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Advanced Fatigue Damage Analysis of Adhesive Bonded Joints

Faculty of Engineering and Applied Sciences
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Academic Year: 2019 - 2025

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This thesis is submitted in partial fulfilment of the requirements for
the degree of PhD

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Abstract

This thesis presents an investigation of advanced fatigue damage analysis in adhesively bonded structures through proposing a novel local-strain-based method for predicting crack propagation and damage evolution. Unlike the common cohesive model-based method for calculating damage increments according to Paris law, the proposed local-strain-based method focuses on defining damage by analysing the strain field in the local fracture process zone (FPZ). This approach challenges traditional research methods that rely on Paris law, like those commonly used in the study of metal fatigue cracks. The thesis provides evidence of the superiority of the proposed method under certain conditions and demonstrates the feasibility of this research method.

This study utilized a complementary approach, combining numerical simulation analysis and experimental work. The first stage of the research focused on proposing a novel method that differs from the traditional approach based on Paris Law. The emphasis was placed on introducing a new method that differs significantly in terms of fracture mechanisms from conventional approaches. Two double cantilever beams (DCB) models with different adherend thickness (1.6 mm & 12.7 mm) were employed for finite element (FE) simulation. The local-strain-based damage model was implemented using the VUMAT subroutine in the commercial finite element analysis (FEA) software ABAQUS. Material parameters for the damage model were calibrated using published test data and were subsequently used to predict specimens of varying geometric sizes. Results demonstrated that the local-strain-based damage model can effectively reproduce the relationship between crack growth rate (da/dN) and G_{max} observed in the test data. Moreover, two commonly recognized methods based on cohesive damage models were selected for make a benchmark study with the local-strain-based method. The two methods defined the damage rate based on the global G_{max} and were implemented using two VUMAT subroutines in ABAQUS. By comparing the simulation results predicted by the three models with the experimental data, the local-strain-based method

exhibited 15% increase of geometry-independence and comparable reducing ability to the test results.

During the second stage, three aluminium DCB joints with varying adherend thickness (5mm, 8mm, and 15mm) were manufactured using a toughened epoxy-based adhesive, 3M2216, and subjected to Mode I quasi-static and fatigue tests. Quasi-static tests, conducted in accordance with the standard ASTM 5528, were used to determine the critical toughness of each DCB joint. The fatigue tests were initiated at the same critical toughness level to investigate the impact of adherend thickness on the results. Full-field speckle images and local speckle images taken by a high-definition camera were used in combination with digital image correlation (DIC) technology to extract the crack length, rotation angles of the crack tip, and local strain field map around the crack tip. The local strain field maps for specimens with different thicknesses were used to identify the FPZ and to reveal damage evolution within the FPZ. Observations of the FPZ in specimens with different adherend thicknesses at the same J -value revealed that the FPZ area increases with increasing specimen thickness, reflecting the overall dynamic movement of damage transmission within the FPZ. Differences in FPZ area and internal damage evolution both contribute to differences in crack propagation rates.

Based on the results, the third stage of this research focused on verifying and calibrating the proposed damage model. Additional simulation was conducted to determine the model's ability to accurately predict crack growth and damage evolution within the failure process zone, and to compare these predictions with experimental results. Notably, the local-strain-based method exhibited superior predictive accuracy in modelling type I fatigue crack growth for various geometric sizes, as compared to the traditional approach based on Paris Law. These findings have significant implications for the design of adhesive structures. Overall, this research provides compelling evidence that the proposed novel damage model is both efficient and accurate, offering a better explanation of the fracture mechanism of fatigue crack growth in adhesively bonded joints.

Keywords:

Adhesively Bonded Joints, Fatigue Crack Growth, Cohesive Zone Methods, Fracture Process Zone, Paris Law, Fracture mechanics, Simulation

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List of Abbreviations

AE	Acoustic Emission
ABJs	Adhesively Bonded Joints
ABSs	Adhesively Bonded Structures
CDM	Continuum Damage Mechanics
CLS	Cracked Lap Shear
CTOA	Crack Tip Opening Angle
CTOD	Crack Tip Opening Displacement
CT	Computed Tomography
CZM	Cohesive Zone Modelling
DCB	Double Cantilever Beam
DIC	Digital Image Correlation
ENF	End Notch Flexure
ERR	Energy Release Rates
FBG	Fiber Bragg Grating
FE	Finite Element
FEA	Finite Element Analysis
FPZ	Fracture Process Zone
IRT	Infrared Thermography
LDV	Laser Doppler Vibrometer
MMB	Mixed Mode Bending
PTFE	Polytetrafluoroethylene
SCL	Simplified Cyclic Loading
SERR	Strain Energy Release Rate
SIFs	Stress Intensity Factors
MMB	Mixed Mode Bending
SCL	Simplified Cyclic Loading
SLB	Single Lap Shear
TSLs	Traction Separation Laws
VCCT	Virtual Crack Closure Technique
WDCB	Wedge Double Cantilever Beam

G	Strain Energy Release Rate
G_{IC}	Critical Strain Energy Release Rate at Maximum Fatigue load
G_{max}	Strain Energy Release Rate at Maximum Fatigue load
K	Stress Intensity Factor
R	Stress Ratio

1 Introduction

1.1.1 Background and Motivation

Adhesive bonded structures are assembled by utilizing adhesive bonding techniques that involve the use of adhesive materials to join two or more substrates, such as metals, polymers, or composite materials, resulting in a sturdy and durable bond. This bonding method has become increasingly popular in various industries, including aerospace, automotive, and construction, due to its significant advantages over traditional bonding methods, such as welding or mechanical fastening. Notably, adhesive bonded structures offer several benefits, including improved structural integrity, reduced weight, and decreased production time and costs. For example, adhesive bonding is extensively utilized in the manufacturing of modern aircraft, such as the Airbus A380, in which it is used to connect the wing-to-body joints and the tail cone to the fuselage. Similarly, the Boeing 787 Dreamliner's fuselage is joined together using adhesive bonding instead of traditional fasteners, leading to a lighter and more fuel-efficient aircraft.

Moreover, compared to the traditional riveting method, adhesive bonding can reduce the risk of extra stress concentration areas, potentially minimizing the design safety hazards in aircraft structures. However, due to the complex loads and stresses experienced by aircraft structures, uncertainties in joint quality can pose a significant threat to structural integrity and efficiency. The limited understanding of damage in adhesive bonded structures has hindered the widespread acceptance of purely adhesive bonding technology as an independent aircraft assembly method. Therefore, research on the damage of adhesive bonded joints has become a critical area of investigation in recent years.

The main damage form for adhesive bonded structures is typically fatigue damage, which can occur due to cyclic loading over time. Cyclic loads experienced by the aircraft during flight, which can result in structural damage over time. These loads can arise from various factors, such as turbulence, take-

off and landing, and changes in altitude and temperature. The accumulation of microscopic cracks in the aircraft's structure is a potential outcome of these loads, which can lead to catastrophic failure if not detected or repaired in time. Aircraft are designed to withstand specific fatigue loads throughout their service life, and fatigue testing is conducted during their development to ensure compliance with these requirements. This testing involves subjecting the aircraft to simulated fatigue loads to evaluate its structural integrity and identify any potential design flaws or weaknesses.

The research on fatigue damage of adhesive bonded joints mainly includes these three aspects: crack initiation and propagation and fatigue life. Generally, the corresponding damage models can be roughly divided into three categories: fatigue life models, phenomenological models, and progressive damage model. Fatigue life or phenomenological models are commonly employed for performing a safe-life assessment of a structure. However, these models cannot evaluate the impact of incidental damage incurred during service or other types of defect analysis. Empirical observations in metal structures have demonstrated that a "slow growth" philosophy can be adopted to design lighter structures. This approach tolerates some degree of fatigue-induced damage growth, and regular inspections are conducted to facilitate timely repair and ensure structural safety. Adopting a slow growth philosophy permits higher load levels within the structure and enables a weight reduction in the design. However, this approach is feasible only if the damage growth behaviour is comprehensively understood to schedule reliable damage detection before it reaches critical levels. In contrast to fatigue life models that usually provide an estimate of the total lifespan of structures, and phenomenological models that rely on macroscopic observation properties, such as residual stress or stiffness in experiments, to describe damage degradation, a progressive damage model aims to analyse the actual damage behaviour, such as delamination, debonding, or cracks, to provide a comprehensive understanding of the damage evolution law.

Delamination, debonding, and cracks are all common types of damage that can occur in adhesive bonded joints. Delamination occurs when layers within a composite material or between different materials separate or split due to shear forces. Debonding refers to the separation of the adhesive layer from the substrate, which can result in a loss of adhesion and eventual joint failure. Debonding can be caused by factors such as shear or tensile stresses, or exposure to extreme temperature or moisture. Cracks refer to fractures or discontinuities in the material caused by cyclic or repeated loading, and can propagate and grow over time, leading to structural failure. Delamination and debonding can often lead to the formation and propagation of cracks in adhesive bonded joints, ultimately resulting in joint failure.

In this regard, research on fatigue crack propagation is effective and reliable to study on the fracture mechanism for damage evolution law of adhesive bonded joints. The crack propagation of adhesive bonded joints is affected by many factors, like the material properties, geometry, and loading conditions. Currently, the most widely used methods are based on the Paris Law, a commonly used model for fatigue crack growth in metallic materials, but it may not be suitable for adhesive bonded structures due to their unique material properties and failure modes.

Moreover, the constants in Paris Law are collected from experimental data fitting, and these two material constants have no real physical meaning. Since the application of Paris law into prediction of fatigue crack growth in adhesive bonded joints is obtaining an empirical relation based on experimental observation, it does not provide a clear understanding of the fracture mechanisms. Simultaneously, due to overly-rely on experiments observation, the changes in non-material properties such as loading mode and geometric size often lead to greatly different experimental results. Considering the errors in the manufacturing process of the bonding structure, and all these factors will cause the Paris Law-based methods to lose its reliability and bring a great challenge for the large-scale use of adhesive joints in industry use. Therefore, novel models based on local strain or damage mechanics may be used to

represent fatigue propagation in adhesively bonded structures. These models consider the local effects of the adhesive layer and substrate on the crack growth rate and provide a more accurate representation of the damage evolution in adhesive joints under fatigue loading. Generally, the following research questions can be raised:

What are the inherent limitations associated with employing the Paris Law to delineate the crack growth behaviour in adhesively bonded joints?

In the context of structures or specimens possessing identical material properties yet varying geometric dimensions, what underlying factors account for the discrepancies observed in the Paris relations concerning crack growth?

Is there an alternative, innovative damage model that eschews the foundational principles of the Paris Law, and is capable of more accurately characterizing fatigue crack growth in adhesively bonded structures?

Addressing these pivotal questions will facilitate a deeper comprehension of the fatigue crack evolution law governing adhesive joints. Furthermore, it will furnish a dependable reference for the structural design of adhesive joints, while also promoting their widespread adoption.

1.2 Aim and Objectives

The overall aim of the PhD research is to propose a novel local strain-based damage model to predict fatigue crack growth rate in adhesive bonded structure. Combined with DCB fatigue tests, The size-independent characteristics of FPZ during crack growth were explained and the accuracy and reliability of the model were verified.

The specific objectives of this study are as follows:

1. To propose a novel local strain-based damage model for adhesive bonded joints under cyclic loads (linked to chapter 3,4)
2. To make a critical assessment of numerical models for fatigue crack growth of adhesive bonded joints using two cohesive models based in Paris Law with the local strain-based damage model (linked to chapter 4)

3. To verify the geometry-independence of the local strain-based models compared with published experiments data (linked to chapter 4)
4. To analysis the effect of the damage evolution law in FPZ during fatigue tests for different DCB specimens with different thickness (linked to chapter 5)

1.3 Outline of the Thesis

The thesis structure is illustrated in Figure 1-1, with Chapter 1 serving as the introduction, providing the background, motivation, aims, and objectives of the research. Chapter 2 presents the literature review, while Chapters 3 to 5 comprise the main body of the thesis, containing the results of the author's PhD research work. Each chapter in the main section is logically connected, and the research is conducted according to the proposed damage model, simulation comparison, experimental verification, and damage model calibration. Chapter 6 presents an overall conclusion, combining the outcomes of each chapter, and suggesting future research directions based on the identified research gaps.

Chapter 1. This chapter provides an overview of the research background, motivation, aims, and objectives, and outlines the thesis structure.

Chapter 2. presents a comprehensive literature review of damage models, numerical methods, and experimental works for the fatigue crack growth rate of adhesive bonded structures.

Chapter 3. presents the methodology employed in the study, encompassing both the simulation calculations and the experimental work.

Chapter 4. This chapter presents a critical assessment of numerical models for fatigue crack growth of adhesive bonded joints. This chapter compares the accuracy of the strain-based simulation method with two widely used methods based on Paris Law combined with CZM and evaluates their geometry-independence abilities using existing DCB experiment data.

Chapter 5. This chapter describes the experimental work conducted on three DCB specimens with varying thickness (5mm, 8mm, and 15mm), obtaining

local-scale strain field maps using digital image correlation (DIC) technology. The strain field distribution and the damage evolution law were also analysed.

Chapter 6. This chapter summarizes the main findings and presents the overall conclusion of the thesis. Future research directions are also suggested.

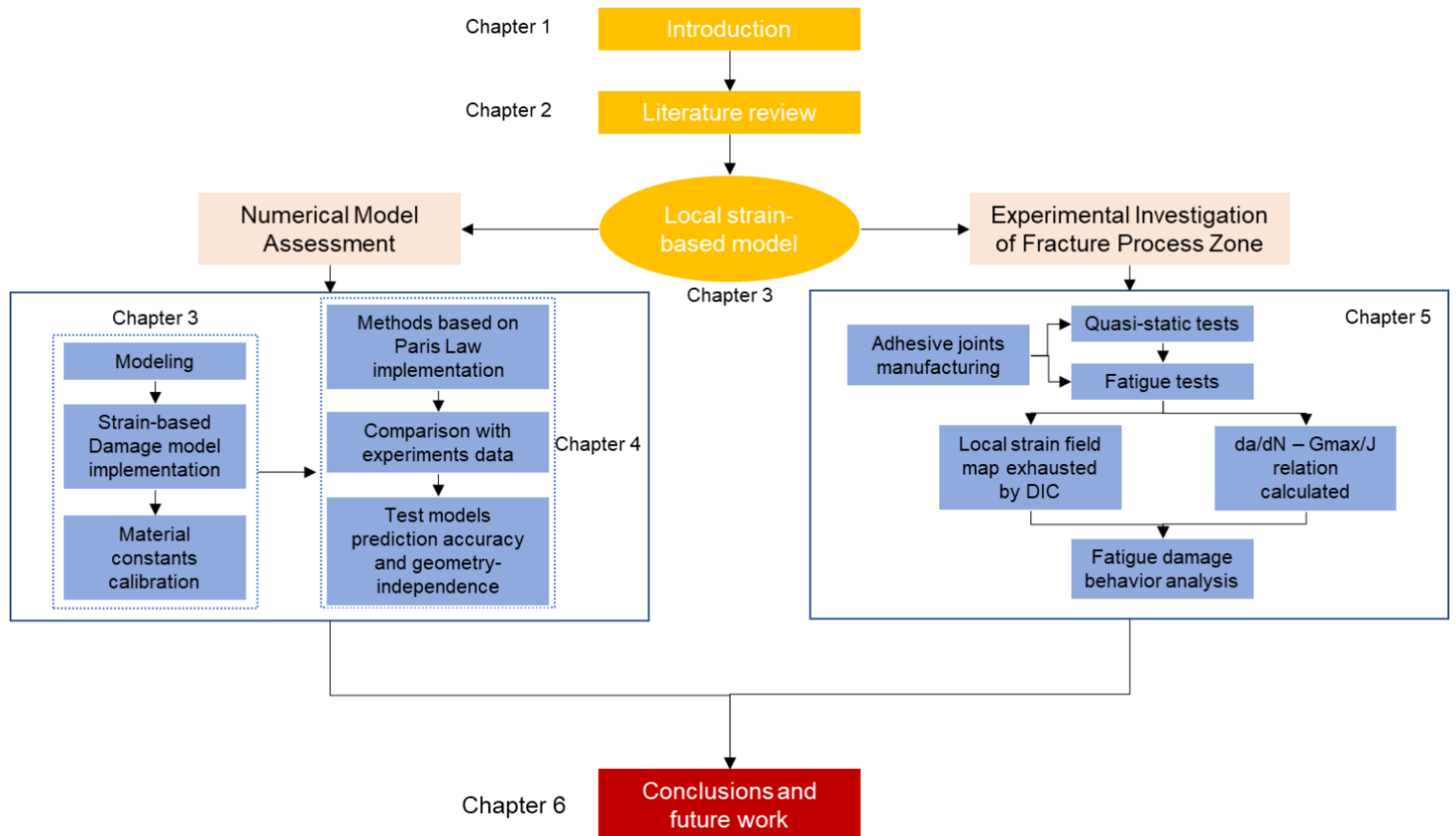


Figure 1-1 Thesis structure.

2 Literature review

2.1 Fatigue Behaviour in Adhesively Bonded Joints (ABJs)

Adhesively bonded joints (ABJs) have emerged as critical structural components across aerospace (Katsiropoulos et al., 2012), automotive (Cavezza et al., 2020), and renewable energy industries (Mair, 1993) due to their advantages in weight reduction, stress distribution uniformity, and corrosion resistance. However, their long-term reliability under cyclic loading remains a significant challenge, as fatigue-induced degradation often governs the ultimate failure of bonded assemblies (Budzik, Jumel, & Shanahan, 2013). The fatigue behaviour of adhesive joints is inherently multifactorial, influenced by a complex interplay of mechanical, material, and environmental variables (Khoramishad et al., 2010). Key determinants include the amplitude (Schneider et al., 2018) and frequency of cyclic loading (Gomatam & Sancaktar, 2006), interfacial adhesion properties (van Dam et al., 2020), geometric configurations of adherends (Sousa et al., 2024), surface pretreatment protocols (Brewis & Critchlow, 1997), and operational conditions such as temperature (Biscaia, 2019) and humidity (Na et al., 2018). These factors not only dictate the propagation rate of fatigue-driven damage but may also fundamentally alter the dominant failure mechanisms, necessitating a mechanistic understanding of fatigue evolution in diverse scenarios.

Failure modes in adhesive bonds are broadly classified into four primary categories (Figure 2-1): adherend failure (Figure 2-1 (a)), cohesive failure (Figure 2-1 (b)), interfacial failure (Figure 2-1 (c)), and hybrid mechanisms (Figure 2-1 (d)). Adherend failure in composite structures typically manifests as interlaminar delamination, where the interfacial strength between composite plies is lower than the adhesive bond strength, leading to premature fracture before adhesive layer rupture (Shenoy et al., 2010). This phenomenon may also arise from preexisting micro-cracks or stress concentrations inherent to the adherend geometry (Budzik, Jumel, & Shanahan, 2013). Cohesive failure, characterized by crack propagation within the adhesive layer, generally indicates superior interfacial adhesion strength exceeding the adhesive's

intrinsic fracture resistance, thereby reflecting optimal bonding performance (Rd, 2003). Conversely, interfacial failure—marked by adhesive debonding from adherend surfaces—often stems from inadequate surface treatments (e.g.,

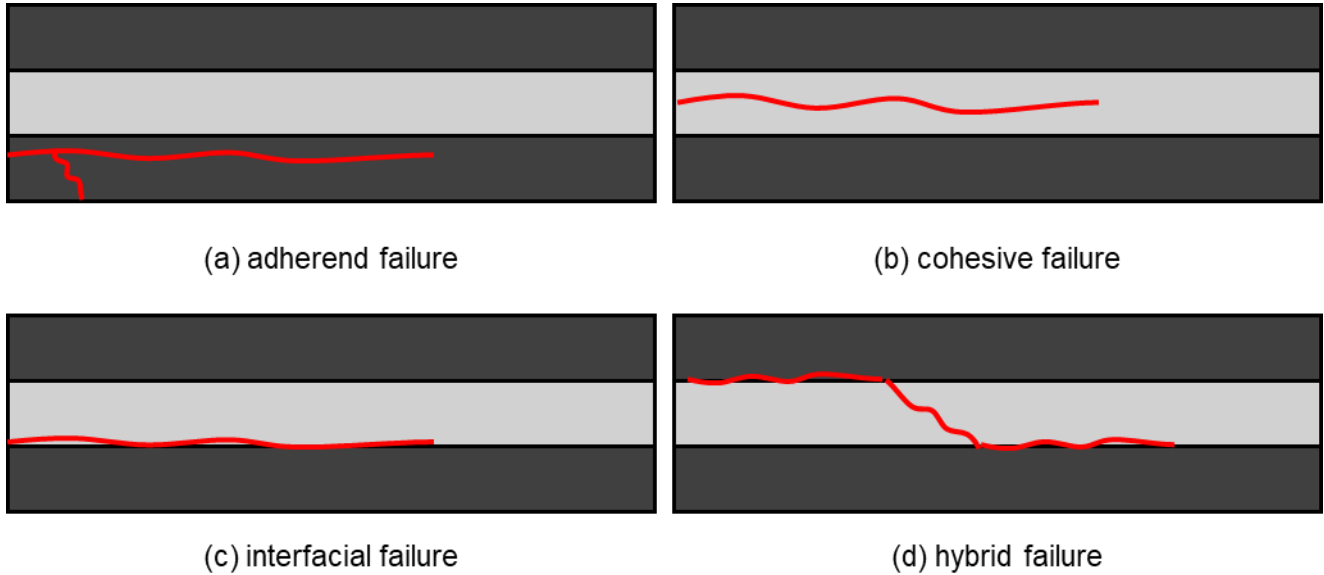


Figure 2-1 Failure modes of adhesively bonded joints.

improper curing or oxidation) or contamination during bonding processes (Critchlow et al., 2006). Hybrid failures, involving concurrent cohesive and interfacial damage, introduce additional complexity to fatigue life predictions due to path-dependent crack propagation dynamics (Ashcroft et al., 2001).

Notably, crack migration between adhesive and adherend phases—observed in configurations such as DCB—is governed by local stress fields and material anisotropy (Sørensen & Jacobsen, 2009). For instance, cracks preferentially propagate through composite layers with fibres perpendicular to the loading direction, as matrix-dominated failure circumvents fibre fracture (Brunner, 2018). Such behaviour underscores the critical role of adherend layup design in mitigating fatigue damage (F. Li et al., 2018).

Recent studies emphasize the sensitivity of fatigue mechanisms to joint geometry and loading parameters (X. He, 2011). For example, tapered adherends (Martiny et al., 2008) or adhesive fillets (Baldan, 2004) can redistribute stress concentrations, delaying crack initiation. Conversely, abrupt geometric transitions may exacerbate peel stresses, accelerating interfacial

debonding (Adams & Harris, 1996). Surface treatments—such as plasma activation (Palmieri et al., 2013), chemical etching (Baldan, 2012), or mechanical abrasion (Davis & Bond, 1999)—enhance interfacial adhesion by modifying surface energy and roughness, thereby shifting failure modes from interfacial to cohesive (Brewis et al., 1990). However, excessive surface roughness may introduce micro voids that act as fatigue crack nucleation sites, highlighting the need for optimized pretreatment protocols (Erli et al., 2003). Environmental exposure further modulates fatigue behaviour: hygrothermal aging plasticizes epoxy adhesives, altering their fracture toughness and fatigue threshold (Banea et al., 2010). These interdependent factors necessitate rigorous experimental design to isolate variable interactions and demand a systems-level approach to fatigue analysis, integrating material characterization (Liu et al., 2019), multiscale modelling (D. Li, 2021), and in-situ monitoring (Mehdikhani et al., 2019) to bridge the gap between laboratory testing and real-world structural performance.

2.2 Methodologies for Experimental Characterization

Experimental data serve as the cornerstone for understanding the fatigue behaviour of adhesively bonded structures. While numerous fatigue damage models have been proposed to predict the degradation of bonded joints under cyclic loading, their accuracy and applicability heavily rely on empirical validation through targeted fatigue experiments. These experiments are indispensable not only for calibrating computational simulations but also for refining damage evolution laws and validating failure criteria.

As illustrated in Figure 2-2, the experimental characterization of adhesive bonds encompasses a multifaceted framework, integrating standardized testing protocols, specimen geometry design, and advanced in-situ monitoring techniques.

A critical aspect of fatigue research lies in bridging the gap between quasi-static and cyclic loading responses. Quasi-static tests, though not directly simulating fatigue conditions, provide essential baseline data on adhesive strength,

interfacial toughness, and failure modes, which are crucial for interpreting fatigue-driven damage mechanisms. For instance, fracture toughness values derived from quasi-static DCB tests often underpin fatigue crack growth rate predictions (Sørensen & Jacobsen, 2009). This section systematically outlines the following components:

1. **Standardized Testing Protocols:** A review of ASTM and ISO standards tailored to evaluate adhesive performance under diverse mechanical and environmental conditions.
2. **Fatigue Testing:** Specific fatigue specimen configurations to address distinct research objectives and different impacts.
3. **In-Situ Monitoring Technologies:** Advanced techniques such as DIC, acoustic emission (AE) and some other techniques for real-time damage detection and mechanism elucidation.

By synthesizing these elements, this chapter aims to establish a methodological framework that enhances the reproducibility of experimental data and ensures its relevance to real-world applications. The integration of standardized practices with cutting-edge monitoring tools not only improves experimental accuracy but also facilitates the development of predictive models capable of addressing the inherent complexity of adhesive fatigue behaviour.

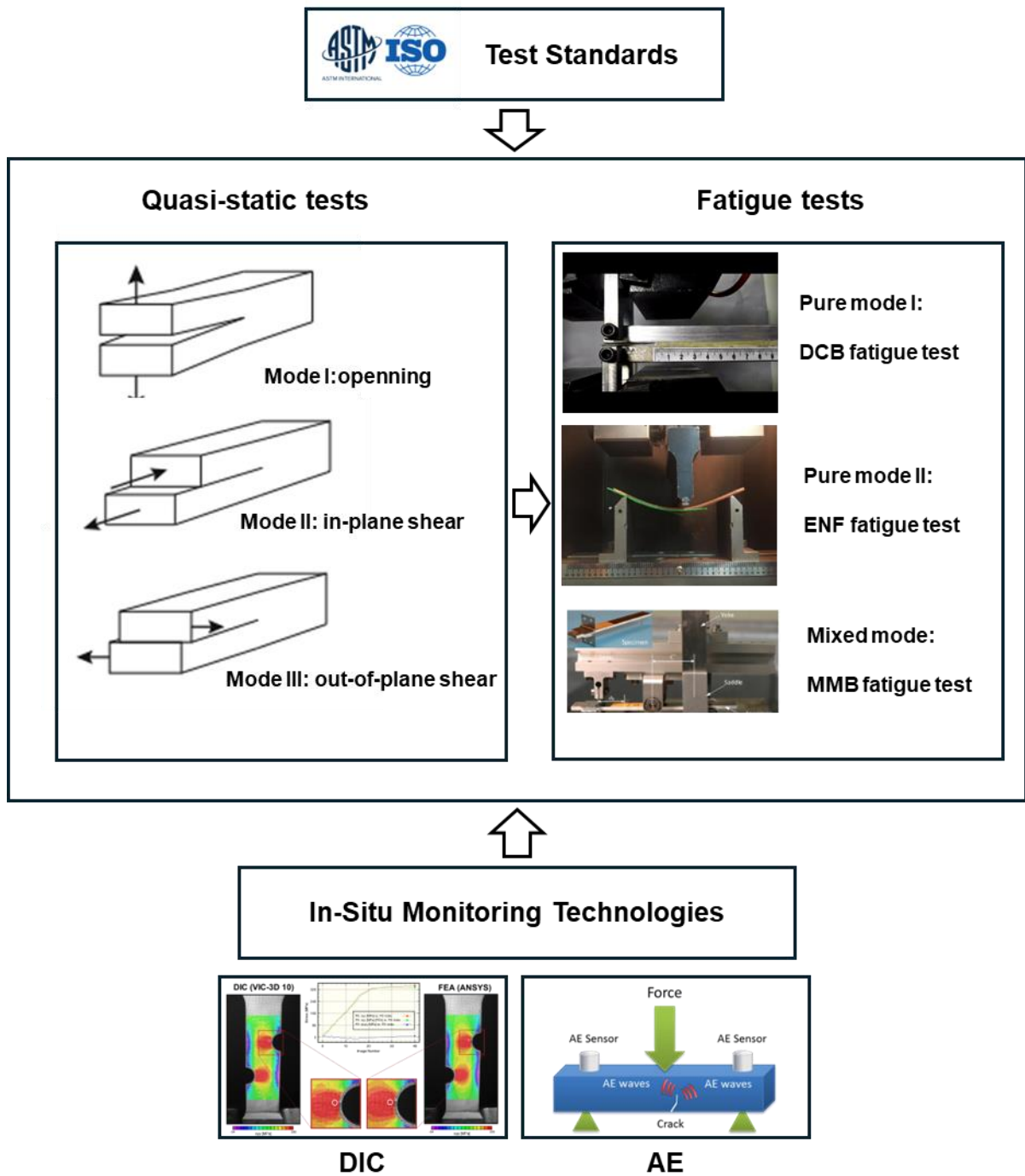


Figure 2-2 ABJs tests framework.

2.2.1 Test Standards

The following Table 2-1 summarizes critical ASTM standards for characterizing ABJs, organized by joint geometry, testing objectives, and relevance to fatigue research.

Standards include ASTM D897-08 (tensile testing of block-shaped adherends), ASTM D1002-10 (single-lap shear strength of metal-to-metal joints), and ASTM D3166-99 (shear fatigue properties), which collectively provide foundational data for quantifying adhesive strength and durability under cyclic loading (Budzik, Jumel, Ben Salem, et al., 2013),(Sørensen et al., 2009). To address fracture mechanics challenges, standards such as ASTM D3433-99 (Mode I fracture toughness of metal adherends) and ASTM D5528-13 (interlaminar fracture toughness of composites) are adapted for fatigue crack growth studies by integrating cyclic loading protocols. These methodologies enable precise characterization of crack propagation kinetics under repeated stress, directly informing adhesive joint lifespan predictions (Blackman et al., 2009; Guo et al., 2017). Surface preparation protocols, exemplified by ASTM D2651-01, emphasize rigorous adherend cleaning and activation to mitigate interfacial failure—a prevalent fatigue-driven degradation mechanism exacerbated by contamination (Ashcroft et al., 2003).

To address fracture mechanics challenges, while these standards deliver essential baseline data for laboratory characterization, replicating real-world fatigue scenarios (e.g., cyclic flexure in aircraft wings) necessitates specialized configurations. Specimen geometries such as the cracked lap shear (CLS) are

engineered to simulate operational loading spectra, enabling systematic investigation of fatigue crack initiation and propagation. The subsequent section, Fatigue Testing Configurations, will elaborate on how these designs, combined with advanced cyclic loading regimes, facilitate predictive modelling of adhesive fatigue behaviour for damage-tolerant design.

Table 2-1 Test standards for ABJs.

Joint Geometry/Cate gory	Standard	Configuration	Research Focus	Fatigue Relevance
Axially loaded (tensile)	ASTM D897- 08 (2016)	Block-shaped adherends under uniaxial tension	Tensile strength of adhesive bonds	Baseline data for stress-life (S-N) curve development under cyclic tension (Budzik, Jumel, Ben Salem, et al., 2013).
	ASTM D2094- 00(2014)/D2 095-96 (2015)	Bar- or rod- shaped adherends	Strength of cylindrical or irregular geometries	Fatigue life prediction in dynamically loaded shafts/pins [2].

Shear-Dominated Joints	ASTM D1002-10 (2019)	Single-lap shear (metal-to-metal)	Shear strength of lap joints	Validation of fatigue degradation models under cyclic shear (Khoramisha d et al., 2011).
	ASTM D3166-99 (2020)	Single-lap shear (metal-to-metal)	Shear fatigue properties	Crack initiation thresholds in adhesive layers (Sørensen et al., 2009).
Fracture Mechanics	ASTM D3433-99 (2020)	DCB	Measures Mode I fracture toughness (resistance to crack opening) under quasi-static loading for mental adherends.	Adapted for fatigue crack growth studies under cyclic loading
	ASTM D5528 – 01(2007)		Interlamina r fracture	Baseline for fatigue crack

			toughness of composites	growth rate studies in composite-adhesive interfaces (Blackman et al., 2009)
	ASTM D6115 – 97(2019)		Fatigue Delamination Growth Onset	For Mode I Fatigue Delamination Growth Onset of Unidirectional Fiber-Reinforced Polymer Matrix Composites (Zhu et al., 2020)
	ASTM D7905M-14 (2019)	End-notched flexure (ENF) for Mode II fracture	Shear-dominated crack propagation in composites	Modelling mixed-mode fatigue delamination in bonded composites (Guo et al., 2017)
	ASTM D6671M-19	Mixed-mode bending (MMB)	Complex loading scenarios	Fatigue crack growth analysis

		for Mode I-II fracture	in aerospace joints	under varying mode-mixity ratios (Ashcroft et al., 2003)
Surface preparation	ASTM D2651-01 (2016)	Surface treatment protocols for metal adherends	Surface cleaning, abrasion, and chemical activation	Poor surface preparation shifts failure modes, reducing fatigue life (Critchlow & Brewis, 1996).
	ASTM D3933-98 (2017)	Surface treatment protocols for aluminium adherends		

2.2.2 Fatigue Testing

Fatigue testing of ABJs is pivotal for ensuring the long-term reliability of structures. This section delineates methodologies, specimen design principles, environmental and manufacturing influences, and standardization challenges inherent to ABJs fatigue evaluation.

2.2.2.1 Methodologies and Objectives

Researchers tailor fatigue testing protocols to specific objectives, leveraging quasi-static experimental frameworks as foundational references:

1. Stress/Strain-Life Approaches: Determine cycles to failure under cyclic loading amplitudes, generating stress-life (S-N) curves for lifecycle predictions in dynamic environments.
2. Damage Mechanics Models: Quantify residual strength or stiffness

degradation as a function of load cycles, necessitating high-resolution strain monitoring to track progressive damage.

Fracture Mechanics Frameworks: Utilize standardized quasi-static tests (e.g., ASTM D5528-13 for Mode I fracture toughness, ASTM D7905M-14 for Mode II shear resistance) adapted to cyclic loading to characterize crack growth kinetics (Carlberger & Stigh, 2010).

Specimen Design and Geometries

Fatigue testing relies on standardized specimen geometries tailored to specific loading modes and research objectives. Lap joints, governed by standards such as BS EN ISO 9664:1995 and ASTM D3166-99, are widely adopted for fatigue life assessment due to their simplicity, though their utility in crack growth analysis is limited by edge stress concentrations (Khan, 2019). For fracture mechanics studies, specialized specimens are employed to isolate loading modes: the DCB subjects' adhesives to pure Mode I tension (Figure 2-3 (a)), enabling direct quantification of crack growth rates under cyclic loading (Sørensen & Jacobsen, 2009); the end notch flexure (ENF) configuration applies Mode II shear via three-point bending (Figure 2-3 (b)), critical for analysing shear-driven delamination in applications such as rotor blades (Blanloeuil et al., 2017); and the mixed-mode bending (MMB) specimen (Figure 2-3 (c)) simulates combined Mode I/II stress states, replicating complex loading scenarios in aerospace joints (de Moura et al., 2010). To bridge the gap between laboratory tests and real-world conditions, the CLS configuration

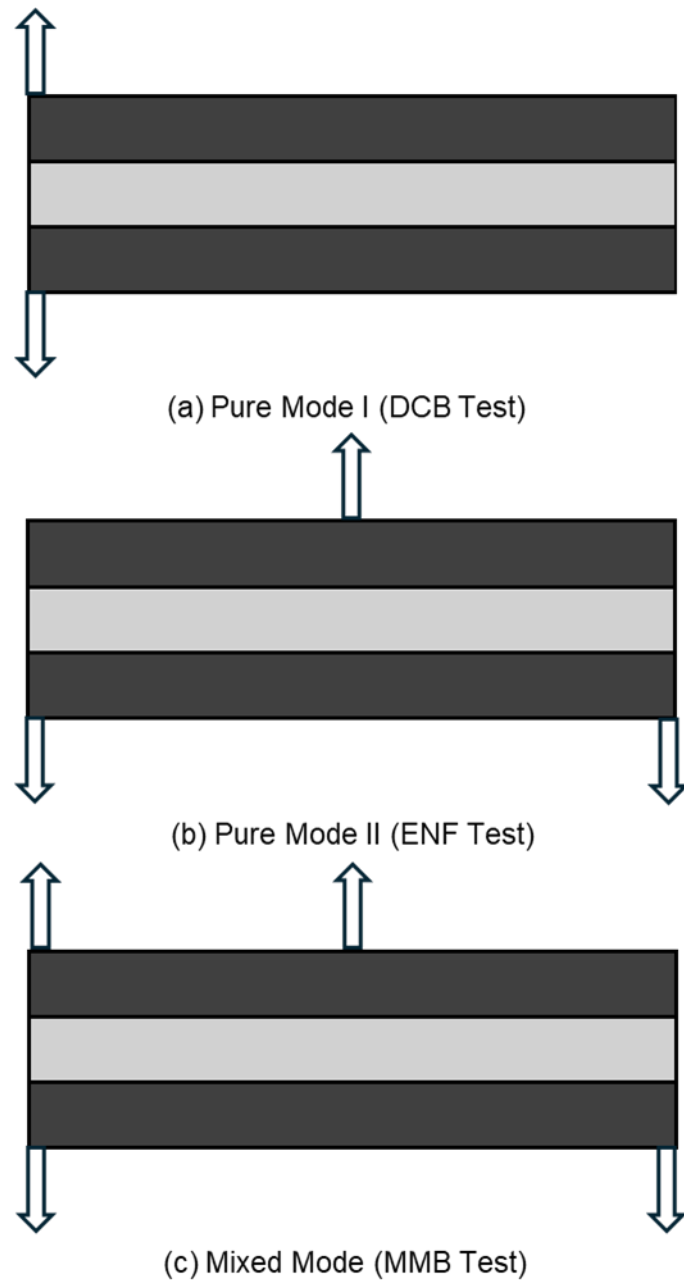


Figure 2-3 Different Loading mode for DCB specimen

replicates stress concentrations at stiffener terminations, offering insights into crack initiation and mode-mixity evolution under operational spectra.

Challenges in dissimilar material testing arise from interfacial strain mismatches like testing metal-to-composite bonds requires adherend thickness adjustments to balance bending stiffness. However, interfacial strain mismatches may persist, altering crack propagation paths despite equivalent global strain energy release rates. For instance, increasing adherend thickness from 2 mm to 5 mm

enhances fatigue strength by only 24% due to counteracting flexural effects (Chai, 2021).

2.2.2.2 Environmental and Manufacturing Impacts

Hygrothermal Degradation: Moisture ingress accelerates fatigue damage, with wet-aged joints exhibiting up to 40% greater life reduction compared to static strength loss (Chen et al., 2023).

Deviations in manufacturing protocols—such as incomplete rinsing of fluorinated residues—can shift failure modes from cohesive to interfacial, as demonstrated in jet wing root joint case studies (Jones et al., 2018). This underscores the necessity of replicating production-grade processes during specimen fabrication.

2.2.2.3 Multi-Axial Fatigue and Standardization Gaps

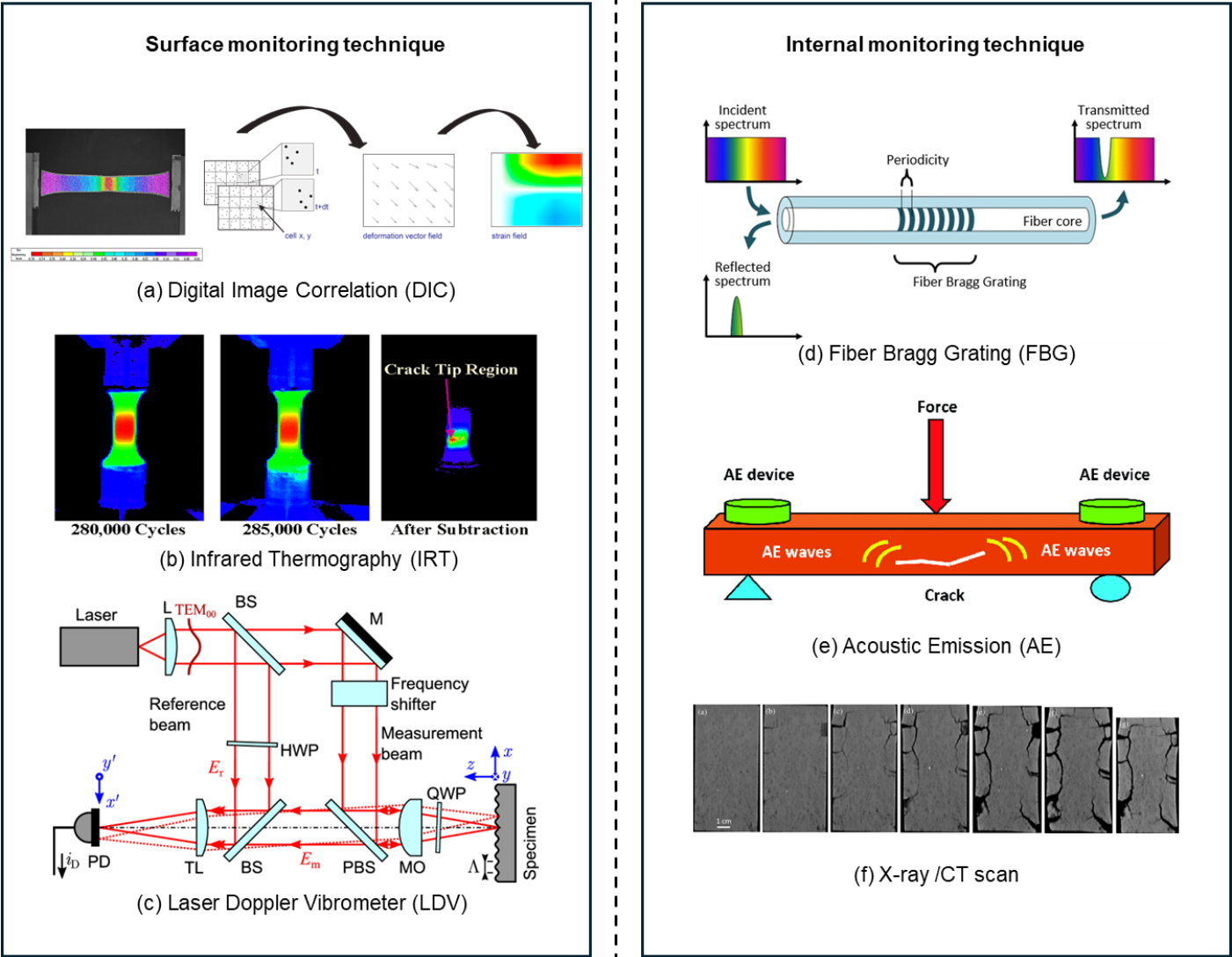
Multi-axial stress states, prevalent in aerospace joints (e.g., wing-to-fuselage attachments), complicate fatigue life prediction due to time-varying principal stress directions. Advanced electro-hydraulic testing systems now replicate operational loading spectra (e.g., proportional vs. non-proportional paths), yet single-lap joints under multi-axial loads remain underexplored (K. S. Li et al., 2022). The absence of universal standards for multi-axial fatigue testing complicates life prediction in aerospace joints necessitates reliance on industry-specific methods, while multi-crack interactions in complex geometries (e.g., double strap joints) challenge predictive accuracy (Aliha et al., 2021).

While standardized protocols provide foundational data, real-world applicability demands advanced configurations and rigorous process control. The subsequent section, In-Situ Monitoring Techniques, will explore methodologies such as DIC, AE and infrared thermography, which enhance fatigue testing resolution through real-time damage diagnostics.

2.2.3 In-suit Monitoring Techniques.

In fatigue failure experiments of adhesively bonded structures (ABSs), real-time monitoring techniques are essential for capturing damage evolution and failure

mechanisms. As illustrated in Figure 2-4, these in-situ monitoring methods can be categorized into surface monitoring techniques and internal monitoring techniques based on their measurement locations. Surface techniques primarily detect strain, crack propagation, temperature fields, or vibrational characteristics on or near the surface of ABJs, while internal techniques focus on identifying interfacial or subsurface defects such as debonding, delamination, and microcracks.



This

following

Figure 2-4 In-suit Monitoring Techniques applied in ABJs.

Table 2-2 provides a concise comparison of techniques, emphasizing their applicability and constraints in fatigue testing of adhesively bonded joints.

Table 2-2 A comprehensive assessment of different in-situ monitoring techniques.

Category	Technique	Principle	Advantages	Limitations	References
Surface Monitoring	Digital Image Correlation (DIC)	Tracks speckle patterns using high-resolution cameras to calculate full-field displacements and strain distributions.	Non-contact, full-field measurement; ideal for surface crack propagation and strain localization.	Requires precise speckle preparation; cannot detect internal damage.	(F. Sun et al., 2021; F. Sun & Blackman, 2020)
	Infrared Thermography (IRT)	Detects temperature variations during fatigue loading via thermal radiation (e.g., frictional heating at cracks).	Rapid large-area scanning; effective for thermal hotspot detection in high-cycle fatigue.	Reduced sensitivity in low-frequency fatigue due to minimal temperature changes.	(Mevissen & Meo, 2019)
	Laser Doppler Vibrometer (LDV)	Measures surface vibration velocity using the Doppler effect to infer dynamic structural responses.	Non-contact, high precision; suitable for local vibration mode monitoring.	Requires reflective surfaces; limited to small measurement areas.	(Cao et al., 2022; Castellini et al., 2006)
Internal Monitoring	Fiber Bragg Grating (FBG)	Monitors internal strain fields via wavelength shifts in embedded optical gratings.	Integrable into adhesive layers/substrates for distributed strain monitoring.	Fiber implantation may affect structural integrity.	(Takeda, 2008)
	Acoustic Emission (AE)	Captures elastic waves from internal damage	Real-time dynamic monitoring;	Susceptible to noise; requires	(Ohtsu, 2016;

		events to localize crack initiation.	sensitive to early-stage damage.	multi-sensor arrays for source localization.	Suzuki et al., 2017)
	X-ray/Computed Tomography (CT)	Visualizes internal defects (delamination, voids) via X-ray penetration and 3D reconstruction.	High-resolution volumetric imaging; suitable for offline/quasi-real-time inspection.	High cost, limited portability, and insufficient real-time capabilities.	(Bull et al., 2013)

Based on literature review, existing studies on ABJs fatigue monitoring predominantly emphasize DIC applications due to its versatility in characterizing surface deformation and crack dynamics under cyclic loading.

2.2.3.1 Application of DIC in fatigue analysis of Adhesively bonded structures

DIC, a non-contact, full-field optical measurement technique, has emerged as a powerful tool for investigating fatigue crack propagation and fracture mechanics due to its high environmental adaptability, wide measurement range, and automation capabilities (Chu et al., 1985). By tracking the displacement of speckle patterns on a specimen's surface before and after deformation, DIC enables pixel-level resolution for reconstructing full-field displacement and strain distributions (Figure 2-4 (a)) (Weidner & Biermann, 2021). The experimental workflow involves three key steps: surface preparation via controlled speckle patterning, continuous image acquisition under cyclic loading, and post-processing using specialized algorithms to extract displacement and strain fields (Szalai & Dogossy, 2021). These features make DIC particularly advantageous for real-time monitoring of dynamic structural responses and characterization of fatigue behaviour in materials such as ABJs (Z. He et al., 2022). For instance, (Gao et al., 2015) employed DIC to evaluate the rate-dependent fracture toughness of adhesive layers using wedge double cantilever

beam (WDCB), ENF, and single-lap shear (SLB) specimens, demonstrating its versatility in multi-mode fracture analysis.

Recent advancements have expanded DIC's role in in-situ detection of fatigue crack initiation and propagation, particularly in composite materials. By analysing displacement discontinuities near crack tips, DIC provides a robust framework for identifying crack onset and tracking growth paths. In carbon fibre/epoxy composites, for example, DIC-based displacement field analysis has enabled precise determination of crack initiation points and propagation trajectories (Feng et al., 2022). Similar applications have been extended to fatigue studies of fibre-metal laminates (Paliwal & Ramji, 2022; Zhu et al., 2020) and woven glass fibre composites (Koohbor et al., 2015), though challenges persist in detecting subsurface delamination due to limitations in surface sensitivity. While DIC has been widely adopted for static and fatigue-induced delamination monitoring, methods relying on speckle-tracking confidence parameters (σ) to estimate delamination lengths remain contentious. The σ parameter is highly sensitive to speckle quality, subset size, and step length, leading to inconsistencies in crack tip identification and delamination quantification (Kalteremidou et al., 2018).

A critical challenge in applying DIC to fracture mechanics lies in achieving accurate crack tip localization, which is essential for deriving fracture parameters such as stress intensity factors (SIFs) and J -integral (YAMANE et al., 2017). Traditional approaches, including visual inspection and compliance-based methods, suffer from subjectivity, inefficiency in large datasets (>10,000 images), and limited applicability under extreme conditions such as high temperatures (C. Zhang et al., 2024). Alternative techniques like electrical potential drop methods are constrained by material resistivity dependencies, narrowing their utility (Ikeda et al., 1991). These limitations underscore the need for standardized, automated solutions. To address these challenges, (X. Sun et al., 2025) proposed an automated crack tip localization algorithm integrating immune optimization with displacement field analysis. This method leverages DIC's inherent strengths by utilizing full-field displacement data to detect

displacement jumps at crack fronts, optimizing speckle patterns to minimize thermal noise in high-temperature environments, and eliminating human bias through automated processing. Such advancements not only enhance real-time fatigue damage assessment but also improve compatibility with fracture mechanics frameworks, enabling reliable extraction of SIFs and strain energy release rates.

The significance of DIC extends to extreme-environment applications, such as aerospace components exposed to high temperatures, where its non-contact nature avoids sensor degradation issues (Han & Goo, 2024). Future developments may integrate machine learning to refine crack prediction models, particularly for complex ABJ geometries, while multi-parameter extraction capabilities (e.g., SIFs, crack closure effects, and energy release rates) promise to advance high-fidelity damage modelling (Y. Li & Xie, 2023). By bridging experimental mechanics with computational fracture analysis, DIC stands poised to redefine fatigue and fracture characterization in next-generation materials.

2.3 Theoretical Frameworks for Fatigue Failure Analysis

2.3.1 Classification by Methodology

The analysis of fatigue failure in adhesive-bonded joints relies on distinct theoretical frameworks that have evolved through decades of interdisciplinary research. These frameworks are primarily categorized based on their underlying physical mechanisms rather than specific structural configurations, reflecting fundamental differences in how they conceptualize damage initiation and progression. The earliest approaches emerged from macroscopic phenomenological observations, where fatigue life prediction was rooted in empirical correlations between cyclic stress levels and failure cycles. The S-N curve methodology, pioneered by Wöhler in the 19th century and later formalized by Basquin (1910), established the foundational stress-life relationship for metals. When adapted to adhesives, this approach faced inherent limitations due to the complex stress states in bonded joints, particularly the non-uniform shear stress distribution that invalidates simple

stress averaging. Recent advancements have integrated probabilistic modifications to S-N curves to account for adhesive porosity and surface pretreatment effects in aerospace joints, achieving $\pm 30\%$ prediction accuracy under variable amplitude loading (Lyathakula & Yuan, 2021).

The emergence of fracture mechanics in the mid-20th century shifted focus to crack-driven failure mechanisms. (Paris & Erdogan, 1963) seminal work on crack growth kinetics via ΔK -controlled models revolutionized fatigue analysis but proved inadequate for adhesives exhibiting significant plastic zones. This limitation spurred the development of the Virtual Crack Closure Technique (VCCT) by (Rybicki & Kanninen, 1977), which quantifies energy release rates (ERR) through computationally efficient finite element implementations. Modern adaptations of VCCT now incorporate mixed-mode ratio adjustments for composite-metal interfaces, enabling accurate prediction of crack path deviation in automotive crash-resistant joints (Kruger et al., 2004). However, the requirement for predefined cracks motivated the parallel development of CZM, first conceptualized by (Barenblatt, 1962) and later operationalized by (Camacho & Ortiz, 1996) for dynamic fracture. Contemporary CZM formulations embed fatigue degradation through cycle-dependent traction-separation laws, successfully capturing crack nucleation in wind turbine blade adhesives under combined thermal-mechanical cycling (Turon, Costa, et al., 2007).

Continuum damage mechanics (CDM), formalized by (Chaboche, 2008), introduced a paradigm shift by treating damage as a continuous field variable rather than discrete cracks. This framework's ability to model distributed micro void evolution has proven particularly valuable for adhesives experiencing hygrothermal aging. The ongoing integration of machine learning with CDM parameters inversely identified from acoustic emission data represents a frontier in real-time remaining life assessment (Lyathakula & Yuan, 2021).

These methodological evolutions reflect an epistemological progression from empirical curve-fitting to physics-based mechanisms and finally to multi-physics coupled systems. While S-N methods remain entrenched in industrial

certification standards due to their simplicity, research paradigms increasingly favor hybrid approaches. For instance, (Dávila, 2020a) connects the phase of fatigue crack initiation and fatigue crack development by cohesive model triggered by SN curve and embedded in continuous damage model. (Allegri, 2020) also tried to use a unified paradigm to obtain the material constant of Paris Law relation directly by SN curve under a cohesive model, which greatly reduced the cost, and integrated several physical mechanism methods to uncover the complete mechanism explanation of the bonding fatigue crack.

Table 2-3 A comprehensive assessment of different methods based on category.

Method Category	Theoretical Framework	Key Advantages	Limitations	References
Macro-Phenomenological Methods	S-N Curve Approach	<ul style="list-style-type: none"> • Intuitive physical interpretation with industrial maturity • Suitable for constant-amplitude loading 	<ul style="list-style-type: none"> • No unique correlation between average shear stress and maximum stress • Ignores damage accumulation • Large extrapolation errors under variable loads 	(Schneider et al., 2018)
	Strength/Stiffness Degradation Models	<ul style="list-style-type: none"> • Enables computational efficiency via stiffness reduction factors • Evaluates macro-scale geometric effects • Partial support for material nonlinearity 	<ul style="list-style-type: none"> • Requires extensive destructive testing for parameter calibration • Fails to describe physical damage evolution mechanisms 	(larve et al., 2017)

Fracture Mechanics Methods	Linear Elastic Fracture Mechanics (LEFM)	<ul style="list-style-type: none"> •Quantifies crack growth rate via Paris' law • Critical SIF/ERR criteria for failure prediction 	<ul style="list-style-type: none"> •Limited to brittle epoxy systems •Neglects plastic zone effects •Poor accuracy under mixed-mode loading 	(Nojavan et al., 2016)
	Virtual Crack Closure Technique (VCCT)	<ul style="list-style-type: none"> •Simulates crack propagation via energy release rate • Low mesh sensitivity 	<ul style="list-style-type: none"> •Requires predefined initial crack •Cannot predict crack initiation •Remeshing needed for crack growth 	(Amiri-Rad et al., 2017)
	Cohesive Zone Model (CZM)	<ul style="list-style-type: none"> •Describes interface failure via traction-separation law •Simulates crack initiation & propagation 	<ul style="list-style-type: none"> •Fatigue crack growth relies on empirical parameters •High cost for constitutive model calibration 	[1][2]
Continuum Mechanics Methods	Continuum Damage Mechanics (CDM)	<ul style="list-style-type: none"> • Directly links damage variables to cycle count •Predicts residual strength/stiffness at any fatigue stage 	<ul style="list-style-type: none"> •Complex constitutive equations •Requires numerous material parameters for multi-field coupling •High computational cost 	(Bak et al., 2014)

Inspired by (Pascoe et al., 2013) seminal classification framework for fatigue behaviour analysis in bonded joints, which categorizes methodologies based on their treatment of damage mechanisms (e.g., empirical stress-life correlations vs. physics-driven crack growth models), the Table 2-3 systematically organizes prevalent fatigue prediction approaches into three paradigms: macro-phenomenological, fracture mechanics, and continuum damage mechanics. This taxonomy reflects fundamental divergences in both mechanistic

interpretation and analytical scale. Macro-phenomenological methods, rooted in Wöhler's 19th-century stress-life observations, prioritize empirical correlations between global stress parameters and fatigue cycles, offering simplicity at the cost of mechanistic insight. Fracture mechanics frameworks, evolving from Irwin's energy-based crack propagation theory, focus on discrete crack growth kinetics but require predefined flaw assumptions. Continuum damage mechanics, emerging from Lemaitre's thermodynamic damage formalism, bridges these scales by modelling distributed microdamage evolution. The table further highlights how modern hybrid approaches (e.g., CDM-VCCT coupling) address historical limitations, enabling multiscale fatigue analysis critical for aerospace and renewable energy applications—advancements underscored by recent case studies in CFRP-aluminium hybrid joints (Adamos et al., 2020; Z. He et al., 2022)

2.3.2 Classification by Research objectives

Fatigue analysis in adhesive joints has evolved through successive refinements aimed at resolving the limitations of prior approaches, reflecting a methodological shift from phenomenological descriptions to mechanistic insights. This progression is best illustrated through four pivotal developments: the S-N curve, Paris' law, CZM, and FPZ characterization. Fundamentally, the study of adhesive fatigue revolves around understanding when and how cracks initiate and propagate until structural failure occurs—a physical phenomenon inherently distinct from metallic fatigue due to the heterogeneous, anisotropic nature of adhesive systems. While methodologies like the S-N curve and Paris' law were initially adapted from metals—where fatigue crack initiation relies on plastic deformation and lattice defects—their application to adhesives faces critical limitations. Adhesive joints, composed of dissimilar materials bonded via viscoelastic polymers, exhibit crack initiation and propagation governed by interfacial constraints, adhesive plasticity degradation, and material defects, which differ fundamentally from the homogeneous, isotropic behaviour of metals.

The S-N curve, often regarded as a material- or structure-dependent empirical parameter, remains a pragmatic tool for predicting total fatigue life in adhesive joints. Its reliability stems from its simplicity and industrial standardization (e.g., ASTM D3166), despite its inability to resolve damage mechanisms. In contrast, Paris law—a purely empirical formulation —fails to physically explain adhesive fatigue behaviour due to the complex interfacial conditions and viscoelastic/plastic coupling inherent to adhesives. Parameters in Paris-type model become unstable under such conditions, rendering them inadequate for capturing crack propagation in bonded systems. Consequently, understanding crack propagation laws in adhesives has emerged as the central engineering and research priority.

Inspired by the successful application of CZM in predicting delamination in composite laminates, researchers have increasingly adopted this framework to study adhesive fatigue. CZM, as a constitutive modelling tool, focuses on simulating the FPZ—the localized region near the crack tip where damage accumulates. By shifting attention from global structural responses to FPZ mechanics, CZM provides a localized, mechanism-driven approach to quantify damage evolution. For instance, consider a DCB specimen under Mode I cyclic loading as Figure 2-5:

- The S-N curve may statistically correlate stress levels with crack initiation cycles but offers no insight into micro-mechanisms.
- Paris law empirically links macroscopic crack growth rates to stress intensity factors but neglects adhesive-specific FPZ effects.
- CZM, however, explicitly models traction-separation degradation within the FPZ, bridging microscale damage accumulation (e.g., void nucleation) to macroscale crack propagation.

Thus, the study of adhesive fatigue inherently involves interrogating these four domains—S-N curves, Paris' law, CZM, and FPZ characterization—through either specialized investigations or integrative methodologies. The following

sections will systematically explore these areas, highlighting both their individual contributions and synergistic potential.

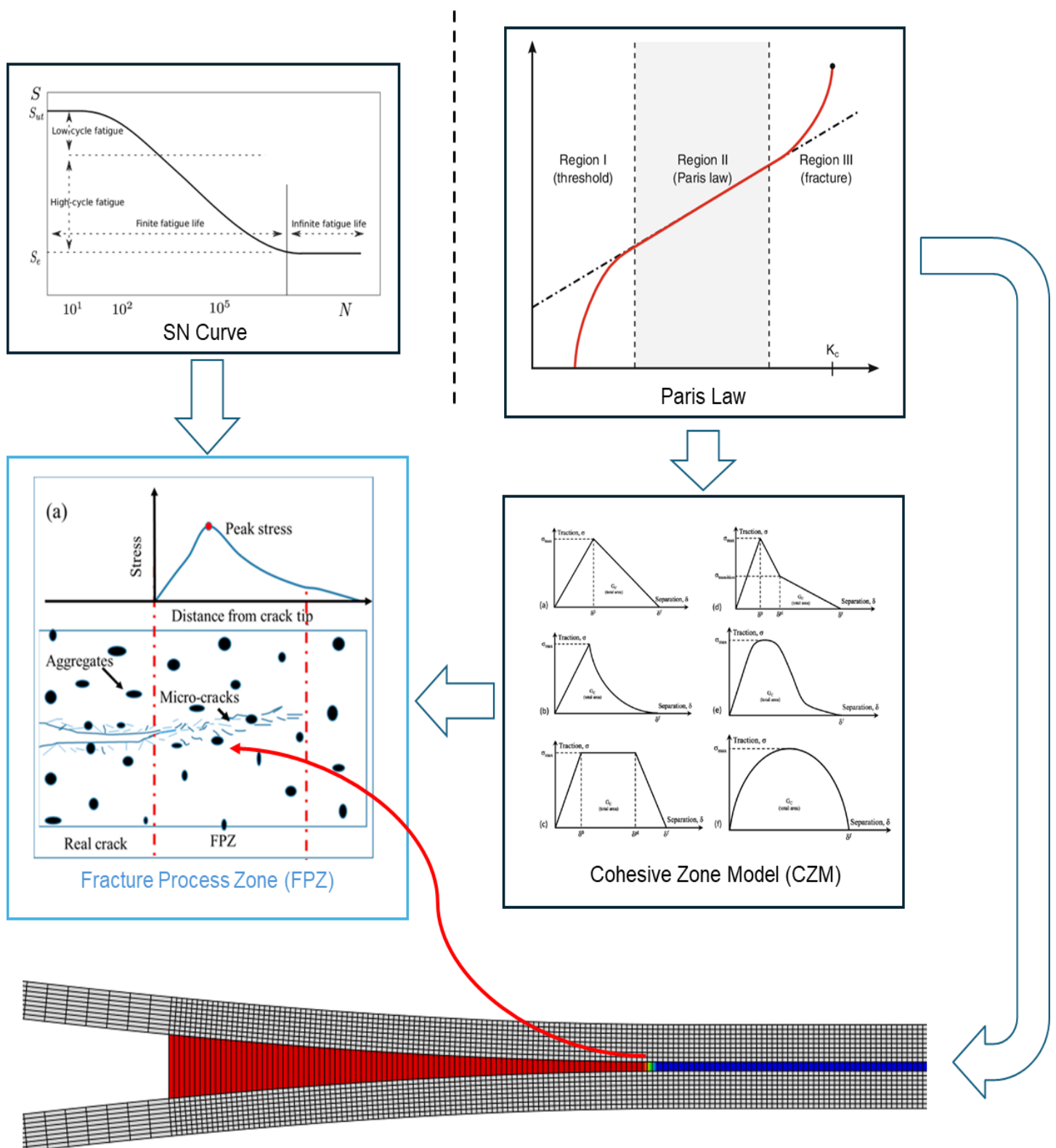


Figure 2-5 An overview of relations of different fatigue parameters.

2.3.2.1 S-N curve in Adhesively bonded structures

The S-N curve methodology, a cornerstone of fatigue analysis studies of metallic fatigue (*Fatigue and Fracture of High-Strength Steels : Improving Reliability in Strength Assessment*, 2024), has been pragmatically adapted to adhesive-bonded joints despite fundamental differences in failure mechanisms. This approach correlates nominal stress amplitudes (e.g., average shear stress) with the total number of cycles to failure (N_f), offering a simplified framework for industrial design and certification. As discussed in the preceding section on standardization, S-N curves for adhesive joints are typically derived from standardized tests such as ASTM D3166 for metal-to-metal bonded systems (Wei et al., 2024), where cyclic shear loading is applied to single-lap or double-lap configurations. However, ASTM D3166 explicitly specifies load-controlled fatigue testing protocols, effectively prioritizing load-life (L-N) curves as the industry-standard dataset. This reflects a critical limitation of the S-N approach in adhesive fatigue studies: while traditional S-N curves can empirically predict total life (N_f) and, under specific conditions, even estimate crack initiation cycles (N_i) (Adams & Harris, 1996), their reliance on nominal stress parameters proves inadequate for heterogeneous adhesive systems.

In adhesive joints, localized stresses (e.g., crack-tip stresses) diverge drastically from nominal values due to interfacial heterogeneity and geometric discontinuities. Consequently, load-controlled frameworks—which correlate external load amplitudes ($(P_{max}, \text{load ratio})$) with (N_f)—are more suitable for fatigue analysis of viscoelastic adhesives (Alves et al., 2018). By circumventing the ambiguities of stress parameterization, L-N curves provide a robust empirical basis for adhesive joint design. The Figure 2-6 illustrates the load-life relationship for an epoxy-aluminium single-lap joint under cyclic shear loading. The L-N curve enables direct fatigue life prediction without requiring complex stress analysis, aligning with industrial demands for rapid design validation.

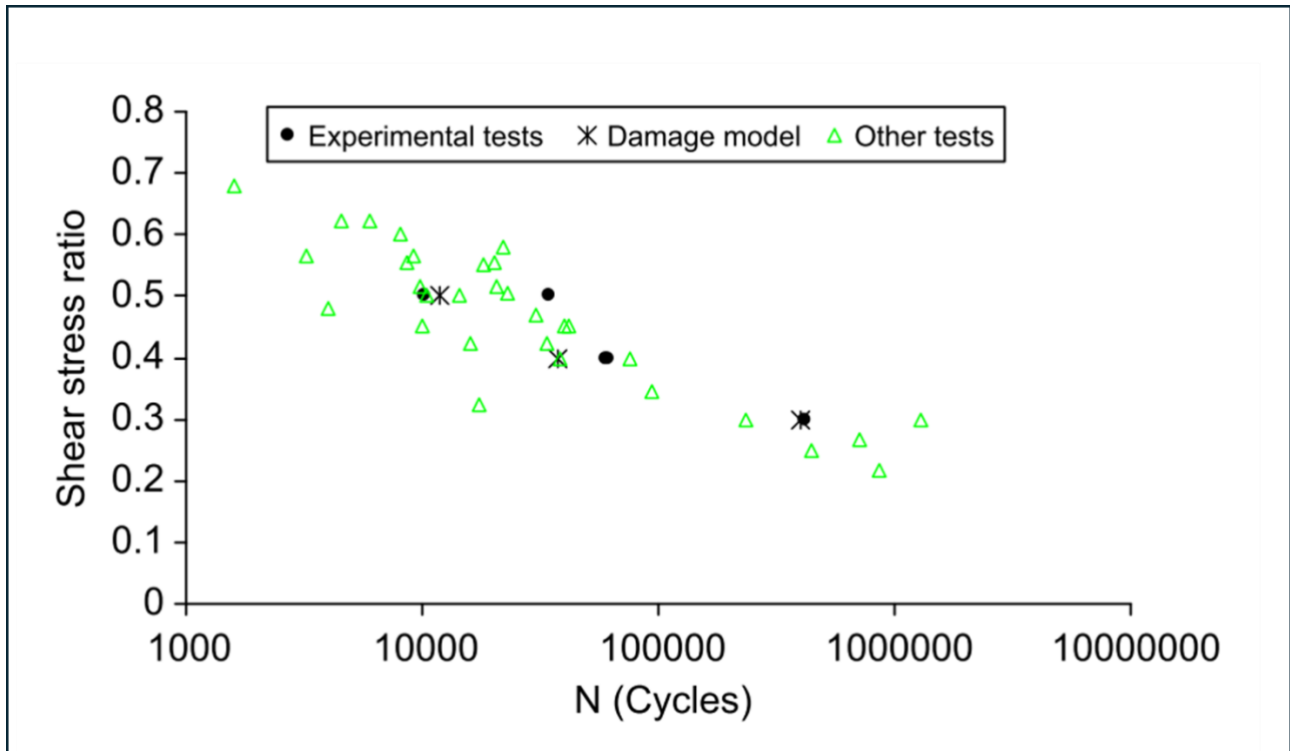


Figure 2-6 A typical L-N curve for ABJs.

The advent of progressive damage models in the 2010s marked a paradigm shift. Studies by (Graner Solana et al., 2010) demonstrated that S-N curves could validate CDM frameworks, where adhesive modulus and yield stress degraded cyclically. For instance, an elastic-plastic damage model calibrated with S-N data achieved $\pm 15\%$ accuracy in predicting blackface strain evolution and N_f for SLJs under spectrum loading (Graner Solana et al., 2010). This synergy highlighted the S-N curve's enduring role as a validation tool despite its mechanistic shortcomings. In fact, this is a great progress to some extent, it breaks the bridge between S-N curve and crack development and makes a reference for the further use of S-N curve.

However, through his research, it was also found many practical problems. Because the stress distribution area of elastoplastic coupling, low-cycle fatigue and high-cycle fatigue interlock affect the internal cracks, it is impossible to accurately distinguish the crack initiation stage or expansion stage.

Davila's study integrates S-N curves into a cohesive fatigue model as Figure 2-7, using idealized S-N data to calibrate damage parameters (β , γ) that govern crack propagation rates. By treating the quasi-static cohesive law as a fatigue damage envelope, the model predicts delamination growth under mixed-mode loading without requiring explicit Paris law experiments. Adjustments via Goodman diagrams account for stress ratio (R) effects, while Simplified Cyclic Loading (SCL) streamlines simulations by holding maximum loads constant. Although the approach successfully links S-N trends to fracture mechanics—demonstrated in DCB and MMB validations—it relies on linear S-N assumptions and uniform endurance limits ($\sigma_e/\sigma_c \approx 1/3$), limiting accuracy in nonlinear or environmentally degraded adhesives. Despite these constraints, the framework bridges empirical S-N data with physics-based modelling, offering a cost-effective tool for industrial fatigue prediction while highlighting the need for future refinements in capturing localized stress heterogeneity and hygrothermal effects.

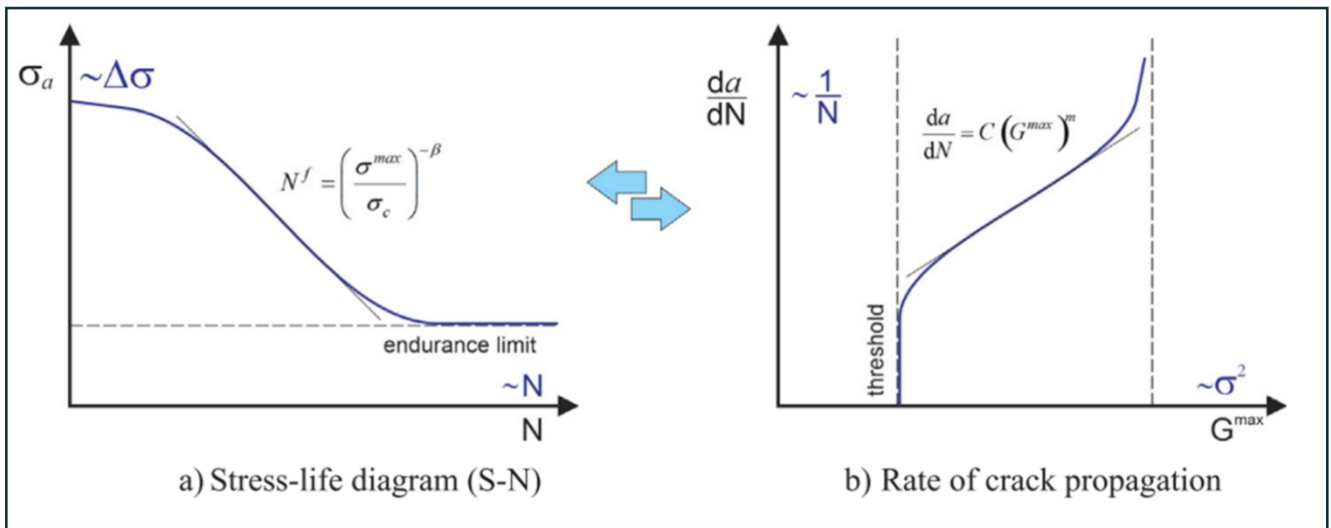


Figure 2-7 The relation of S-N curves and Paris' Law

2.3.2.2 “Paris Law” in Adhesively bonded structures

Paris Law, formulated by (Paris & Erdogan, 1963), originated from fracture mechanics studies on metallic fatigue. Their seminal work established a power-

law relationship between crack growth rate (da/dN) and the stress intensity factor range (ΔK):

$$\frac{da}{dN} = C(\Delta K)^m \quad (2-1)$$

Where C and m are material constants. This empirical model revolutionized fatigue crack growth prediction by linking macroscopic crack propagation to localized stress fields at the crack tip.

However, composites exhibit anisotropic behaviour and delamination driven by interfacial energy release rather than local stress concentration. This necessitated replacing ΔK with strain energy release rate ($SERR$, G) (Irwin, G. R., 1957), which globally quantifies energy dissipation during delamination. Currently, it is usually written in the following form and is also called “Paris Law”:

$$\frac{da}{dN} = C(f(G))^m \quad (2-2)$$

Where $f(G)$ is a function of strain energy release rate. Modifications included adopting G_{max} (peak energy) (Azari et al., 2013) or ΔG (energy range) (Yao et al., 2014) to account for mixed-mode loading (I/II) (de Morais & Pereira, 2008) and crack closure effects like fibre bridging (Yao et al., 2017). Further refinements addressed R -ratio dependence (Y. Li et al., 2012), temperature (lost, 1993)/ humidity impacts (Kamiya et al., 2011), and variable amplitude loading (larve et al., 2017). These adaptations transformed Paris Law into a framework balancing empirical correlations with energy-based physics, enabling broader applicability to composite fatigue delamination despite remaining phenomenological in nature.

Moreover, the application of strain energy release rate (G) is inherently constrained by the linear elastic assumption, which fails to account for large-scale plastic deformation, rendering it inadequate for describing damage in ductile adhesive layers. This limitation naturally necessitates an alternative parameter: the J -integral. Proposed in 1968 by the American mechanician

James R. Rice, the J -integral was originally developed to address crack-tip field analysis in elastoplastic materials. Traditional fracture mechanics parameters like stress intensity factor (K) and strain energy release rate (G) are restricted to linear elastic materials, yet materials such as metals often exhibit significant plastic deformation during crack propagation, invalidating linear elastic assumptions. Rice introduced a path-independent energy integral—the J -integral—defined as a contour integral around the crack tip:

$$J = \int (W dy - T \cdot \frac{\partial u}{\partial x} ds) \quad (2-3)$$

where W is the strain energy density, T denotes the traction vector along the contour path, u represents the displacement field, and ds is the differential arc length. Pioneering work by Yao's team applied J -integral theory to energy dissipation analysis in mode I fatigue delamination of composites under fibre bridging effects (Yao et al., 2014). Conventional Paris law (based on G) fails under fibre bridging due to violated self-similarity. Integrating J -integral theory with micro-damage analysis localized energy dissipation at delamination fronts, revealing cyclic strain energy storage/release in bridging fibres. Their refined Paris model reduced data scatter, showcasing J -integral unified fracture characterization across elastic/plastic regimes for complex composites.

The J -integral also exhibits limitations under cyclic loading conditions. In high-cycle fatigue, where crack-tip plastic zones are minimal (small-scale yielding) and material behaviour approximates linear elasticity, parameters like ΔK or ΔG remain more practical, as J -integral unnecessarily complicates modelling without clear advantages. Crucially, J -integral's calculation relies on plastic deformation energy, yet repeated crack-tip yielding during cyclic loading-unloading accumulates plastic strain, violating its path independence (Lu et al., 2021). Under ratcheting effects (asymmetric cyclic loading), progressive plastic strain accumulation drives continuous crack-tip field evolution, rendering J -integral inadequate for directly characterizing damage evolution rates. These constraints necessitate cautious parameter selection, prioritizing context specific applicability over theoretical generality.

Recent studies increasingly integrate Paris Law with CZM (Kawashita & Hallett, 2012) or damage mechanics to simulate fatigue damage mechanisms in bonded structures. While its phenomenological application is well-established, its mechanistic coupling with advanced frameworks will be further discussed in subsequent sections.

2.3.2.3 CZM in Adhesively bonded structures

CZM is a computational framework for simulating interfacial failure, focusing on crack initiation and propagation. Rooted in the pioneering work of (Barenblatt, 1962; DUGDALE, 1960), CZMs address stress singularities at crack tips by introducing a finite degradation zone ahead of the crack. Dugdale's "process zone" concept and Barenblatt's "cohesive crack" theory laid the foundation for modelling crack growth in ductile and quasi-brittle materials. Over time, CZMs have become essential for analysing adhesive joints, where progressive damage and mixed-mode failures dominate.

CZMs uniquely define traction-separation laws (TSLs) to govern interface behaviour under loading, capturing nonlinear damage from bonding strength to decohesion. Unlike traditional methods like VCCT or XFEM, CZMs require no predefined crack paths, enabling simulations of complex failures in aerospace, automotive, and composite structures.

This study synthesizes decades of CZM research, emphasizing advancements in fatigue modelling and numerical implementations. Early work established static crack analysis, while later studies (e.g., (Degrieck & Van Paepegem, 2001)) introduced cyclic damage mechanisms and unified crack initiation-propagation criteria. Recent innovations, such as parameter-free methods (Bak et al., 2017) and nonlinear damage calibration (Wei et al., 2024), aim to reduce experimental dependencies. Challenges remain, including empirical parameter reliance, limited validation under extreme loads, and the need for universal TSLs. Future directions prioritize machine learning for parameter optimization, unified crack evolution frameworks, and applications in extreme environments. This evolution underscores CZM's role in balancing model simplicity, physical accuracy, and computational efficiency.

The following Table 2-4 summarizes key CZM studies, with emphasis on fatigue-related contributions:

Table 2-4 A comprehensive assessment of different CZMs.

Ye ar	Resear chers and Refere nce	Study Object	Key Contribution s/Methods	TSL Type	Fatigue- Related Contrib utions	Limitations/In novations
19 60	(DUGD ALE, 1960)	Metallic material s	Proposed the "process zone" concept to eliminate stress singularity at crack tips.	-	-	Limited to ductile materials; no consideration of quasi-brittle materials or fatigue loads.

1962	(Barenblatt, 1962)	Quasi-brittle materials	Introduced the "cohesive crack" model, establishing the theoretical foundation for CZM.	-	-	No dynamic or fatigue load analysis.
1987	(Needleman, 1987)	Composite interface debonding	First application of CZM to simulate interface failure in composites.	-	-	Static analysis only; no fatigue damage considered.

1996	(Camacho & Ortiz, 1996)	Dynamic cracks in brittle materials	Developed irreversible CZM to simulate damage accumulation.	-	-	Not explicitly applied to fatigue delamination.
2000	(Foulk et al., 2000).	Metal matrix composites	First explicit simulation of irreversible interface behaviour under cyclic loading.	Empirical	✓ Incorporated unloading-reloading constitutive models.	Parameters relied on empirical fitting.

2001	(Degrieck & Van Paepegem, 2001)	Quasi-brittle fatigue cracks	<p>Proposed a polynomial fatigue damage model validated against the Paris-Erdogan law via BEM.</p> <p>Unified crack initiation and propagation by including</p>	Polynomial	✓ First inclusion of unloading-phase damage.	Required additional crack propagation criteria.
2001	Nguyen et al.	Microstructural short cracks	<p>Introduced plasticity and hysteresis effects to model non-Paris crack growth.</p>	Exponential decay	✓ Simplified cyclic damage calculation.	Lacked physical mechanisms; difficult to extend to macro-cracks.

2005	(Robinson et al., 2005)	Composite fatigue delamination	Separated static and fatigue damage mechanisms using staged damage parameters.	Coupled criterion	✓ Decoupled static and fatigue damage.	Parameter selection lacked physical basis.
2007	(Turon, Costa, et al., 2007).	FE-CZM fatigue analysis	Proposed a superposition model for static and fatigue damage with recalibrated process zones. Combined a Paris-law variant with cycle-	Bilinear	✓ Estimated fatigue process zone length.	Required parameter calibration; assumed fixed static-fatigue zone ratio.

2007	(Turon, Dávila, et al., 2007)	Composite high-cycle fatigue	Combined Paris-law variant with cycle-dependent total work.	Bilinear	✓ Calibrated cohesive zone length (acz) based on ΔD_{max} .	Mesh sensitivity required engineering optimization.
2010	(Harper & Hallett, 2010)	Adhesive joint fatigue damage	Coupled CZM with fracture mechanics, using Paris-law-driven SERR for damage parameters.	Mixed mode	✓ Required calibration of quasi-static cohesive zone length.	Limited validation on independent datasets.

2012	(Kawashita & Hallett, 2012)	Dynamic energy release rate	Developed crack-tip tracking algorithm to avoid predefined process zones.	Dynamic model	-	High algorithmic complexity.
2016	(Nojavan et al., 2016).	Fatigue delamination propagation	Unified crack initiation and extension using S-N curves and displacement-based power laws.	Power-law	✓ Integrated initiation and propagation.	Relied on experimental calibration.

2015	(Bak et al., 2015).	High-precision prediction	Parameter-free CZM with high robustness against interface stiffness variations.	Non-prior model	✓ No fitting parameters; high accuracy.	High computational cost for complex structures.
2020	(Dávila, 2020b)	Heuristic fatigue modelling	<p>Predicted Paris-law behaviour using only static and initiation data.</p> <p>Predicted Paris-Erdogan behaviour using static and</p>	Heuristic	✓ No additional parameters required.	Limited validation under complex loading.

2023	(Wei et al., 2024)	Nonlinear damage interfaces	Proposed a prior-free parameter calibration method for nonlinear damage evolution.	Non-prior model	-	Enabled precise simulation of nonlinear damage.
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The evolution of CZMs reflects a trajectory from foundational theories to advanced fatigue and multi-physics applications. Early studies focused on static crack analysis, while later work integrated cyclic damage mechanisms, fracture mechanics, and parameter-free methods. Challenges such as empirical dependencies and mixed-mode TSL limitations persist, but emerging trends like machine learning and extreme-environment modelling offer promising solutions.

2.3.2.4 FPZ

The FPZ as shown in Figure 2-8 is a finite region ahead of a crack tip where material degradation occurs through micro-cracking or plasticity. Originating from (Barenblatt, 1962; DUGDALE, 1960), the FPZ resolves classical fracture mechanics' stress singularities by modelling crack growth as a gradual

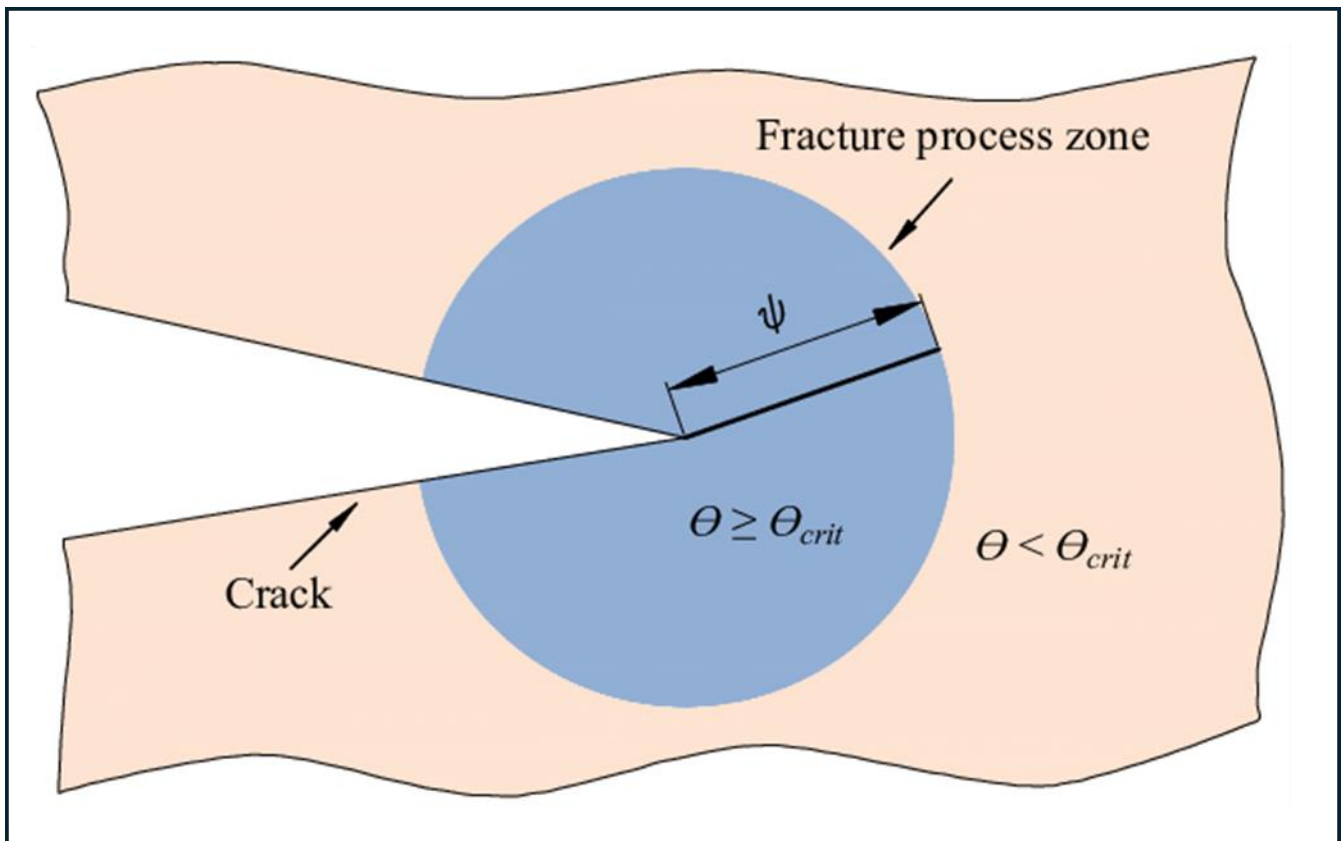


Figure 2-8 A schematic diagram of fracture progression zone.

separation process. In adhesive materials, this concept is critical for capturing progressive interface failure.

2.3.2.5 The physical meaning of FPZ

The FPZ provides crucial mechanistic insights bridging continuum fracture mechanics and material damage physics. Traditional J -integral theory, rooted in nonlinear elastic deformation theory (Hutchinson, 1974), encounters fundamental limitations when describing plastic unloading during crack propagation. This necessitates the application of incremental plasticity theory to properly characterize energy dissipation mechanisms (Lu et al., 2019) In elastic-plastic materials, the crack tip exhibits weakened stress singularity compared to purely elastic cases, theoretically leading to vanishing crack driving forces that contradict experimental observations of sustained crack growth (Lu et al., 2022).

The introduction of FPZ resolves this paradox by restoring finite energy dissipation through two essential mechanisms: (i) the non-singular stress redistribution within the micro-damage zone ahead of the crack tip, and (ii) the explicit consideration of work conjugate quantities for damage evolution (Xiao et al., 2017). This framework provides physical justification for non-zero crack driving forces required to overcome the material's intrinsic fracture resistance.

Furthermore, the FPZ concept establishes critical connections between macroscopic fracture parameters and microscopic failure mechanisms. For instance:

- Crack tip opening angle (CTOA) correlates with void growth kinetics in ductile metals (Lu et al., 2020)
- Crack tip opening displacement (CTOD) relates to microcrack coalescence in quasi-brittle materials (Cui et al., 2014)

(Allegri, 2020) provided an insightful analogy comparing fracture propagation to adhesive tape peeling. Just as tape separation occurs through progressive fibril rupture across a finite peel zone rather than instantaneous interfacial failure, material fracture propagates via distributed damage evolution within the FPZ. This process zone essentially "smears" the discontinuous fracture event into a finite domain governed by cohesive traction-separation laws. The characteristic FPZ length scale (L_{pz}) emerges as a fundamental material property, satisfying the condition $L_{pz} \gg$ microstructure characteristic length to maintain continuum-scale validity (Lu et al., 2021).

2.4 Summary

This literature review explores the fatigue behaviour of ABJs, emphasizing their multifactorial failure mechanisms under cyclic loading. Key determinants include mechanical parameters (load amplitude, frequency), material properties (adhesive viscoelasticity, interfacial adhesion), and environmental influences, with failure modes categorized into adherend, cohesive, interfacial, and hybrid types. Experimental methodologies, such as standardized ASTM tests and advanced in-situ monitoring (digital image correlation, acoustic emission),

provide critical data for validating computational models. Theoretical frameworks like S-N curves and Paris Law, while foundational, inadequately address adhesive-specific complexities such as interfacial heterogeneity, crack migration between phases, and viscoelastic-plastic coupling. CZM and FPZ analysis offer more nuanced insights by simulating traction-separation degradation and localized damage accumulation but require integration with empirical and fracture mechanics approaches. Future research must prioritize a unified methodology coupling theory, experiment, and simulation to resolve fatigue crack propagation—the critical nexus of damage evolution in ABS. This entails advancing beyond traditional S-N Curve/ Paris Law paradigms by embedding FPZ dynamics and CZM into multiscale frameworks that account for mixed-mode loading, interfacial constraints, and path-dependent crack growth. Such an approach would bridge macro-scale life prediction with micro-scale damage mechanisms, enabling robust fatigue analysis tailored to the unique challenges of bonded structures.

3 Methodology

The primary objective is to establish a comprehensive damage evolution model for Chapter 4, beginning with the implementation of the CZM to characterize constitutive relationships across distinct damage phases. The formulation integrates traction-separation laws to simulate progressive interfacial degradation under cyclic loading. Subsequently, the inverse validation process is elaborated, detailing parameter calibration strategies to align numerical predictions with empirical observations. The latter sections systematically describe experimental protocols, including specimen fabrication procedures (e.g., surface pretreatment, adhesive curing conditions) and instrumentation configurations. Finally, a concise overview of data acquisition and post-processing methodologies is provided, ensuring reproducibility of fatigue testing outcomes. This integrated approach bridges theoretical modelling, numerical validation, and experimental verification to address the multiscale nature of adhesive fatigue mechanisms.

3.1 Theoretical Framework of Damage Evolution

Building upon the preceding discussion of the CZM, this framework fundamentally characterizes the constitutive behavior of the FPZ by adapting its parameters to distinct damage phases. By embedding tailored CZM formulations into the actual crack propagation process, a holistic damage evolution model is established. As illustrated in Figure 3-1, a simplified 2D DCB model, the primary focus of this study, is employed to define the progressive damage stages under loading. The adhesive layer undergoes three distinct phases: undamaged (linear elastic or pre-displacement), damaged (coupled quasi-static and fatigue degradation), and fully failed (cumulative damage coefficient = 1).

In the damaged phase, quasi-static degradation follows a bilinear traction-separation envelope, while fatigue degradation is governed by cycle-dependent damage accumulation. The superposition of these mechanisms manifests as a progressive stiffness reduction in the adhesive layer. Figure 3-1 further depicts

the FPZ evolution through a displacement-distance profile ahead of the crack tip. Notably, compressive stresses near the tip (induced by beam bending) create a transition zone where tensile strain initiate ($\epsilon = 0$). While this transition point is conventionally defined as the FPZ onset in literature, its deviation from the actual FPZ origin becomes non-negligible only under specific conditions—such as high adhesive toughness or low-cycle fatigue with dominant plastic deformation. This simplification aligns with the study’s objective to develop a generalized framework for fatigue damage analysis in bonded structures, balancing computational efficiency and mechanistic fidelity.

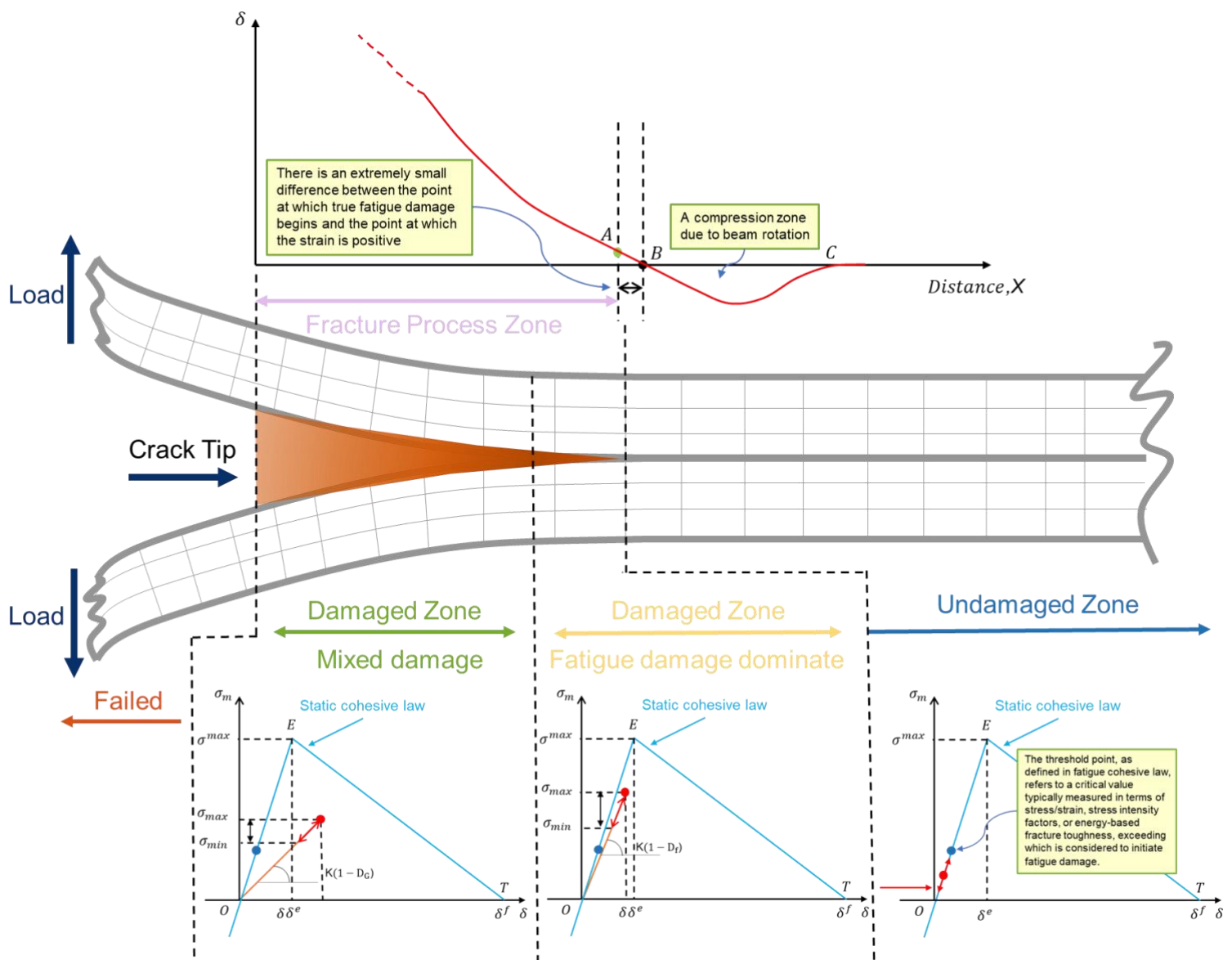


Figure 3-1 An overview of the damage model and corresponding damage stage.

3.2 Simulation Inversion Work

The simulation will limit the discussions to mode-I growing cracks in two-dimensional elastic–plastic bodies to avoid geometrical and notational complications. For simplicity, the heat transfer and body forces throughout will be ignored. This work investigates various CZM damage models and examines their applicability to Mode I adhesive structures. Inspired by these investigations, a two-dimensional DCB model is developed, and the framework for a VUMAT subroutine based on this model is constructed. A Python script is employed to automatically adjust the material parameters of the damage models and control the ABAQUS finite element software for automated computations. The outputs include key physical quantities under different parameter combinations, such as the geometric characteristics of the FPZ, strain distribution, and the Paris Law relationship. A comparison between these numerical results and experimental data is made to determine the optimal damage model parameters. The validation process, as illustrated in the Figure 3-2, is then adopted for simulation-based inversion analysis.

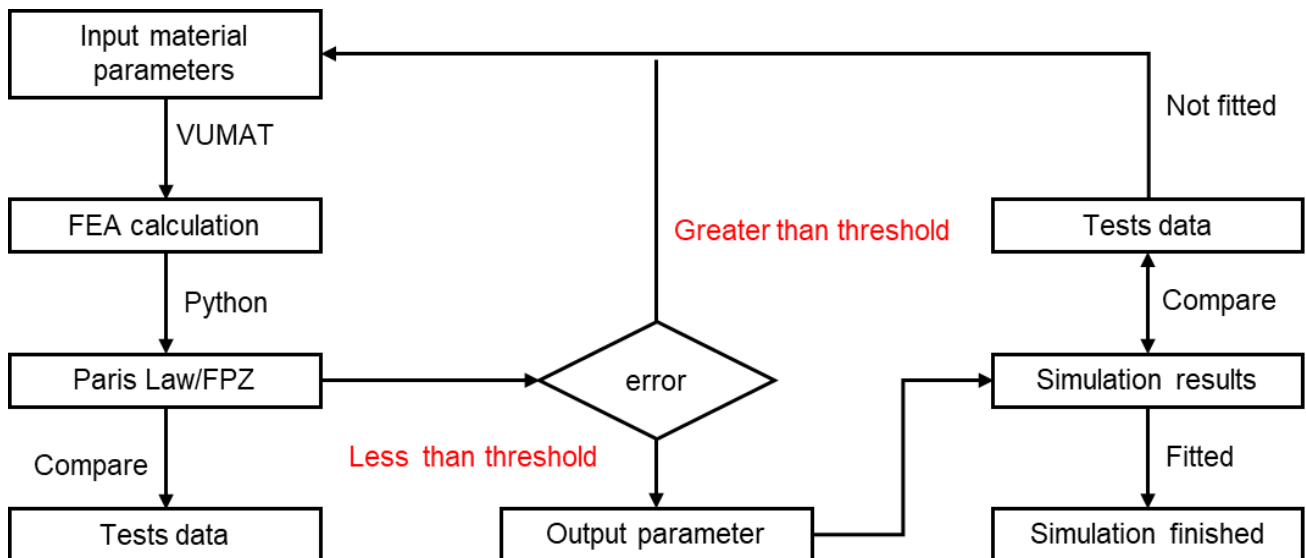


Figure 3-2 Reverse verifies the simulation logic flow chart.

To ensure the accuracy of the simulation inversion results, the simulation outputs for additional physical quantities, such as the strain field within the FPZ and the mechanical response curves under the optimized parameters, are compared with experimental values. This comparison verifies the accuracy of the parameter fitting and assesses the quality of the simulation inversion results.

3.2.1 Material Constants Calibration

For the damage models, a parametric study of material constants is necessary. The method of average distance for coincidence comparison is considered reliable and efficient. By varying the material parameters in the fatigue damage model, optimal matching between two points can be achieved, enabling the simultaneous fitting of the in-plane FPZ size and Paris Law under specific loading conditions. A Python script is developed to automate the process of invoking Abaqus for finite element simulations under different damage model parameters and to output the simulation results.

A two-dimensional fitting space is constructed using the upper and lower bounds of c and m values, and the optimal values are determined through t rounds of optimization. The fitting process for the two parameters is briefly described below:

1. **Identifying bounds:** Set the upper and lower bounds for each of the two parameters, divide each parameter range into m segments, and perform finite element simulations for all m^2 parameter combinations.
2. **Dimensionality reduction:** Compare the results of the simulations, select the four nearest points with the smallest errors to form a new two-dimensional space, divide the new bounds into $m-1$ segments, and compute finite element models for the $((m-1)^2)$ parameter combinations.
3. **Error assessment:** After t rounds of dimensionality reduction, if further division is not possible ($m-t=1$), check whether the error at each parameter node is below the threshold. If it is, output the average values of the four parameter nodes; if not, repeat step 1.

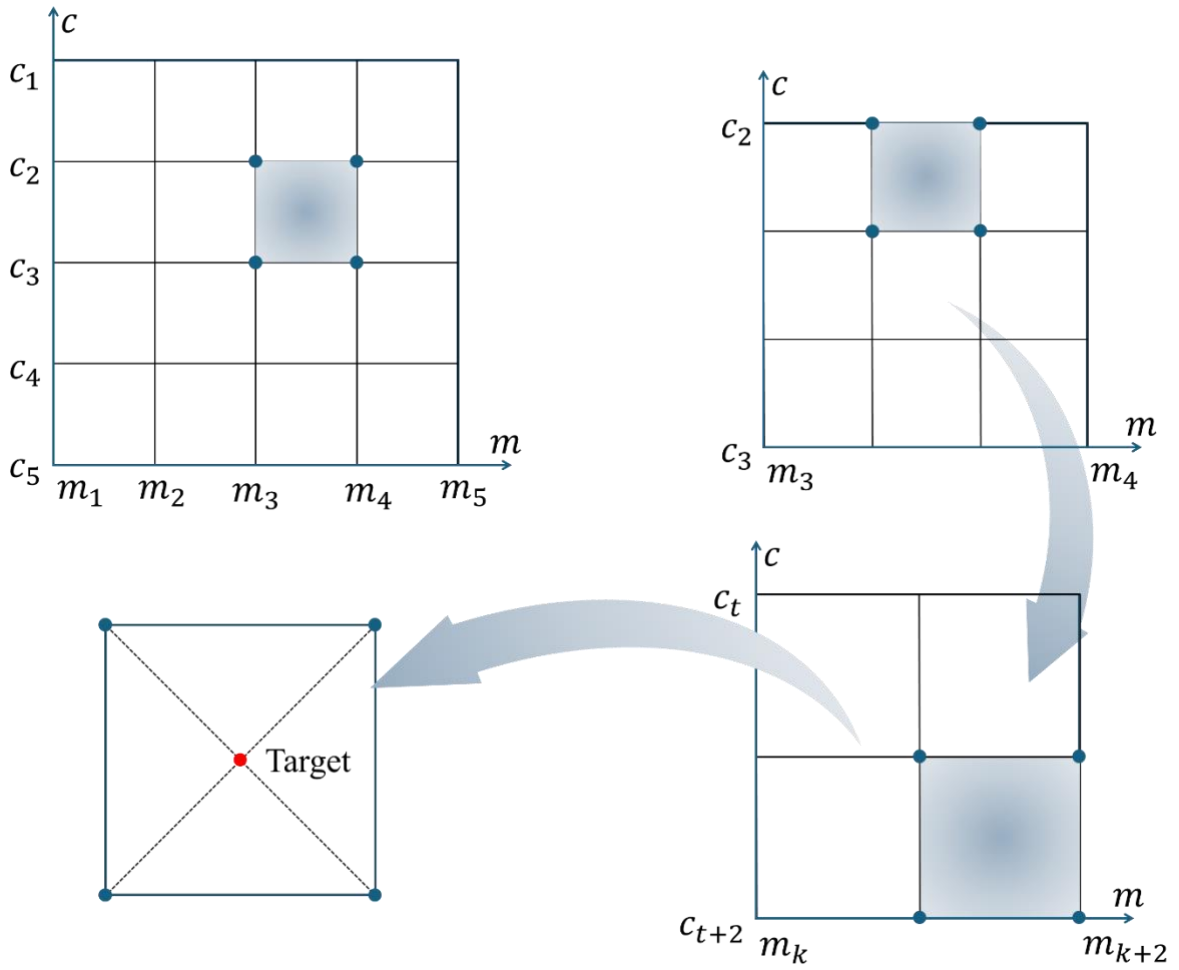


Figure 3-3 Two-parameter two-dimensional mesh search fitting procedure.

3.3 Experiment

3.3.1 Specimen Description

The DCB specimen is commonly used to investigate Mode I fracture behaviour of materials. Due to the similarity between the debonding of bonded joints and the delamination of composite laminates, the dimensions of the specimens in this study were designed based on the ASTM 5528 standard. This section describes the preparation process of the joints, including a stable phosphoric acid anodizing surface treatment. Methods to effectively control the adhesive layer thickness are also explored, enabling simple and efficient mass production

of specimens with consistent failure modes, which forms a solid foundation for formal testing.

The specimen shape adheres to the ASTM 5528 standard, with a design length $\times$ width of 140 mm $\times$ 20 mm and a prefabricated crack length of 57 mm, as illustrated in Figure 3-4. As substrate thickness is a variable in this study, aluminium alloy substrates with thicknesses of 5 mm, 8 mm, and 15 mm were selected.

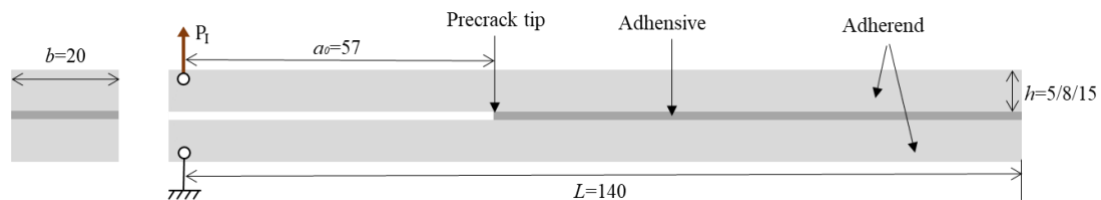


Figure 3-4 Specimen size diagram.

The aluminium alloy used is GB7075, which has a Young's modulus of 71 GPa. This alloy is a typical high-strength aluminium used in the aerospace industry. The adhesive is 3M 2216, a two-component epoxy structural adhesive known for high shear and peel strength, resistance to extreme impacts, vibrations, and bending, and is widely used in encapsulation and transportation sectors. Specific parameters are shown in Table 3-1.

Table 3-1 Material parameters of commercial adhesive 3M-2216.

Name	Mixing ratio (mass) B: A	viscosity (CP)	Mixing working time (minutes)	Time to operating strength (hours)	T-type stripping strength (kN/m)	Overlapping shear strength (MPa)
2216	5: 7	80000	90	10	25	2500
B/A						

To achieve good bonding performance, an electrochemical method was employed for aluminium surface treatment, forming a stable oxide layer. This oxide layer is suitable not only for bonding but also provides stability when the bonded assembly is exposed. Common electrochemical methods include etching and anodizing. The etching method does not produce a repeatable oxide layer that enhances the durability of bonded assemblies under thermal and humid conditions. Therefore, anodizing is the mainstream method for large-scale industrial applications, which will be the focus of this subsection.

Conventional anodizing solutions typically use single solvent formulations, with sulfuric acid, chromic acid, and phosphoric acid being the most common in industry. The primary differences among these anodizing treatments lie in the oxide thickness, pore size, and chemical composition. Sulfuric acid anodizing produces a thicker oxide layer with smaller pores and some sulphate content. Chromic acid anodizing results in a thinner oxide layer with larger pores, consisting essentially of pure Al_2O_3 . By controlling the anodizing process and applying stepwise voltages, an oxide layer with abundant pores can be obtained. Phosphoric acid anodizing yields the thinnest oxide layer, featuring an open, highly porous structure with some phosphate ions. After a comprehensive

comparison of several methods, this study adopts a phosphoric acid and organic acid anodizing approach, as the process flow shown in Figure 3-5.

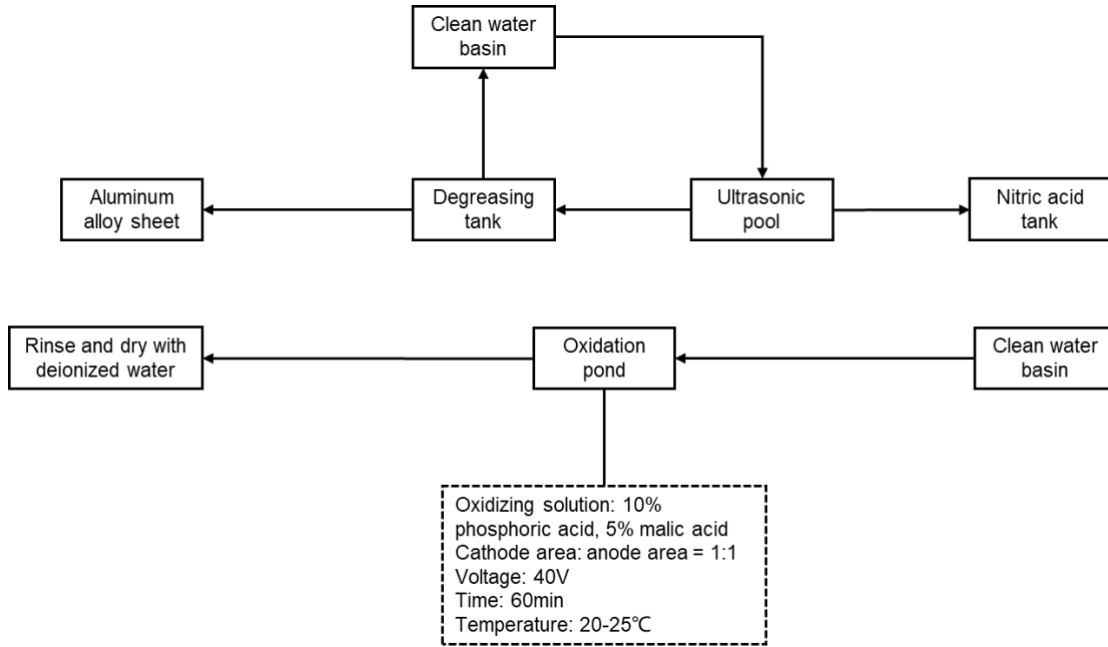


Figure 3-5 Flow chart of anodizing process of phosphoric acid and organic acid.

The anodizing solution comprises 10% phosphoric acid and 5% malic acid.

Lead plates serve as the cathode, while the aluminium plates to be treated act as the anode, maintaining a cathode-to-anode area ratio of 1:1. The voltage is held at 40 V for 60 minutes. The oxide layer formed on the aluminium surface under this procedure has pore sizes of 80–100 nm, as measured by microstructure analysis. The adhesive layer thickness is typically controlled by adding spacers to the adhesive, preventing excessive compression of the substrates during bonding. In this study, glass microbeads were used as spacers, added to the adhesive at a mass fraction of 1%. The microbeads are uniformly distributed throughout the adhesive layer, acting as point supports, and can be sieved to select microbeads of specific diameters, ensuring uniform

adhesive thickness. The selected microbeads have diameters between 0.3 mm and 0.35 mm, resulting in an adhesive layer thickness of 0.373 mm.

To maintain high bonding quality, the bonding process for each specimen batch must follow the same standardized procedure. Figure 3-6 illustrates the entire adhesive bonding process. To improve efficiency, larger aluminium sheets can be bonded at once and then machined into multiple specimens after curing.

Prefabricated cracks are introduced by attaching a 60 mm long polytetrafluoroethylene (PTFE) film to one end, with a small amount of adhesive applied to ensure close adhesion to the substrate and minimize air bubbles. The adhesive components A and B are mixed, and pre-sieved glass microbeads (1% mass fraction) are added. To enhance wetting of the adhesive on the substrate surface, the substrate can be preheated to 50°C, reducing adhesive viscosity and improving adhesion. The adhesive is applied in a single direction, ensuring both substrates are evenly coated. The substrates are then gradually pressed together from one end, avoiding bubble formation. Compression can be applied using a universal testing machine to ensure good contact between the adhesive and the substrate surface, but the pressure should not exceed 1000 N to prevent crushing the microbeads. Once the adhesive reaches its working time, and viscosity increases significantly, the bonded sheets should be removed, shaped, and cured in a drying oven. After curing, excess adhesive is removed, and the specimens are cut to the specified dimensions. The specimen edges can be polished using wet sanding, progressing from 400-grit to 800-grit to 2000-grit sandpaper, and adhesive layer quality is checked under an optical

microscope. The resulting adhesive layer shows no noticeable air bubbles and has a thickness between 0.36 mm and 0.39 mm.

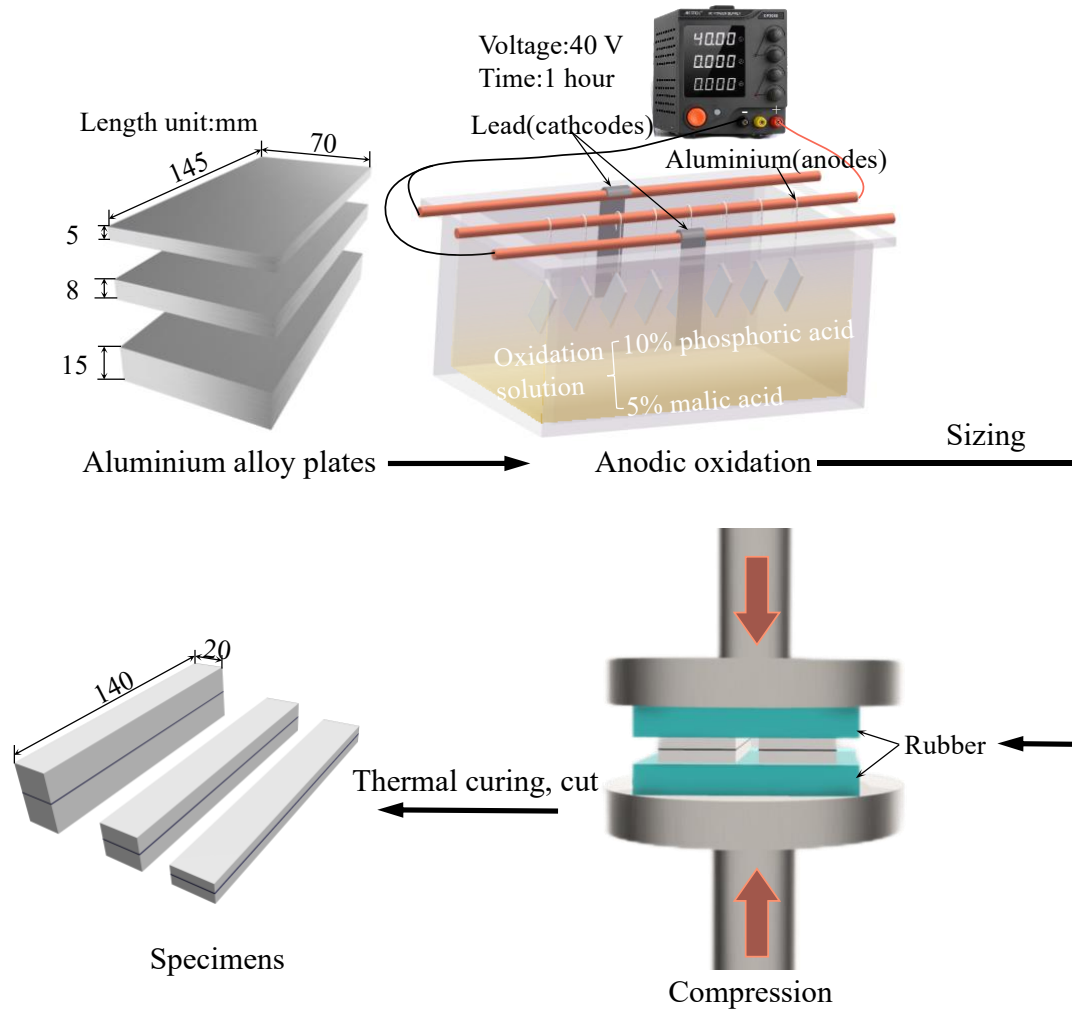


Figure 3-6 The whole process flow chart of specimen treatment.

3.3.2 Test Configuration

To determine the fracture properties of bonded specimens under quasi-static tensile conditions, a static test platform was established, as shown in Figure 3-7 (a). The experiments were conducted on an Instron 5848 testing machine equipped with a load cell with a 2000 N capacity and a measurement accuracy of 0.5%. Load and displacement data were collected in real-time at a frequency of 10 Hz. The loading mode was displacement-controlled, with different loading

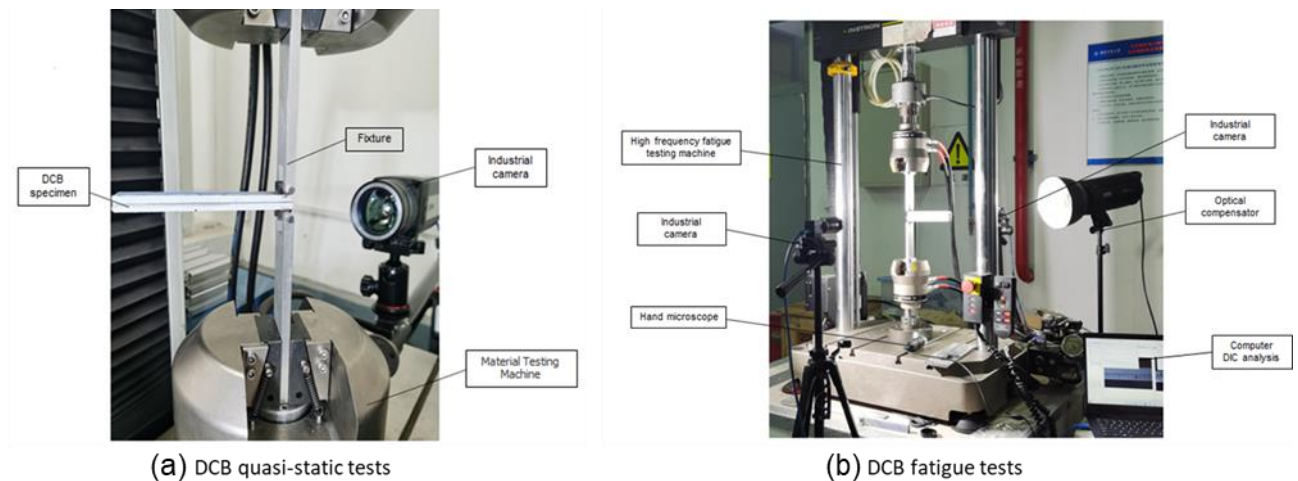


Figure 3-7 Quasi-static and fatigue tests configuration.

rates set for various specimens to achieve similar loading durations. Before testing, the alignment of the two grips was calibrated, and a square ruler was used to ensure they were perpendicular to the specimen plane, preventing specimen twisting or misalignment during the test.

Outside the loading system, a DAHENGTM industrial camera with a 37 mm focal length lens was used to capture the test images. The camera was synchronized with the experiment to facilitate subsequent DIC analysis, which was used to determine the global strain and displacement fields of the specimen during loading. For optimal speckle quality in the DIC analysis, the lighting was adjusted to evenly illuminate the specimen without overexposure. The camera plane was kept parallel to the specimen plane, and the uncracked upper and lower edges of the specimen needed to remain parallel to the image edges to ensure accurate 2D measurement system matching. A scale was attached to the specimen edge to calibrate the pixel-to-length conversion. All tests in this study were conducted at room temperature.

To investigate the Mode, I crack propagation characteristics of bonded specimens, a fatigue test platform was designed and constructed, focusing on local strain measurements at the crack tip, as shown in Figure 3-7 (b). The cyclic loading tests were performed on an Instron 8872 hydraulic servo testing machine, with a 5000 N load cell. Since the load cell exhibited significant measurement deviations when the load fell below 50 N, an additional 2000 N

load cell was used as a reference. Two cameras were employed to capture deformation images from both sides of the specimen. The rear camera setup was identical to that used in the static tests: a DAHENGTM industrial camera with a 3 mm focal length lens and an image resolution of 5496 pixels × 3672 pixels, capturing full-field deformation data. The front camera was a BASLERTM global shutter industrial camera with a telecentric zoom lens, providing an image resolution of 2592 pixels × 2048 pixels and covering a recording area of approximately 4 mm per image. This camera was mounted on an XYZ three-axis automated stage, which allowed it to move along a pre-programmed path, automatically capturing and returning to specified exposure positions.

3.3.3 In-situ Characterization

The main characteristics of fatigue tests are the high vibration speed and long duration, which make it impossible to record the full deformation process with a camera, as is done in quasi-static tests. Since DIC technology fundamentally compares the movement of speckle patterns in a sequence of images relative to their initial positions, in situ crack observation requires image capture at the same displacement points. Therefore, in this fatigue test, displacement control was used. The testing machine paused at target cycle numbers, triggering the camera to capture images and preventing motion blur caused by vibrations. The positions for pausing were set at the peak displacement points. Additionally, in displacement control mode, the crack propagation speed gradually slows down over time (Yao et al., 2017). To improve test efficiency, the test frequency gradually increased, and the image recording intervals were correspondingly extended. Although the frequency of cycles has been found to affect fatigue life in some materials (Al-Khateeb & Ghuzlan, 2014), all specimens in this study experienced the same loading process, so it is considered that frequency changes do not impact the consistency of the results.

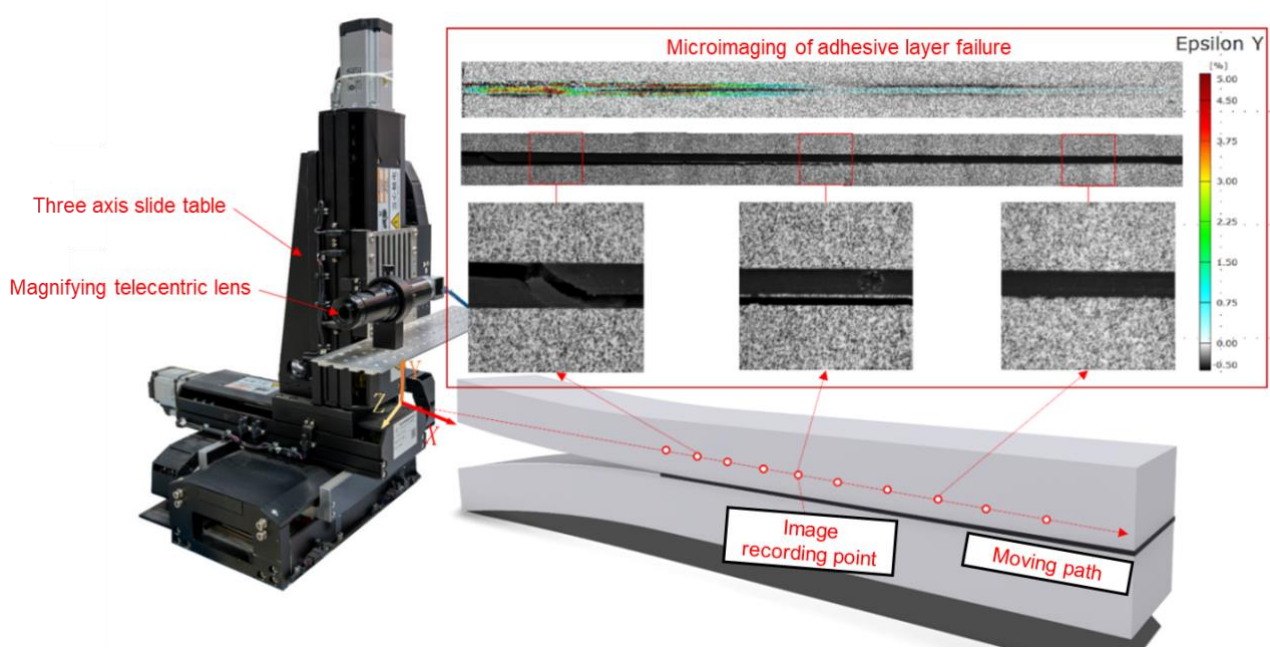


Figure 3-8 Microscopic thickness inspection.

The adhesive layer thickness in the bonded specimens is relatively small, occupying less than 1% of the image area, making it difficult to maximize camera resolution using traditional crack propagation recording methods. By using a telecentric zoom lens, the field of view can be focused locally, with the adhesive layer utilizing over 15% of the pixels in a single image. However, since the crack propagation path is 80 mm long and the telecentric lens can only capture an area of approximately 4 mm, single images cannot record the entire crack propagation process. To balance local clarity of the adhesive layer with overall continuity, a local strain measurement system was designed, as shown in Figure 3-8. This system used an XYZ three-axis sliding stage, with a telecentric zoom lens and a global shutter industrial camera. During each testing interval, the stage followed a pre-programmed path and exposure positions to capture images, which were later stitched together to reconstruct the deformation information of the entire adhesive layer. For the same specimen, the optimal imaging path was established through pre-test calibration, and consistent image capture points were set to meet the requirements of fixed-point DIC photography. The repetitive movement and fixed-point capture by the XYZ stage enabled continuous scanning and

recording of adhesive layer deformation. The collected images were input into DIC software in chronological order to analyze the adhesive layer's deformation.

Due to the stage's vibrations and lens distortion during testing, the speckle images required corrections to minimize errors. A self-calibration method based on the experimental setup was used to calibrate the camera(Z. Zhang, 2000a). Additionally, to address vignetting and chromatic aberration at the lens edges, a custom program was developed to batch-crop the images.

Camera calibration is the process of accurately measuring and computing the camera's internal and external parameters to establish a mathematical model, allowing pixel coordinates to be converted into real-world coordinates and correcting image distortions caused by the camera itself. Internal parameters include the principal point coordinates, focal length, and distortion coefficients, while external parameters refer to the camera's position and orientation in 3D space. In this experiment, a checkerboard calibration target with square grid sizes of $0.25\text{ mm} \times 0.25\text{ mm}$ was photographed 30 times from different angles. The Camera Calibrator tool in MATLAB was then used to solve the calibration parameters, as illustrated in Figure 3-8, which shows the camera sensor's spatial position relative to the calibration board. The calibration procedure was as follows:

Input the specifications of the calibration board, including the grid size and number of corners, into the Camera Calibrator, and import the images for automatic corner detection, as shown in Figure 3-9 (a).

Perform automatic calibration to obtain internal parameters and the reprojection error. Images with excessive reprojection errors were excluded, keeping the error below 1, as shown in Figure 3-9 (b). The reprojection error is a crucial indicator of calibration accuracy, the smaller the value, the more accurate the calibration. A reprojection error below 0.5 is considered excellent, while errors between 0.5 and 1 are acceptable. Errors greater than 1 indicate inaccurate calibration, requiring a review or optimization of the calibration process. In this experiment, the average reprojection error was 0.59, which was deemed acceptable.

Export the camera parameters from the Camera Calibrator to MATLAB, batch-correct the images, and then perform batch cropping. Only the central 2 mm-wide area of each image was retained, as shown in Figure 3-9 (c).

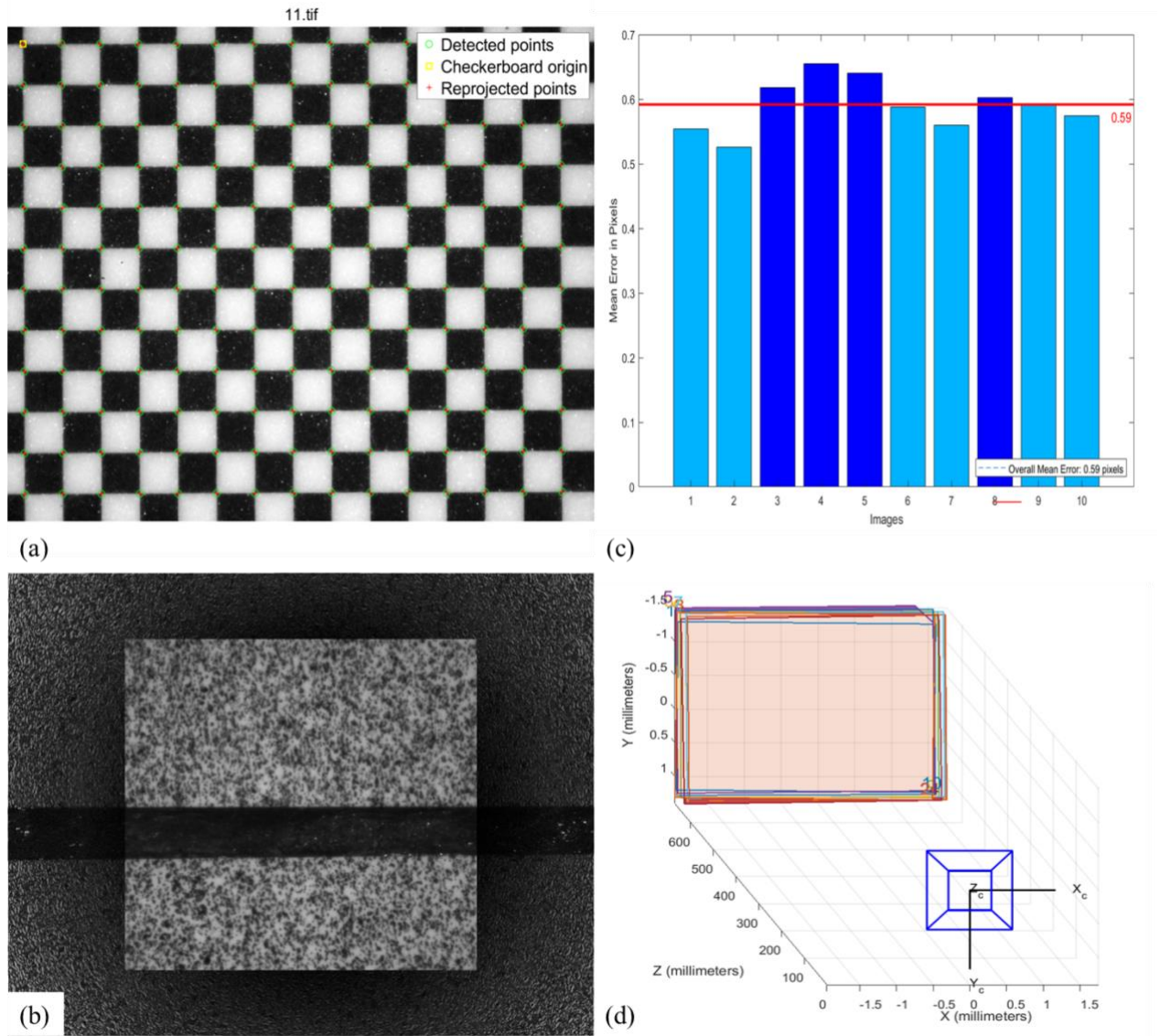


Figure 3-9 DIC image batch processing flowchart.

3.3.4 Data Processing

The data processing methods used during the experiment includes image processing and the handling of load-displacement information recorded by the loading system. Images captured during the test primarily document the deformation of the specimens, from which DIC analysis can extract key information such as the opening angle at the initial crack tip, crack length, and

strain distribution in the adhesive layer. The resolution of the full-field speckle images corresponds to 1 pixel per 24.5 μm and is analysed using Aramis software, with a subset size of 25 and a step size of 15. For the local speckle images, the resolution is 1 pixel per 1.5 μm , and these images are analysed using XTDIC software, with a subset size of 15 and a step size of 10. By coupling the information obtained from DIC analysis, such as the opening angle and crack length, with the original load-displacement data from the loading system, it is possible to calculate physical parameters that characterize the damage state, such as the strain energy release rate and crack propagation rate.

This study referred to the analytical algorithm of J -integral adopted in (Cui et al., 2014), and the stress situation of DCB specimen under type I loading was as follows::

$$P_I = \frac{3c - L}{4L}P + \frac{3c_g}{4L}m \quad (3-1)$$

Where P_I is the equivalent load of the DCB beam and c is the distance from the load to the center, c_g is the distance between the center of mass and the center, and m is the weight of the beam. The bearing capacity of DCB sample is shown in Figure 3-10. A force and a moment are applied to the initial crack tip:

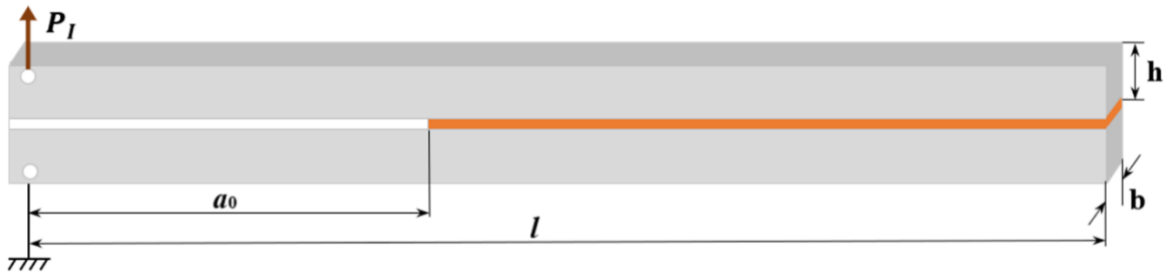


Figure 3-10 Diagram of bearing capacity of DCB sample.

$$F = P_I/b, \quad M = P_I a/b \quad (3-2)$$

As shown in Figure 3-10, the path-independent J -integration performed counterclockwise along path ABCD is as follows:

$$J_{ABCD} = \int_{ABCD} (Wdy - T \cdot \frac{du}{dx} ds) \quad (3-3)$$

$W = \int \sigma d\varepsilon$ is the strain energy density, $\varepsilon = \frac{1}{2} [\nabla u + (\nabla u)^T]$, T is surface traction vector ($T = \sigma \cdot n$), n is the normal vector component that points outside the path ABCD. For the bonding studied in this paper, there are no singularities in the ABCD region, so the integral J_{ABCD} is equal to zero n:

$$J_{ABCD} = J_{AB} + J_{BC} + J_{CD} + J_{DA} = 0 \quad (3-4)$$

Applying Euler-Bernoulli beam theory to a cantilever beam, the loading conditions for paths AB and CD are:

$$\sigma_{xx} = \frac{-12M}{h^3} y, \quad \tau_{xy} = \frac{F}{h}, \quad u(x) = -y \frac{dw(x)}{dx} \quad (3-5)$$

Where h is the thickness of the adhesive beam and w is the measure of deformation at the opening of the specimen, expressed by curl. Each term in the formula (3-4) can be calculated by the following formula:

$$\begin{aligned} J_{AB} &= \frac{6a^2 F^2}{Eh^3} + Fw_1' \\ J_{BC} &= \int_t^0 \int_0^\varepsilon \sigma d\tilde{\varepsilon} dy = \int_t^0 \int_0^w \frac{1}{t} \sigma d\tilde{w} = - \int_0^w \sigma d\tilde{w} \\ J_{CD} &= \frac{6a^2 F^2}{Eh^3} - Fw_2' \end{aligned} \quad (3-6)$$

$$J_{DA} = 0$$

Where t is the thickness of the adhesive layer, E is the Young's modulus of the aluminum plate, and $E=71000\text{MPa}$ is taken in this paper. w_1' and w_2' are the curl of the upper and lower substrates at the initial crack tip. Combined with formulas (3-2), (3-3), (3-4) and (3-6), the energy dissipation in mode I within the bonding layer becomes:

$$J_I = \int_0^w \sigma d\tilde{w} = \frac{12a^2}{Eh^3b^2} (4P_I)^2 + \left(\frac{4P_I}{b}\right)(w'_1 - w'_2) \quad (3-7)$$

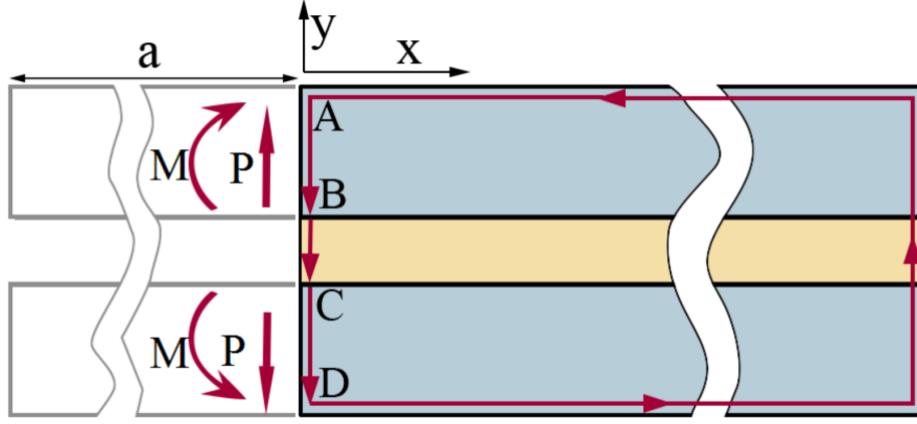


Figure 3-11 J integral calculation diagram from (Cui et al., 2014).

w'_1 and w'_2 are the curl of the adhesive at the initial crack tip, defined as follows:

$$w'_1 = \frac{U_1 - U_2}{y_1 - y_2} \quad (3-8)$$

$$w'_2 = \frac{U_3 - U_4}{y_3 - y_4} \quad (3-9)$$

As shown in Figure 3-12, after speckles were sprayed on the side of the specimen, the image was processed by ARAMIS software to obtain the coordinates and displacement field of the points taken. (x, y) represents the coordinates, and (U, V) represents the displacement.

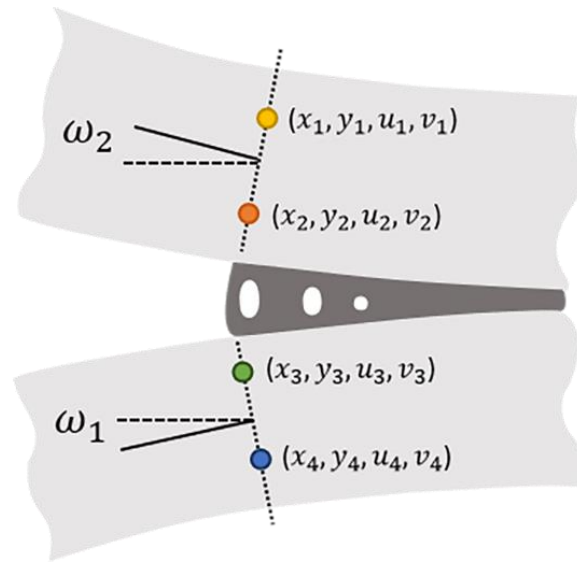


Figure 3-12 Image processing diagram of ARAMIS.

4 Numerical Assessment of Fatigue Crack Growth Models

4.1 Introduction

Interface cracks caused by fatigue loading are a very important form of damage in adhesively bonded joints. With the development of advanced adhesives, adhesive bonded joints have a superior joint performance such as uniform stress distribution, weight reduction and corrosion resistance over traditional joining methods like screws, spot welds and rivets. (Budhe et al., 2017). Many researches (Cui et al., 2014; Gorman & Thouless, 2019; Lißner et al., 2018, 2020; Moroni & Pirondi, 2011) have conducted investigations on adhesive bonding performance. Particularly, fatigue performance, prediction of fatigue crack growth specifically, is an important research aspect. Over the past four decades, stemming from the success of Paris and colleagues in utilizing fracture mechanics to investigate fatigue crack growth in metals (Paris & Erdogan, 1963), the prevailing approach employs fracture mechanics via correlating fatigue crack growth with the *SERR*. Contemporary simulation methods predominantly rely on fracture mechanics, and their formulations are largely grounded in Paris' Law, reformulated in terms of *SERR*. As such, the fundamental expression of the Paris Law equation, which underpins most fracture mechanics-based delamination growth models, is as follows:

$$\frac{da}{dN} = Cf(G)^n \quad (4-1)$$

Extensive researches (Degrieck & Van Paepegem, 2001; Harper & Hallett, 2010; Jablonski, 1980; Robinson et al., 2005; Roylance, 1988; Turon et al., 2004) have been conducted to investigate fatigue crack growth using the Paris Law, and some studies (Adamos et al., 2020; Azari et al., 2013; Carvajal et al., 2020; Hadavinia et al., 2003; Roe & Siegmund, 2003) have successfully applied it to phenomenally describe interface fatigue crack growth, such as composite delamination and debonding of adhesive joints. However, the inclusion of constants obtained through experimental observations in the Paris Law provides researchers with considerable flexibility. The *C* & *n* of the equation

(4-1) are usually considered as material constants which are often calibrated with experiment data. This makes the damage models inevitably over rely on the observation of experiments. and there is still no physical explanation of these two material constants or clear formulation to calculate the two constants. In addition, owing to the boundary conditions of fatigue damage at the elastoplastic interface, the law governing crack expansion at the interface may not align entirely with that of the metal. Consequently, the application of Paris Law can only encapsulate this behavior through observation and experimental results, lacking an explanation rooted in its inherent fracture mechanism. The lack of adequate physical understanding of the material behavior still makes many unanswered questions (Pascoe et al., 2013). A wide range of models has been proposed to deal with a variety of factors like R-ratio effect(Andersons et al., 2004; Dávila, 2020a), Mode mix (Quaresimin & Ricotta, 2006)] and environment influences(Burianek & Spearing, 2002), but progress in mechanical research pertaining to the generation and transmission of interlayer fatigue damage has been sluggish (Pascoe et al., 2013).. In investigating the impact of varying adherend thicknesses on fatigue crack growth of adhesive joints, Azari observed notable disparities in the Paris Law for DCB specimens with different thicknesses. (Azari et al., 2013)This suggests a significant size dependence of the Paris Law in the fatigue crack growth of bonded structures. Consequently, for specimens of identical structure but differing sizes the corresponding material constants for Paris Law are usually significantly different based on models with different geometry, leading to a substantial number of experiments are imperative to discern the crack growth pattern.

Building upon findings from simulation studies (Azari et al., 2011), Azari provided a conjectural explanation for this size effect (Azari et al., 2013). According to this explanation, during the fatigue crack development process, a plastic zone concentrates at the crack tip, and the size of this plastic zone, along with the stress within it, significantly influences the fatigue resistance of the structure. A higher stiffness is associated with increased fatigue damage accumulation. To substantiate this assertion, it becomes imperative to concentrate on investigating the local microscopic evolution of damage around

the crack tip. But methods based in Paris Law is lack of micro scale to the crack growth and cannot explain the local physics behaviors around the crack tip. Ahead of the crack tip, the quasi-brittle behavior demonstrates that there is a nonnegligible FPZ. Thus, the study of FPZ behaviors is a key approach to study the foundation behaviors of crack development. In addition, the study of FPZ influences the damage modelling and experiments data processing. According to (Cui et al., 2014; Lißner et al., 2020) studies, the existence of FPZ in the adhesive layer has influence in calculation of the fracture energy. Andrew Bergan's study also found that a contour that encloses the entire FPZ is often difficult to identify without complementary inspection to assess the extent of damage or an iterative approach to verify path independence (Bergan et al., 2016). Therefore, the debonding geometric dependence may be explained by studying the damage evolution law in FPZ and the influence of the characteristics of FPZ itself on the crack growth law.

In this work, a novel local strain-based approach for predicting fatigue-induced delamination between adhesive layers is introduced. For comparative purposes, two methods (Bak et al., 2017; Harper & Hallett, 2010) based in Paris Law are also employed. Additionally, a benchmark comparison of the three simulation methods is conducted for a case test. In alignment with the experimental data collected (Azari et al., 2011, 2013) two DCB models (mode I) with distinct adherend thicknesses are employed for the simulations. This article is organized as follows: Section 2 provides a concise overview of two methods based on Paris Law and introduces the novel approach proposed in this study. Sec.3 presents the benchmark studies and detailed working process, with the introduction of FEA work. Section 5 details the results and engages in a thorough discussion. Lastly, Section 6 draws conclusions on this work.

4.2 Damage Models

This study employs three methodologies: two CZMs grounded in Paris' Law and a novel approach based on local strain, which is introduced in this paper. The damage models associated with the local strain-based method will be elucidated in this section.

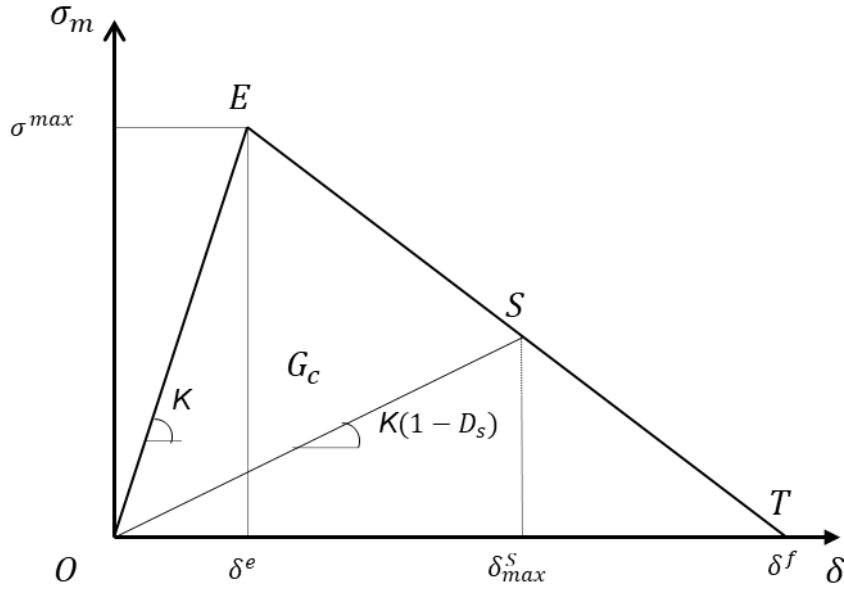


Figure 4-1 Cohesive static damage law.

4.2.1 Quasi-static Cohesive Damage Model

A bilinear cohesive law is illustrated in Figure 4-1. The shape of the traction-separation relation is defined based on the cohesive stiffness K , the cohesive strength σ^{max} , the displacement at failure δ^f . The value of critical energy release rate G_c is equivalent to the area of the cohesive law shape. The parameter δ^e is the displacement corresponding to the traction equivalent to cohesive strength, which is also the resultant displacement for damage initiation. The outline of bilinear cohesive law is composed of two curves: From O to E illustrates an elastic range followed with E to T a softening stage where stiffness decreases with quasi-static damage increases. A brief definition of displacement-based quasi-static damage parameter D_s is given in terms of the resultant cohesive displacement as equation (4-2). As a result, the stress σ can be calculated via equation (4-3):

$$D_s = \begin{cases} 0, & \delta^{max} \leq \delta^e \\ \frac{\delta^f(\delta^{max} - \delta^e)}{\delta^{max}(\delta^f - \delta^e)}, & \delta^e < \delta^{max} \leq \delta^f \\ 1, & \delta^{max} \geq \delta^f \end{cases} \quad (4-2)$$

$$\sigma = \begin{cases} \delta_{max}K, & \delta^{max} \leq \delta^e, \\ \delta_{max}K(1 - D_s), & \delta^e < \delta^{max} \end{cases} \quad (4-3)$$

4.2.2 Cohesive Fatigue Damage Model

Inspired by (Graner Solana et al., 2010), the maximum strain was reasonably assumed to quantify fatigue damage progression in adhesively bonded joints. A strain-based fatigue damage model was subsequently proposed and successfully applied to predict fatigue life. Accordingly, a novel local-scale method based on the resultant displacement or strain was proposed to predict the fatigue crack growth of adhesive bonded joints by the author. In contrast to the conventional cohesive zone model (Bak et al., 2015; Harper & Hallett, 2010; Kawashita & Hallett, 2012; Robinson et al., 2005) that computes the crack growth rate using the Paris Law relation, linking the strain energy release rate to quantify fatigue damage assigned to the fracture process zone. The cohesive zone model deduces damage evolution solely from the strain near the crack tip. The fatigue damage induced by strain therefore enables the assessment of fatigue crack initiation and growth in adhesive bonded joints.

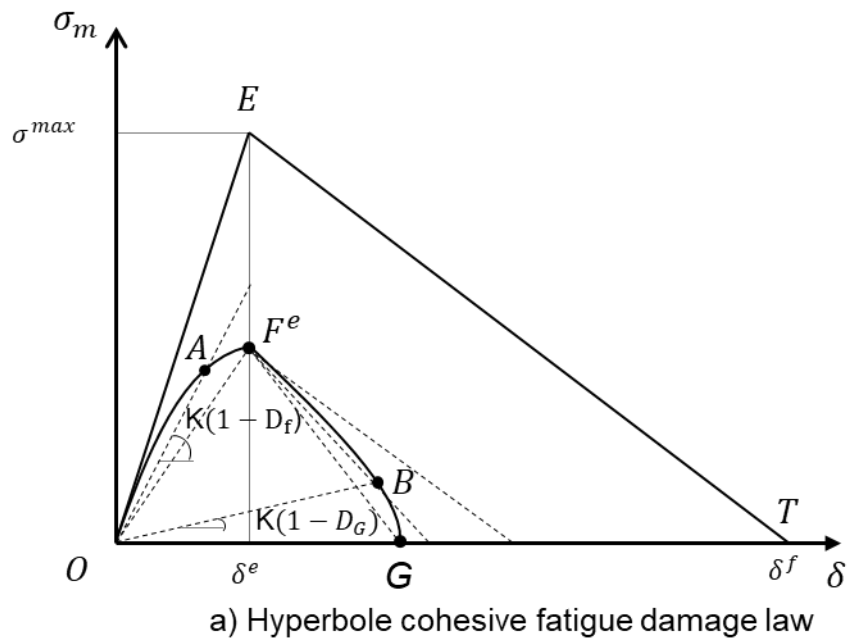
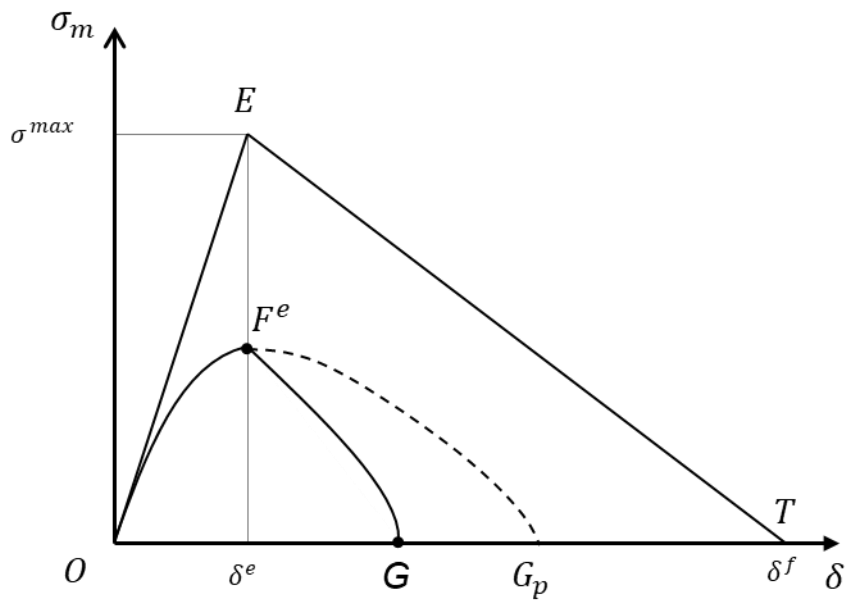


Figure 4-2 Cohesive fatigue damage model.

A heuristic cohesive damage law has been illustrated by the author in Figure 4-2 (a). Under quasi-static loading, the adhesive layer follows the O-E-T path. During the O-E path, the displacement δ , increases from 0 to the maximum strength displacement, δ_e , without any damage, with a stiffness of K . As δ

continues to increase up to the maximum displacement δ^f , quasi-static damage accumulates from 0 to 1, causing K to decay to 0. In the fatigue process, a new path, O- F^e -G, is introduced. When the displacement is less than δ_e , only fatigue damage occurs, following the O-A- F^e path. In this phase, stiffness decay is solely due to fatigue damage, D_f . However, when the displacement exceeds δ_e , both fatigue and quasi-static damage contribute to the decay, and the path follows F^e -B-G. In Figure 4-2. (b), the region enclosed by F^e , G, and G_p represents the additional loss of fracture toughness caused by the overlap of static damage.

Different with the methods based on Paris's Law, the strain-based method put the fact as prerequisite that the delamination happened in the adhesive is an elastic-plastic response, and the damage was represented as a form of degrading the constitutive properties of the adhesive. In (Graner Solana et al., 2010), the fatigue damage was defined as equation (4-2):

$$\frac{\Delta D}{\Delta N} = b(\varepsilon_{max} - \varepsilon_{th})^z \quad (4-4)$$

where $\frac{\Delta D}{\Delta N}$ is the fatigue damage rate at every time step, b and z are parameters input when calibrating the damage model. ε_{max} and ε_{th} are maximum and threshold strain. Usually, during fatigue testing, the significant stress concentration at the FPZ tip often results in a minimal threshold strain or displacement. In the simulation, representing such a small threshold displacement in a relatively large incremental step is challenging, and is thus approximated as 0 for simplicity here. To facilitate calculations, damage is regularized using the mean displacement in the quasi-static state, ultimately yielding formulation (4-3):

$$\frac{dD}{dN} = c \left(\frac{\delta_f^{max}(1-R)}{\delta^f - \delta_{mean}} \right)^m \quad (4-5)$$

where c and m are input, parameters collected by calibrating the damage model to fit experimental crack growth rate data, R is the stress ratio of cyclic loading. δ_f^{max} is the adhesive's maximum principal displacement. The

formulation is normalized by the maximum failure displacement in quasi-static condition where δ^f is the displacement at quasi-static failure and δ_{mean} is the mean displacement of the fatigue cycle:

$$\delta_{meas} = \frac{1 + R}{2} \delta_f^{max} \quad (4-6)$$

In each increment, the fatigue damage at the integration point of adhesive was updated according to the following equation and stopped when D reached 1 which means that integration point was failed.

$$D_{f(i)} = D_{f(i-1)} + \left[\left(\frac{dD}{dN} \times \Delta N \right) \right] \quad (4-7)$$

Where D_i is the current fatigue damage value with the D_{i-1} is the fatigue damage value of last increment. A flow chart of the model is presented in Figure 4-3 For each increment, the global damage is updated combined with the loss of stiffness from quasi-static and fatigue damage:

$$D_g = 1 - (1 - D_s)(1 - D_f) \quad (4-8)$$

and current stress is updated as:

$$\sigma = \delta_{max} K (1 - D_g) \quad (4-9)$$

The value of threshold strain in the workflow chart was set to 0 in this work, which means displacement due to compression is not considered to have contributed to the damage as there is a compression zone in front of the crack tip.

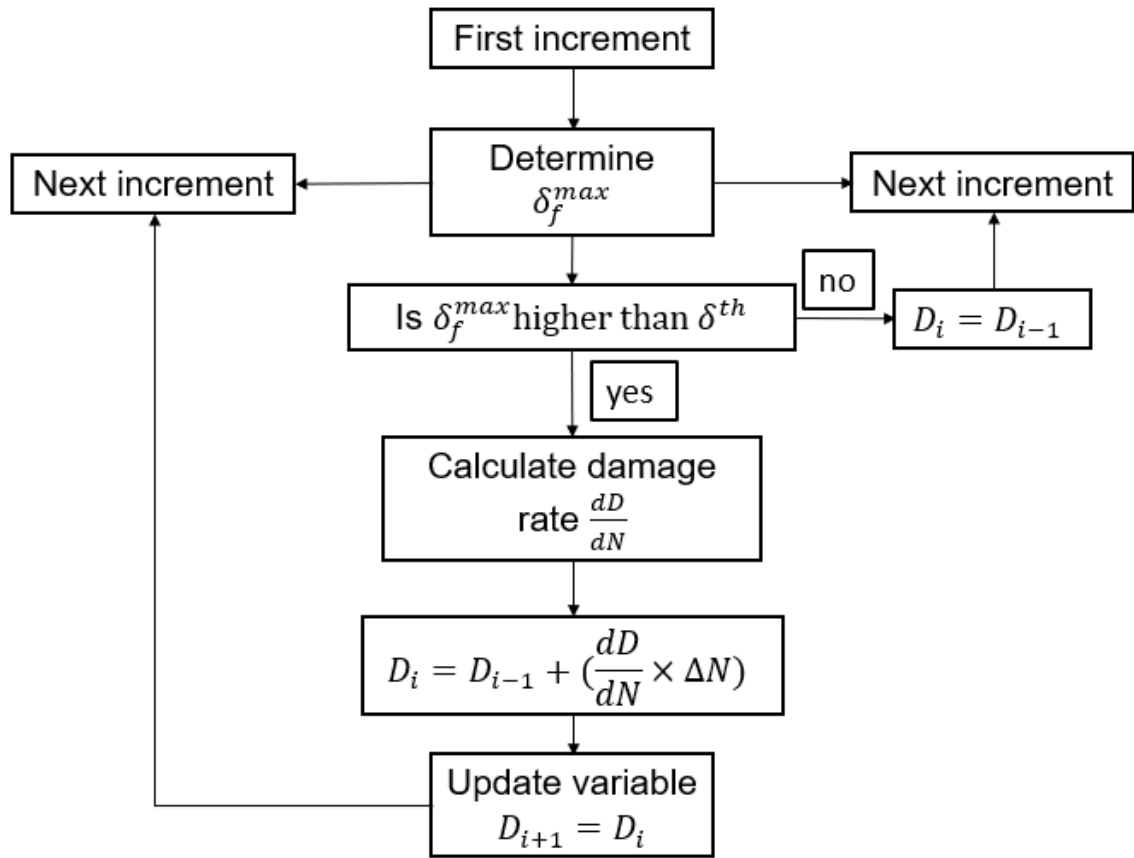


Figure 4-3 Flow chart of the strain-based method.

Bak (Bak et al., 2017) conducted a benchmark study involving six prominent simulation methods grounded in CZM. The study focused on simulating fatigue delamination cracks in composite laminated structures under cyclic loading, evaluating each method in terms of prediction accuracy, dependence, and sensitivity to quasi-static material parameters and fitting variables. Based on Bak's findings, this paper selects two methods (Bak et al., 2015; Harper & Hallett, 2010) identified as having 'superior' performance in the benchmark study for comparative analysis.

The fatigue damage rate $\frac{dD}{dN}$ formulation for Hallett's and Bak's are presented as Eq. (4-10) and (4-11).

$$\frac{dD}{dN} = \frac{D_f}{Le(f(\Delta G, G_m, \emptyset)^{-1}} \quad (4-10)$$

$$\frac{dD}{dN} = (F_{\beta} \frac{\partial \beta}{\partial a} + F_{\delta} \frac{\partial \beta}{\partial a})(f(\Delta G, G_m, \phi)) \quad (4-11)$$

Bak's method is built upon common variants of Paris' Law and incorporates the J -integral to address the challenge of defining the length of the fracture process zone during simulations. Hallett's method adopts a different framework. Rather than directly linking damage progression to the cyclic variation of total specific work at each point within the cohesive interface, it draws on various adaptations of Paris' Law. This results in a unique perspective on energy release rate determination, a critical parameter in fracture mechanics. Hallett's model offers a nuanced approach to damage assessment, moving beyond conventional reliance on cyclic variation. These premium features make the two cohesive zone methods based on Paris law promising. More details about the two methods used here can be found from their original papers.

4.3 Benchmark Studies

4.3.1 Test Case

The benchmark studies on the three cohesive zone damage models will be conducted based on DCB models as Figure 4-4. Two aluminium DCB fatigue tests varying different adherend thickness under Mode I loading (Azari et al., 2013) were used to verify methods mentioned above. The 'thinner' specimen was manufactured as 1.6mm×19.05mm AA6061-T651 sheets and the 'thick' specimen was made as 12.7mm×19.05mm AA6061-T651 flat bars as shown in Figure 4-4. All the tests used a commercial, single-part heat-cured highly toughened epoxy adhesive, with an adhesive layer kept the same thickness of 0.38mm. All fatigue tests were conducted under displacement control, with a constant displacement ratio $R = \frac{\delta_{min}}{\delta_{max}} = 0.1$.

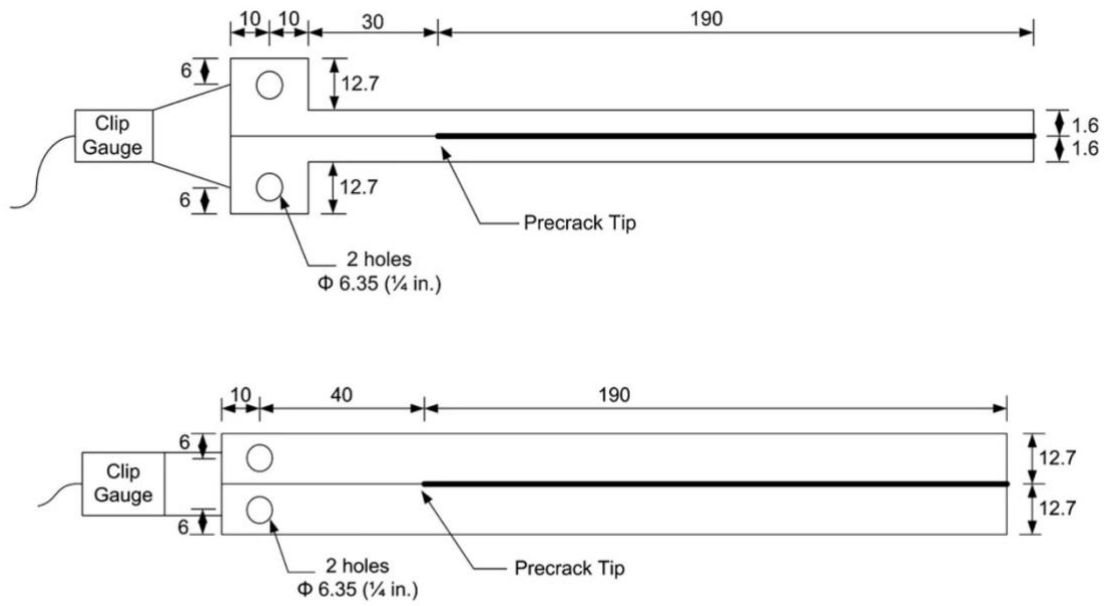


Figure 4-4 Geometry of 1.6 mm and 12.7 mm DCB joints.

The material properties used as input in the simulations are shown in Table 4-1.

Table 4-1 Quasi-static properties, fatigue properties common for all the studies presented unless otherwise stated.

Property	Cohesive Parameter	Units	Value
Cohesive law stiffness	K	[MPa/mm]	3947
Cohesive law Strength	σ^{max}	[MPa]	45
Critical energy release rate	G_{IC}	[N/mm]	3.5
fracture toughness for crack initiation	G_0	[N/mm]	0.344
threshold fracture toughness	G_{th}	[N/mm]	0.06

Stress ratio of cyclic loading	R ratio	[-]	0.1
Material constants of Hallett's methods based on Paris Law	C_I	[-]	3.87
Material constants of Hallett's methods based on Paris Law	M_I	[-]	3.84
Material constants of Bak's methods based on Paris Law	C_{II}	[-]	0.0286
Material constants of Bak's methods based on Paris Law	M_{II}	[-]	3.84
Material constants of strain-based method	M_{III}	[-]	10
Material constants of strain-based method	C_{III}	[-]	5
Al 6061 Density	ρ	[g/cm ³]	2.7
Al 6061 Young's Modulus	E	[MPa]	68900
Al 6061 Poisson's Ratio	μ	[-]	0.3

4.3.2 Finite Element Implementation

The damage FEA of the DCB tests was conducted, using ABAQUS/Explicit software. The FEA model consisted of two aluminium adherends and an adhesive bonding, meshed with CPE4R and COH2D4 elements respectively as Figure 4-5, and the material model of the adhesive and aluminium specimens were given above. The boundary condition was set to one end of the cantilevers in the Y (vertical) axis displacement. For the 'thinner' specimen was 6mm of both edges of the cantilevers and the 'thicker' was 1mm. The element in adhesive was chosen cohesive element and the width for the 'thinner' specimen was 0.25mm and for the 'thicker' specimen was 0.1mm, which allowed observing the crack growth continuously and avoided mesh size having a large influence on the simulation results. To implement these three methods, the adhesive behaviour was modelled using the VUMAT user material interface in ABAQUS. A flow chart of the calculation process is presented in Figure 4-6.

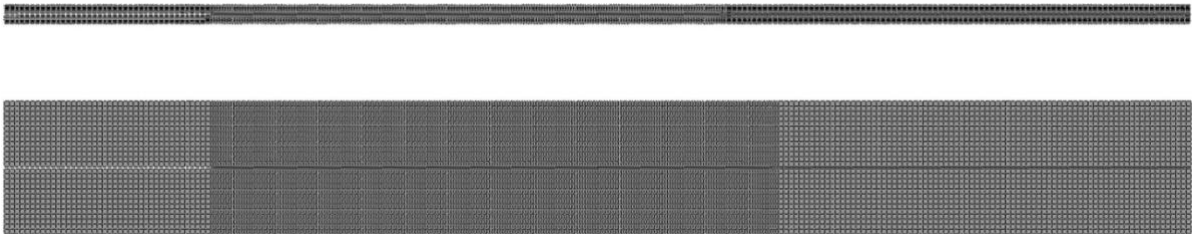


Figure 4-5 Meshed simulation DCB models in ABAQUS.

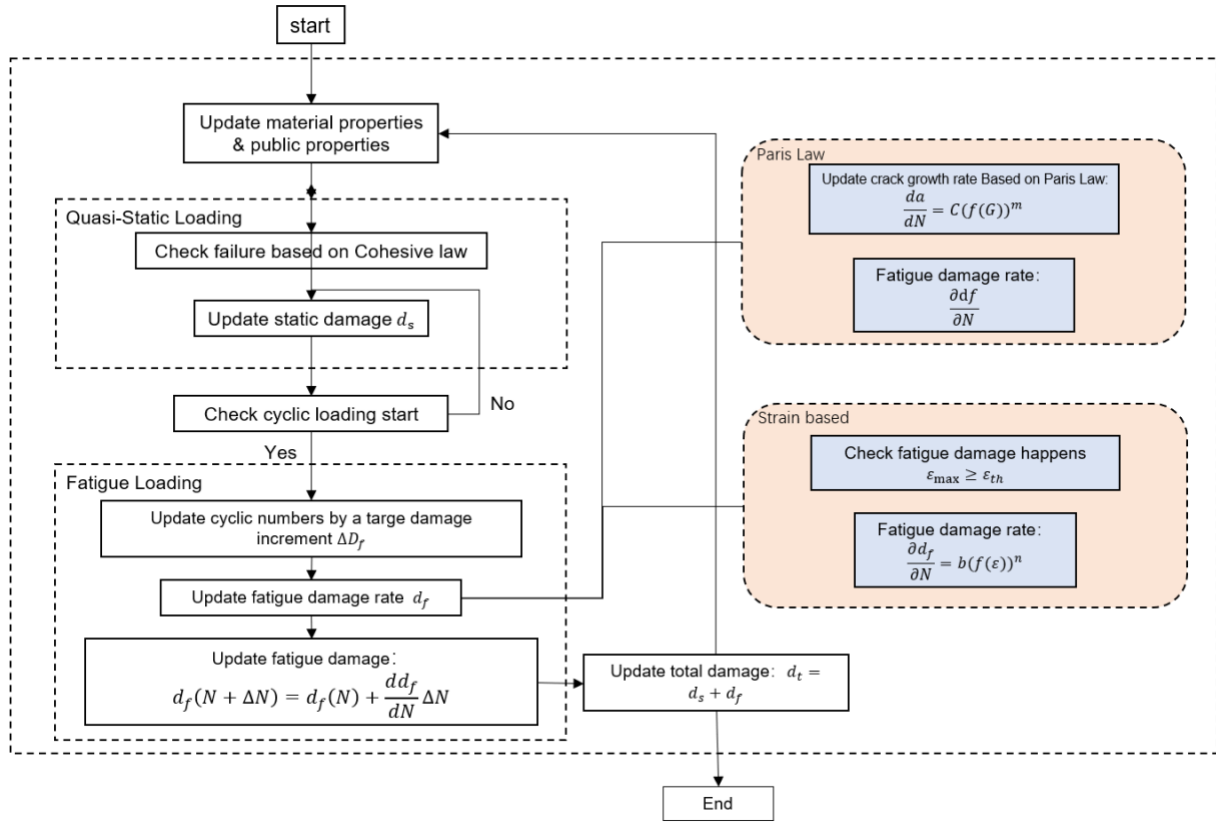


Figure 4-6 Flow chart of calculations of the three methods for delamination simulation.

The simulation is divided into two steps: the first part is under quasi-static load, and it ends at target time set when the fatigue loading starts. The target time is also the time when the maximum displacement at the loading end. The whole quasi-static loading process is conducted under a smooth amplitude with frequency is 0.01. When the target time is reached, quasi-static load stops, and cyclic load starts under the R ratio given above. The frequency of cyclic load is updated by the formulation at each step:

$$Time_{hz} = \frac{\Delta D_{target}}{dD * dt} \quad (4-12)$$

Where $Time_{hz}$ is the frequency of loading, dD is the fatigue damage increased in last step time, and the dt is the interval every step. To ensure a Continuously smooth crack growth, the $Time_{hz}$ was bound between 1-1e14.

The opening Mode I energy release rate can be calculated using the integration of tractions as functions of the displacement jump.

$$G = \int_0^{\delta^{max}} \sigma d\delta \quad (4-13)$$

For each increment step, the integration can be carried out by:

$$G_{t_c+\Delta t} = G_{t_c} + \int_{\delta_{t_c}^{max}}^{\delta_{t_c+\Delta t}^{max}} \sigma d\delta \quad (4-14)$$

Where t_c is current simulation time and Δt is the increment in time for the next equilibrium sub step. G_{t_c} is the current energy release rate and $G_{t_c+\Delta t}$ is the energy release rate after Δt . δ_{t_c} is the current displacement and $\delta_{t_c+\Delta t}$ is displacement after Δt

4.3.3 Convergence Study

4.3.3.1 Mesh Study

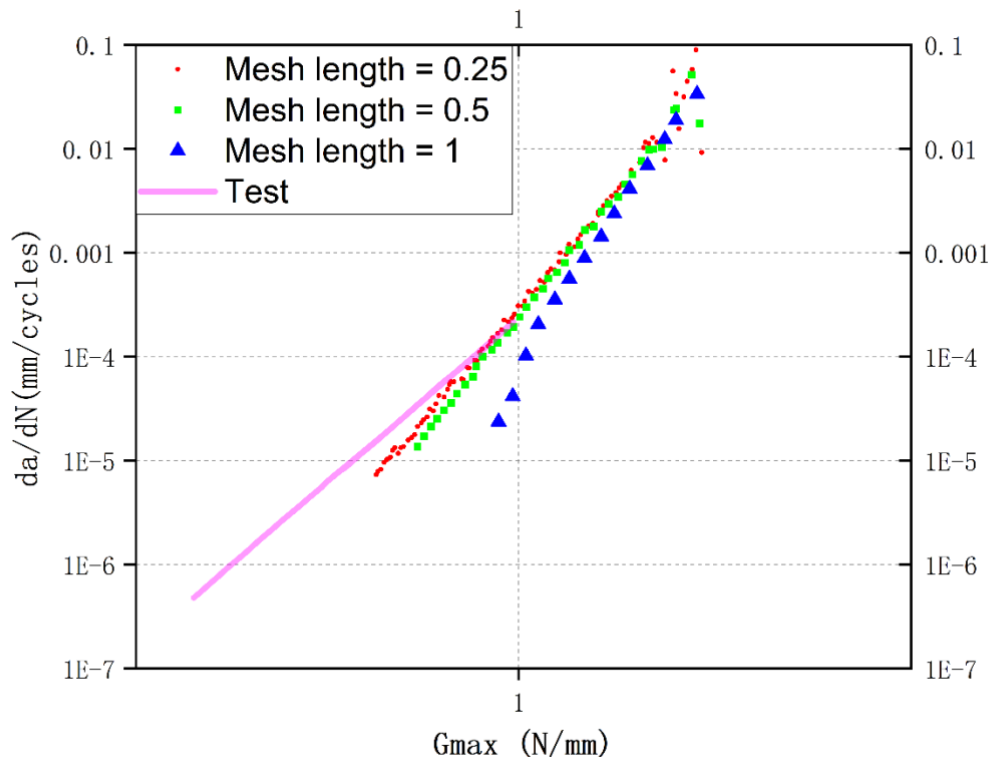
Some studies have been done on the element size effects on the FEM simulation results when using CZM (larve et al., 2017; Remacle et al., 2012; Song et al., 2008). The predictions made with coarser meshed overpredict significantly the experiment results (Turon, Dávila, et al., 2007). The element size was considered needs to be less than the cohesive zone length. A widely used formulation for predicting the cohesive zone length can be conducted as follows:

$$GL_{cz} = ME \frac{G_c}{(\tau_0)^2} \quad (4-15)$$

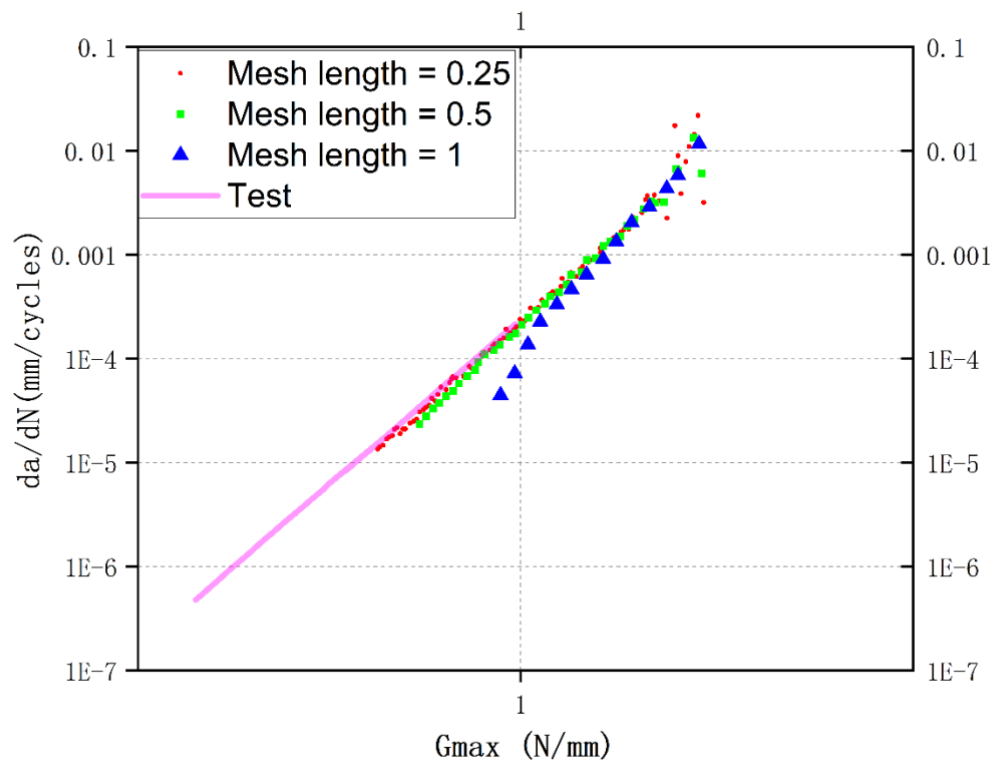
Where E is the young modulus of the material, G_c is the critical energy release rate, τ_0 is the maximum interfacial strength, and M is a constant which depends on each model. Generally, the M is ranged from 0.2 to 1. The value of the cohesive zone length is close to 24 to 120mm based on Table 4-1. In order to represent the fracture energy accurately, the number of elements in the cohesive zone cannot be too few. (Carpinteri et al., 2003) suggests the number

of elements in cohesive zone more than 10. To ensure the accuracy of prediction, several analyses were carried out for element sizes ranging between 0.25mm and 1mm for the '1.6mm' specimen.

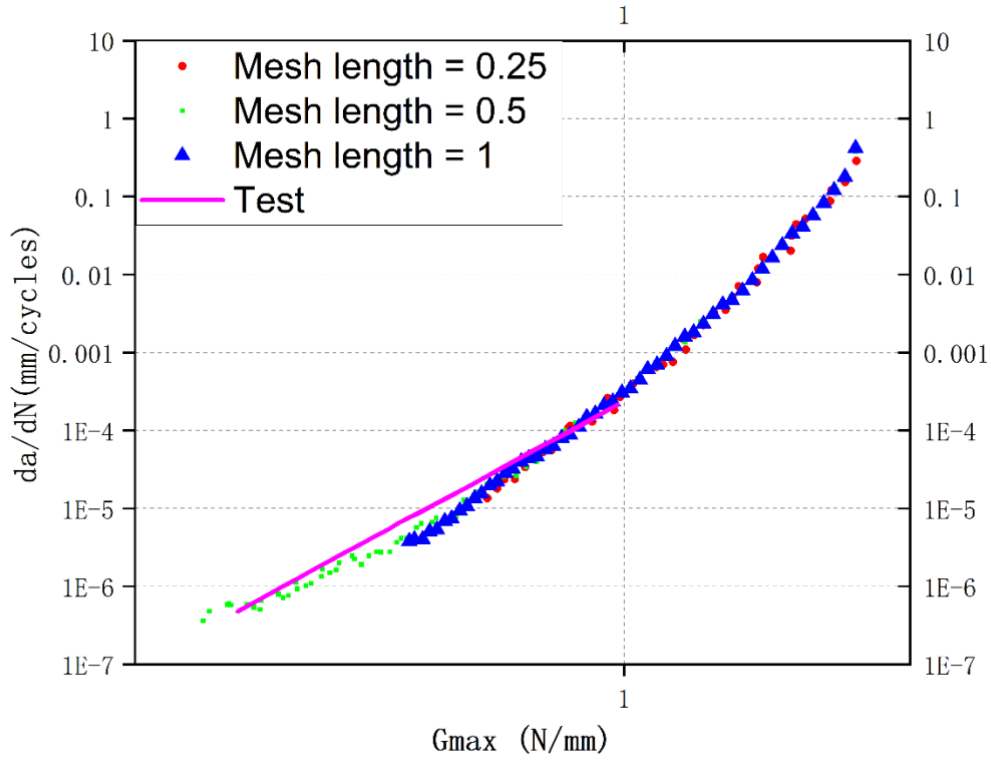
The results by two methods based on Paris Law with different cohesive element size are shown below Figure 4-7 (a) (b). The results indicate that the mesh size of $l_e=1\text{mm}$ not obtained converged solutions. It's necessary to select a mesh size greater than 0.5mm to accurately represent the fracture energy. The second set of Figure 4-7 (a) (b) show that both for the two methods based on Paris Law, the mesh length should be greater than 0.5mm to predict the crack initiation process as the results of 1mm mesh length was too long to decrease the sensitivity of capturing the changing nodes along the adhesive. The Figure 4-7 (c) shows mesh length increased from 0.5mm to 1mm, the prediction results decrease from 12.5% to 19.6% for Hallett's method and Bak's method.



(a)



(b)



(c)

**Figure 4-7 Study on the effect of mesh length on DCB fatigue delamination with
a) Bak's Method. b) Hallett's Method. c) Strain-based Method.**

The results by strain-based method with different cohesive element size are shown above in Figure 4-7 (c). With the decrease of element length, the decay rate of G_{max} has a remarkable increase trend. From the results of relationship between crack length growth rate and G_{max} , the changing of mesh length has no significant effect on the prediction.

4.3.3.2 Interval Time Study

The fracture process zone will not be captured exactly while the element size of cohesive zone is too large. For minimization of CPU cost and ensure the accuracy capture of fracture process zone, the results will be discussed in 0.25mm element size of cohesive zone.

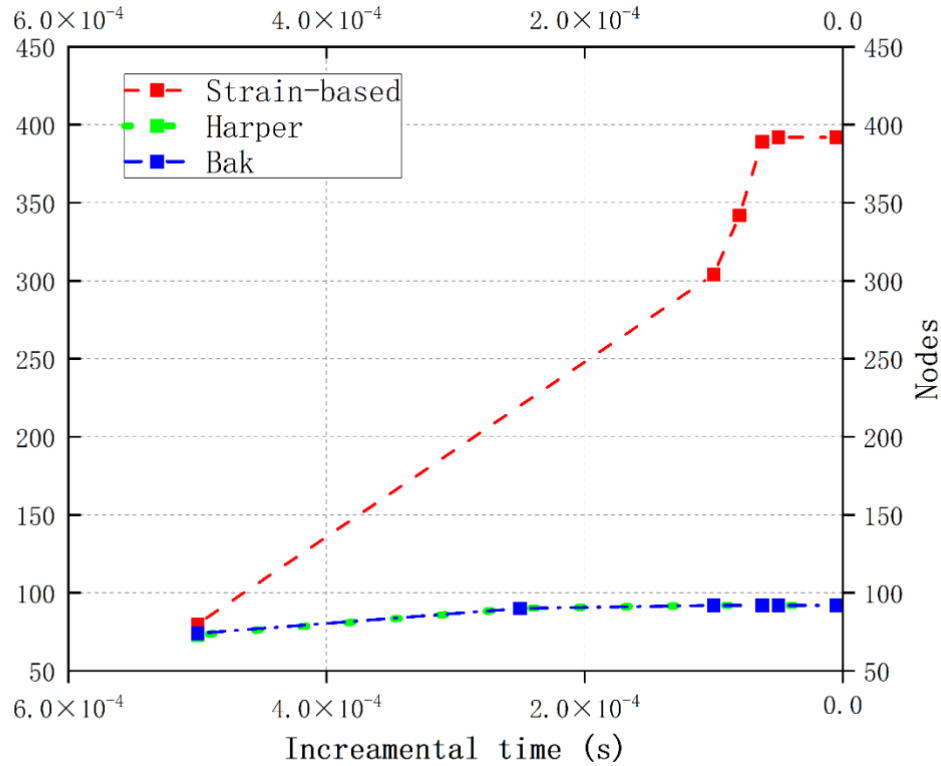


Figure 4-8 The capture of crack growth changing point-Interval

To ensure the convergence of the simulation results. The change of interval time was also discussed. 6 sets of analyses were carried out for interval between 0.000005s to 0.0005s. The Figure 4-8 shows that the change of incremental time has an impact on the capture of the crack growth changing point for all the three models. While the mesh size kept same, the strain-based method collected more changing nodes reflects that the strain-based determine a greater area experiencing fatigue damage than the method based on Paris Law.

The sensitivity of capture increases with the decrease of incremental step size. There is a threshold. When the incremental step reaches a threshold value (SN: 5×10^{-5} s, Paris Law: 1×10^{-4} s), the incremental step no longer affects the ability of the capture of crack growth changing point. It's obvious that the strain-based method was 258% more affected from the incremental time changing from 0.0005s to 0.00025s than two methods based on Paris Law.

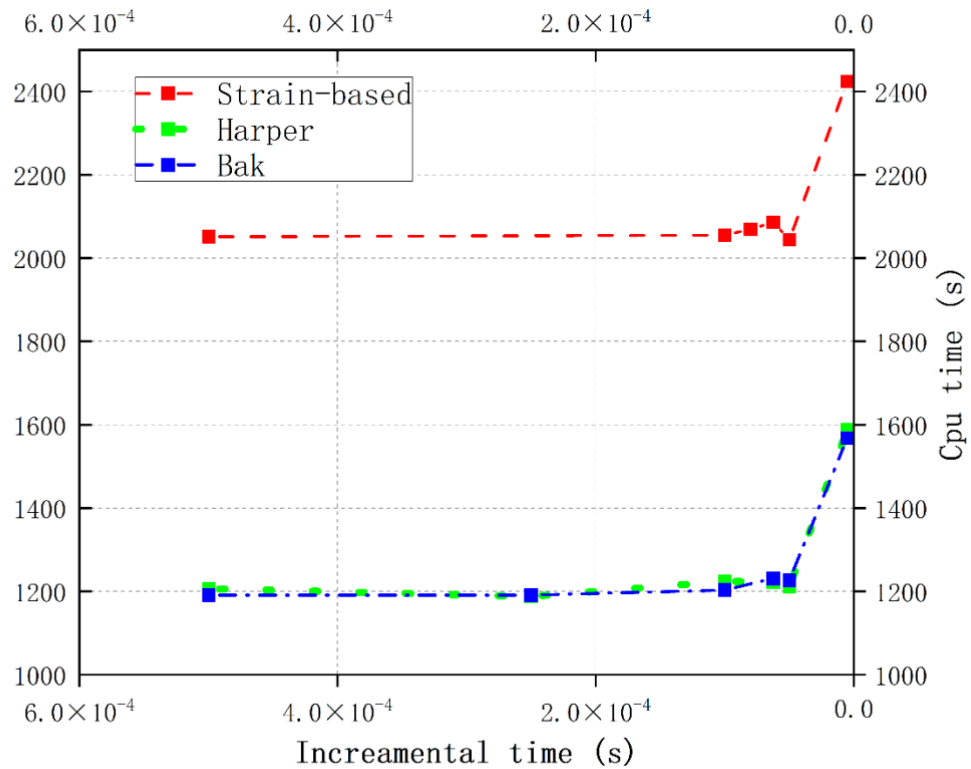


Figure 4-9 WALLCLOCK time-Interval time

The change of incremental time has also impact on the calculation time as shown in Figure 4-9. The CPU time keeps same as the incremental time decreases. When the incremental step reaches a threshold value (Strain-based :5E-5 s, Paris Law:1E-4 s), the CPU time increases with the incremental time continue to decrease.

There is an overlap between the two threshold incremental times and there is a fluctuation in the calculation time when incremental time approaching the threshold. It is speculated that there is a “convergence point”, which can accurately capture the growth of crack and save the calculation time.

4.3.4 Parameter Calibration

The basic material properties of the adhesive and adherend are all from the tests used, the material constants of methods based on Paris Law were fitted with the experimental data. The $f(G)$ in Paris Law is under different forms to be

used in the two methods, and the tests processed the data in a function of G_{max} while the form of Hallett's method is:

$$f(G) = \frac{\Delta G}{G_c} = \frac{G_0(1 - R)^2}{G_c} G_{max} \quad (4-16)$$

And for Bak's method is:

$$f(G) = \frac{G_{max}}{G_c} \quad (4-17)$$

No clear consensus has so far been reached on which form of strain energy release rate should be most suitable for equation 1. In this work, the same form of $f(G)$ with the formulation in the two methods based on Paris Law will be used in the simulation to avoid calculation errors. But for validation of simulation results with tests data, the form of $f(G)$ uses the G_{max} consistent with tests.

A workflow Figure 4-10 to explain the material parameters calibration process. For the two methods based in Paris 'Law, the material constants can be captured from the tests and then used as input parameters while for the methods based in strain the damage parameters c & m were calibrated by compared with the tests data in terms of the relation of strain energy release rate and crack growth rate. The damage parameters of Pari's Law and strain-based damage model would then be used as input parameters to do the simulation work.

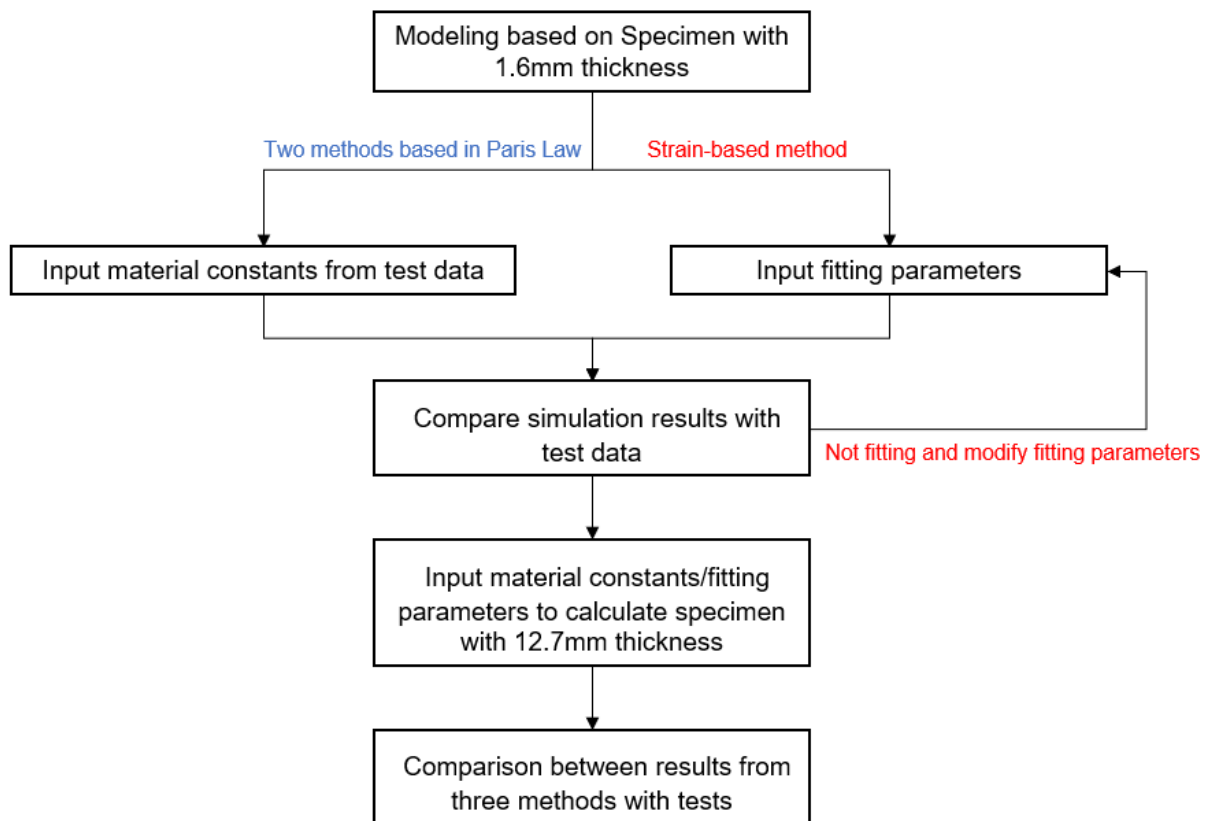


Figure 4-10 Flow chart of material constants calibration process.

A brief benchmark studies have been given here, and the details will be demonstrated in the section of results and discussion.

- Loads undertaken during both the quasi-static and fatigue driven crack propagations.
- Transition response from quasi-static to fatigue propagation: simulated da/dN vs. a .
- Ability to reproduce Paris 'law: a vs N .
- Ability to reproduce the Paris 'law used as input to the methods or which the method is calibrated with da/dN vs. G_{max} .
- The developments of FPZ length during the whole propagation: FPZ length vs simulation interval.
- Relation of Traction and Separation for different position in cohesive

elements.

4.4 Results and Discussions

4.4.1 DCB Models Results.

The reaction of loading on the end of the whole simulation process is shown in Figure 4-11. Both for the 1.6mm and 12.7mm thick models, there is an analogous trend that the reaction force both for the three methods keeps the same in quasi-static load phase as there is no difference between the three methods when simulating quasi-static loading process to compare the characteristics of fatigue expansion stage to the greatest extent. Until the cyclic load starts, the two methods based on Paris Law basically share the same attenuation trend and this illustrates the difference of methods based on Paris Law mainly focus on the way how distribute the fatigue damage caused by the reduction of strain energy release rate, namely the toughness of the DCB. When both methods are of high quality, there is no significant influence on the fatigue delamination growth. However, the attenuation form and value of the strain-based method are clearly different. The toughness of the model based on Paris Law decreased apparently faster than the model based on strain and the toughness of the strain-based method shows a nearly linear downward trend. The reaction force of the 'thick' specimen experiences a steep drop before the cyclic load starts. This is because under the influence of size, with the increase of thickness, the local stress on crack tip also increases greatly. So, a part of the elements in adhesive has already failed before entering the fatigue crack development.

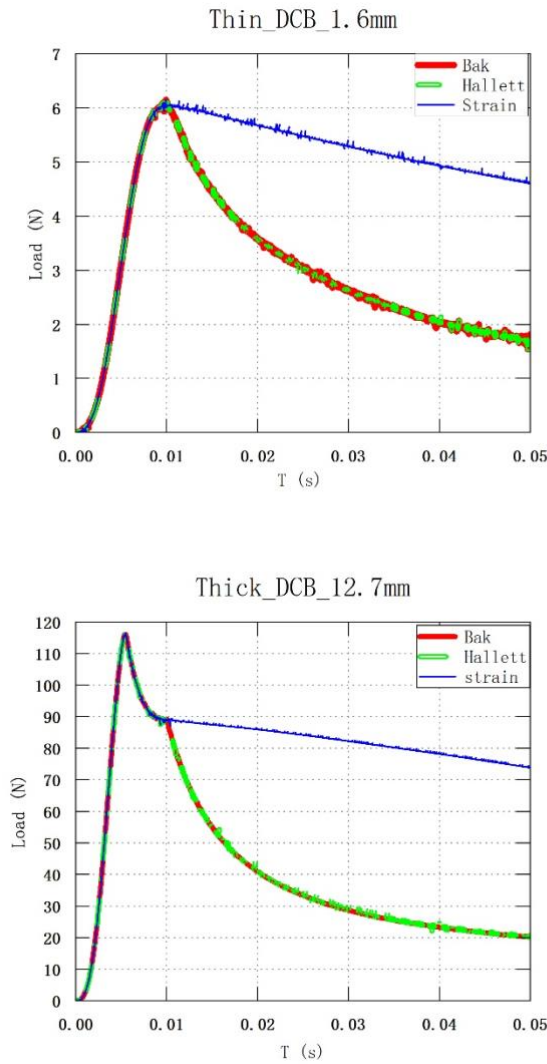


Figure 4-11 Load during the whole simulation work.

4.4.2 Prediction of Crack Growth

The results of the relation between G_{max} and crack growth rate da/dN for the three damage models and tests results have been presented in Figure 4-12. It is clear from the figure on the upper one that both method of Harper and strain-based methods reproduced the test results with 1.6mm Aluminium adherends well, better than Bak's. From comparison between the two figures that strain-based method has predicted the thicker specimen with 12.7mm Aluminium adherends better than two methods based in Paris Law, which implies strain-based method has a better size-independence than Paris-Law based methods.

In addition, the fluctuation degree of the curve can reflect the stability degree of simulated crack growth during simulation calculation. It can be obviously found that the strain-based method has better stability under different geometric sizes.

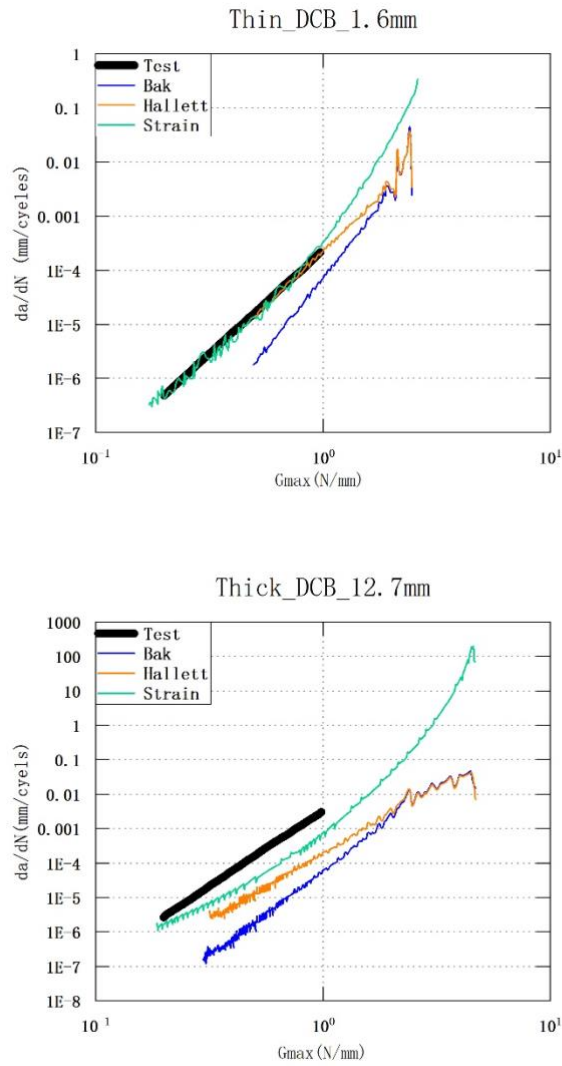


Figure 4-12 Comparison of the experimental data with the crack growth rate obtained from the numerical simulation for a Mode I DCB test.

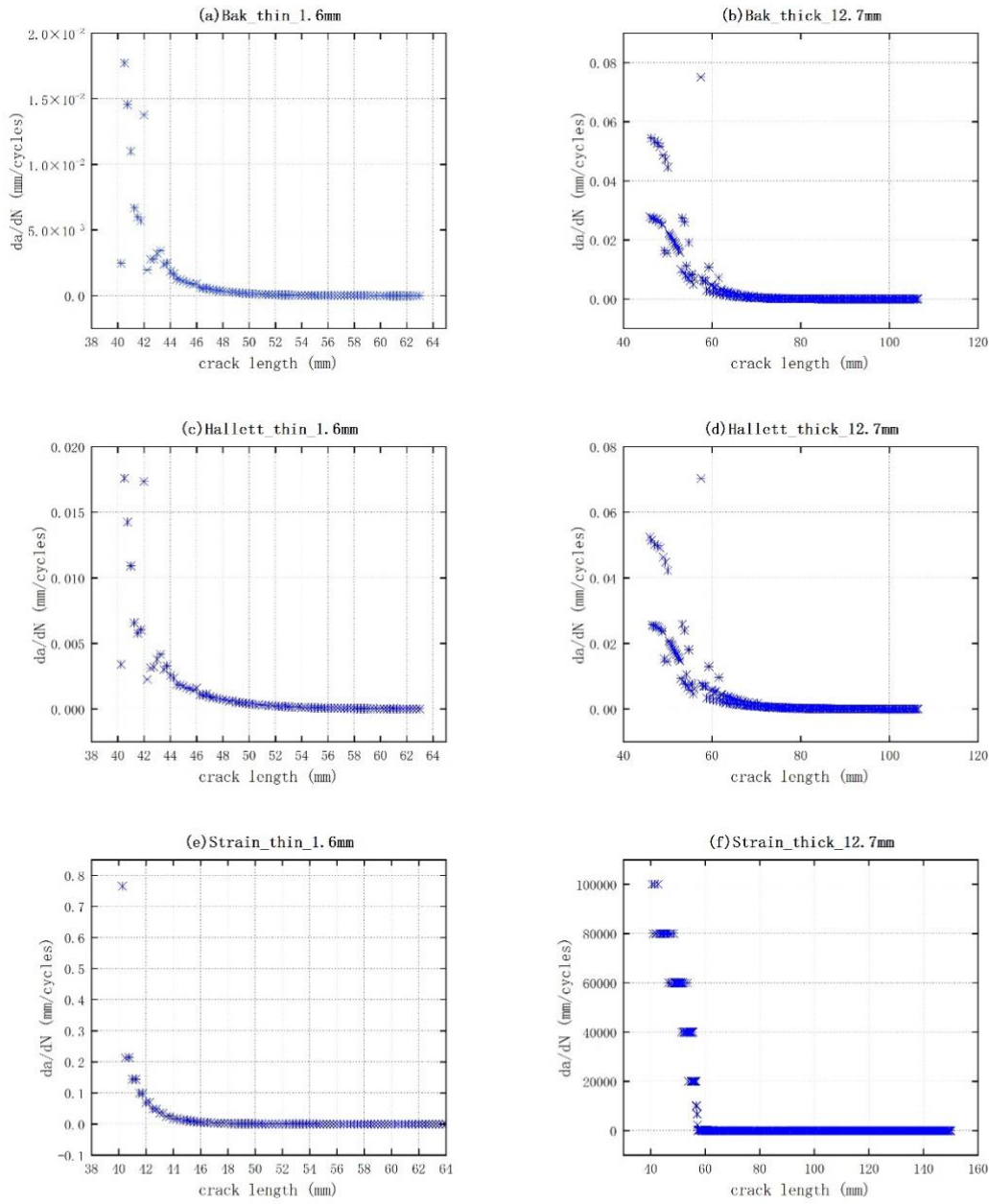


Figure 4-13 Results of da/dN vs. crack length.

The results of transition response from quasi-static to fatigue propagation: simulated da/dN vs. crack length, a , for the three damage models have been presented in Figure 4-13 and Figure 4-14. According to Paris 'Law equation (4-1), the crack growth rate should be constant after the initiation phase. Strain-based method reaches the stage of stable crack growth much more quickly than

methods based in Paris 'Law, which means better ability to get through the transition phase to the damage stage dominated by fatigue damage.

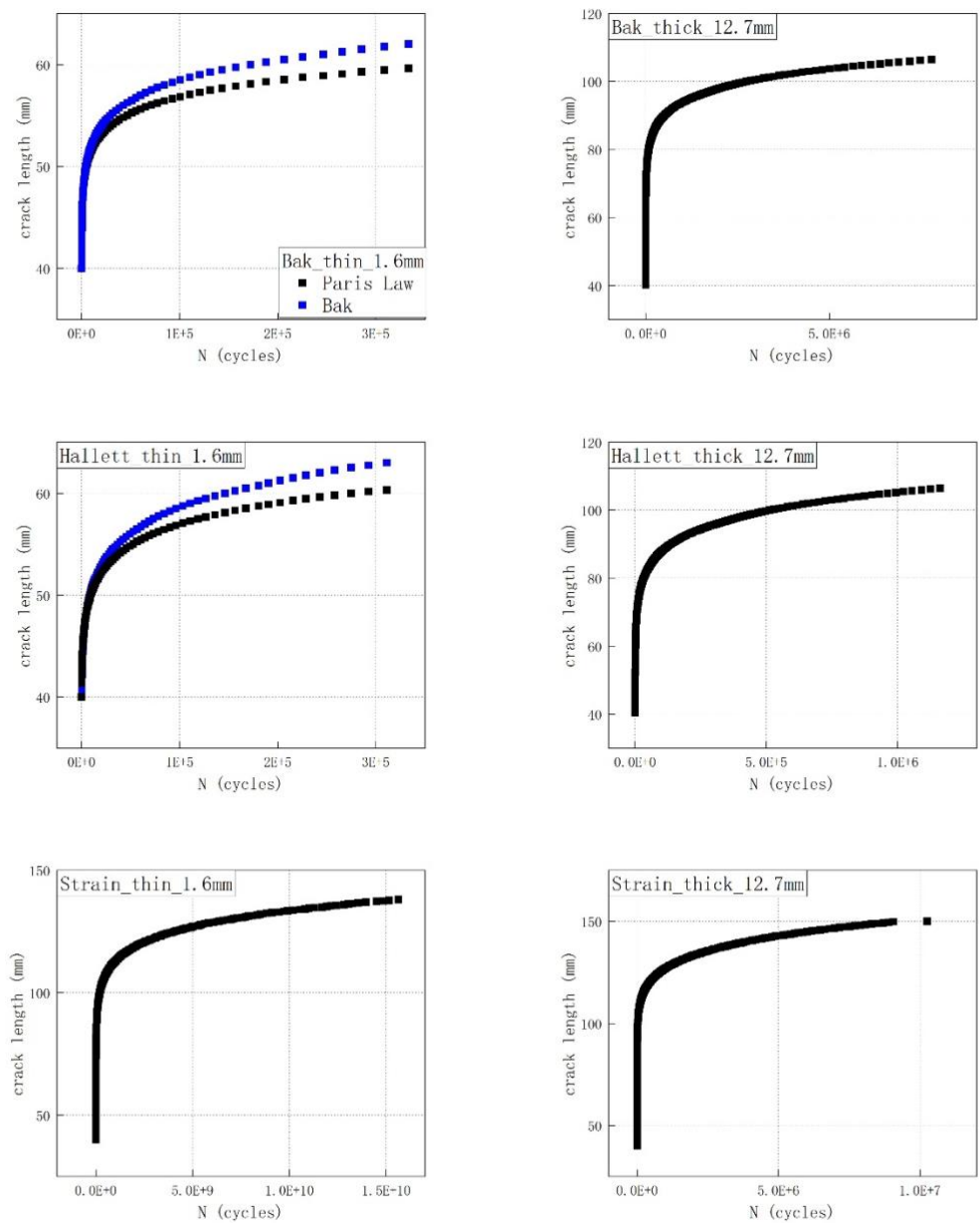


Figure 4-14 Results of crack length vs load numbers.

4.4.3 FPZ Study

The resulting relations between crack length and cyclic numbers for the three damage models have been presented above. The top two pictures on the left also contain the relations between. Crack length and cyclic numbers got by Paris 'Law to compare with the simulation results from the two methods based in Paris' Law, which reflect the two Paris 'Law-based methods' ability to reproduce the equation.

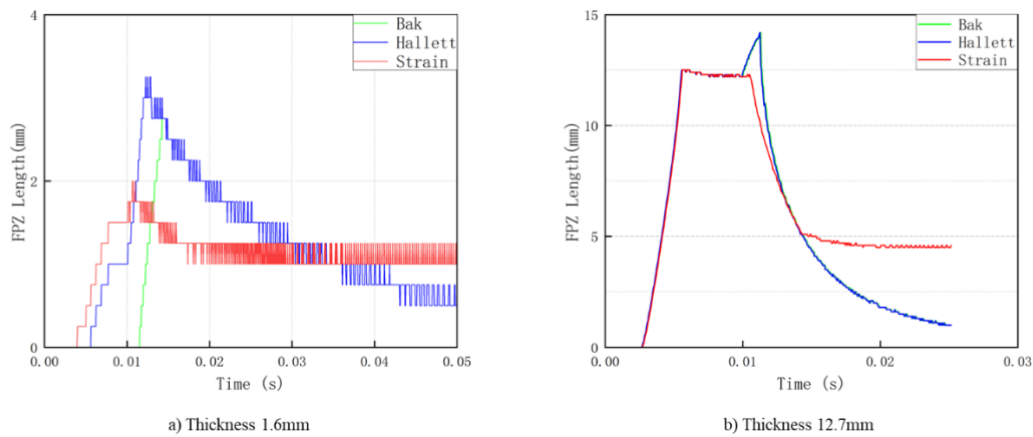


Figure 4-15 Comparison of the length of FPZ vs. Interval time simulated by three methods with a) 1.6mm thickness model b) 12.7mm thickness model.

The resulting relation of FPZ length vs simulation integration step time have been presented in Figure 4-15. The fluctuation of FPZ length was caused by cohesive elements' size while finer mesh will improve the convergence of the relation curve. After crack initiation stage the FPZ length kept same between two methods based in Paris 'Law.

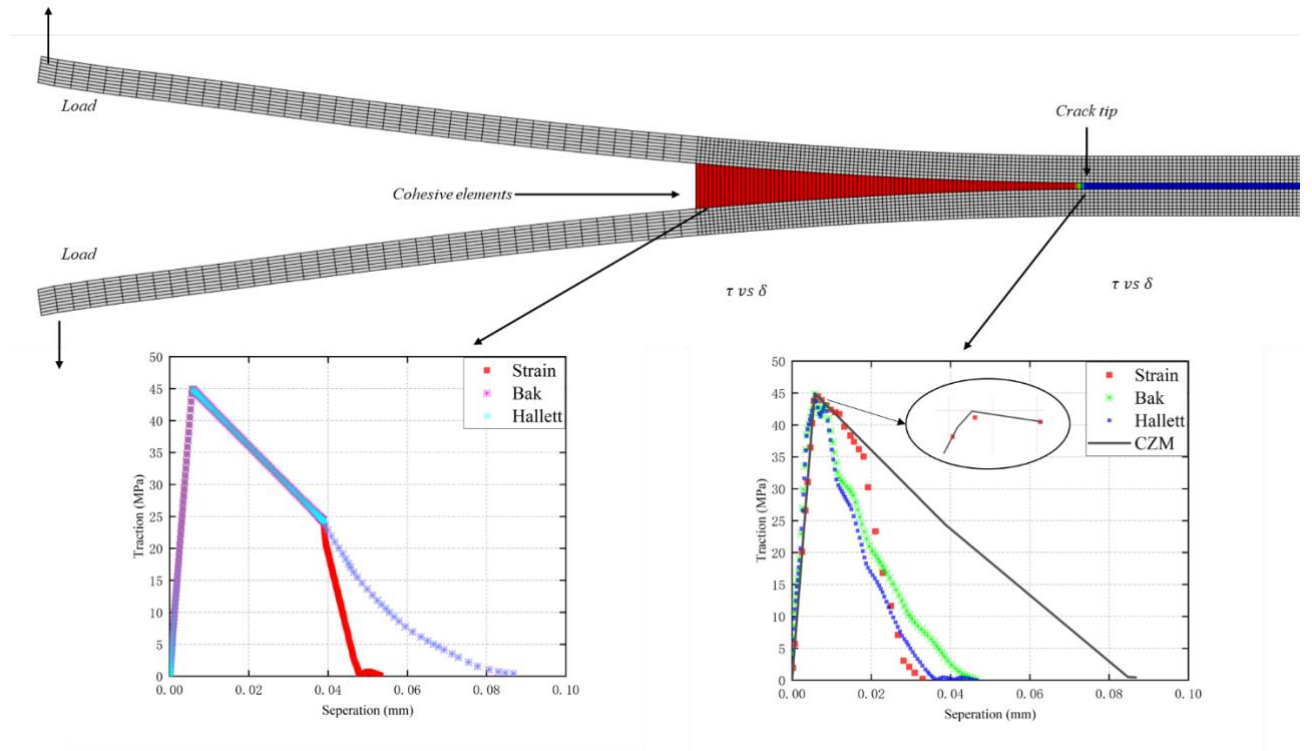


Figure 4-16 The Traction τ vs Separation δ for different points of adhesive for 1.6mm thickness model.

The relation of traction and separation for 1.6mm and 12.7mm thickness adherend models have been plotted as Figure 4-16 and Figure 4-17. Upon observing the relationship between traction force and crack tip displacement in the 1.6mm test model, it is evident that all three methods adhere to the same cohesive law during the quasi-static process. However, when fatigue load started, differences were revealed in fatigue damage due to variations in the fatigue damage models. As illustrated in Figure 4-16, there is negligible difference between Bak's and Hallett's methods concerning the cohesive law at the pre-fixed crack tip. Conversely, the strain-based method exhibits a more pronounced impact on fatigue damage, leading to a faster failure compared to the other two methods. The relationship between traction and separation on the right side of Figure 4-16 can be explained that when fatigue damage dominates, the cohesive law corresponding to the three damage models can be deduced. Given that the test model with an adherend thickness of 1.6mm was used to calibrate the material constants required for the three modes, the area G_{max}

covered by the three modes closely aligns. Additionally, though not overt in the figure, magnification reveals that the maximum tractive force in the strain-based method does not reach the corresponding maximum value dictated by the quasi-static cohesive law due to the fatigue-induced damage.

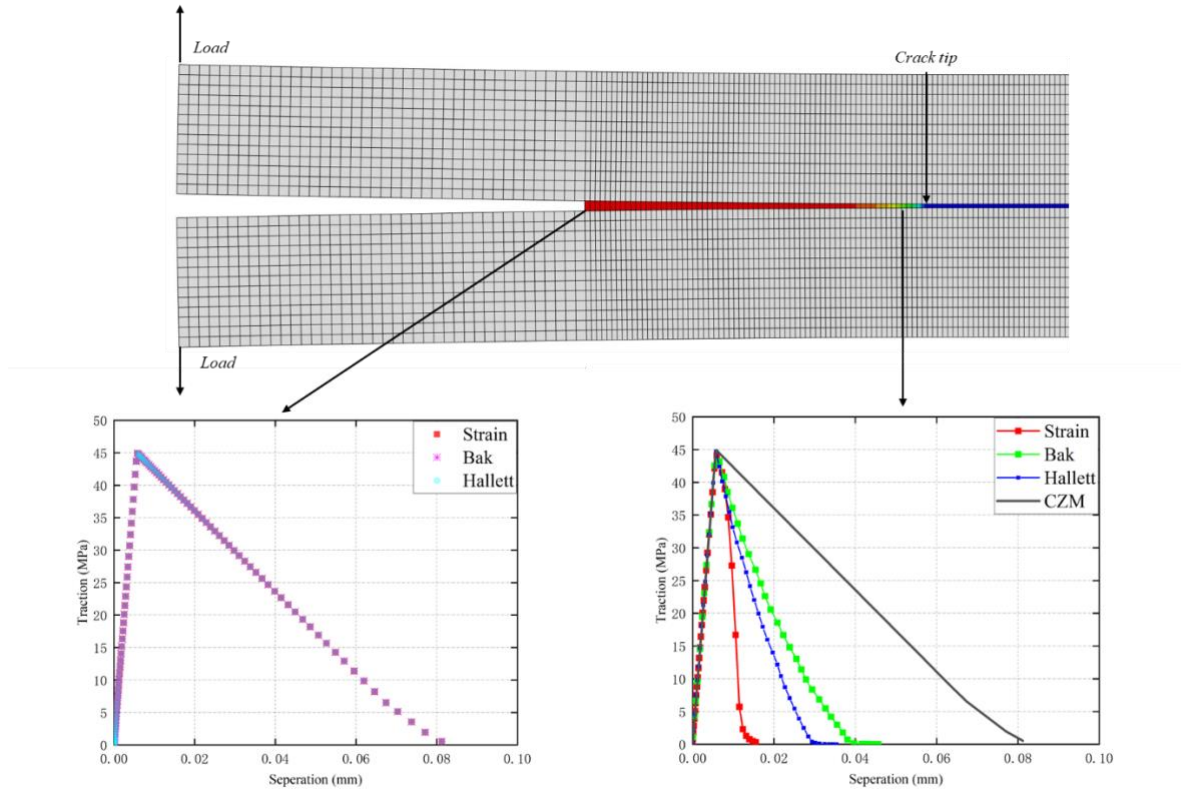


Figure 4-17 The Traction τ vs Separation δ for different points of adhesive for 12.7mm thickness model.

For predicting fatigue crack development in the test model adherend with a thickness of 12.7mm, as depicted in Figure 4-17, the prefabricated crack tip underwent a complete quasi-static loading process. In forecasting fatigue cracks growth, the decay processes observed through three methods differ. Notably, the strain-based method's results suggest that the entire fatigue damage process requires less energy consumption, as evident in Figure 4-17. A synthesis of the results allows us to draw a conclusion: under identical G conditions, a higher predicted da/dN implies a more conservative prediction. This finding holds significant implications for guiding product design that need

considering debonding and delamination like composite structures. Simultaneously, it demonstrates that the strain-based approach is more adaptable to variations in size compared to the method based on Paris law.

4.5 Conclusion

A novel simulation method based on strain has been proposed for analysing fatigue delamination in adhesively composite structures under cyclic loading. Different from existing methods for fatigue delamination analysis method, the method proposed is not based on the traditional understanding of fracture mechanics.

The study of mesh length sensitivity shows that the accuracy of simulation has a weak influence when using coarse mesh for strain-based method. The strain-based method is not influenced by parameters not directly linked to damage rate model and the preliminary work for input parameters before simulation starting is greatly reduced. The efficiency of simulation is improved accordingly.

The tests results are successfully reproduced by strain-based method, and the results are obviously superior to the existing two methods in the simulation prediction of specimens with different sizes, which reflects a high-quality dimensional adaptability, which is meaningful to conduct conservation prediction for models with different sizes.

As an original attempt to use interlaminar strain to predict fatigue delamination growth, a novel research approach was provided for researchers to analysis the damage mechanism of fatigue delamination.

5 Experimental Investigation of Fracture Process Zone Dynamics

5.1 Introduction

The growing application of laminated composites and adhesive bonding in advanced structures has raised the concern of fatigue damage in these materials. Delamination and debonding, being the dominating failure modes, can be considered as a representative form of elastoplastic interface failure. Its distinctive characteristics when compared to traditional homogeneous materials is the constraint imposed on the crack tip expansion by the surrounding high strength/stiffness materials. Thorough understanding of the adhesive fatigue damage is key for development of high-performance structure against cyclic load and may also shed light into better understanding of fatigue damage at these elastoplastic interfaces.

Paris law, which prescribes crack growth rate as a function of strain energy release rate at crack tip (Paris & Erdogan, 1963), has been utilized as a criterion for evaluating fatigue damage at elastoplastic interface (Chiu, 2016; Jablonski, 1980; Nojavan et al., 2016), drawing inspiration from its successful application in describing metal fatigue damage and predicting crack growth (Pascoe et al., 2013). A large number of researchers have implanted Paris Law into CZM to simulate the interfacial crack growth under fatigue load (Bak et al., 2015; Harper & Hallett, 2010; Kawashita & Hallett, 2012; Turon et al., 2006; Turon, Dávila, et al., 2007). By incorporating the Paris Law, which describes macroscopic cracks, into the local damage evolution, certain models necessitate the introduction of new variables that cannot be predetermined (Robinson et al., 2005; Turon, Dávila, et al., 2007). This significantly diminishes the predictive capability of these models (Bak et al., 2017). On the contrary, some models successfully assign the crack growth rate obtained from the Paris Law to the damage process region by introducing the J -integral through a complex variation formula (Bak et al., 2015; Carreras et al., 2019). However, the suitability of the Paris Law as a material property for describing interfacial fracture remains uncertain (Pascoe et al., 2013). Paris Law encounters

limitations in predicting fatigue crack growth at the interface (Allegri, 2018). It has been observed that the applicability of the Paris Law is contingent on the geometry in DCB tests (Azari et al., 2013). Various studies have been proposed to normalize of the Strain Energy Release Rate (SERR)(Hojo et al., 2002; Jones et al., 2012; Yao et al., 2014). However, it remains challenging to get unified Paris law that can account for diverse load ratio and boundary conditions (Azari et al., 2013); it was also suggested that the size of the plastic zone is influenced by adherend thickness and loading conditions, affecting crack propagation behaviour due to variations in stress distribution (Azari et al., 2011, 2013)

The SN curve, employed to represent a material's fatigue life, is considered suitable for delineating the initiation of crack. Some researchers have incorporated the principle of SN curves into the construction of CZM models. The SN curve has been employed to define the initiation of damage, and successfully reproduced the mixed mode Paris law (Nojavan et al., 2016). A reduction of strength can be introduced into SN curve, for modelling continuum damage (larve et al., 2017). A heuristic fatigue cohesive model was recently proposed (Dávila, 2020a), where the accumulation of damage during crack propagation was associated with SN curve. A unified cohesive zone formulation has been presented (Allegri, 2020) to describe the fatigue-driven crack onset and propagation. Considering that unstable fracture happens when the number of cycles is reached for a given stress level on SN diagram, the fatigue damage evolution at any single material point is not fully traversed. This distinction is notable when compared to the complete failure of materials within the FPZ during a crack propagation event. There is a noticeable gap in understanding the continuous damage evolution process with FPZ, that cannot be covered by S-N curve approach.

Although the complete damage of material on the crack growth path has been implicitly covered in the formulation of Paris law, the local damage evolution process is not considered, which also leads to the difficulty of defining the damage of each cohesive element in numerical analysis in the context of

nonlocal approach (Bak et al., 2015; Choi et al., 2020; Dávila, 2020a). The other drawback of Paris law in practice is the ambiguity of defining crack tip in ductile materials, and the terminology of “crack” may not be sufficient in the context of ductile interface (Cui et al., 2014; Sills & Thouless, 2015). The failure at each material point in FPZ is also governed by SN curves until the stress surpasses its quasi-static limit. Therefore, an in-depth understanding of what happens in FPZ during fatigue crack growth may be vital for improving the accuracy of Paris law approach and can hopefully bridge the gap between Paris law and SN curve regarding the fatigue damage mechanisms. Despite the increasing attention on the FPZ (Allegri, 2020; Cui, 2016; F. Sun & Blackman, 2020; Zhu et al., 2020) few experimental works have been reported.

This paper aims to reproduce the geometric dependence of Paris law at elastoplastic interface and provides an experimental study on the evolution of FPZ under fatigue loads. The specimen preparation and test methods are introduced in Section 5.2, methods for detecting FPZ are presented in Section 5.3. Section 5.4 presents the results of the tests conducted on specimens with varying adherend thicknesses and includes a discussion on the influence of the FPZ on crack propagation from the fracture mechanism. Finally, a brief and succinct conclusion is presented in Section 5.5, summarizing the key findings and contributions of the study.

5.2 Experiments and Data Processing

5.2.1 Specimen Preparation

The DCB specimen, in which a ductile adhesive layer was bonded between aluminium plates, has been prepared in this paper. The DCB specimen with different adherend thicknesses of 5mm, 8mm, and 15mm were employed here, to highlight the geometrical dependence of Paris law, aiming to investigate the change in FPZ.

The aluminium alloy (AA7075-T651) was used as the substrate material. The 3M™ Scotch-Weld™ Epoxy Adhesive 2216 was employed here, which is a two-component epoxy adhesive toughened with Kaolin powders. The nominal

thickness of adhesive layer is $380 \pm 20 \mu\text{m}$, and the DCB specimen is 140mm long and 20mm wide, as suggested by the standard (ASTM D5528-01, 2014).

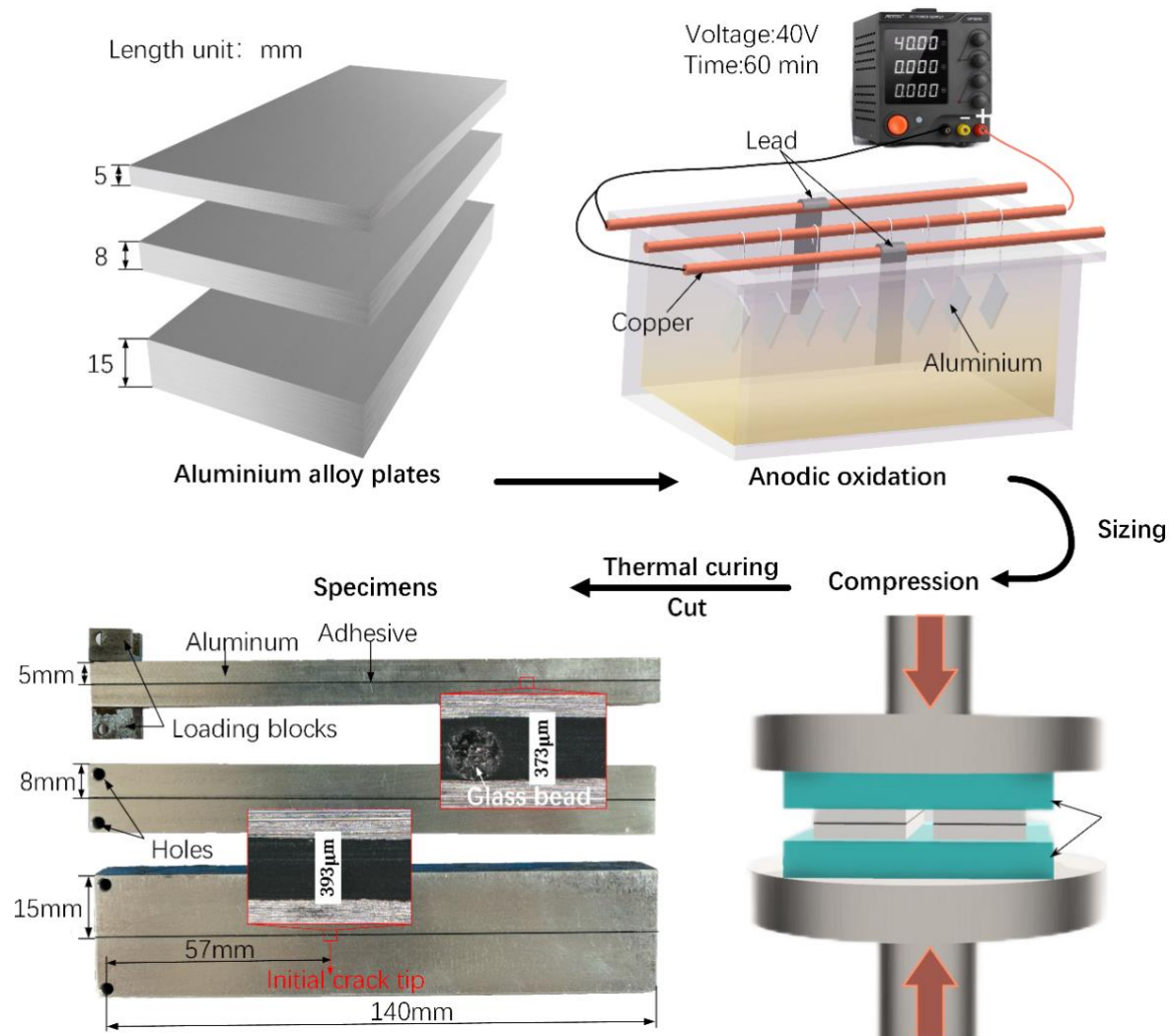


Figure 5-1 Process diagram of specimens' fabrication.

As shown in Figure 5-1, aluminium plates were anodized for 1h with 40V voltage in an oxidation solution of 10% phosphoric acid and 5% malic acid. Immediately following the oxidation, the substrates were rinsed with deionized water and then dried in an air circulating oven before they were bonded with adhesive. Glass beads of 1% weight fraction were mixed with adhesive paste for controlling the bond line thickness. An initial crack was prepared by placing a 12.5 μm thick PTFE film at the end of each substrate. The adhesive joints were cured in oven for 12h at 60°C, as suggested by the adhesive manufacturer. After curing, the structure was cut into specimens with a width of 20mm and a length

of 140mm, all excess adhesive was removed with a grinder. The loading blocks were bonded onto the end of the of the 5mm specimens where a pre-crack is present, while thicker specimens were drilled with symmetrical holes. The edges of the adhesive layer were then polished to the level of 2000-grit to improve the visibility of the crack.

5.2.2 Test Methods

Two specimens of each configuration were employed for static testing, while the other two were tested under fatigue load. Experimental setup for the tests under fatigue is shown in Figure 5-2 (a). To coordinate with digital image correction (DIC) system, two sizes of speckles were used. One side was approximately 0.02mm in diameter, while another side was about 0.2mm in diameter, as shown in Figure 5-2 (b), (c). These speckles were generated by “speckle” software and then printed onto water transfer paper. The paper was precisely affixed to both the upper and lower substrates, ensuring a firm attachment to the adhesive layer.

Table 5-1 Load configuration in quasi-static and fatigue tests.

Adhered thickness	Static tensile rate	Maximum displacement in fatigue
5mm	1mm/min	2.31mm
8mm	0.5mm/min	1.23mm
15mm	0.3mm/min	0.69mm

The quasi-static DCB tests were performed on an Instron 5848 testing with a 2 KN load cell, as shown in Figure 5-2. A 20-megapixel DAHENG™ camera with 50 mm lens was used for monitoring the failure process. The tensile machine and camera were triggered simultaneously, and the camera's acquisition frequency was set at 1 FPS. To maintain consistent crack growth rate, varying loading rates were adopted for different groups of specimens, as shown in Table 5-1.

All fatigue experiments were carried out on an electro-hydraulic servo fatigue test system (Instron 8872) with a 5 KN load cell. Experimental setup for the tests under fatigue is shown in Figure 5-2 (a), with cameras installed on both the front and back sides of the specimen. The rear side employed a 20-megapixel DAHENG™ camera, identical to that used in quasi-static testing, which was responsible for capturing full-field images, as shown in Figure 5-2(b). On the front side, a 5-megapixel BASLER™ global shutter camera was mounted on a programmable three-axis automated platform, tasked with capturing localized images of the adhesive layer, as shown in Figure 5-2 (c). The experiments were conducted under displacement control, with a constant displacement ratio, $R=\delta_{min}/\delta_{max}=0.1$, the δ_{max} of different groups were shown in Table 5-1. Three different frequencies were employed during the fatigue tests to facilitate image acquisition and expedite the experimental process; the details will be discussed later. Once reached the predetermined number of cycles, the test was paused, and the specimen was held at the maximum displacement position until images were saved. During the fatigue tests, the computer system recorded the peak loads (P_{max} and P_{min}), peak displacements (δ_{max} and δ_{min}), and number of cycles (N) with interval of 10 loading cycles.

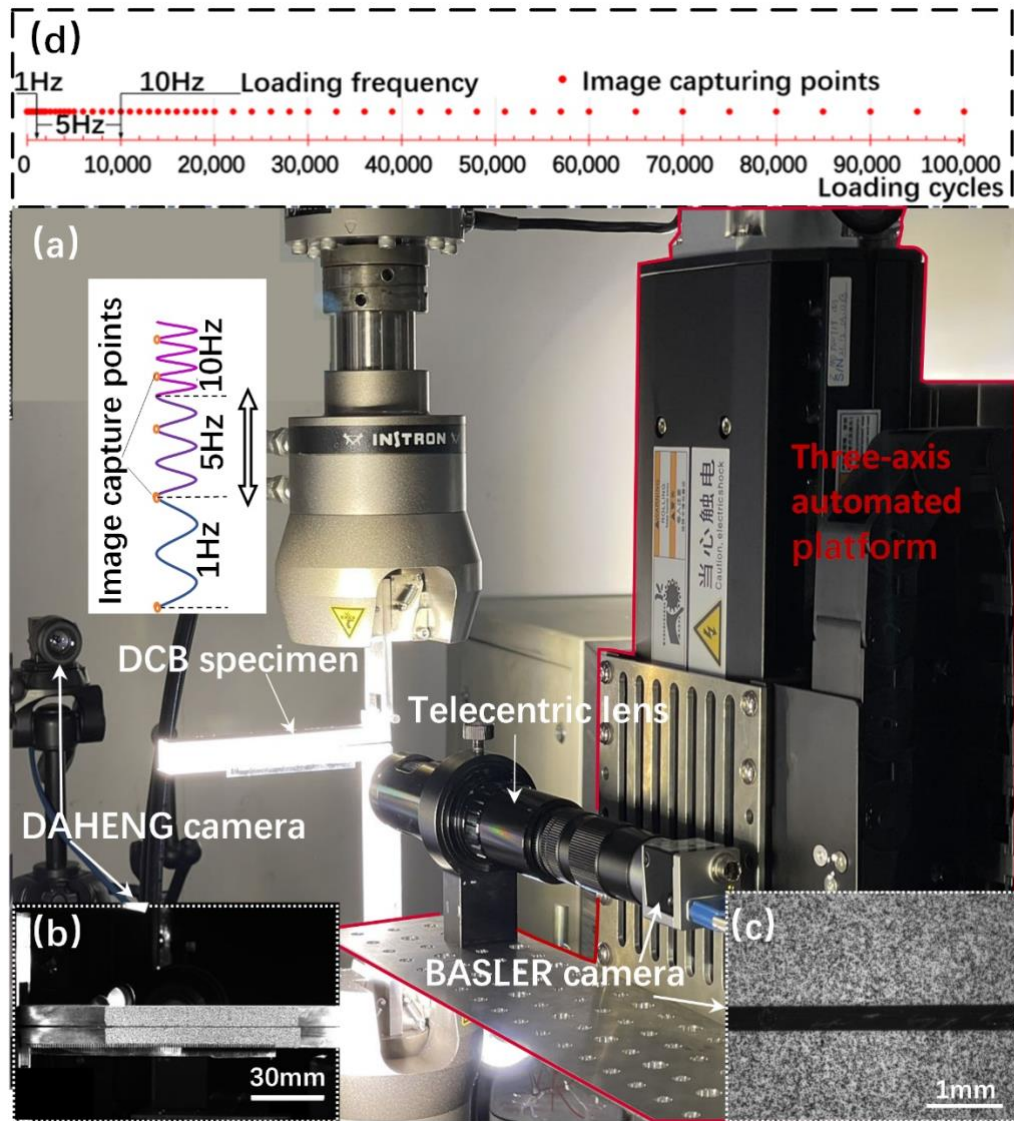


Figure 5-2 (a) Experimental setup for the tests under fatigue;(b) Image captured by the DAHENG camera;(c) Image captured by the BASLER camera;(d) Fatigue test frequency and image capturing frequency axis diagram.

The applied maximum displacement $\delta_{\max}$, ranging from 0.69 to 2.31 mm, was determined to ensure that the initial values of energy release rate (G_{initial}) for each test group were at the similar level. Since energy release rate decreases with accumulated cycles, it was necessary for the G values of different test groups to have a certain degree of overlap. In other words, to compare various data at a certain G value, the G_{initial} had to be greater than that value. The G_{initial} was set 20% to 30% of Mode I fracture toughness G_{IC} , the fluctuation of this ratio was established considering that obtaining precise G_{IC} from experiments is

practically challenging. The adjustments should be based on pre-experimental crack growth behaviours. For instance, if the crack propagation rate is too fast, the $\delta_{\max}$ should be reduced to decrease the G_{initial} .

As illustrated in Figure 5-2 (a), this experiment employed three distinct frequencies: 1Hz for the initial 1000 cycles, followed by acceleration to 5Hz until the 10,000th cycle, and finally shift to 10Hz until the end of the test. While it has been observed in some materials that the frequency of cycles can influence fatigue life (R. Adimi, 1998, (Al-Khateeb & Ghuzlan, 2014), it's important to note that in this experiment, the frequency was kept consistent across all specimens. Therefore, it is believed that the frequency setting did not impact the differences of results. Due to the characteristics of crack growth (Yao et al., 2017), the image capture points were densely arranged during the crack initiation period, with a frequency of 1 capture every 10 cycles within the initial 100 cycles. As the crack propagation rate gradually decreased, the interval between image captures was extended, as shown in Figure 5-2 (d). After 60,000 cycles, the frequency was adjusted to every 5,000 cycles.

During each experiment pause, the rear camera captured a full-field image, as shown in Figure 5-2 (b). The front camera, paired with a variable-magnification telecentric lens, was able to record an about 4mm-long region within a 2592-pixel image. The platform's movement error remained below 5 micrometres. After the camera was fixed, the platform moved 1mm along the direction of crack propagation. This process of capturing images and moving was repeated over 50 times, achieving a continuous scan, and recording of the adhesive layer. In this experiment, at least 80 sets of photographs were collected for each specimen. These images were grouped according to their distance from the initial crack tip, with each group containing a complete set of fatigue progression images for the respective positions. For example, for the 8mm specimens, maximum position images were captured from 0 to 125007 cycles at distances ranging from 0 to 58mm from the initial crack tip.

2.3 Maximum J -integral calculation

The maximum Mode I J -integral was calculated at the maximum applied displacement δ_{max} during the cyclic loading by the J -integral method. The path independent J -integral was proposed by Rice, and its analytical expression was derived by (Cui et al., 2014) Where a_0 is the initial crack length as 57(mm), P is load, h is adherend thickness, b is specimen width, E_f is the flexural modulus of aluminum substrate as 71000(MPa), ω is the rotation of the adherends at the initial crack tip:

$$J_{max} = \int_0^{\omega} \sigma d\tilde{\omega} = \frac{12a_0^2}{E_f h^3 b^2} P_{max}^2 + \frac{P_{max}}{b} \omega \quad (5-1)$$

5.3 . Analysis of FPZ

5.3.1 The Definition of FPZ

Considering the zone near the crack tip in an adhesive joint under mode I load, the traction and strain ahead of the crack tip are illustrated in Figure 5-3. The load applied in the experiment has consistently been tensile, resulting positive strain within a specific area (i.e., zone OB) in the specimen (F. Sun & Blackman, 2020). For a more detailed discussion, the tension zone can be divided into FPZ and elastic tension zone. FPZ is generally defined as a inelastic region near the crack tip where damage processes are active (Blaysat et al. 2015). In other words, FPZ exists many discontinuous microcracks, but this region still participates in transmitting forces, serving as the transitional area from the damage initiation to complete fracture. The term "complete fracture" refers to the zero-traction region, the end of which is considered to be point O. Point A corresponds to the initiation of damage, but the size of elastic tension zone is very small due to the high interface stiffness (Turon, Dávila, et al., 2007). In addition, it is more straightforward to detect zero-strain point by DIC method, this study therefore assumes $l_{FPZ} \approx l_{OB}$. This study effectively defines a "zero-traction" point which corresponding to the visible crack tip, and a "zero-strain" point which corresponding to extended crack tip, whereby the full displacement continuity of the pristine material is restored within the FPZ

(Allegri, 2020). These two points can be detected from the recorded images as follows.

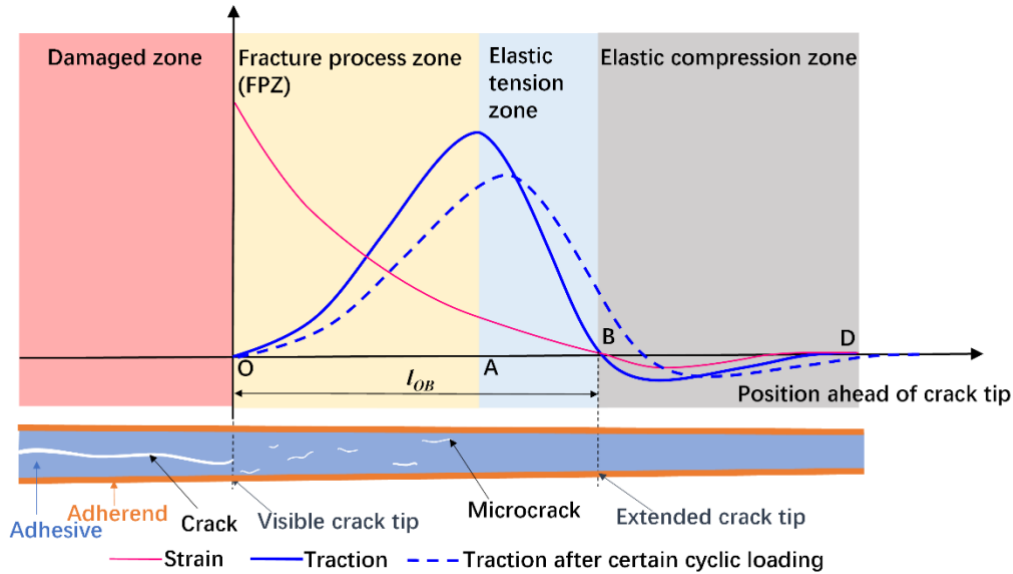


Figure 5-3 The traction and strain ahead of the Crack Tip.

5.3.2 The Localization of the FPZ.

The strain fields obtained from DIC processing on the full-field images captured at two different loading cycles are shown in Figure 5-4. The software used there is ARAMIS, and the facet size was 25, step size was 15. In the variable strain zone, the strain gradient varies from red to blue from the left to right, indicating a decrease in strain along the crack path. The extended crack tip is situated at the point where the strain gradient vanished, indicating a position of zero strain. The motions of the extended crack tip with proceeding loading cycles are illustrated in Figure 5-4 (a) - (b). The crack length, a , as defined by this approach, effectively encompasses the FPZ. It is noteworthy to mention that due to constraints imposed by camera resolution, the extended crack tip cannot be detected precisely. But it is straightforward to initially locate the extended crack tip using this method and then update its position based on local images. Stitching together the magnified images of the localized photos reveals a distinct adhesive layer shape. The visible crack tip is positioned at the termination point of the continuous crack, as shown in Figure 5-4 (a)-(b). Figure 5-4 and Figure 5-5 depict illustrations of crack tip positioning in the same

specimen under two different cyclic states, demonstrating that the extended cracks are approximately 8mm longer than visually observed cracks.

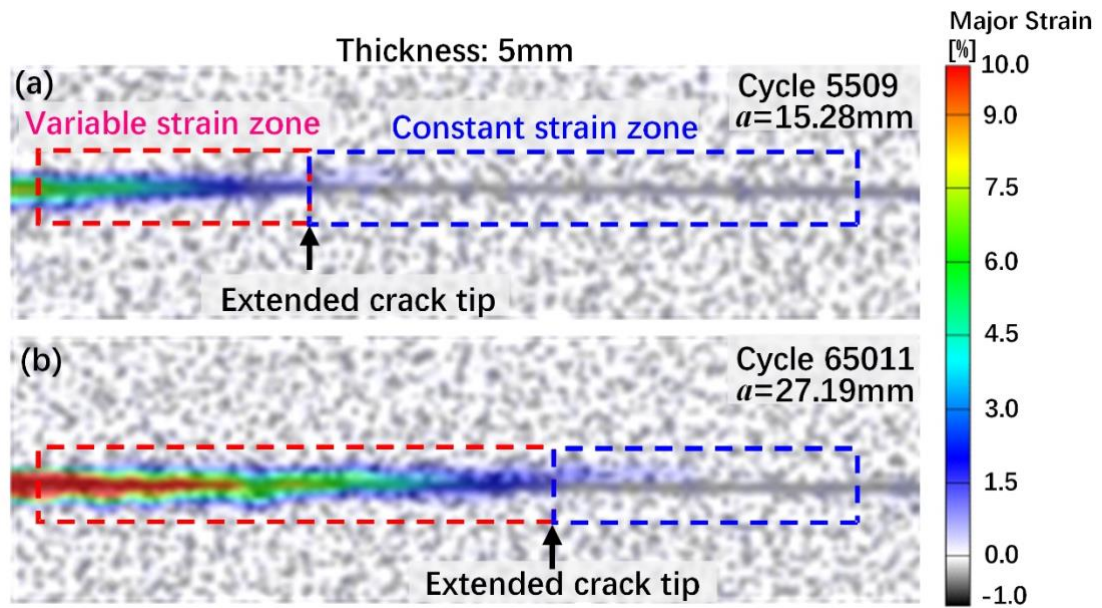


Figure 5-4 A typical case of extended crack tip (starting point of FPZ) detection using DIC. The specimen with a thickness of 5mm is shown at (a) Cycle5509, where $a=15.28\text{mm}$; (b)Cycle65011, where $a=27.19\text{mm}$.

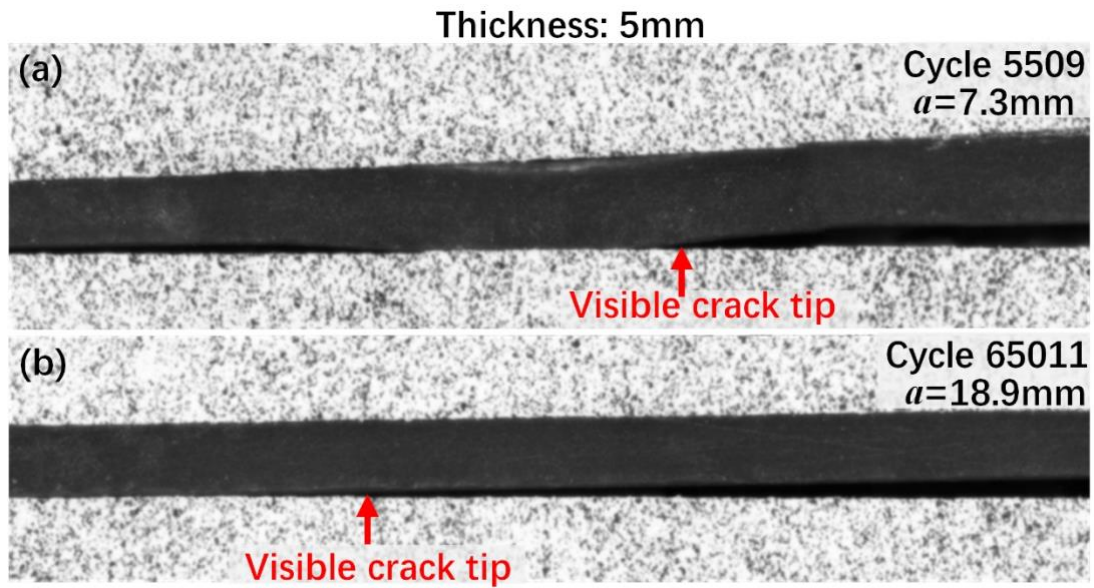


Figure 5-5 Identification of the visible crack tip (ending point of FPZ). The specimen with a thickness of 5mm is shown at (a) Cycle5509, where $a=7.3\text{mm}$; (b) Cycle65011, where $a=18.9\text{mm}$.

5.3.3 Strain Extraction of FPZ

After establishing the starting and ending points of the FPZ, the next step was to extract strain from the localized images. The entire process of capturing and analysing the FPZ is illustrated in Figure 5-6. Due to equipment vibrations and lens distortions, these localized images were sensitive to errors. Hence, in this study, a self-calibration technique based on experimental methods (Z. Zhang, 2000b) was employed to determine the internal and external parameters of a camera. The Camera Calibrator plug-in integrated into the MATLAB toolbox was utilized to estimate all the parameters using the closed form solution and refined all parameters by minimizing lens distortion parameters. Moreover, due to the presence of chromatic aberration and vignetting at the image edges, it is necessary to uniformly crop the unclear parts of the image, as shown in Figure 5-6, with an actual image length of approximately 2mm.

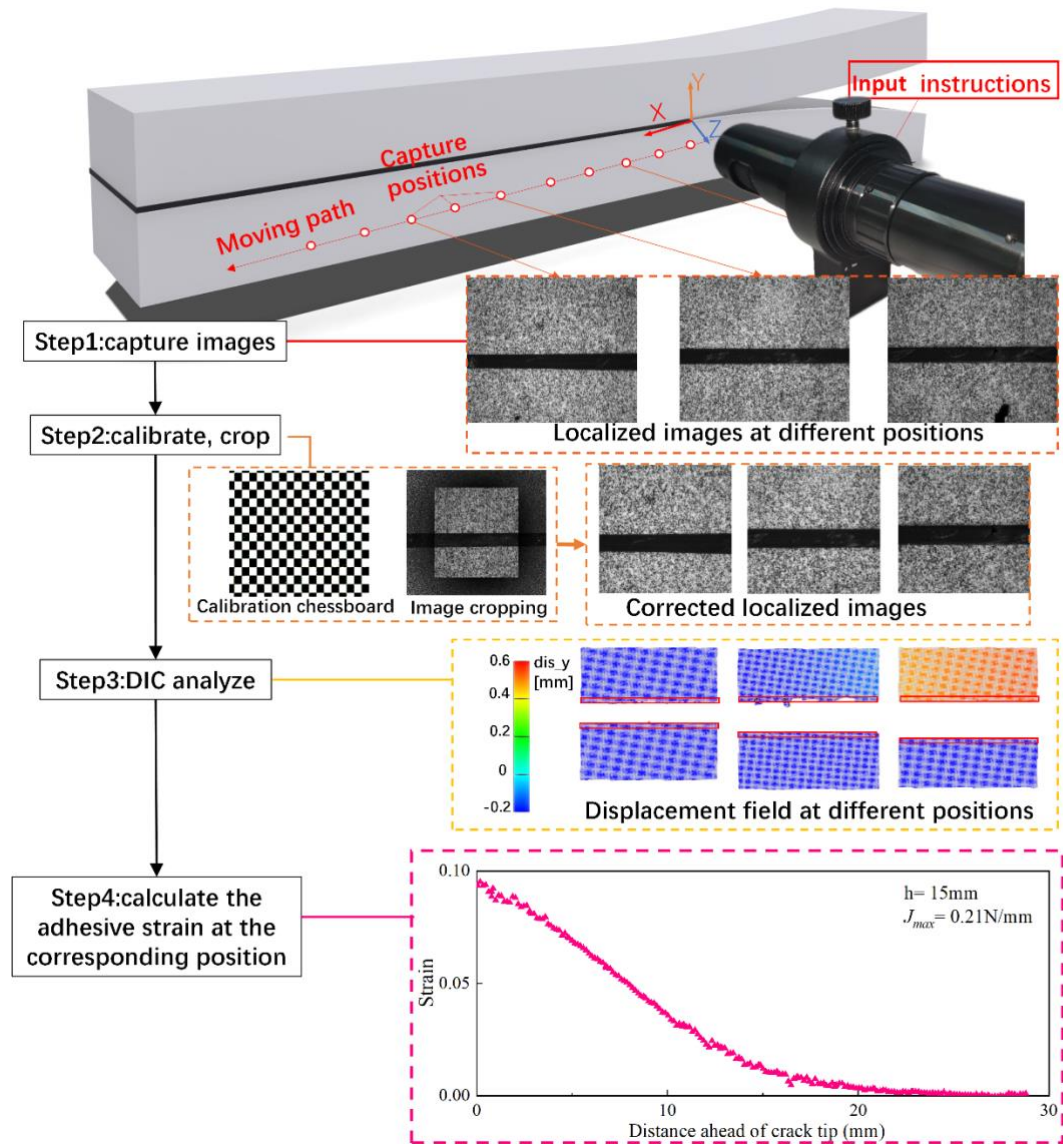


Figure 5-6 Strain map extraction procedure.

The corrected images described above were imported into the XTDIC software for displacement analysis. The facet size used in the software was 15 and step size was 10, corresponding to an actual size of 15 μm for the divided grid. The DIC software can output the displacement and coordinate information of all grids on the image. As the thickness of the adhesive layer is known, we selected two rows of grids near the upper and lower edges of the adhesive layer, identified two points with the same column coordinate, and employed the Eq. (5-2) to calculate the strain. Where y is vertical displacement, with the

subscript's u/d indicating the upper/lower edge, and t is the thickness of the adhesive layer.

$$\varepsilon = \frac{|y_u - y_d|}{t} \quad (5-2)$$

In this study, the image was divided into 150 columns, a point was extracted from every 10 columns, which resulted in a practical spacing of 0.15mm. The adopted approach enabled the precise capture of the size and shape of the FPZ.

5.4 Results and Discussion

5.4.1 Quasi-static Tests Results

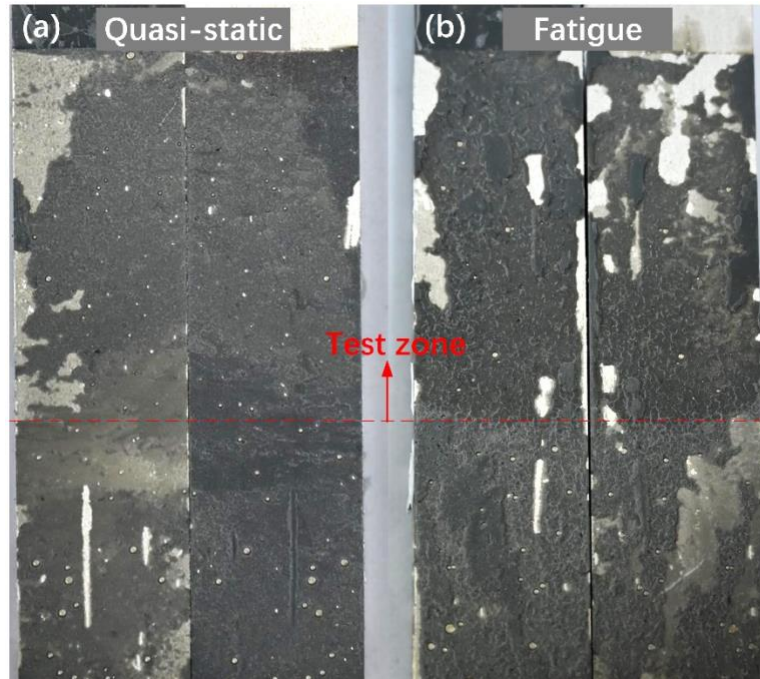


Figure 5-7 The fracture surfaces of 8mm specimens after (a)Quasi-static testing; (b)Fatigue testing.

The cross-section morphology of the 8mm specimens after the static and fatigue tests is presented in Figure 5-7 respectively. It is evident that the adhesive layer exhibits predominantly uniform distribution on both sides of the joint, indicating that cohesive failure dominated the failure mode.

Fracture energies for specimens of different thicknesses were assessed using experimental data through the J -integral method outlined in Section 5.2.3. Utilizing DIC measurements, the opening displacement data, Δ , from the initial crack tip were collated and plotted against J -integral, as shown in Figure 5-8 (a). The J curve arrived at G_{IC} within a relatively close displacement range across varying thicknesses, indicating that the influence of thickness variation on G_{IC} was minimal. Upon reaching G_{IC} , specimens with different thicknesses exhibit divergent responses with the increase of crack tip opening displacement. Owing to the impact of the size effect, the fracture energy of 15mm specimens experience a rapid decrease, which related to the unstable crack propagation behaviour observed shortly after reaching the peak load, whereas the 5mm and 8mm specimens enter its plateau value state. The influence of measurement errors and noise on DIC analysis under low loads induces some fluctuations in the platform during this period.

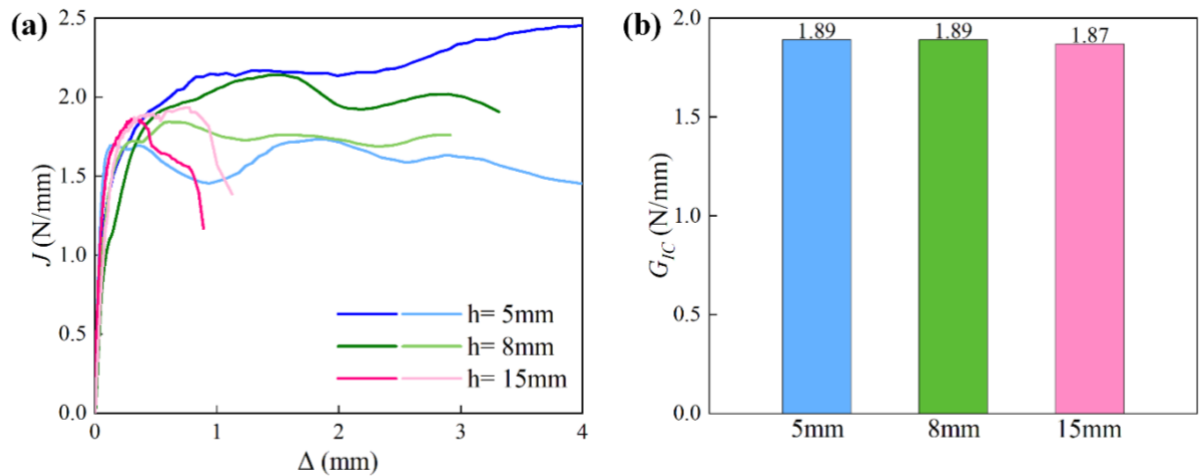


Figure 5-8 (a) J vs. Displacements diagram of specimens with different thickness for Quasi-static tests; (b) The values of G_{IC} measured for the three sets of specimens.

This G_{IC} value is crucial for ensuring the preset maximum displacement in fatigue tests. The average G_{IC} is computed and illustrated in Figure 5-8 (b), revealing no significant variance with changes in thickness. This observation underscores the characteristic that G_{IC} is considered a material property, as G_{IC} values are nearly identical for all three types of specimens.

5.4.2 Fatigue Tests Results

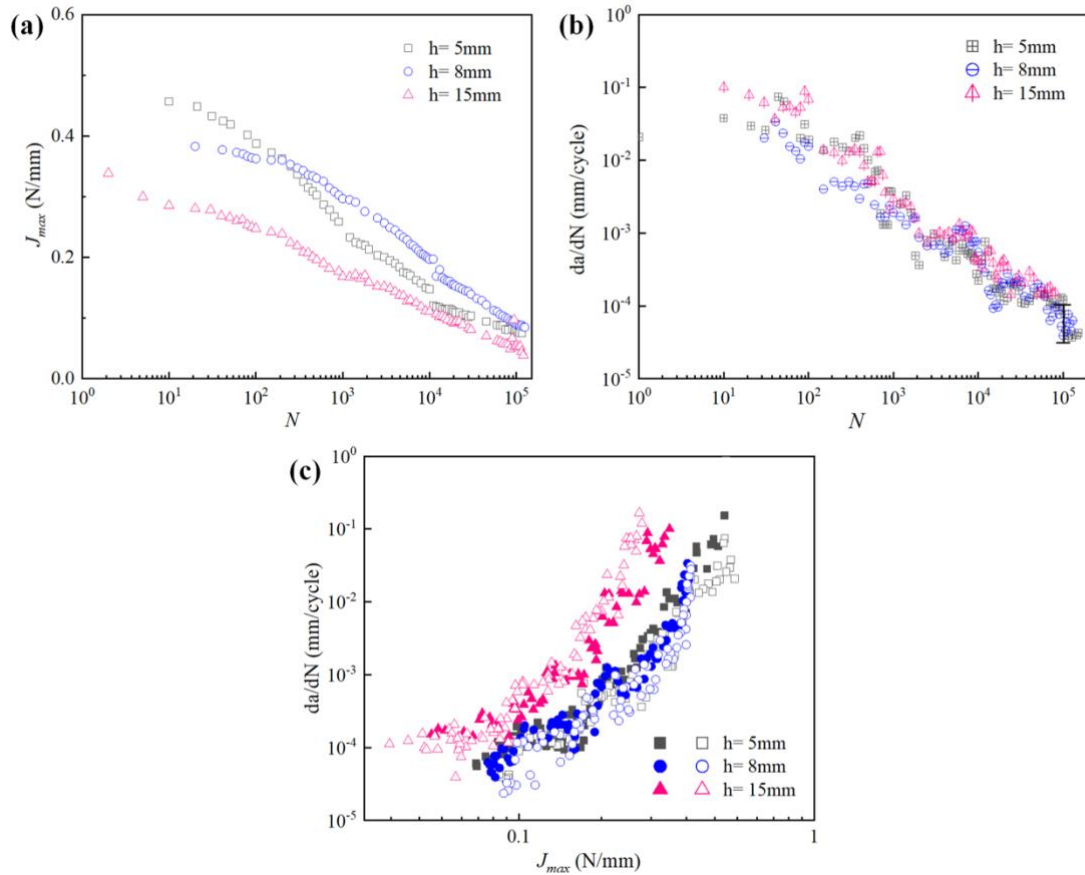


Figure 5-9 (a) maximum strain energy release rate vs. the number of cycles; (b) crack propagation rates vs. the number of cycles; (c) effect of adherend thickness on fatigue crack growth rates of mode-I DCB joints.

The experimental data were processed into scatter figure as Figure 5-9. The relationship between the maximum strain energy release rate and the number of cycles is shown in Figure 5-9 (a). It is observed that the J_{max} decreases with an increasing number of loading cycles for all specimens, because of the propagation of crack at constant displacement load. The 15mm specimen is significantly lower than the other two groups, which is due to the deliberate lower setting of $G_{initial}$ for the 15mm specimen, at 20% G_{IC} , as described in Section 5.2.2.

The crack propagation rates and loading cycles for all specimens are plotted in Figure 5-9 (b). With increasing crack length, the driving energy around crack tip

was decreased, and more cycles are needed to drive crack growth at low stress state. The crack growth rate, as function of strain energy release rate, is shown in Figure 5-9 (c). The difference of crack growth rate between specimens with adherend thickness of 5mm and 8mm is not significant, in comparison with scatters in these data; however, the crack growth rate in specimen with 15mm thick adherend is noticeably higher. Similar results has been reported previously by Azari S (Azari et al., 2013) , and it seems evident that the fatigue crack growth rate at same strain energy release rate is dependent on specimen geometry. This may help to explain the inaccuracy of Paris law when modelling interface fatigue crack growth in composite laminates and adhesive bonding. Further investigation of this observation is discussed as followings.

5.4.3 Discussion on FPZ

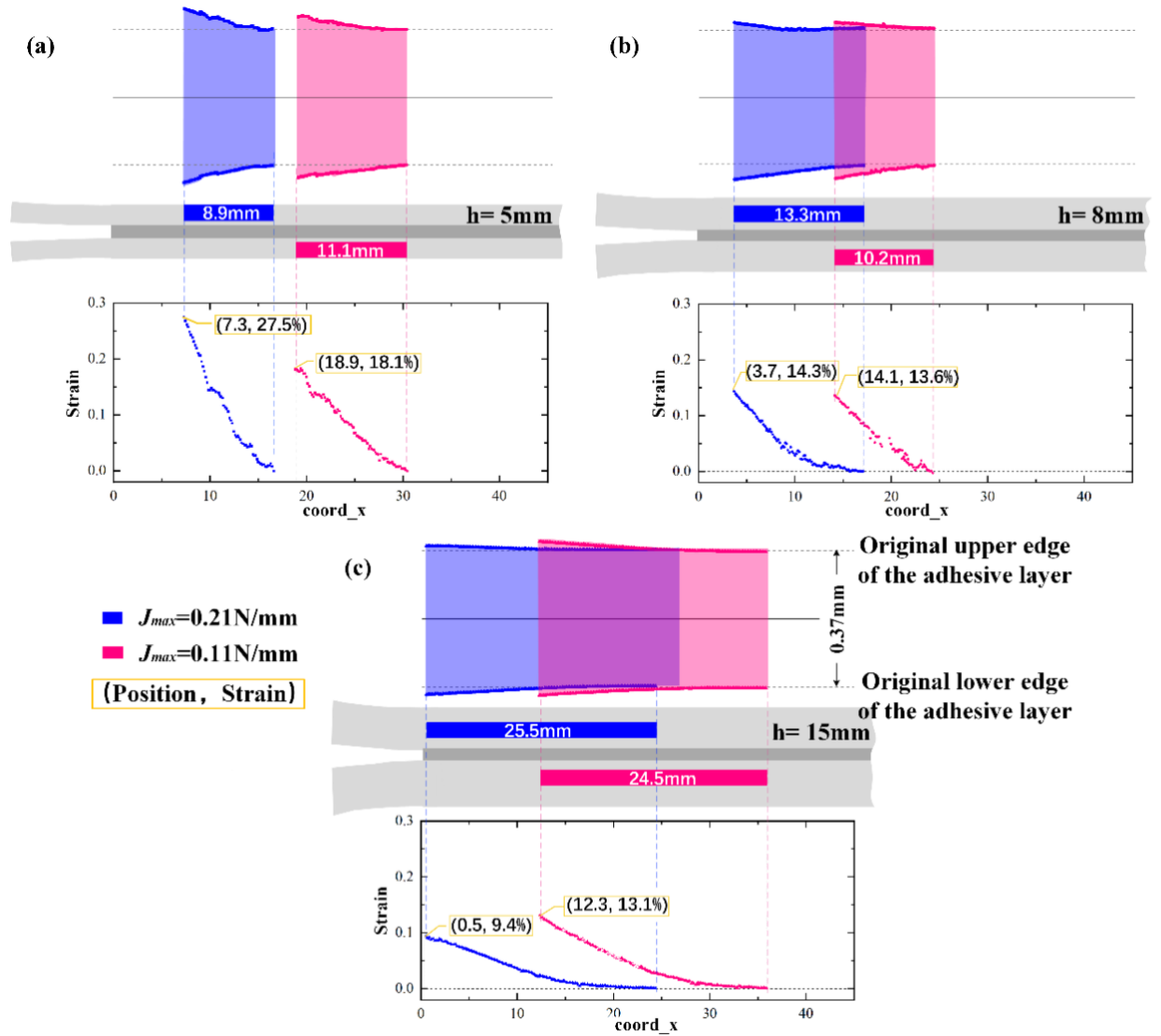


Figure 5-10 The shape of the FPZ and strain plot along the initial crack tip ahead at different J_{max} values.

Table 5-2 Fatigue test data for FPZ.

Group	Average length of FPZ, l_{fpz}	Average failure strain, ϵ_f	Number of hysteresis cycles, N_h
5mm	10mm	22.8%	2302
8mm	11.8mm	14%	5991
5mm	25mm	11.2%	27975

The shape of the FPZ and the strain distribution in this region are shown in Figure 5-10, and the two selected segments were at $J_{max}=0.21\text{N/mm}$ and $J_{max}=0.11\text{N/mm}$, respectively. Some key data was averaged and put into Table 5-2 for comparison. It's apparent that the FPZ lengths for the 5mm and 8mm specimens are quite similar, while the FPZ length for the 15mm specimen is more than twice as long as the other groups, measuring 25mm, which represents over 30% of the entire crack propagation zone (80mm). It is presumed that the variation in FPZ geometric shapes results in different internal stress distributions, leading to the differences in crack growth rates observed in Figure 5-10 (c).

The marked left point of the FPZ in the Figure 5-10 is the point of complete failure, the strain at this point is defined as the failure strain, where the region to the left of the location (opposite to the crack growth) loses its load-bearing capacity. The failure strain is founded to be decrease with thicker adherend thickness. Additionally, this study records the number of cycles that material points remained within the FPZ region, representing the number of cycles required for a material point to go from participating in loading to complete failure. This value differs from the macroscopic global fatigue cycles, and is denoted as the hysteresis cycles, N_h , as shown in Table 5-2. The hysteresis cycle is found to be proportional to the length of FPZ. In other words, for a given da/dN , the longer the FPZ, the larger number of cycles for the material point to reach complete failure. Additionally, since the da/dN decreases with increasing cycles (Figure 5-10 (b)), the increase in FPZ length brings about an exponential increase in N_h .

The different stiffness of the adherend with varying thickness introduces distinct boundary conditions, resulting in different shapes of the FPZ. Under fatigue loading, the failure behaviour of the material in the large-sized FPZ generated by the elastoplastic interface is nonlinearly dominated. The linear elastic fracture mechanics theory of crack growth driven by the *SERR*, is not applicable in this scenario where crack growth is dominated by nonlinear effects.

5.4.4 Failure mechanisms

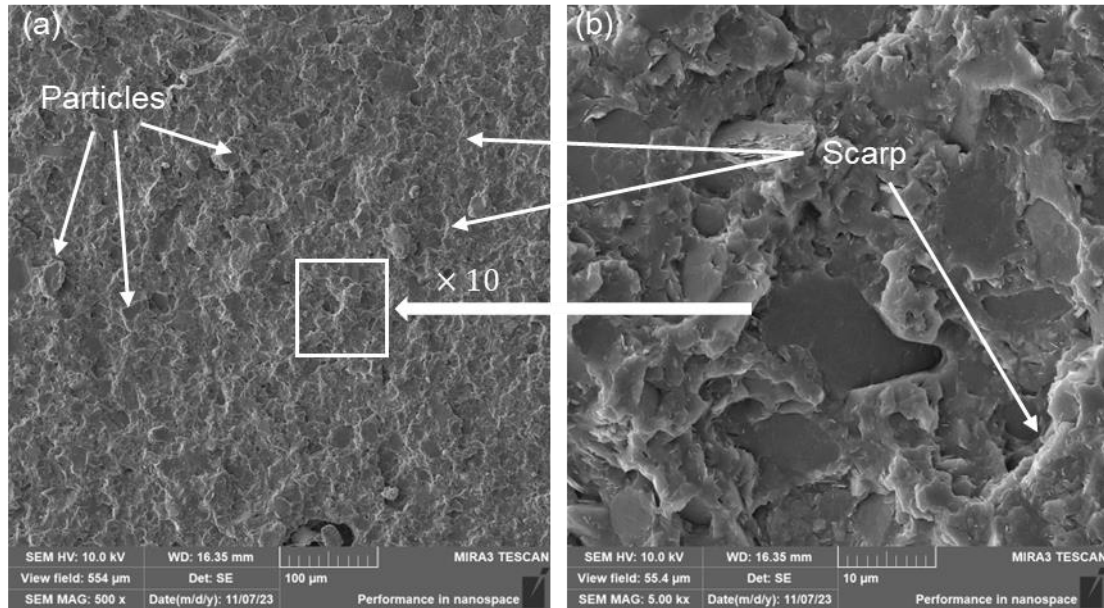


Figure 5-11 Fracture surface of 5mm under quasi-static loads.

The failure mechanisms of specimens with different thicknesses were investigated by conducting characterization at the microscale using a scanning electron microscope (SEM, MIRA3 TESCAN operated at 10 kV accelerating voltage). Representative regions were selected through preliminary visual inspection of fracture surfaces for SEM analysis. Typical fracture morphologies under different conditions are presented as follows:

Figure 5-11 displays the representative cohesive fracture morphology of a 5mm-thick specimen under quasi-static loading. The toughening particles embedded in the adhesive complicate fracture morphology interpretation ("Delamination-Dominated Failures in Polymer Composites," 2009). A sharp "Scarp" feature observed between two terraces resembles metallic cleavage facets, indicating energy concentration and rapid crack propagation. The planar fracture surface with no tilted cracking suggests negligible shear stress involvement, confirming pure Mode I fracture dominance (Wang et al., 2018). This establishes a reference for analysing fatigue fractures under cyclic loading. The elastoplastic coupling deformation induced by cyclic loading significantly increases the uncertainty of fatigue fracture form. ("Fatigue Failures of Polymer

Composites," 2009). Ensuring a single dominant damage mode is therefore critical for analytical reliability. Combined macro-/microscopic analyses validate cohesive-dominated crack growth, effectively minimizing mixed-mode stress interference in pure Mode I fatigue crack investigations

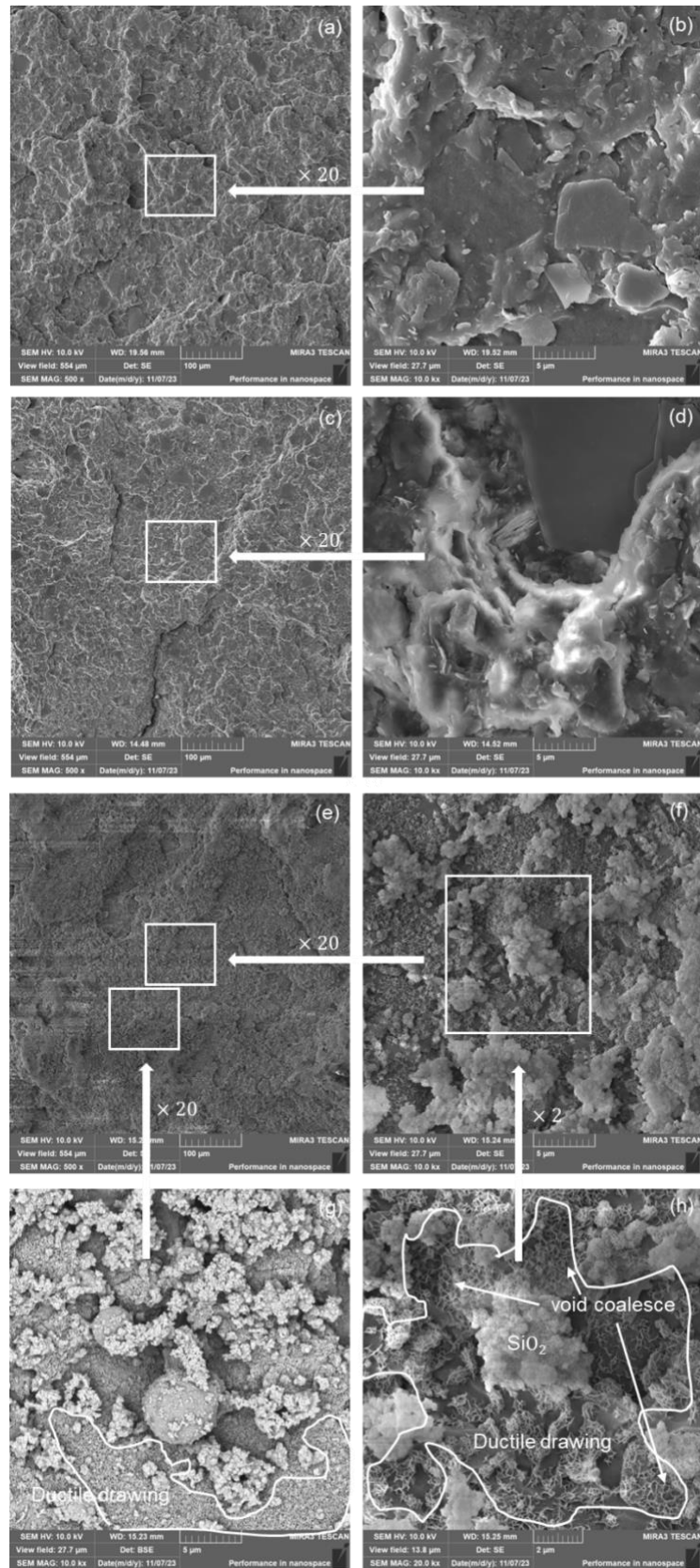


Figure 5-12 Fracture surface of 5mm under fatigue loads (a) (b) for 5mm, (c) (d) for 8mm, (e) (f) (g) (h) for 15mm.

Comparative analysis of fatigue fracture surfaces (5mm: Figure 5-12 (a-b); 8mm: Figure 5-12 (c-d)) versus quasi-static counterparts (Figure 5-12) reveals consistent roughness characteristics and morphological continuity across both loading conditions. This congruence aligns with the pure Mode I fracture paradigm governing epoxy adhesives, demonstrating minimal mixed-mode failure induction during fatigue testing.

Close inspection identifies intensified "ravine" features and delamination in the 5mm specimen (Figure 5-12 (a-b)), correlating with its concentrated strain field distribution. These observations confirm energy-localized brittle fracture dominance within the constrained FPZ of thinner specimens.

In stark contrast, the 15mm specimen exhibits distinct failure evolutionary morphology (Figure 5-12 (e-h)):

- **Macroscale Transition:** Coarse textures evolve into fine "beach-mark" patterns (Figure 5-12 (e))
- **Mesoscale Breakdown:** Cleavage-like platforms disintegrate into SiO_2 particle clusters, verified by EDS elemental mapping (Figure 5-12 (f-g))
- **Nanoscale Plasticity:** Dense dimple-fibril networks emerge at 20,000 $\times$ magnification (Figure 5-12 (h)), mirroring ductile void coalescence mechanisms [37].

This thickness-dependent ductility contradicts conventional thickness-plasticity suppression models (Azari et al., 2013). Crucially, the absence of shear-induced features in 5mm/8mm specimens eliminates mixed-mode interference as a causal factor.

Consistent with the FPZ analysis in preceding sections, the enlarged FPZ in 15mm specimens facilitates:

1. Homogenized stress distribution enabling progressive damage accumulation
2. Sequential micro void evolution (nucleation $\rightarrow$ growth $\rightarrow$ coalescence) through adhesive plasticity

Conversely, reduced FPZ dimensions in 5mm/8mm specimens promote:

- Plane stress dominance with intensified stress concentration
- Rapid crack propagation under elevated energy release rates
- Suppressed plastic deformation

Microscopic fractographic analysis reveals a thickness-dependent fracture mode transition: While 5mm and 8mm adhesive layers exhibit brittle fracture dominance, the 15mm DCB specimen demonstrates conclusive plastic deformation evidence during cyclic crack propagation. This counterintuitive finding challenges conventional shielding mechanism theories where plastic zone expansion typically enhances toughness and impedes crack growth.

Intriguingly, our observations partially align with J.A. Pascoe's paradoxical concept of plasticity-driven crack acceleration through increased energy dissipation. However, this study achieves stricter experimental isolation: The complete transition from plasticity-absent (5mm/8mm) to plasticity-dominated (15mm) failure modes essentially originates from strain distribution homogeneity within the FPZ. Specifically:

- **Thin specimens (5-8mm):** Localized strain concentration → Brittle fracture mechanics prevail
- **Thick specimen (15mm):** Homogenized FPZ strain field → Plastic dissipation enables progressive crack advancement

This thickness-governed FPZ evolution redefines the plasticity-crack growth relationship, suggesting strain distribution uniformity rather than absolute plastic zone size dictates failure mode transitions.

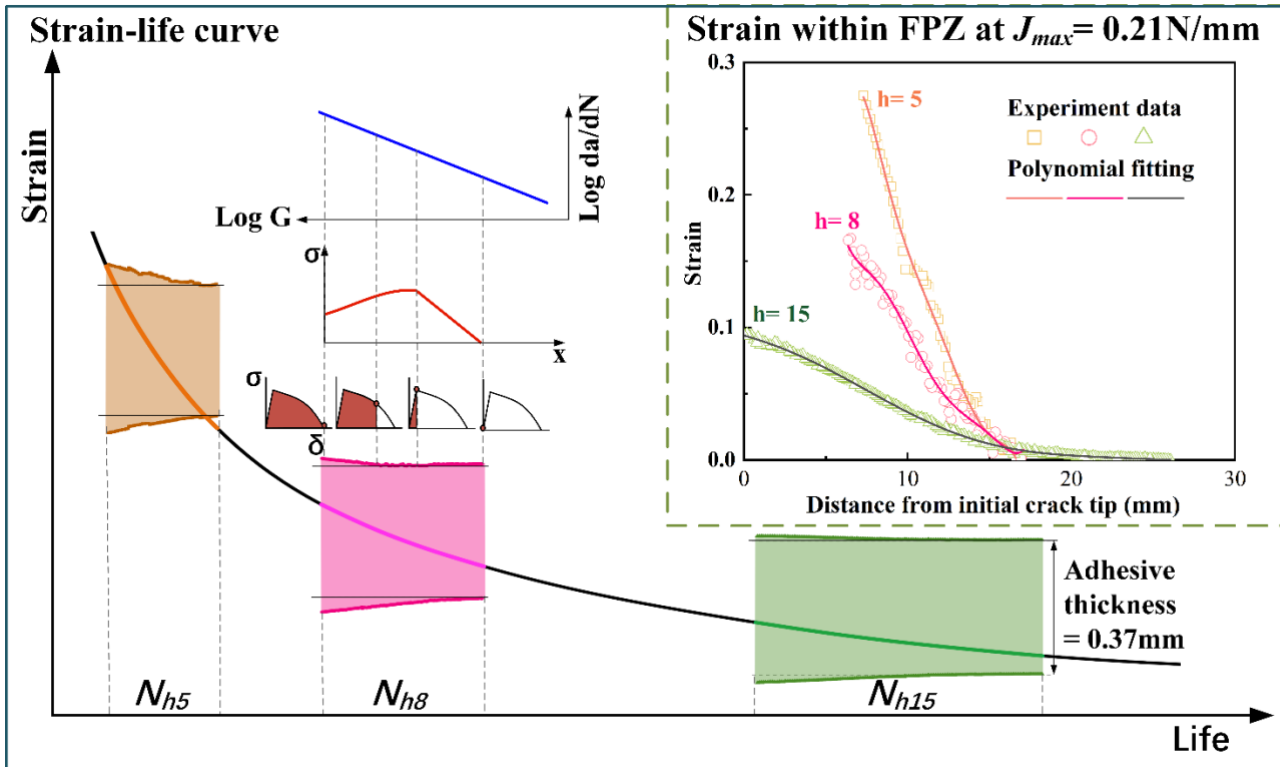


Figure 5-13 The correspondence between FPZ and the strain-life curves.

Tongue is evident from preceding discussion that a thicker adherend results in a more elongated FPZ, and the different stress distribution within the process zone leads to distinct macroscopic crack propagation behaviours. The strain in the FPZ is measured directly here, and the correspondence between FPZ shapes and strain-life curves is depicted in Figure 5-13. Under the stage of fatigue loading with same J -integral, a longer FPZ implies that material points will enter the FPZ earlier, accumulating more damage before completely traversing this area. At different stages within the FPZ, material points also exhibit different states, with "traversal" indicating the transition from zero separation and zero traction to threshold separation and zero traction.

5.5 Conclusion

DCB tests with varying adherend thickness were employed to study the fracture mechanism of adhesively bonded joints. It is revealed that, at a given strain energy release rate, the specimens of thicker adherend exhibited significantly faster crack propagation rate. This geometrical dependence may be responsible

for the difficulty of modelling fatigue damage of elastoplastic interface with Paris law.

The FPZ ahead of visible crack has been monitored in detail experimentally, its length has been found to increase with thicker adherend, yet the failure strain is reduced. Microscopic failure morphology suggested that local material have experienced higher number of cycles before failure when the adherend is thicker. It is suggested from these observations that, there is competing mechanisms involved in the fatigue crack growth: the increased number of loading cycles at each material point, and the increased fatigue life because of reduced strain level in the principle of SN curve.

This study highlighted the importance of FPZ in understanding the fatigue growth of elastoplastic interface and may help to bridge SN curve and Paris law from the perspective of continuum damage.

6 Conclusions and Future Work

6.1 Conclusions

This study advances the understanding of fatigue damage evolution in adhesively bonded joints (ABJs) through a novel local-strain-based framework, bridging fracture process zone (FPZ) dynamics, cohesive zone modelling (CZM), continuum damage mechanics (CDM), and stress-life (S-N) relationships. Key contributions are summarized as follows:

1. Local-Strain-Based Model with Multidimensional Linkages:
 - A geometry-independent damage model was established by analyzing strain field dynamics within the fracture process zone (FPZ). Unlike traditional Paris Law-based approaches, this method directly links fatigue crack growth to localized strain evolution, eliminating empirical dependencies on global stress intensity factors or energy release rates.
 - Numerical simulations demonstrated a 15% improvement in geometry-independence compared to cohesive zone models (CZMs) calibrated with Paris Law.
2. FPZ Dynamics and Strain-Life Synergy:

Experimental characterization via digital image correlation (DIC) revealed that FPZ length scales with adherend thickness (e.g., 10 mm for 5 mm vs. 25 mm for 15 mm adherends). Remarkably, strain distributions across FPZs of varying thicknesses collapsed onto a unified strain-N curve, analogous to S-N relationships. This suggests a strain-driven fatigue criterion where local strain amplitude governs failure modes, independent of global geometry.
3. Failure Mode Transition and Multiscale Mechanisms:

Fractographic analysis revealed thickness-dependent failure transitions: thin adherends (5–8 mm) exhibited brittle fracture with FPZ strain localization, while thick adherends (15 mm) showed ductile failure due to homogenized strain distribution and prolonged hysteresis cycles ($\approx 27,975$). This challenges classical plasticity-crack growth paradigms, emphasizing the role of FPZ strain uniformity in dictating damage modes.
4. Practical Validation and Cross-Paradigm Integration:

The strain-based model successfully predicted fatigue crack growth rates across DCB specimens of varying geometries, demonstrating alignment with experimental data (> 0.92). By linking FPZ strain metrics to S-N trends, this work establishes a multiscale framework that connects localized damage initiation (CZM), continuum-scale degradation (CDM), and structural fatigue life (S-N), offering a unified tool for damage-tolerant adhesive joint design.

While the current model demonstrates robust predictive capabilities within its defined scope, several limitations are acknowledged. Primarily, its validation is confined to Mode-I loading conditions, and its efficacy under more complex mixed-mode scenarios remains unverified, where complex stress states may compromise accuracy and computational efficiency. Furthermore, although grounded in physical concepts, the model's empirical material constants lack explicit physical definitions, rendering it phenomenological and dependent on substantial experimental data for calibration. Consequently, the parameter identification process is less straightforward than traditional approaches like Paris' law, requiring optimization against third-party datasets. These specific limitations, however, directly highlight valuable pathways for future research, particularly through the integration of multi-scale modeling and machine learning techniques to enhance generality and physical grounding.

6.2 Future Works

To further advance the understanding and application of fatigue damage analysis in adhesively bonded joints, the following directions are proposed:

1. Model Refinement and Multiscale Integration:
 - Extend the strain-based model to account for mixed-mode (II/III) loading conditions and interfacial defects.
 - Integrate micromechanical analyses (e.g., void nucleation, fibre bridging) with continuum-level simulations to develop multiscale fatigue criteria.
2. Multi-scale physical models with machine learning
 - Develop AI-powered surrogate models: Employ deep neural networks (e.g., Graph Neural Networks) trained on high-fidelity micromechanical

simulations to create ultra-fast surrogates that map microstructural features (e.g., void distribution, fibre orientation) directly to damage evolution and fatigue life predictions. This will enable probabilistic analysis and drastically reduce computational costs.

- Incorporate physics-informed neural networks (PINNs): Integrate the fundamental governing equations of fatigue damage (e.g., energy conservation laws) directly into the machine learning training process. This enhances predictive robustness, ensures physical consistency, and reduces reliance on excessively large datasets by embedding domain knowledge.
 - Establish a multiscale data fusion framework: Combine the AI surrogate models with continuous-level simulations to create a seamless prediction tool. For instance, use the neural network to rapidly predict the effective properties of the adhesive layer's Fracture Process Zone (FPZ) based on local microstructure, which are then fed into a macroscopic joint model for full-scale life assessment.
 - Explore generative AI for optimal design: Utilize generative models to inversely design optimal microstructural architectures (e.g., idealized fiber bridging patterns) or adhesive fillet geometries that maximize fatigue resistance based on the learned multi-scale relationships.
3. Environmental and Operational Factors:
- Investigate the synergistic effects of hygrothermal aging, cyclic temperature variations, and chemical degradation on FPZ dynamics.
 - Explore the role of surface pretreatment and adhesive viscoelasticity under variable amplitude loading.
4. Advanced Experimental Techniques:
- Develop in-situ microscopy setups to track real-time FPZ evolution at submicron resolutions.
 - Combine acoustic emission (AE) and infrared thermography (IRT) with DIC for Multiphysics damage monitoring.
5. Industrial Applications and Standardization:

- Establish guidelines for geometry-independent fatigue life prediction in industry standards (e.g., ASTM, ISO).
- Validate the model on complex joint configurations (e.g., tapered lap joints, curved interfaces) relevant to aerospace fuselage and wind turbine blade designs.
- Optimize adhesive fillet geometries and layup designs using topology-driven fatigue resistance criteria.

By addressing these challenges, this work lays the foundation for next-generation adhesive joint design, enabling safer, lighter, and more durable structures in critical engineering applications.

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