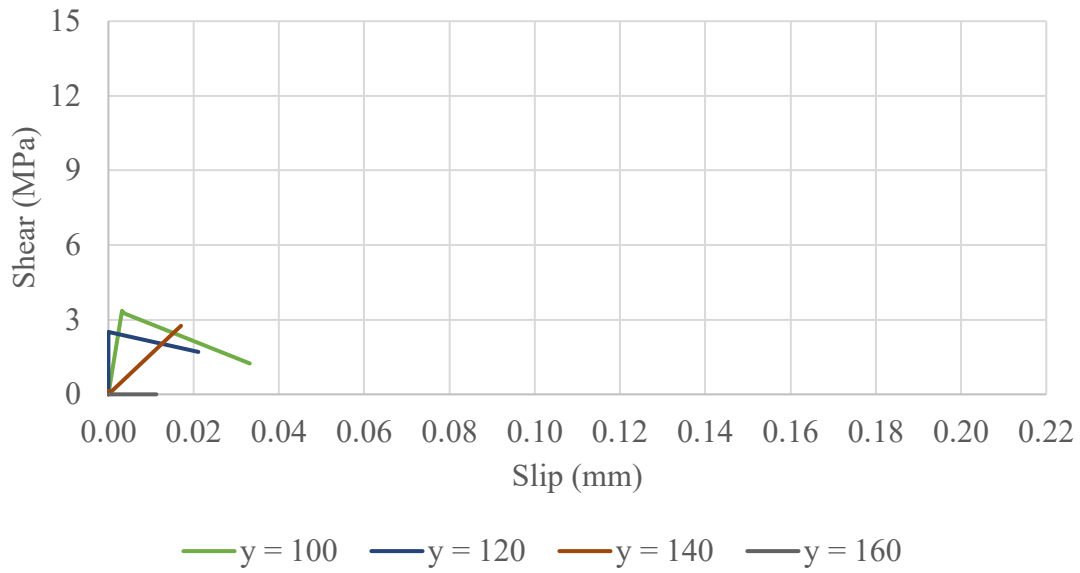


Fig. 5-25. Bond-slip curves at various distances near the middle-placed defect region along the right edge section line (RE) for Specimen VR-VL-2.

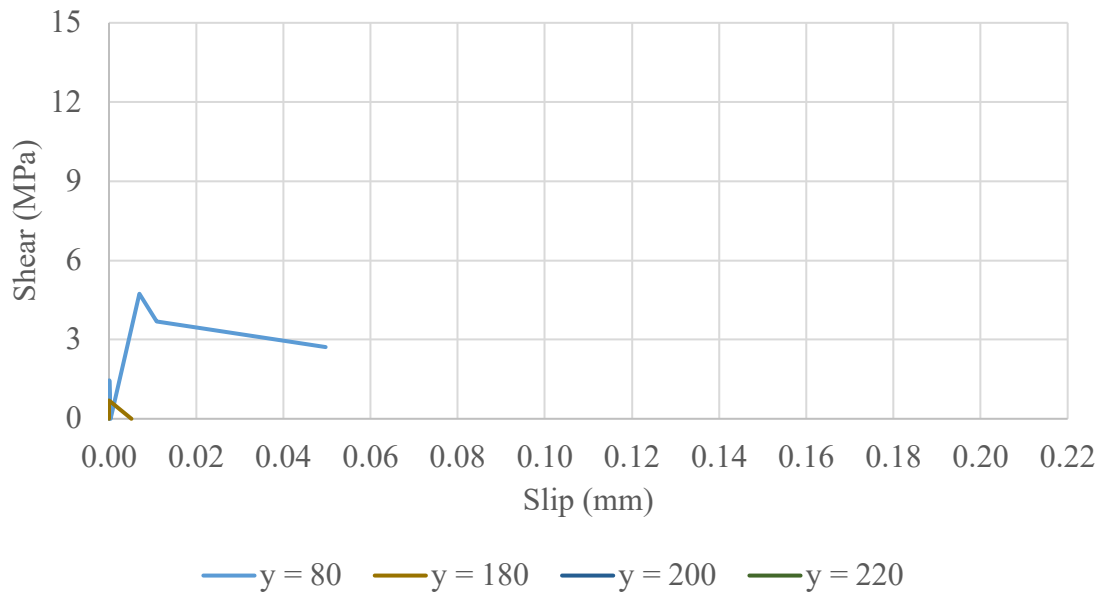


Fig. 5-26. Bond-slip curves at various distances away from the defect regions along the right edge section line (RE) for Specimen VR-VL-2.

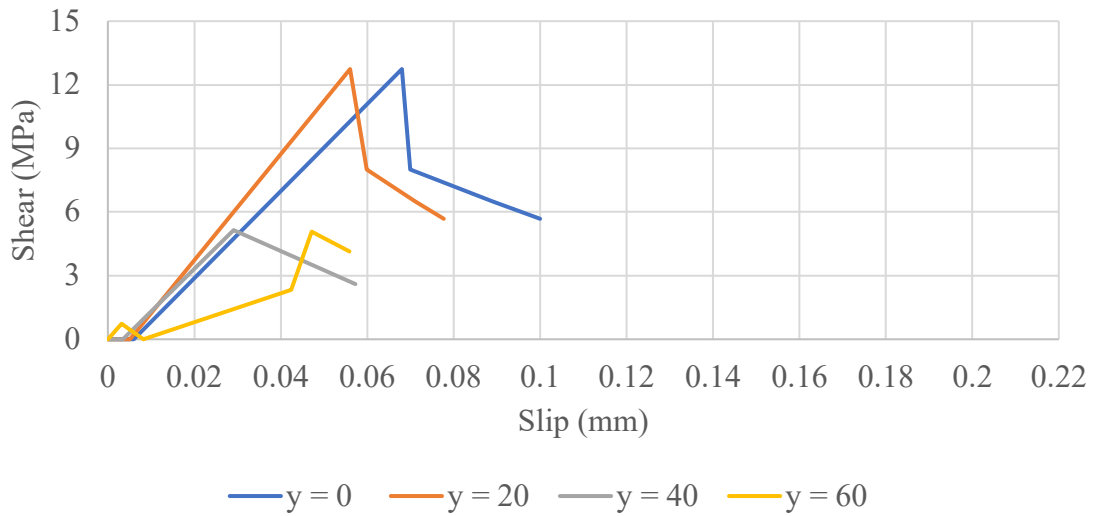


Fig. 5-27. Bond-slip curves at various distances near the load end defect region along the left edge section line (LE) for Specimen VR-VL-2.

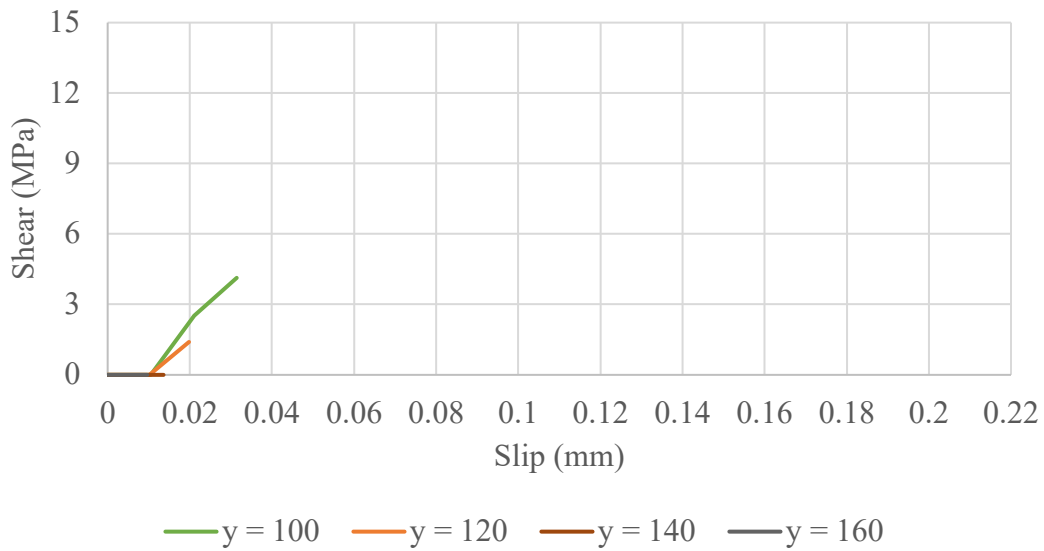


Fig. 5-28. Bond-slip curves at various distances near the middle-placed defect region along the left edge section line (LE) for Specimen VR-VL-2.

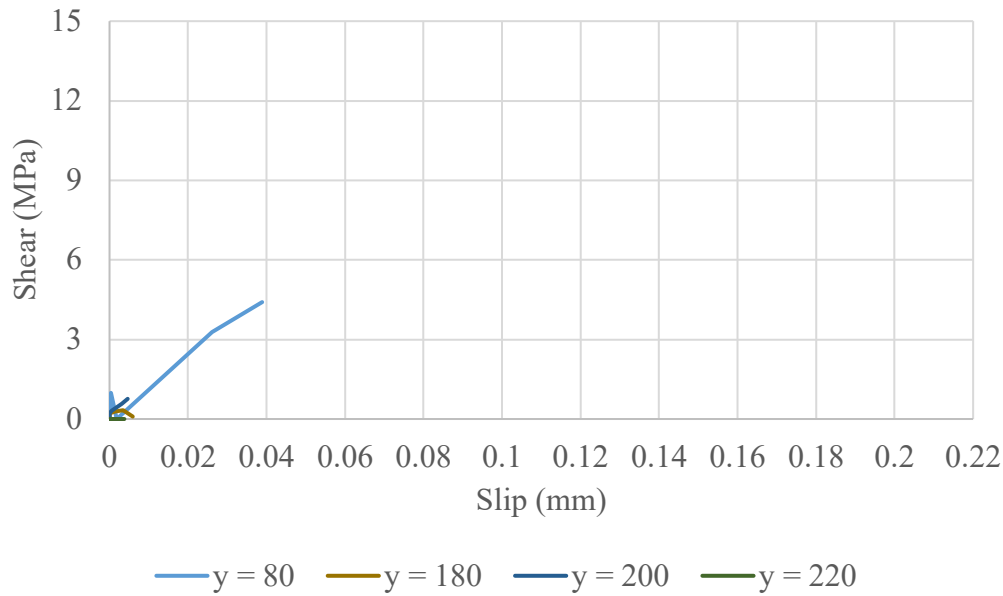


Fig. 5-29. Bond-slip curves at various distances away from the defect regions along the left edge section line (LE) for Specimen VR-VL-2.

The envelope bond-slip curves for the dual specimens, as shown in Fig. 5-30, further corroborate the discussed findings. Among all specimens, the LE of VR-VL-2 demonstrated the highest stresses, particularly in regions with significant stress concentrations. However, this edge exhibited lower slips and reduced initial stiffness compared to the RE. These observations reinforce the critical impact of localized stress redistribution caused by defects, particularly on bond stiffness and deformability.

Specimen VC-VC-2 exhibited the best overall performance, achieving the highest slip values and the largest interfacial energy. This performance highlights the effectiveness of centrally aligned defects with relatively small widths. Such defects caused less critical stress redistribution and allowed the specimen to maintain its load capacity. These findings suggest that properly confined,

centrally aligned defects can mitigate the adverse effects typically associated with imperfections, preserving both stiffness and energy absorption capacity.

In contrast, the bond-slip curves for specimen VR-VR-2 validated the detrimental effects of aligned defects placed along the RE. These defects led to a significant loss of stiffness, delayed the peak bond stress, and resulted in smaller interfacial energy. Additionally, the RE in this specimen failed at smaller slip values compared to the LE. This behaviour indicates that stress distribution along the RE was insufficient to prevent premature peeling of the laminates. Consequently, VR-VR-2 exhibited a critically lower load capacity, highlighting the adverse impact of defect alignment along a single edge.

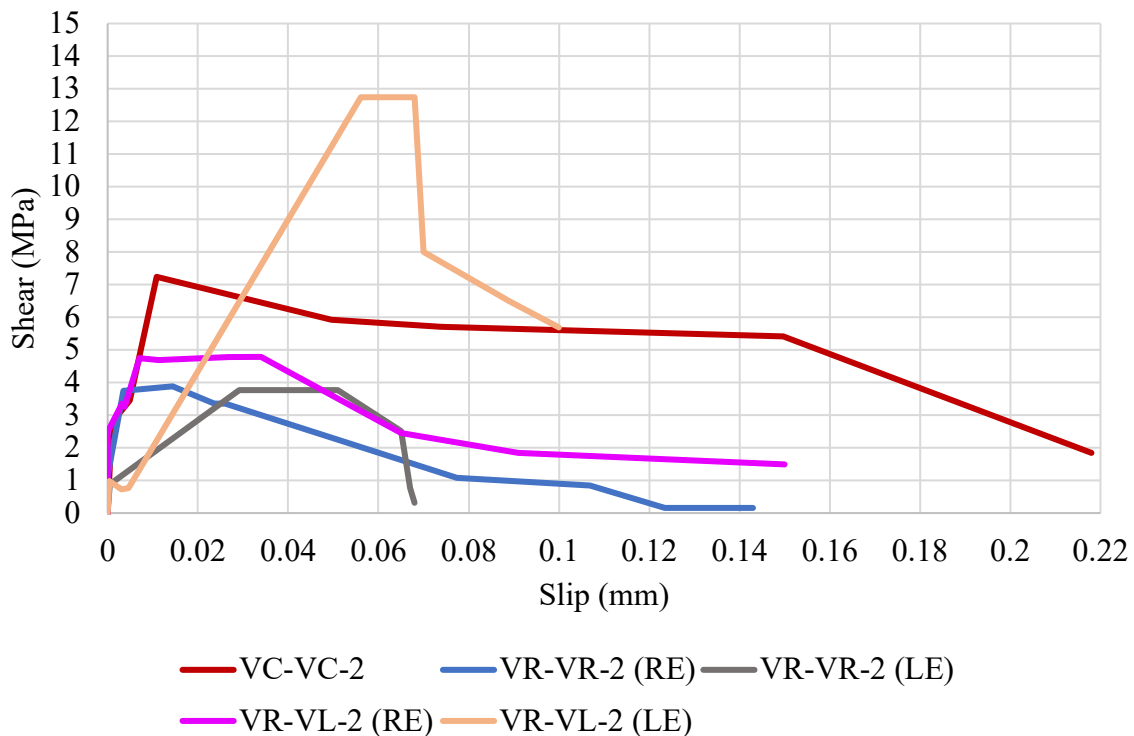


Fig. 5-30. Comparison of experimental bond slip envelope curves for Specimens VC-VC-2, VR-VR-2 (RE & LE), & VR-VL-2 (RE & LE).

5.4.5 Summary

While this study focuses on small-scale, displacement-controlled specimens, the observed behaviours carry relevant implications for real-world EB FRP applications. In field structures, defects may arise in dispersed or interacting forms, and a purely localized assessment may prove insufficient. A hybrid approach—accounting for both local bond–slip mechanisms and global bond integrity—is warranted, especially in zones of high stress transfer such as anchorage regions and crack-prone spans.

As demonstrated in subsections 5.4.2 and 5.4.3, even isolated defects can reduce bond performance when located in or near high-tension zones, such as crack-initiated anchorage regions. In this study, defects positioned near the loaded (or anchorage) end of the bonded joint caused sharper stress concentrations and earlier initiation of slip, consistent with premature softening in local bond–slip behaviour. In contrast, mid-span defects—corresponding to regions initially remote from the critical crack zone—had minimal early-stage influence but were later mobilized as stress redistributed along the bonded length. This behaviour mirrors the findings of Zhou et al. (2017), as well as field observations from structures such as the Champlain Bridge diaphragms (Fowai et al., 2023), where interfacial imperfections commonly occur near anchors or along concrete irregularities.

Importantly, the inclusion of both isolated and combined defect scenarios in this study offers a novel insight into stress redistribution pathways in defective systems. In specimens with middle placed defects, observed load drops likely reflect localized bond rupture and sudden stress rebalancing under displacement-controlled conditions. While such drops may not manifest identically in full-scale force-controlled structures, analogous effects—such as accelerated

displacement or crack propagation—could still arise if anchorage is insufficient. Therefore, sufficient bond redundancy and anchorage robustness are essential in design, particularly in regions where interfacial irregularities may coincide with high stress gradients.

Ultimately, the incorporation of local bond–slip models offers a promising framework to predict such behaviours. However, validation through further testing on large-scale members with representative defects is necessary to fully confirm these mechanisms and their implications in field applications.

5.5. Experimental vs Analytical Bond Slip for Control Specimens

5.5.1 Specimen C-1

The experimental bond-slip curves for Specimen C-1 (Fig. 4-55) demonstrate a brittle debonding failure, characterized by a significant slip of 0.39 mm at failure. The bilinear behaviour observed in most curves reveals an initial increase in stress followed by a stiffness loss and a sudden rise in slip. Dominant locations along the bond length, such as $y = 60, 80, 100,$ and 120 mm, exhibit this progression. However, regions near the loaded end ($y = 0$ to 60 mm) and the free end (beyond $y = 140$ mm) display minimal shear and slip values, making their behaviour less apparent in the figure.

To better examine these regions, Fig. 5-31 presents the bond-slip curves at smaller slip values (up to 0.02 mm where the bond shear attained its peak value) along with the envelope curve. This closer examination shows that locations at the free end (e.g., $y = 200, 220,$ and 240 mm) exhibit a linear response until adhesion is lost, while loaded end locations show early failure under small bond shear and slip values, resembling the bilinear behaviour of middle regions. These

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observations suggest a non-uniform stress distribution potentially influenced by out-of-plane displacement.

At the loaded end, the mixed-mode loading, combining peeling and pulling actions, contrasts with the predominantly pure shear loading at the free end. Furthermore, the observed stiffness loss in certain regions during the initial phase—prior to bond stresses reaching their peak—indicates the presence of micro-cracks. These micro-cracks likely contributed to localized adhesion loss without significantly affecting the overall bond performance.

Post-failure observations support this hypothesis, as cohesive concrete failure and peeling of concrete chunks were perceived at the loaded end. This indicates that tensile stresses induced by mixed-mode loading and out-of-plane behaviour may have exceeded the tensile capacity of the concrete, contributing to localized failure.

Additionally, concrete peeling was also observed near the bottom end of the laminate and may be attributed to localized out-of-plane deformation and negative strains in the laminate, which increase normal stresses at the interface. However, it is important to recognize that such failure is not exclusively driven by bending effects. Even in cases classified as pure shear failure, crack initiation often occurs within the concrete substrate due to complex interfacial stress conditions. Consequently, the observed detachment likely reflects a combined influence of shear and normal stresses, exacerbated by local substrate characteristics and heterogeneity.

Further discussions on the out-of-plane behaviour for specimen C-1 are provided in subsection 5.6.2.

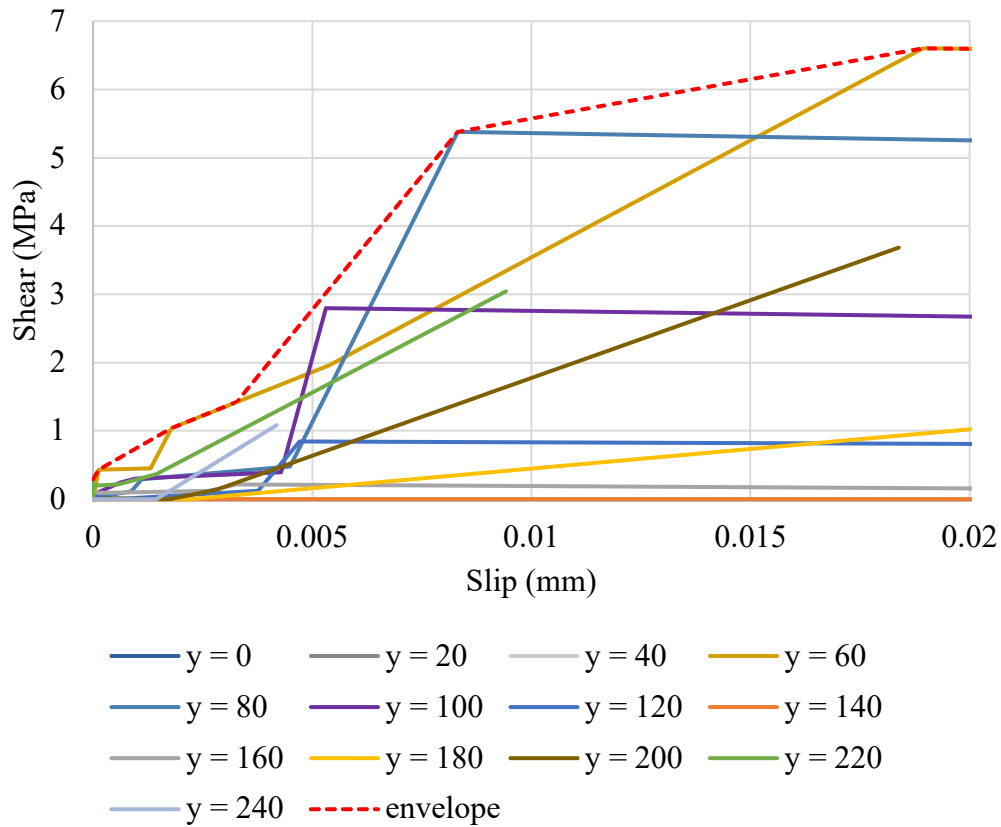


Fig. 5-31. Experimental bond-slip curves for specimen C-1 at smaller slip values with envelope curve.

Mechanistically, the sharp increase in slip likely corresponds to the loss of adhesion/cohesion at the CFRP-concrete interface, but the lack of intermediate data prevents a detailed understanding of the transition.

Regions with zero bond stress ($y = 0, 20, 40, 140$ mm) indicate localized non-uniform stress distributions, likely linked to the shear distribution and out-of-plane rotation (which will be further discussed in Section 5.6).

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The larger slip values observed prior to failure suggest extensive crack propagation and damage accumulation prior to debonding.

The observed stress distribution and resulting bond-slip behaviour are influenced by the ratio of mode I/mode II stresses. At the loaded end, out-of-plane rotation increases normal/peeling stresses, contributing to premature failure.

The methodology provides a reliable representation of the overall bond-slip behaviour but underestimates finer details due to missing intermediate data points. Increasing the frequency of data collection during the test could yield more accurate curves.

Future studies should focus on capturing early and intermediate stages more comprehensively to better understand the transition from small to larger slip values. Additionally, examining out-of-plane effects and stress distributions in greater detail would further clarify the observed behaviour.

The generation of bond-slip curves at various locations enabled the plotting of envelope, average, and minimum bond-slip curves, providing a comprehensive representation of interfacial performance. The average curve served as the basis for calibrating the analytical model. Lu et al.'s (2005) bilinear model was selected for its similar shape, widespread relevance, and simplicity. Although the calibrated model exhibited a higher shear peak and slightly lower initial elastic stiffness compared to the experimental data, it demonstrated strong agreement with the experimental average curve. The total error was 14% for peak stress, 0% for the corresponding slip, 0% for ultimate slip, 0% for interfacial energy. The calibrated coefficients for this specimen were determined as $\alpha_1 = 1.15$, $\alpha_2 = 0.007$, and $\alpha_3 = 0.39$.

5.5.2 Specimen C-2

Similar to specimen C-1, bond-slip models were generated for specimen C-2 using the same methodology. The bond-slip curves for C-2 displayed greater variability and comprehensiveness, capturing more intermediate slip values and demonstrating improved overall performance. Specimen C-2 also exhibited brittle failure, with a lower ultimate slip of 0.19 mm. The envelope, average, and minimum bond-slip curves were computed, and Lu et al.'s (2005) bilinear model was again selected for calibration against the experimental average curve. The calibrated coefficients for C-2 were determined as $\alpha_1 = 0.96$, $\alpha_2 = 0.005$, and $\alpha_3 = 0.11$. The total error comprised 0% for peak stress, 0% for the corresponding slip, 28% for the ultimate slip, and 0% for the interfacial energy. Despite lower elastic stiffness in the initial stage of the bond-slip curve and a reduced ultimate slip, the calibrated model showed strong agreement with the experimental data.

5.5.3 Unified Model

The unified bond-slip model provides a significant advantage by offering a single generalized framework to represent the bond-slip behaviour of defect-free specimens. This approach simplifies the modeling process by avoiding the need for specimen-specific calibrations while maintaining sufficient accuracy, thereby enhancing its practicality as a reference for comparative analyses and broader investigations. Its adaptability suggests potential applicability beyond the scope of this study.

The unified model demonstrates strong agreement with experimental results, as evident from its capacity to replicate the peak shear stresses of specimens C-1 and C-2 while providing a reasonable approximation of ultimate slip and interfacial energy. Despite exhibiting a slightly higher ultimate

slip than specimen C-2 and lower interfacial energy than specimen C-1, these deviations remain within acceptable ranges for practical applications. Fig. 4-65 illustrates its close alignment with the experimental average bond-slip curves of both specimens. The calibration coefficients— $\alpha_1 = 1.06$, $\alpha_2 = 0.006$, and $\alpha_3 = 0.25$ —effectively capture essential behavioural characteristics, including peak shear stress, ultimate slip, and interfacial energy. The model's peak shear stress of 2.9 MPa closely approximates the experimental values of 2.7 MPa for C-1 and 2.6 MPa for C-2, confirming its robustness in predicting stress responses.

The calibration coefficients reflect an effective balance between stiffness and energy dissipation, which are key traits observed in the individual specimens. The higher ultimate slip and interfacial energy exhibited by the unified model suggest its enhanced ability to represent long-term deformability and potential failure mechanisms, critical for assessing structural safety and serviceability.

Nonetheless, averaging calibration coefficients introduces certain compromises. While the unified model achieves general applicability, it may lack precision in capturing specimen-specific behaviours, particularly initial stiffness variations and localized stress redistributions. This limitation highlights the importance of considering the compromise between simplicity and accuracy in model development. Future work should validate the unified model across a wider range of specimens to assess its reliability under varying geometric and material conditions.

5.5.4 Comparison with Defected Specimens

Fig. 5-32 presents the envelope bond-slip curves derived from experimental data for both control specimens (C-1 and C-2) and single defected specimens. The figure illustrates how bond-slip

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behaviour varies at different locations relative to the defect emphasizing the alignment of scattered bond-slip data with the control envelopes. Similarly, Fig. 5-33 depicts the envelope bond-slip curves obtained from experimental data for the control specimens and dual defected specimens. The purpose of these figures is to first, compare the influence of single and dual defects on bond-slip response; and second, to demonstrate how defects disrupt or adhere to established bond-slip patterns observed in control specimens.

It can be observed that while some stress concentrations are evident, particularly at the shear peaks of the single-defect specimens, the data aligns well with the envelope bond-slip curves of C-1 and C-2. Most defected data falls above the curve of C-2 and below that of C-1, with slip values between those of the two control specimens. Specimens VL-2 and HL-2 exhibit the highest stiffness, reaching greater shear peaks than the control specimens at lower slip values, highlighting the early stress concentrations caused by defects positioned at the load end of the bond area.

Specimen VL-2 achieves a similar peak to HL-2 but with slightly greater slip, indicating higher deformability. This observation supports previous findings that partial width-covering defects promote better stress redistribution through their bonded edges, leading to stress concentrations further along the bond length. Although all specimens follow trends similar to those of the control specimens, specimen VM-2 achieves lower shear values in its second stage, emphasizing the detrimental impact of a middle-positioned defect on bond performance. This defect induces reduced capacity, lower interfacial energy, and smaller slip values, which suggest potential early failure. The ultimate load of VM-2 (10.1 kN) is 23% lower than that of C-2 (13.2 kN).

Finally, the lower stiffness observed in both C-1 and VM-1 is evident in their higher deformability compared to C-2 and VM-2. The curves further validate that middle-placed defects reduce both

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shear and slip values during the second stage, likely due to early crack initiation and debonding near the defect. However, the data also suggests that FRP stiffness could play a significant role. While VM-2 achieves a higher peak than C-2 (although with slightly higher slip), VM-1 exhibits a smaller peak, potentially confirming that higher stiffness facilitates better stress distribution, even in the presence of defects.

On the other hand, Fig. 5-33 shows that while specimen VR-VR-2 has a notably lower load capacity (8.9 kN) compared to C-2 (13.2 kN), both specimens exhibit qualitatively similar bond-slip performances, with comparable shear values and interfacial energies. The lower load capacity, despite the similar interfacial shear energy, may be attributed to the defects' impact on the bond area. The presence of defects reduces the available bonding area, and the criticality of their placement could offset the positive contributions of the interfacial energy. Investigating the influence of varying FRP ply configurations could provide further insight into the stiffness characteristics in this context.

In contrast, specimen VC-VC-2's data appear to follow the shape of the bond-slip envelope curve of C-2, but with higher shear values likely due to stress concentrations caused by the defects. Similar to the behaviour of single-defect specimens, VC-VC-2 exhibits a combined behaviour of both load-end and middle-placed defects. This is characterized by high initial bond-slip stiffness, elevated peak shear stress, and increased interfacial energy. The specimen's failure slip is more in line with C-2, similar to VM-2, indicating that while the dual defects affect the stress distribution, they do not significantly reduce deformability at ultimate failure. The response suggests enhanced peak performance due to stress redistribution, which still remains within the general trend of control specimens' behaviour. This highlights the dual defects' complex impact, introducing

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localized stress concentrations while maintaining much of the overall plastic performance observed in the control specimens. This is likely due to the relatively small width of the defects (19 mm, or 38% of the bond width).

Lastly, specimen VR-VL-2 displayed an intriguing behaviour. Although less critical than VR-VR-2, the dual opposite defects induced stress redistribution along both edges opposite to where the defects were placed. As previously discussed in Section 5.2, the redistribution was visually represented in the S-shape of the strain contour map. The right edge (RE), crossing the load-end defect, shows similar early bond-slip behaviour to C-2, while the left edge (LE) reaches a higher bond stress and higher corresponding slip values. However, in the second stage of bond-slip performance, the shear stress in the LE decreases at a steeper rate than the RE. Conversely, the RE (opposite the middle-placed defect) shows higher slip values, reflecting greater deformability. This altered behaviour further validates the findings discussed in this Chapter, which suggest that deformations primarily occur opposite the defect placement. The LE is initially more stretched, but as shear stresses propagate downwards along the laminate, the RE experiences more stretching.

Overall, VR-VL-2 qualitatively showed higher peak stress values, greater slip, and higher interfacial energy. Despite most of the scattered data from the dual defected specimens fitting between the control envelope curves, the bond performance along the RE of VR-VL-2 exhibited unusually high stresses compared to both C-1 and C-2. However, the RE and LE slips were smaller

than C-2, indicating an intriguing shift in the deformability and stress redistribution pattern due to the unique placement of the defects.

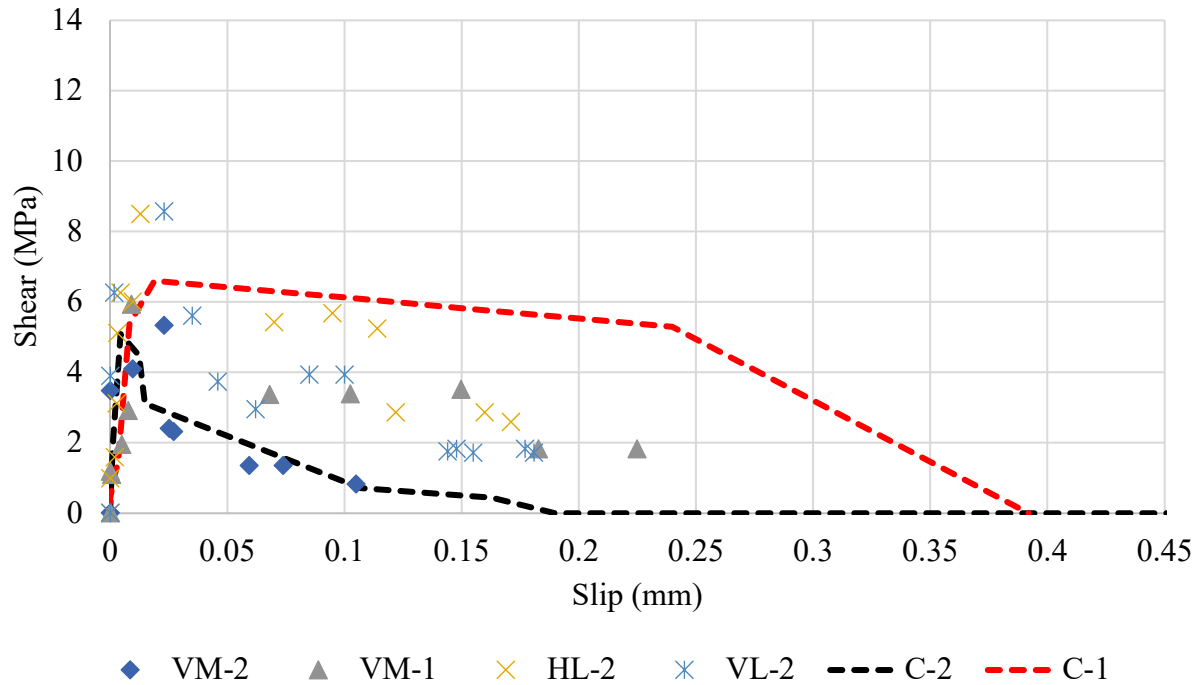


Fig. 5-32. Comparison of envelope bond-slip curves for control specimens (C-1 & C-2) and scattered data of single defect specimens.

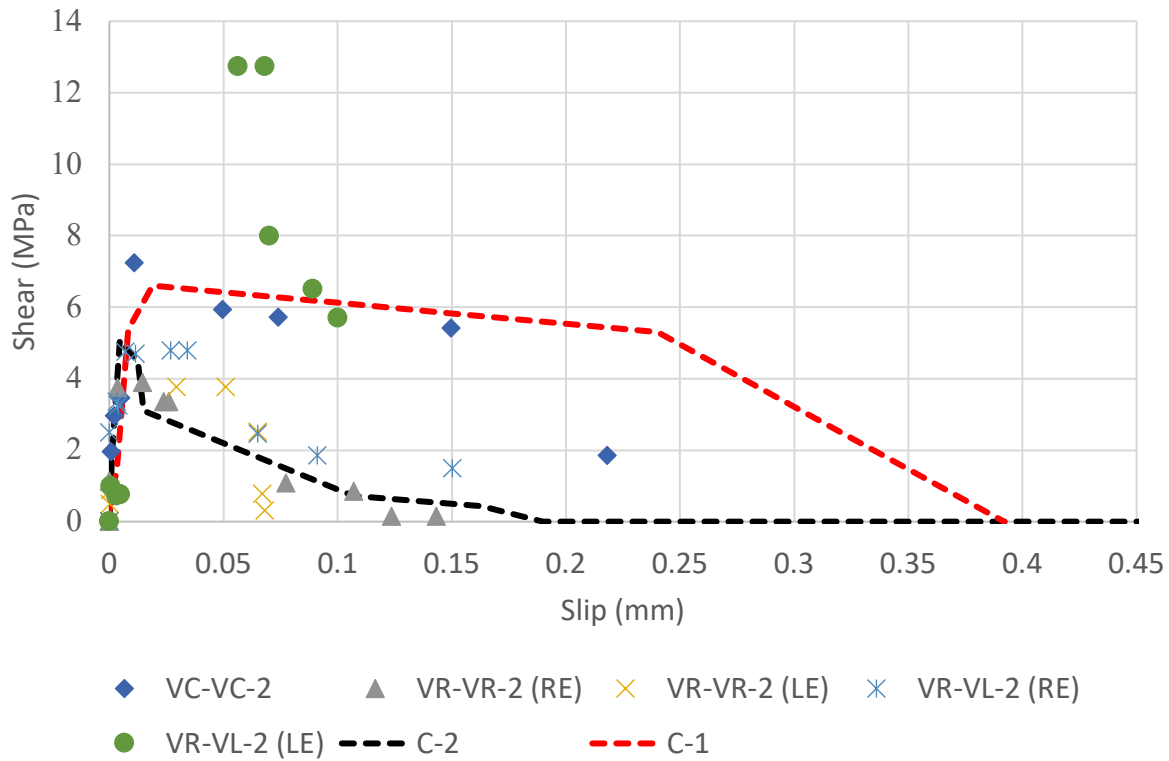


Fig. 5-33. Comparison of envelope bond-slip curves for control specimens (C-1 & C-2) and scattered data of dual defect specimens.

5.6. Out-of-plane Behaviour

5.6.1 Typical Behaviour

Specimens exhibited out-of-plane displacement and rotation during testing, resulting in reduced or negative strains in certain regions, particularly where bending occurred in the unbonded portions of the laminates. The typical out-of-plane behaviour is represented by specimen C-2, whose out-of-plane contour maps at various load stages are shown in Fig. 5-34. These maps reveal bending of the unbonded laminate portions during the initial three load stages as debonding progressed, with out-of-plane displacement values reaching a maximum of 3 mm away from the cameras.

Furthermore, specimen rotation became evident as the applied load increased, corresponding to a displacement of 2 mm.

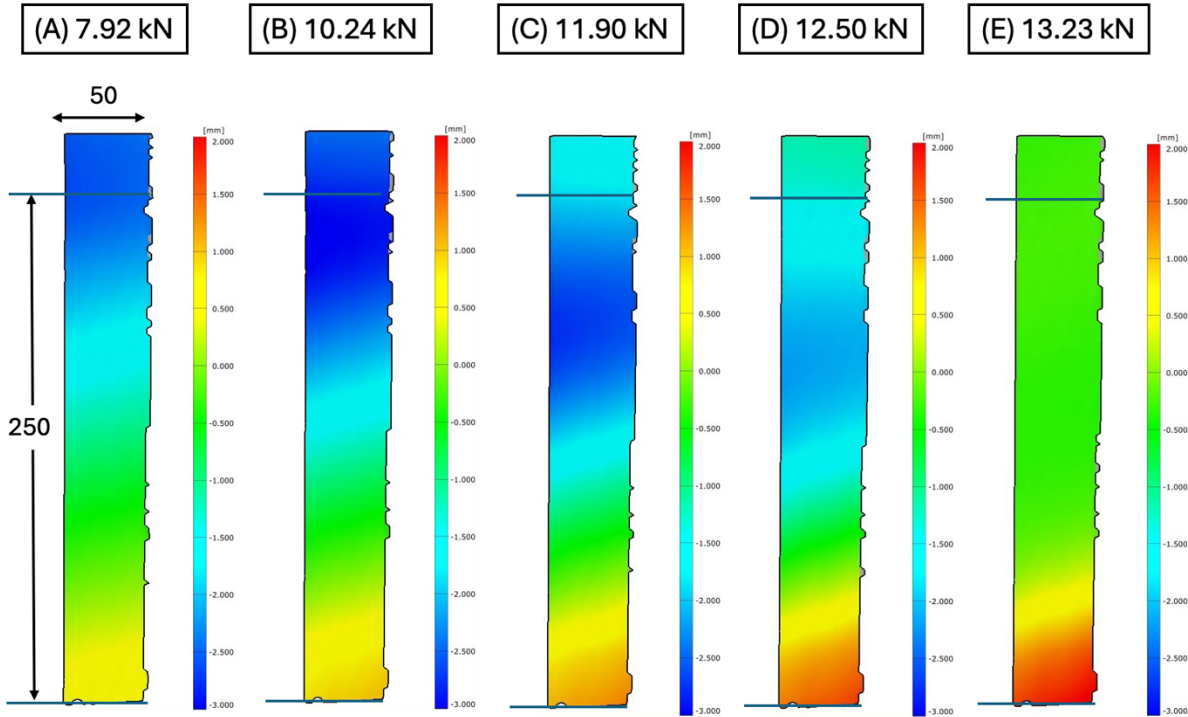


Fig. 5-34. Out-of-plane displacement contour maps at various stages for Specimen C-2.

Typical out-of-plane behaviour can be categorized into three stages. Initially, the specimen is aligned, and a reference image is captured using the DIC software. As the test progresses, out-of-plane rotation occurs, causing the CFRP laminate to begin peeling away from the concrete prism. During this phase, the top part of the concrete block rotates backward, while the bottom part rotates forward towards the camera. Finally, bending of the unbonded portion is observed as the block undergoes further rotation, potentially introducing inaccuracies in the strain readings during analysis. These behaviours are depicted in Fig. 5-35. This behaviour aligns with the findings of Czaderski et al. (2010), describing that the interface between concrete and adhesive under tensile loading is subjected to mixed-mode stresses (a combination of mode I and mode II). These stresses

are primarily induced by bending moments arising from eccentricities present in the tensile load application.

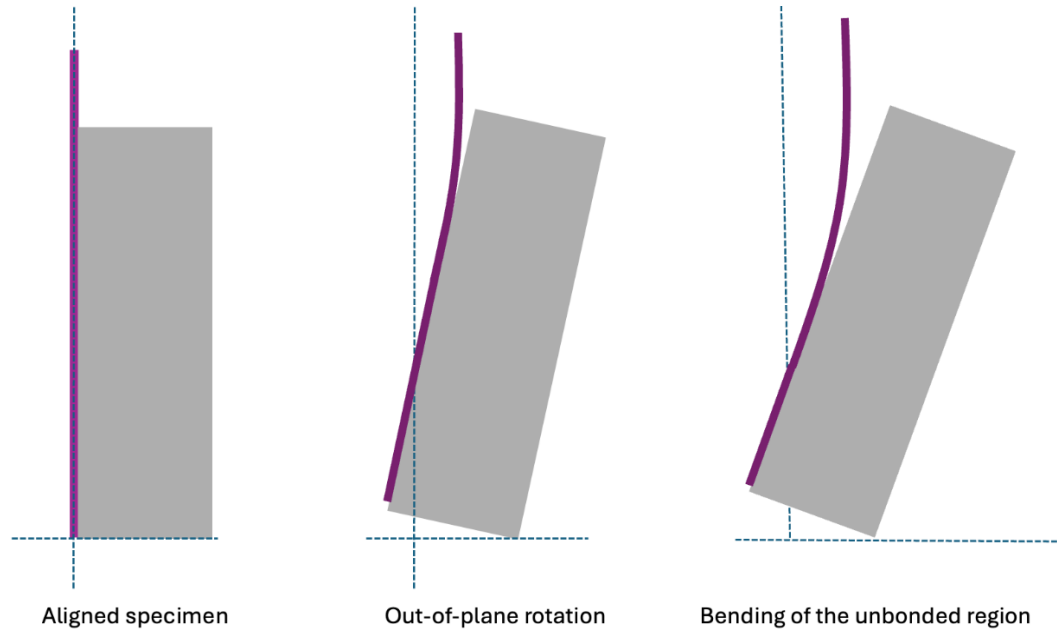


Fig. 5-35. Out-of-plane displacements encountered during testing.

5.6.2 Variation in Behaviour

Compared to the typical behaviour described in subsection 5.6.1, specimen C-1 exhibited a slightly different response. The out-of-plane contour map during the ultimate load stage (approximately 12.7 kN) is illustrated in Fig. 5-36. Although minimal, the displacement of 1.6 mm at the bottom right edge compared to 1.0 mm at the bottom left edge indicates rotation along the specimen's longitudinal axis, resembling a torsional effect. Additionally, eccentric bending was observed in two regions: the unbonded top portion of the laminates and the middle portion of the bond area, where out-of-plane displacement reached approximately 4.7 mm on the right edge compared to 3.6 mm on the left edge.

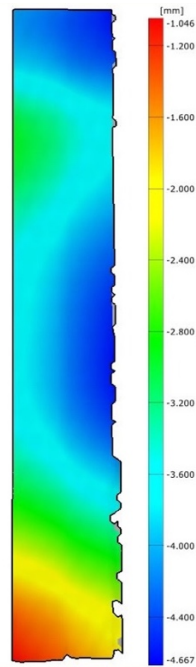


Fig. 5-36. Out-of-plane displacement contour map at the ultimate stage ($P = 12.7$ kN) showing multiple bending regions of the laminates for Specimen C-1.

Similarly, specimen VR-VR-2, where two defects were positioned on the right edge of the bond area, exhibited out-of-plane rotation, with the bottom portion of the block displacing approximately 1.9 mm. Additionally, eccentric bending was observed on the left edge of the laminates, where the displacement reached around 4 mm. The out-of-plane displacement contour maps of specimen VR-VR-2 are shown in Fig. 5-37.

Notably, the VR-VR-2 specimen demonstrated a significantly lower load capacity compared to VC-VC-2, with ultimate loads of 13.9 kN and 8.9 kN, respectively. This observation suggests that defects located on the right edge of the bond area are more critical in reducing load capacity.

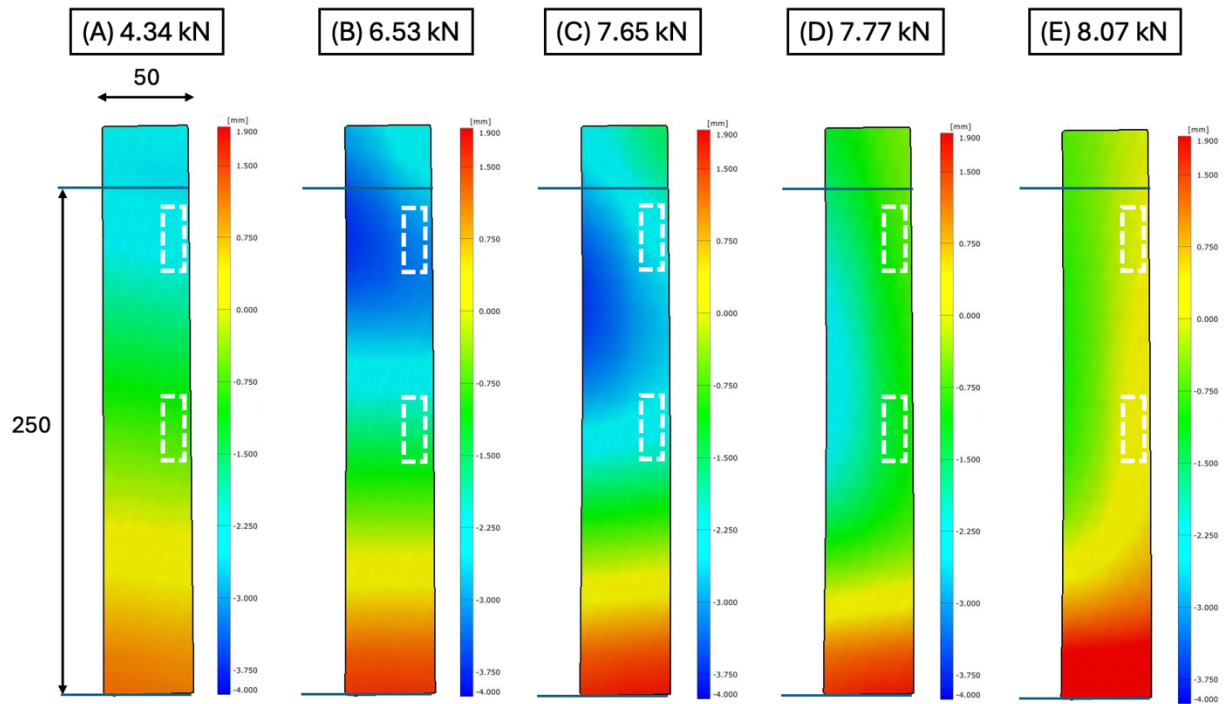


Fig. 5-37. Out-of-plane displacement contour maps at various load stages for Specimen VR-VR-2.

5.6.3 Irregular Data

The data generated for specimen HM-2 was deemed irregular based on the observed strain behaviour. Strain maps and distributions (Figs. 4-17 and 4-18) revealed unusual strain peaks at the bottom of the bonded length, while out-of-plane displacement maps for various loading stages (Fig. 5-38) indicated additional inconsistencies.

Induced bending and eccentricities at the right edge were also observed in the displacement images, further highlighting the non-uniformity in strain and stress distributions. Strain measurements beyond the defect regions, especially towards the middle and right edge, exhibited irregular patterns inconsistent with other specimens.

Moreover, surface imperfections identified on the concrete substrate after surface treatment may have aggravated these issues. These imperfections are discussed further in subsection 5.7.2.

Although the specimen exhibited similar behaviours to those described in subsection 5.6.2—such as eccentric bending along the right edge of the laminate and rotation of the block, both backward and around its axis—the strain data appears to be corrupted.

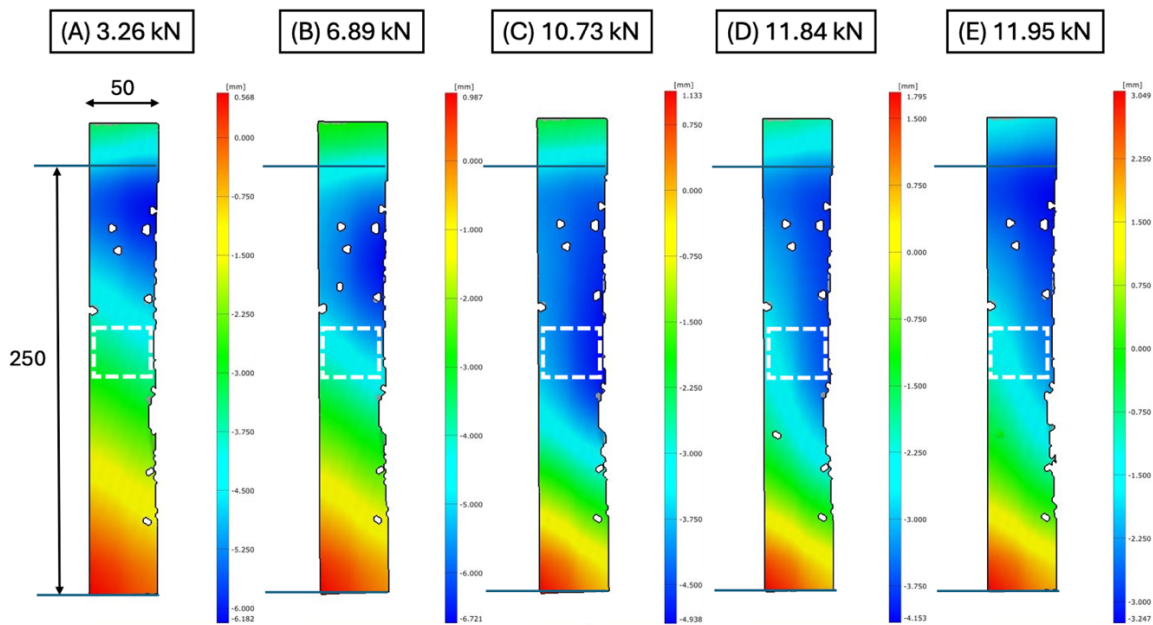


Fig. 5-38. Out-of-plane displacement contour maps at various load stages for Specimen

HM-2.

5.7. Other Limitations

5.7.1 Eccentricities & In-Plane Tilting

In addition to the out-of-plane displacement and rotation of the specimens along their perpendicular axes, a potential limitation was identified in the form of in-plane rotation and tilting of the specimens. This phenomenon, illustrated in Fig. 5-39, demonstrates how the concrete block

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could tilt along its longitudinal axis, introducing additional eccentricities beyond those typically associated with single lap shear testing. The tilting could have been caused by various factors, including an unlevelled top face of the concrete block, misplacement of the specimen, fabric misalignment, or the flexibility of the test setup (such as a loose bolt-to-rod connection that was assumed to be rigid, or the relative displacement of the threaded rods during testing). This phenomenon, referred to as "elbowing," results in an angular pulling of the laminates away from the longitudinal central axis of the bond area. The eccentricities cause the specimen to tilt, leading to an in-plane bending of the laminates at the load-end region. This bending, similar to the shape of an elbow, introduces additional strain gradients: increased tension at the outer edge of the laminates and negative strain at the inner edge, where the laminates are subjected to compression.

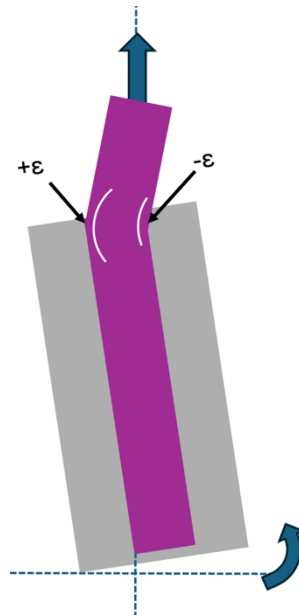


Fig. 5-39. In-plane tilting of the specimen during testing and its effect on strain reading.

An example of eccentric strain distributions is shown for specimen C-2 (Fig. 5-40), where the strain distribution along three different section lines (left, middle, and right edge of the laminates)

is presented. Although the differences are minimal, the graph clearly demonstrates that the strains along the left edge of the sheets are higher than those at the middle section line, which in turn, are greater than those at the right edge section line. For example, at $y = 70$ mm, the strain readings are 0.13%, 0.12%, and 0.11% for the left, middle, and right edge section lines, respectively.

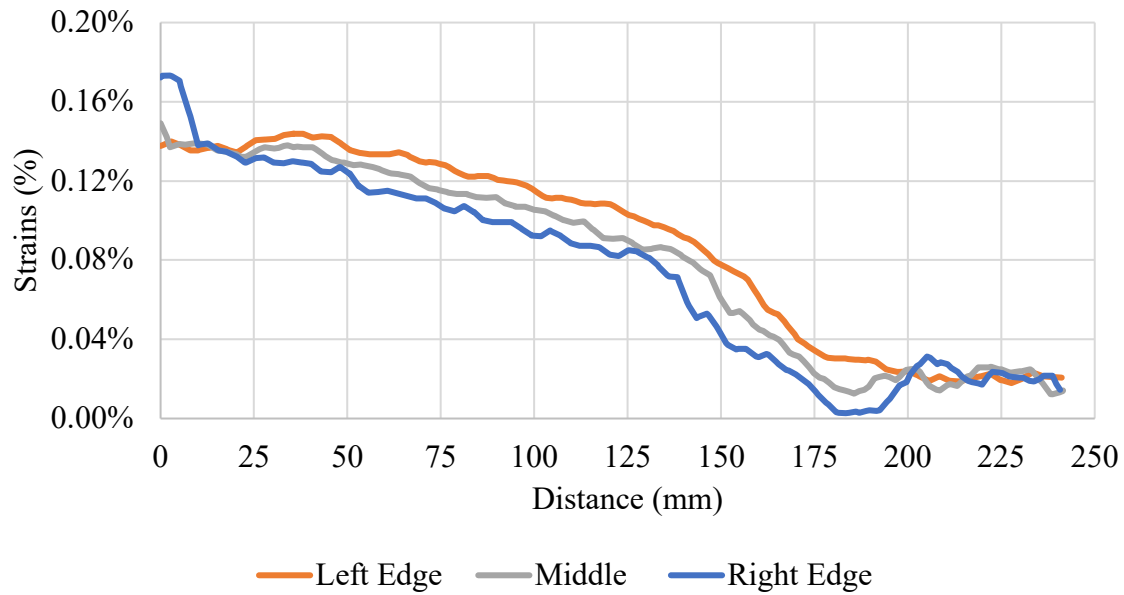


Fig. 5-40. Strain distributions along different section lines of the CFRP for specimen C-2.

5.7.2 Specimen Fabrication & Test Set Up

5.7.2.1 Experimental Matrix

Despite the comprehensive experimental matrix that covered a range of defect placements, orientations, and CFRP ply configurations, each specimen was unique in its setup, resulting in individual outcomes. Testing multiple specimens with the same defect configuration and number of plies would have allowed for more robust conclusions and better capture the variability in the bond performance. While the unique configuration of each specimen provided valuable insights

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into bond behaviour under different imperfections and stiffness conditions, a larger dataset for each specimen (e.g., three specimens per configuration) would have increased the reliability of the results.

The defect sizes selected for this study represent a cumulative bond area ratio of approximately 15.2%, consistent with the methodology of Li et al. (2023), which aimed to simulate localized debonding. The individual defect widths were confined within the laminate width, representing common imperfections such as resin voids or partial debonding. In practice, these would correspond to medium-sized defects (approximately 2 to 25 in²), typically necessitating field remediation through resin injection or laminate replacement. Although these dimensions are more conservative than those often encountered in the field, they provide a controlled baseline for initial evaluation. To enhance generalizability, future studies should broaden the parameter space by incorporating FRP materials of differing stiffness (e.g., CFRP, GFRP, BFRP). Such comparative investigations would clarify how material stiffness and bond compatibility influence defect sensitivity and load transfer behaviour.

Additionally, only five load stages were selected and presented in strain contour maps, strain, shear, and slip distributions to capture the overall progression of the bond parameters. While two additional load stages were computed to represent the load-slip behaviour, only seven data points were used in total. Incorporating more data points would have provided a more comprehensive understanding of the bond slip behaviour throughout the test.

5.7.2.2 Surface Conditions & Substrate Defects

In addition to the induced eccentricities and bending of the laminates, as well as the rotation of the specimens during testing, one critical constraint was the substrate quality. Several blocks, after undergoing shotblasting, exhibited surface imperfections, including cavities and voids. Examples of these anomalies are shown in Fig. 5-41.

Despite adherence to the manufacturer's instructions for surface priming and sheets placement, these surface defects presented a limitation to this study. Specimen HM-2, in particular, exhibited multiple pores and voids across its surface, predominantly within the bond area (Fig. 5-42). Although these imperfections were not quantified, their presence was significant and may have had an impact on the specimen's behaviour during testing.

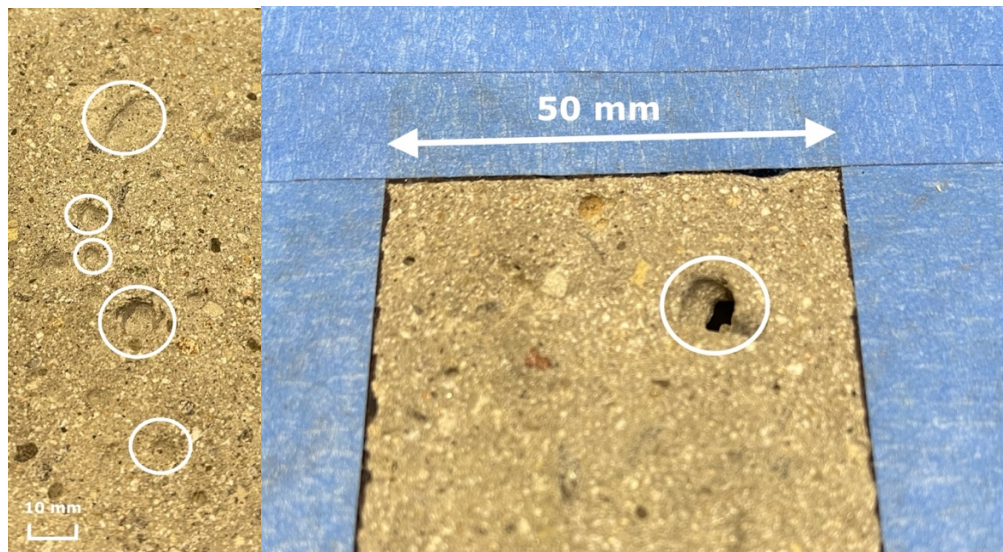


Fig. 5-41. Typical surface imperfections in the bond area.



Fig. 5-42. Surface imperfections in the bond area of specimen HM-2 showing voids and cavities on the concrete substrate.

5.7.2.3 Longitudinal Grooves & Fabric Alignment

Another limitation involves the introduction of longitudinal grooves during the preparation and cutting of the CFRP laminates to the required width. The wet lay-up method, which requires the impregnation and manual placement of dry CFRP fabric, often results in loose threads along the cut edges. These threads tend to unravel and misalign during placement, introducing additional material defects. To mitigate this, the laminates were initially cut to a width greater than required, ensuring that the bond area remained unaffected by loose threads. After curing, the excess material was trimmed to the desired width (50 mm) using a grinder with a thick diamond blade.

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Despite these precautions, the grinding process, necessitated by the material properties of both concrete and CFRP, introduced longitudinal grooves into the concrete substrate (Figs. 5-43 and 5-44). Additionally, the inherent flexibility of the dry fabric made it prone to deformations during placement, potentially causing further misalignments. These factors, though minimized to the extent possible, may have contributed to premature rupture, localized stress concentrations, and additional eccentricities in the test specimens.

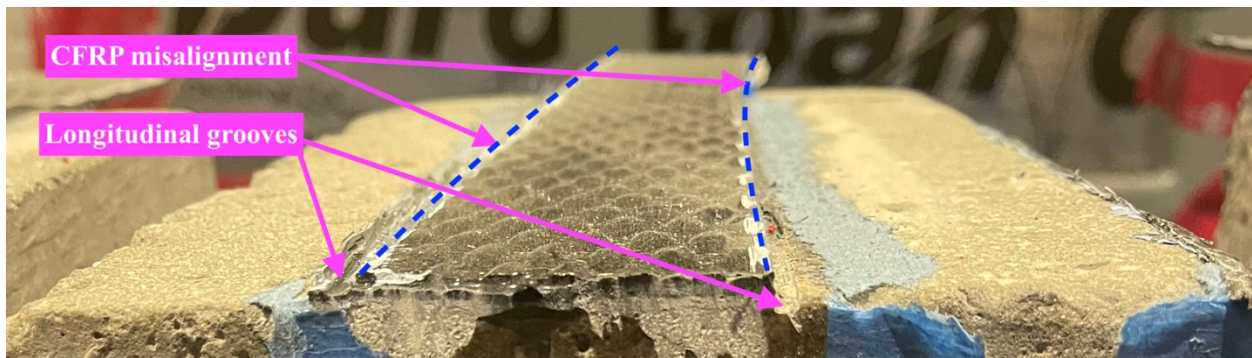


Fig. 5-43. Longitudinal grooves and misalignment of the CFRP sheets in specimen VR-VR-

2.

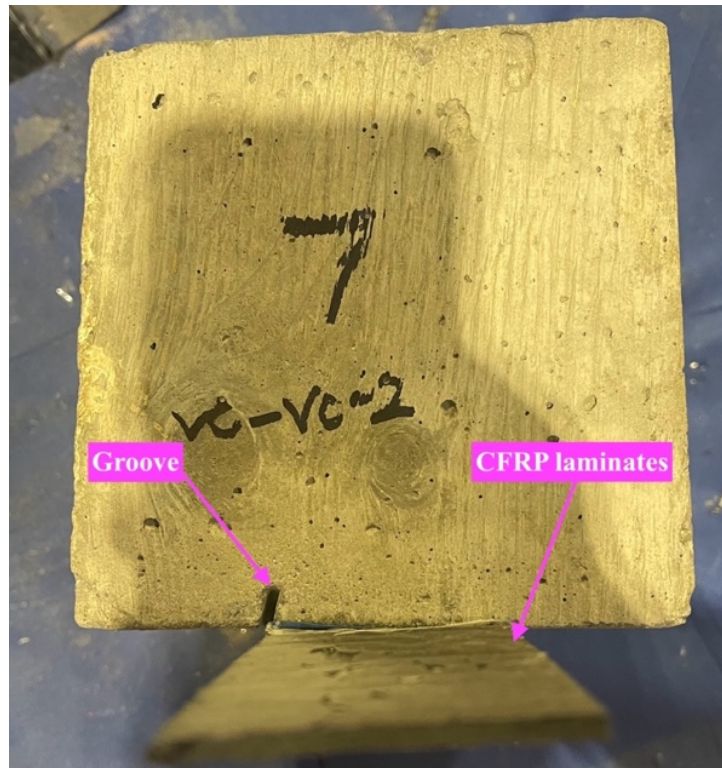


Fig. 5-44. Top view of specimen VC-VC-2 highlighting a deep groove along the left edge of the laminates.

An additional issue arose during the cutting of the width for specimen VR-VL-2. As illustrated in Fig. 5-45, the cut was mistakenly offset by approximately 8 mm, exposing the load-end defect along the right edge of the sheet. This offset reduced the effective bond width, potentially altering the bond behaviour and partially compromising the accurate assessment of the load-end defect's impact.

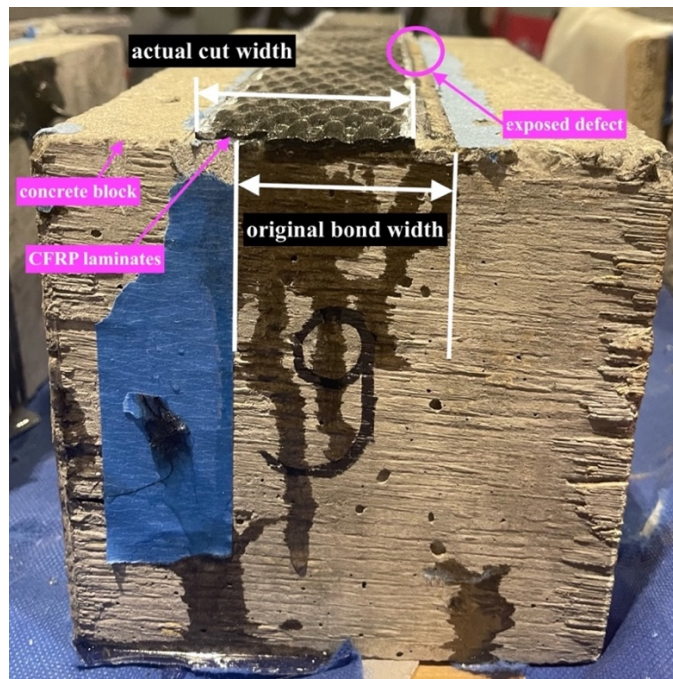


Fig. 5-45. Exposed defect and offset cut in specimen VR-VL-2

5.7.2.4 Test Set Up

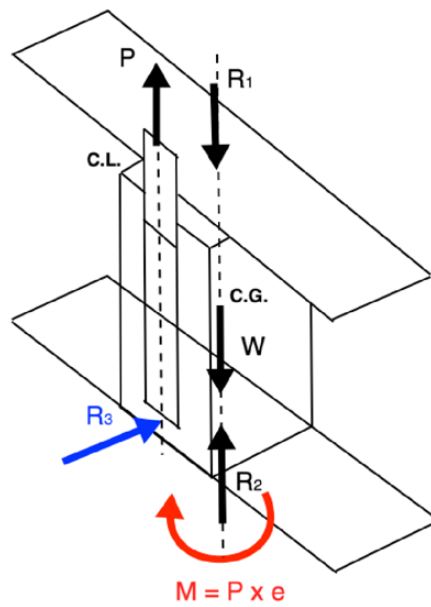


Fig. 5-46. Forces acting on the specimens during the test.

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The test setup effectively secured the specimens after placement, though certain limitations in the setup may have influenced the test results. Despite precautions to ensure proper leveling, alignment, and central positioning of the grip to prevent rotation, factors such as uneven top surfaces, insufficient rod fixation, and slight displacement could have introduced out-of-plane behaviour and minor misalignments. As stated by Serbescu et al. (2013), specimen's alignment is difficult to achieve as the block is prone to several eccentricities which may weaken the bond and cause premature failure. Further, the authors also stated that the blocks are generally very sensitive to be handled and are therefore prone to misalignment issues during placement (Serbescu et al. 2013).

As shown in Fig. 5-46, the forces acting on the specimens during testing involved vertical resistance from both the top and bottom plates, while the block's self-weight and eccentric center of gravity induced a moment at the bottom end, causing rotational tendencies. The bolts and rods, intended to provide rigid fixation, may have been too thin or insufficiently secured, potentially allowing small displacements that affected specimen stability. Additionally, the top plate, designed to minimize interference with the laminates, only partially covered the concrete block, which may have further contributed to instability.

While the uneven top surfaces of the concrete blocks posed a challenge, a high-strength neoprene sheet was used to improve stress distribution. Despite these efforts, surface irregularities could still have affected alignment to some degree.

Lastly, while efforts were made to restrain the concrete blocks from rotating toward the cameras, slight vertical displacement occurred due to the looseness of the bolts and rods, allowing the block to rotate backward during testing as the CFRP sheet peeled away. A more rigid test set up could

have been used to include rotational restraint at the base of the concrete block, as illustrated in Fig 47.

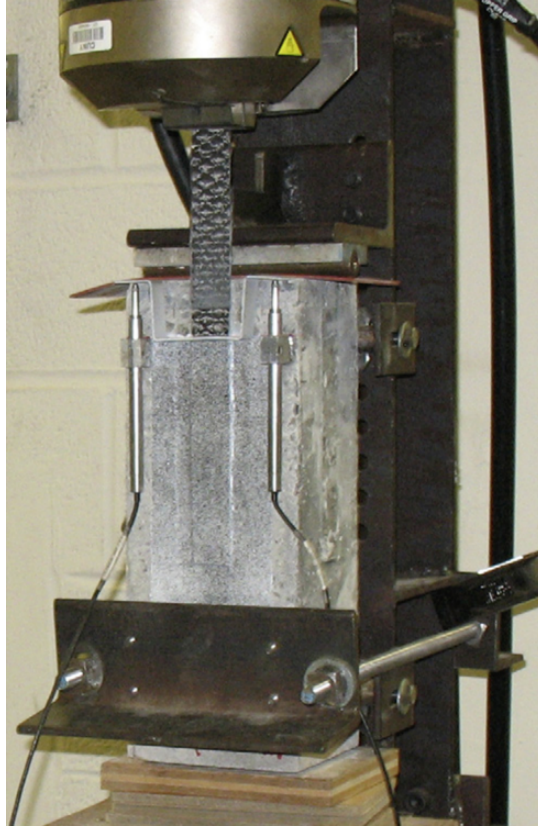


Fig. 5-47. Enhanced shear lap test setup with rotational restraints to prevent forward block rotation during testing (Carloni et al. 2012).

Although these factors could have influenced the bond behaviour, the data still provide valuable insights into the bond-slip relationship. Future studies with refined setup conditions could confirm these findings and address the limitations identified in this study.

5.7.3 Material Limitations

This study utilized high-strength concrete prisms. The literature presents conflicting findings on the influence of high-strength concrete on the bond performance of EB FRPs, particularly due to its brittleness and reduced strain capacity (Garcez et al. 2021; Issa et al. 2016), as well as its diminished impact on fracture energy, strain gradients, and bond strength (Shrestha et al. 2015). Additionally, higher compressive strength can reduce bond interlock and alter the debonding mode to cohesive adhesive failure (Shrestha et al. 2015), which may limit the findings of this study.

Fig. 5-48 illustrates a typical shear strain distribution from a numerical simulation of a shear lap test, showing that as the FRP laminate is subjected to tension, interfacial shear stresses transfer from the FRP-adhesive interface into the epoxy and deeper into the concrete substrate. Generally, debonding occurs within the substrate due to its comparatively weaker properties relative to the adhesive and FRP laminate (Verstrynge et al. 2016). However, the high-strength concrete used in this study may have influenced the debonding mechanism, shifting failure from cohesive shearing of the concrete to a mixed-mode interfacial fracture, where the adhesive ruptured across multiple regions of the bonded area.

Moreover, poor substrate conditions in some specimens (discussed in subsection 5.7.2.2) likely contributed to bond failure and reduced overall capacity. This aligns with findings by Camli and Binici (2007), who reported lower bond strength in substrates with weak surfaces and discontinuities, and Verstrynge et al. (2016), who observed that poor surface conditions promote interfacial debonding. Such deficiencies may have shifted the failure mode to the interface rather than the substrate, leading to premature failure.

The adhesive used had a reported tensile strength of 36 MPa and an elastic modulus of 2.22 GPa. Despite adequate curing conditions, manual application may have resulted in excess resin or uneven distribution, potentially affecting bond performance. The adhesive's mechanical properties may have also played a role—a more flexible resin might have mitigated stress concentrations caused by poor substrate conditions and interfacial imperfections, allowing for more uniform bond stress transfer and improved capacity.

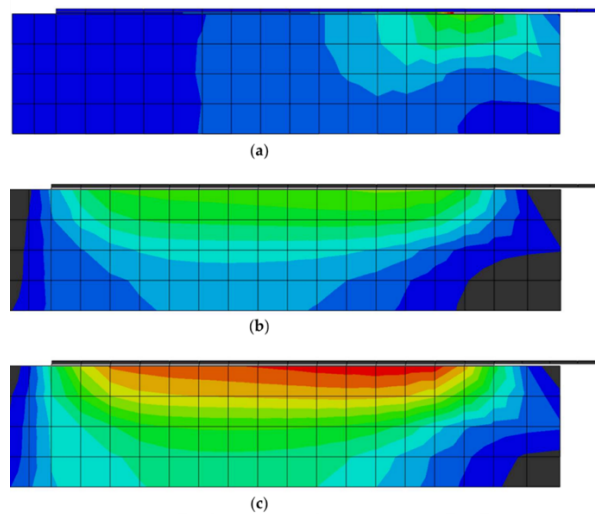


Fig. 5-48. Typical shear strain distribution from a numerical study during a shear lap test (Kwiecień et al. 2018).

5.7.4 DIC Challenges

3D DIC acquisition was employed alongside UTM readings to correlate strain data and load values at different stages of the test. However, since these readings were obtained separately, the manual correlation of data by matching times may have introduced errors.

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The accuracy of speckle tracking is critical for the reliability of strain measurements during testing. Fig. 5-49 presents a reference image taken prior to DIC acquisition for a specimen, illustrating multiple voids, primarily along the longitudinal grooves, where the painted speckles were difficult to capture. Additionally, underexposed areas across the exposed surface were noted. Surface irregularities, including longitudinal edge grooves, voids, and noisy regions, combined with sun exposure in the laboratory, led to poor speckle coverage in some areas, disrupting the uniformity of the speckle pattern. These imperfections introduced additional challenges in the analysis, reducing the reliability of strain evaluation in regions where portions of the surface were unreadable.

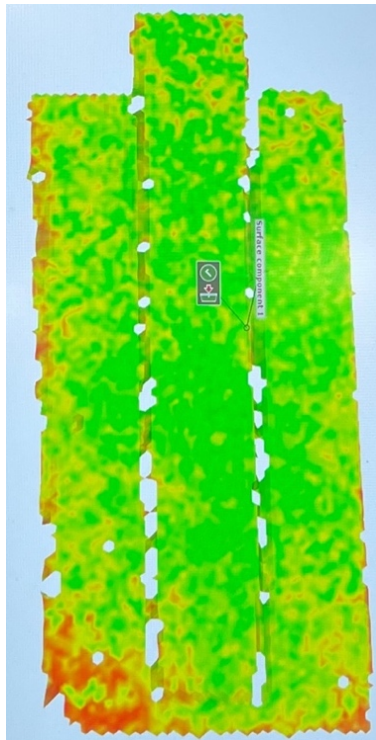


Fig. 5-49. Reference image from the DIC software illustrating the effect of longitudinal edge grooves, voids, and noisy regions.

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Furthermore, noise was minimized through the application of multiple filters in the DIC software and by adjusting strain distribution intervals during shear calculations. However, inconsistent strain gradients in certain regions led to inaccuracies in the observation of bond strength progression. In an effort to refine the strain and shear distribution presented in Chapter 4, a 20 mm interval was employed to smooth out the data and minimize noise. However, to assess the impact of the selected distance interval on the strain and shear distribution, a finer 10 mm interval was applied to specimen HL-2, resulting in slightly different distributions. The strain and shear distributions at both 10 mm and 20 mm intervals are compared in Fig. 5-50.

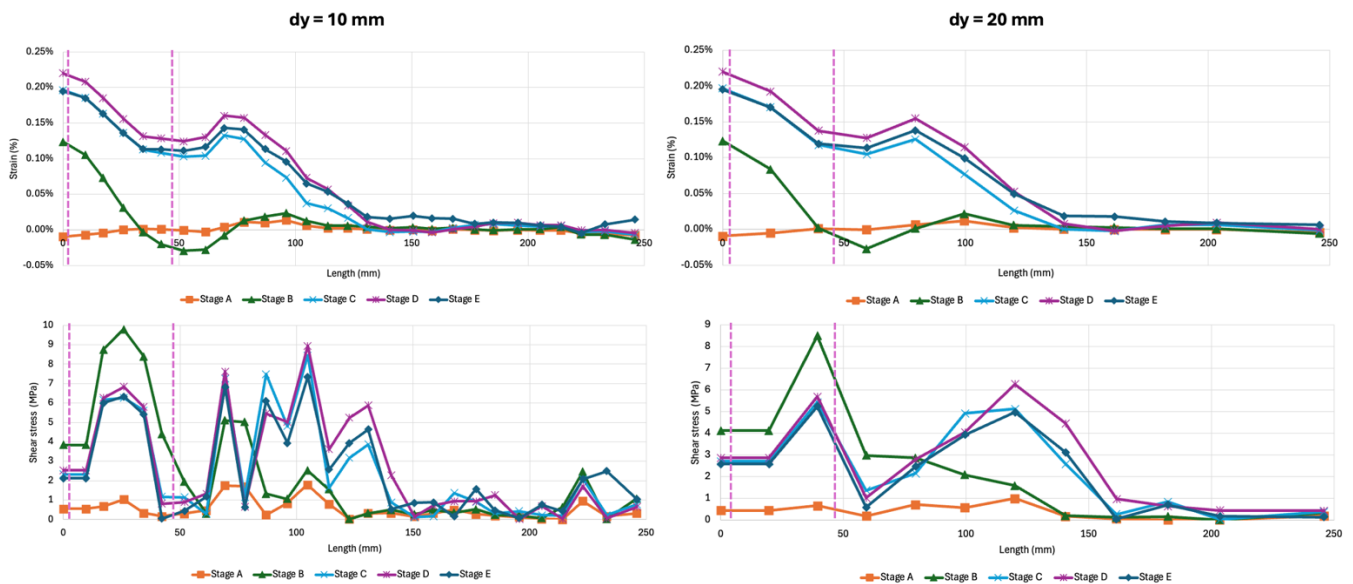


Fig. 5-50. Comparison of strain (top) and shear (bottom) distributions for specimen HL-2 using 10 mm (left) and 20 mm (right) distance increments.

The analysis revealed that using the 10 mm interval produced a more granular distribution, capturing finer details of the bond slip behaviour due to the increased number of data points (twice as many). In contrast, the 20 mm interval resulted in a smoother representation, particularly in

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regions exhibiting less strain variation. While both interval distributions displayed the same overall behaviour—where bond stress peaks occur near the defect—the 10 mm interval led to a more progressive increase in shear near the defect. Here, the bond stresses peaked in the middle of the defect zone, rather than at its end, and the minimal stress value was attained before the end of the defect region for the first four load stages. After this, shear increased again with multiple peaks (hence the granular distribution). In contrast, the 20 mm interval caused the shear distribution to continue dropping beyond the defect region, reaching its minimum at around $y = 60$ mm, before rising again to a second singular peak.

These differences highlight the trade-off between achieving higher resolution and maintaining a smoother, less noisy dataset. The comparison of these two approaches underscores the limitations inherent in the choice of interval and emphasizes the importance of careful consideration when selecting the optimal interval for shear and strain distribution analysis.

5.8. Relevance to Field Application & Future Work

In summary, the artificial defects introduced in this study – such as partial bond interruptions and misaligned regions – were simplified yet representative interfacial imperfections reflecting field-observed issues like voids, zones of inhibited adhesion, or substrate conditions. These were introduced in a controlled manner to enable reproducible parametric investigation. While such idealized imperfections do not capture the full stochastic nature of deterioration and delamination found in the field – such as in the Champlain Bridge, for example – they provide a foundational approximation to study localized bond behaviour.

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As observed in Section 5.4.3, the position of a defect – particularly near the loaded end – can significantly influence bond performance, echoing field-reported anchorage vulnerabilities in structures such as the Champlain Bridge diaphragms. These findings reinforce the need for defect-tolerant design strategies in critical load-transfer regions.

Accordingly, future work should expand toward field-validated or randomly distributed defects to enhance real-world applicability. Despite the small scale and limited bond lengths of the specimens, the governing failure mechanisms remain rooted in local bond–slip interactions, thereby informing broader interpretations of defect tolerance and failure progression in larger-scale FRP repair applications.

Nevertheless, direct extrapolation of these findings to real structures should be approached with caution. Several uncertainties remain—namely, the variability of in-situ substrate conditions, the irregular morphology and spatial distribution of field defects, and environmental influences such as moisture conditions or temperature fluctuations. To enable broader applicability, future research should focus on: (i) capturing the statistical nature of field defects, (ii) incorporating variable substrate and environmental parameters into experimental and analytical frameworks, and (iii) validating these behaviours through larger-scale or field-correlated testing. These steps would progressively refine the predictive accuracy of defect-sensitive models and support the development of design-oriented tools for practical implementation.

CHAPTER 6. CONCLUSIONS & RECOMMENDATIONS

The present study investigated the bond behaviour of ten high-strength concrete prisms externally strengthened with CFRP laminates under various ply and defect conditions through destructive SLST and 3D DIC analysis. The experimental program provided valuable insights into strain contour and linear distribution, shear and slip distribution along the bond length of the specimens, as well as load-slip and bond-slip behaviour. Key findings are summarized as follows:

- Specimens primarily failed due to adhesive cohesive failure in a mixed-mode rupture, with stress concentrations near defects leading to localized peeling and bond deterioration.
- Reduced axial stiffness decreased load capacity, increased deformability, and caused uneven stress distribution, further promoting localized peeling and reducing system efficiency.
- Experimental bond-slip curves were derived for control specimens, and the bilinear model by Lu et al. (2005) was calibrated into a unified model with strong agreement to experimental results. Future work should refine calibration with specimen-specific coefficients and environmental factors for improved predictive accuracy.
- Calibration of Li et al.'s (2023) load drop model reduced mean absolute percentage error (MAPE) from 45.1% to 13.5%, demonstrating improved robustness and applicability across different datasets.
- Localized strain reductions occurred near defects, with some specimens exhibiting negative strains indicative of bending or mode shifting, reflecting complex stress states.

Chapter 6. Conclusions & Recommendations

- Loaded-end defects weakened stiffness locally, increasing slip around the defect region. Full-width defects had limited stress redistribution capacity, while middle-positioned defects caused proportional load drops, potentially triggering localized debonding.
- Edge-aligned defects reduced load capacity and created stress concentrations on the opposite side of the bond area. Specimen VR-VR-2 exhibited a 33% reduction in load capacity compared to the control specimen C-2.
- Combined dual-defect configurations caused cumulative effects, reducing initial stiffness, producing intermediate load drops, and demonstrating potential for partial recovery of bond capacity – depending on residual bonded length and stress redistribution.
- The observations – though derived from controlled laboratory conditions – highlight the importance of hybrid assessment approaches that combine local bond-slip behaviour with global integrity metrics. These are especially critical in field applications, such as beam or diaphragm strengthening, where anchorage, surface defects, or interfacial delamination (e.g., as seen in the Champlain Bridge) pose reliability risks.
- Further research is needed to develop comprehensive defect typologies reflecting field randomness and variability, including studies on misalignment, surface degradation, or out-of-plane displacements. Enhanced setups with improved stiffness and DIC optimization are recommended to capture dynamic debonding events with higher fidelity.
- Broader experimental investigations incorporating variations in laminate type and stiffness, as well as adhesive properties, and substrate strength are warranted to generalize findings and refine predictive models.

Ultimately, this study advances the understanding of CFRP-concrete bonding, offering a solid framework for future research and practical applications.

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APPENDIX A: Debonding Strain Design Equations from International Codes

This appendix presents several equations for calculating debonding strain in accordance with international standards. Among the most prominent North American guidelines for the design of externally strengthened concrete structures with CFRP are the American ACI 440.2R (ACI 2023), the Canadian CSA S806 (CSA 2012), and the CAN/CSA S6 (CSA 2019), also known as the Canadian Highway Bridge Design Code (CHBDC). The debonding strain prescribed by these codes is expressed in Eq. A-1. Notably, the ACI 440.2R and CAN/CSA S806 standards specify an upper bound limit for the design debonding strain of 0.007. Conversely, the CHBDC imposes a stricter upper bound limit to 0.006, reflecting its emphasis on bridge safety and serviceability considerations.

$$\varepsilon_d = 0.41 \sqrt{\frac{f'_c}{n_f E_f t_f}} \quad \text{Eq. A-1}$$

Similarly, the equation proposed by the Chinese Strengthening Report (CECS 2003) is presented in Eq. A-2

$$\varepsilon_d = \min \left\{ \begin{array}{l} 1.16 - \frac{n_f E_f t_f}{308,000} \\ \frac{2}{3} \varepsilon_{cu} \\ 0.01 \end{array} \right. \quad \text{Eq. A-2}$$

The Japanese code (JSCE 2001) proposes a different equation, as shown in Eq. A-3.

$$\varepsilon_d = \sqrt{\frac{2G_f}{n_f E_f t_f}} \quad \text{Eq. A-3}$$

The Italian strengthening code, CNR DT200 (NATIONAL RESEARCH COUNCIL 2006), specifies a different equation, as expressed in Eq. A-4.

$$\varepsilon_d = 0.484 \sqrt{\frac{\beta_w \sqrt{f_c f_t}}{n_f E_f t_f}} \quad \text{Eq. A-4}$$

The Swiss fib bulletin report (fib 2001) also suggests a different design equation (Eq. A-5).

$$\varepsilon_d = \frac{k_{cr} k_k k_b}{\gamma_{fd}} \sqrt{\frac{2}{E_f t_f}} f_t^{2/3} \quad \text{Eq. A-5}$$

Lastly, the British TR 55 report (Arya et al. 2002) proposes a similar but different equation, which is depicted in Eq. A-6.

$$\varepsilon_d = 0.52 \beta_w \sqrt{\frac{f_t}{E_f t_f}} \quad \text{Eq. A-6}$$

APPENDIX B: Effective Bond Length Models

The effective bond length (EBL) plays a critical role in the performance and design of CFRP-strengthened concrete structures and has been extensively investigated by researchers. Numerous models for predicting the EBL are available in the literature and be categorized into six distinct types, which are summarized in this appendix.

6.1. Type 1

Type 1 model, expressed in Eq. B-1, predicts the EBL as a function of the FRP stiffness and the concrete tensile capacity. The parameters a and b in the equation represent empirical coefficients calibrated by various researchers. The specific values of a and b used in the literature are summarized in Table B-1.

$$L_e = a \sqrt{\frac{E_f t_f}{b \times f_t}} \quad \text{Eq. B-1}$$

Table B-1. Empirical Coefficients a and b for type 1 EBL model.

Reference	a	b	Notes
fib 14 (2001) Neubauer and Rostasy (1997)	1	2	-
TR 55 – Arya et al. (2002)	0.7	1	≥ 500 mm
Niedermeier (2000)	1	4	-

6.2. Type 2

Type 2 model, proposed by Chen and Teng (2001) and presented in Eq. B-2, predicts the EBL as a function of the stiffness of FRP and the concrete compressive strength. This model is widely cited in the literature and has been adopted in both the ACI 440.2 and CAN/CSA S806 standards.

$$L_e = \sqrt{\frac{nE_f t_f}{\sqrt{f'_c}}} \quad \text{Eq. B-2}$$

6.3. Type 3

Type 3 model, expressed in Eq. B-3, predicts the EBL as a function of the FRP stiffness alone, without incorporating the concrete tensile capacity or the concrete compressive strength as in previous model types. This model uses two empirical coefficients (c and d) to account for variations in material properties and experimental conditions. The values of c and d from various references are provided in Table B-2.

$$L_e = c \times (E_f t_f)^d \quad \text{Eq. B-3}$$

Table B-2. Empirical Coefficients c and d for type 3 EBL model.

Reference	c	d	Notes
CAN/CSA S6 (2019)	0.5	0.5	≥ 300 mm
Sato et al. (2001)	1.89	0.4	-
ISO model – JCI (2003)	0.125	0.57	-

6.4. Type 4

Similar to type 3, type 4 model proposes an EBL prediction as a function of FRP stiffness and concrete compressive capacity and is expressed in Eq. B-4. Different values for the empirical coefficients (g and h) are presented in Table B-3.

$$L_e = e \times \frac{(E_f t_f)^g}{f_c'^h} \quad \text{Eq. B-4}$$

Table B-3. Empirical Coefficients e, g and h for type 4 EBL model.

Reference	e	g	h	Notes
Kanakubo et al. (2003)	0.7	0.5	0.1	-
Wu et al. (2009)	0.395	0.54	0.09	-
Zhou (2009)	1.6841	0.5	0.333	-

6.5. Type 5

Type 5 model, proposed by Maeda et al. (1997) and Khalifa et al. (1998) is presented in Eq. B-5.

This model predicts the EBL as a function of the FRP stiffness alone, similar to type 3.

$$L_e = e^{6.13 - 0.58 \times \ln(E_f t_f)} \quad \text{Eq. B-5}$$

6.6. Type 6

The last type of EBL models is proposed by Yang et al. (2001) and is presented in Eq. B-6. This model suggests a constant value of 100 mm irrespective of materials properties. This approach assumes that the bond length remains unchanged regardless of the variations in FRP stiffness or concrete properties.

$$L_e = 100 \text{ mm}$$

Eq. B-6

APPENDIX C: Bond Length Factors

To account for the shorter length of EB FRP concrete joints relative to the EBL (also noted L_e) in their load capacity, multiple formulae for the length factor have been introduced by standards and guidelines. One of these formulae is proposed by Chen and Teng (2001) and is expressed in Eq. C-1.

$$\beta_l = \sin\left(\frac{\pi L}{2L_e}\right) \quad \text{Eq. C-1}$$

Another equation used by researchers like Zhou (2009) and Niedermeier (2000) as well as some guidelines like the Swiss fib Bulletin 14 (2001) and Italian code CNR DT200 (2013) is presented in Eq. C-2.

$$\beta_l = \frac{L}{L_e} \left(2 - \frac{L}{L_e}\right) \quad \text{Eq. C-2}$$

Similarly, Lu et al. (2005) and Al Roussan et al. (2013) have proposed a different equation, expressed in Eq. C-3, relative to the ratio of the FRP bonded length (L_f) to the concrete prism longitudinal length (L_c).

$$\beta_l = \sqrt{\frac{2.25 - \frac{L_f}{L_c}}{1.25 + \frac{L_f}{L_c}}} \quad \text{Eq. C-3}$$

Another type was proposed by Wu and Jiang (2013) in Eqs. C-4 and C-5, where Eq. C-6 expresses the β coefficient (Yun 2011).

$$\beta_l = \frac{\eta \sqrt{1 - \eta^2} \sinh \left(\sqrt{1 - \eta^2} \left(\frac{L}{\beta} \right) \right)}{1 + \eta \times \cosh \left(\sqrt{1 - \eta^2} \left(\frac{L}{\beta} \right) \right)} \quad \text{Eq. C-4}$$

$$\eta = -3.61e^{-0.4454 \frac{L}{\beta}} + 4.11e^{-0.3835 \frac{L}{\beta}} \quad \text{Eq. C-5}$$

$$\beta = 169.46 f_c'^{-1.0302} (E_f t_f)^{0.3231} \quad \text{Eq. C-6}$$

The ISO model (JCI 2003) proposes a simpler equation, expressed in Eq. C-7.

$$\beta_l = \frac{L}{L_e} \quad \text{Eq. C-7}$$

Lastly, another type is proposed by Wu et al. (2009) and is presented in Eq. C-8.

$$\beta_l = \sqrt{\frac{L}{L_e}} \text{ or } \left(\frac{L}{L_e} \right)^{1.2} \quad \text{Eq. C-8}$$