

NUMERICAL AND EXPERIMENTAL INVESTIGATION OF  
TRANSVERSE LOW-VELOCITY IMPACT ON  
THICK-PLY AND THIN-PLY CFRP COMPOSITE LAMINATES

A THESIS SUBMITTED TO  
THE GRADUATE SCHOOL OF NATURAL AND APPLIED SCIENCES  
OF  
MIDDLE EAST TECHNICAL UNIVERSITY



BY  
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IN PARTIAL FULFILLMENT OF THE REQUIREMENTS  
FOR  
THE DEGREE OF MASTER OF SCIENCE  
IN  
AEROSPACE ENGINEERING

DECEMBER 2023



Approval of the thesis:

**NUMERICAL AND EXPERIMENTAL INVESTIGATION OF  
TRANSVERSE LOW-VELOCITY IMPACT ON  
THICK-PLY AND THIN-PLY CFRP COMPOSITE LAMINATES**

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**I hereby declare that all information in this document has been obtained and presented in accordance with academic rules and ethical conduct. I also declare that, as required by these rules and conduct, I have fully cited and referenced all material and results that are not original to this work.**

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## ABSTRACT

### NUMERICAL AND EXPERIMENTAL INVESTIGATION OF TRANSVERSE LOW-VELOCITY IMPACT ON THICK-PLY AND THIN-PLY CFRP COMPOSITE LAMINATES

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December 2023, 133 pages

The objective of this thesis is to investigate the low-velocity impact induced damage mechanisms observed within the cross-ply composite laminates with different ply-group thicknesses. This thesis is divided into two independent yet connected parts: In the first part, the objective is to model the *in situ* observations of low-velocity line impact experiments conducted by Bozkurt and Coker (2021) on  $[0_5/90_3]_s$  CFRP laminates by constructing a high-fidelity finite element model that accurately captures the matrix cracking using a continuum damage model with LaRC05 initiation criterion, delaminations utilizing the cohesive zone model, and experimental boundary conditions by proposing a heuristic modeling approach that replicates the experiment boundaries through the assembly of spring elements. The simulations successfully replicated the *in situ* observations on the composite failure patterns and sequences and provided insights into underlying damage mechanisms, validating the capabilities of the numerical model. In the second part, the effect of ply-group thickness on damage mechanisms under low-velocity line impact is investigated by conducting *in situ* experiments on  $[0_4/90_4/0_2]_s$  and  $[0_2/90_2/0_2/90_2/0_2]_s$

CFRP laminates, and performing high-fidelity numerical simulations employing the finite element model constructed in the first part. Our experiments and simulations revealed that the damage mechanisms differ for different ply thicknesses, with the failure mechanism changing from only matrix cracking induced delamination to matrix cracking induced delamination with fiber breakage. The comparison between numerical simulations and experimental observations provides evidence supporting the so-called in-situ strength concept, where employing it played a very important role not only in global response but also in failure mechanism choice. This thesis demonstrates the necessity of high-fidelity modeling to accurately capture the high-fidelity *in situ* observations of dynamic failure mechanisms.

Keywords: Low-velocity Impact, Polymer-matrix Composites, High-fidelity Finite Element Analysis, *In situ* Experiments, Damage Mechanics

## ÖZ

### İNCE KATMANLI VE KALIN KATMANLI CFRP KOMPOZİT LAMİNATLARDA DÜZLEM DIŞI DÜŞÜK-HIZLI DARBENİN SAYISAL VE DENEYSEL OLARAK İNCELENMESİ

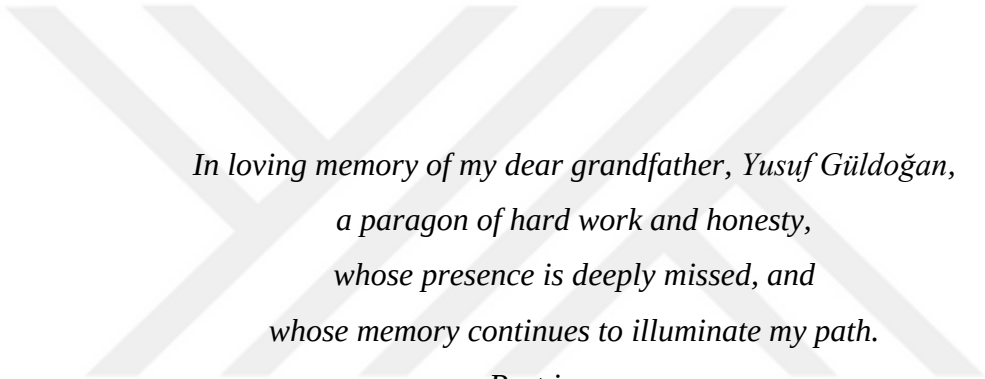
Batmaz, Onur Ali  
Yüksek Lisans, Havacılık ve Uzay Mühendisliği  
Tez Yöneticisi : Prof. Dr. Demirkan Çöker

Aralık 2023, 133 sayfa

Bu tezin amacı, farklı katman-grubu kalınlıklarına sahip çapraz-katmanlı kompozit laminatlarda gözlemlenen düşük hızlı çarpma kaynaklı hasar mekanizmalarını araştırmaktır. Bu tez, iki bağımsız ancak birbiriyle bağlantılı bölüme ayrılmıştır: İlk bölümde,  $[0_5/90_3]_s$  dizilimli CFRP laminatları üzerinde Bozkurt ve Coker (2021) tarafından gerçekleştirilen düşük hızlı çarpma deneylerinin yerinde (*in situ*) gözlemlerini yüksek-mertebeli bir sonlu eleman modeli oluşturarak modellemeyi amaçlanmaktadır. Bu sonlu eleman modeli, matris kırılması için LaRC05 hasar başlangıç kriteri içeren sürekli ortamlar hasar modeli, katman ayrılması (delaminasyon) için yapışkan bölge modeli ve deneysel sınır koşulları için yay elemanlar kullanan sezgisel bir modelleme yaklaşımı içermektedir. Simülasyonlar, kompozit hasarının deneysel olarak gözlemlenen ayrıntılarını başarıyla simüle etmiş ve esas hasar mekanizmalarına dair içgörüler sağlayarak sayısal modelin yeteneklerini doğrulamıştır. İkinci bölümde, katman-grubu kalınlığının düşük hızlı

izgi arpmaya karşı hasar mekanizmaları üzerindeki etkisi, [0<sub>4</sub>/90<sub>4</sub>/0<sub>2</sub>]<sub>s</sub> ve [0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>]<sub>s</sub> CFRP laminatları üzerinde yerinde deneyler gerekleřtirilerek ve ilk blmde oluřturulan sonlu eleman modelini kullanarak yksek-mertebeli sayısal simlasyonlar yaparak arařtırılmıřtır. Gerekleřtirilen deneyler ve simlasyonlar, farklı katman-grubu kalınlıkları iin hasar mekanizmalarının farklılık gsterdiėini, hasar mekanizmasının sadece matris atlamasıyla indklenen delaminasyondan, matris atlamasıyla indklenen delaminasyona ve fiber kırılmasına deėiřtiėini ortaya koymuřtur. Sayısal simlasyonlar ile deneysel gzlemler arasındaki karřılařtırma, literatrdeki “in-situ” mukavemet konseptini destekleyen kanıtlar sunmuřtur; bu kavramın kullanımı, sadece global darbe davranıřında deėil, aynı zamanda hasar mekanizması seiminde de nemli bir rol oynamıřtır. Bu tez, dinamik hasar mekanizmalarının yksek-mertebeli yerinde gzlemlerini doėru bir řekilde yakalamak iin yksek-mertebeli modellerin gerekliliėini ortaya koymuřtur.

Anahtar kelimeler: Dřk Hızlı arpma, Polimer-matris Kompozitler, Yksek-mertebe Sonlu Eleman Analizi, Yerinde Deneyler, Hasar Mekaniiėi



*In loving memory of my dear grandfather, Yusuf Gldođan,  
a paragon of hard work and honesty,  
whose presence is deeply missed, and  
whose memory continues to illuminate my path.  
Rest in peace*

## ACKNOWLEDGMENTS

I would like to express my sincere appreciation to my advisor, Prof. Dr. Demirkan Çöker, for his invaluable guidance and encouragement throughout my academic journey. Beyond his role as an advisor, he has profoundly influenced my life perspective, acting as a mentor who has significantly contributed to my personal and academic development.

I would like to thank Assoc. Prof. Dr. Ercan Gürses, for not only attending my thesis defense but also for his guidance, which played a pivotal role in this study. I wish to acknowledge the priceless contributions of Miraç Onur Bozkurt that transformed this study into an original work of literature, and to extend my appreciation to both Miraç Onur Bozkurt and Can Muyan for their dedicated efforts in conveying their expertise in mechanics, proving invaluable to my career.

I would like to thank Prof. Dr. Altan Kayran, Assoc. Dr. Serdar Göktepe and Prof. Dr. Erdem Acar for attending my thesis defense presentation and providing valuable comments. I would also like to acknowledge Prof. Dr. Oğuz Uzol for offering me a peaceful working space and the opportunity to use METUWIND facilities.

I am thankful to the members of the Coker Research Group, Ahmet Çevik, Aydın Amireghbali, Emine Burçin Özer, Erdem Dinler, Pakize Temiz, Tutku Ilgın Özcan, Umut Altuntaş, and my colleagues Ali Ata Adam, Fırat Ergin, Serhat Onur Çakmak for their friendships which made this challenging time enjoyable. I want to emphasize Fırat's irreplaceable friendship and support not only in this study but throughout my life.

Last but not least, I express heartfelt gratitude to my entire family, with special appreciation for my mother Tülay Batmaz, for their unwavering support. I am deeply thankful for their endless love and eternal faith in me that has been a constant presence in my life.

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## **CHAPTER 1**

### **INTRODUCTION**

Impacts on engineering structures can result in severe consequences, including loss of strength or even complete structural collapse [1]. To avoid potential catastrophic scenarios, it is crucial to understand and characterize extreme events encountered in various engineering applications, some of the examples of such events are presented in Figure 1.1. These events encompass a wide range of phenomena, such as bird strikes [2], tool drops [3], and hail storms [4] affecting aerostructures, debris striking aircraft wings during landings [5], hypervelocity micro-meteorite showers impacting space vehicles [6], and even water droplet collisions on wind turbine blades [7] and forward-facing airplane components [8] causing erosion.

In many engineering applications within modern aerospace, renewable energy, and transportation, composite materials have been favored particularly for having exceptionally high strength-to-weight ratios [9]. Still, their relatively weak interfacial characteristics make them vulnerable to out-of-plane loadings [3], such as the abovementioned impact events, necessitating detailed investigations.

#### **1.1 Low-velocity Impact Damage in Composites**

Among impact events, low-velocity impacts (LVIs), typically characterized by impactor speeds below 10 m/s [10], pose particular challenges for composite structures due to the difficulty in detecting the resulting damage. Unlike metals that often exhibit plastic deformation in the vicinity of the impact region as visually detectable dents, composites may develop internal damages that are barely visible or

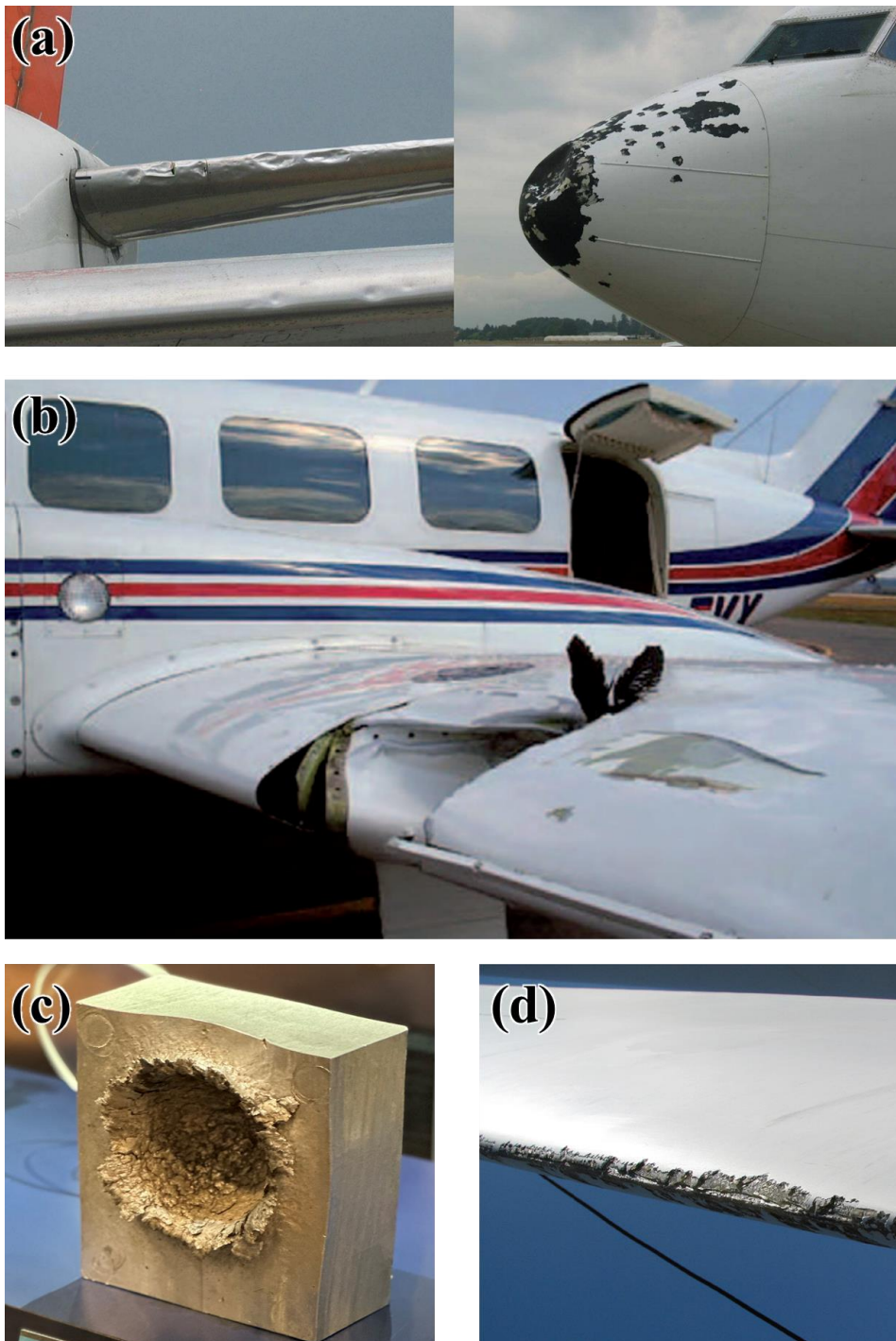


Figure 1.1 Examples of impact events experienced by engineering structures: **(a)** hail damage to the tail and radome structures of a Boeing 737 aircraft [11], **(b)** bird strike to the wing structure [12], **(c)** a 10 cm-thick aluminum block impacted by a 14g plastic cylinder traveling at hypervelocities (photograph taken in science museum of NASA Space Center in Houston), and **(d)** severely eroded leading edge coating of a wind turbine blade due to droplet impact [7].

completely undetectable from the outer surfaces due to LVI events [13]. Figure 1.2 presents a composite structure that developed internal damages resulting from LVI that are undetectable from the outer surfaces. Such internal failures, in the form of matrix cracking and delamination, as seen in Figure 1.3, lead to a considerable loss in strength and stiffness of the structures [3], and necessitate the use of sophisticated inspection methods for detection [14].

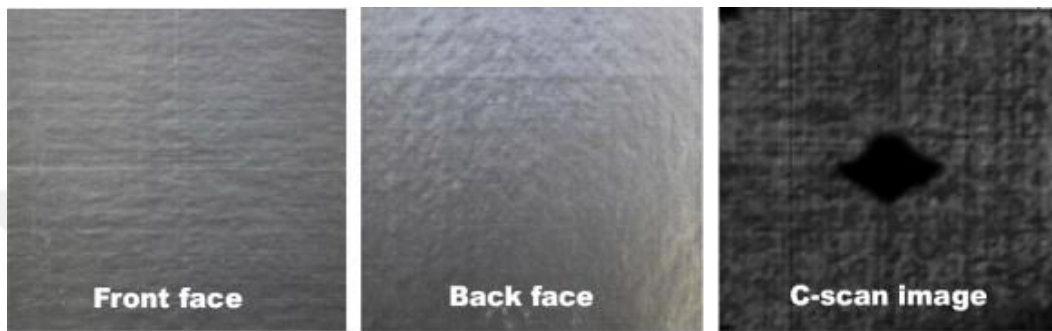


Figure 1.2 Low-velocity impact-induced damage in a composite plate. The damaged region from the outer front and back surfaces and internal damages were inspected with C-scan imaging [15].

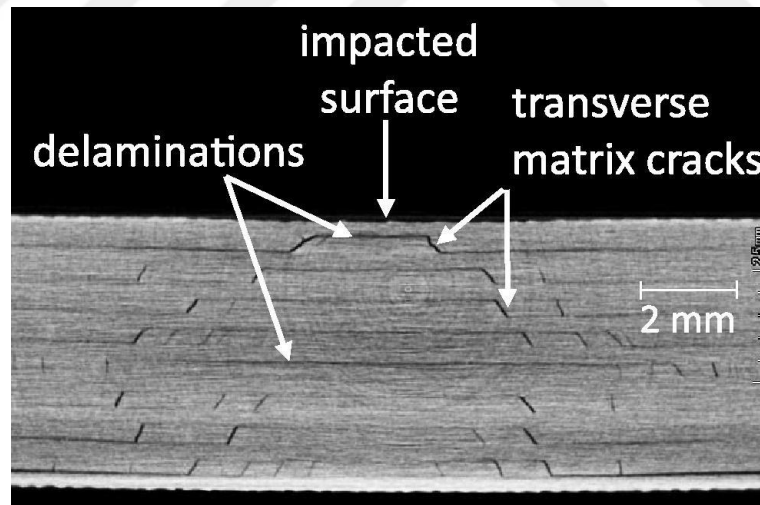


Figure 1.3 Typical impact-induced matrix cracking and delamination damage in a composite structure [16].

To design load-bearing components capable of withstanding low-velocity impacts, it is essential to understand the underlying damage mechanisms and accurately predict the onset of damage.

## 1.2 Predicting the Impact-induced Damage

Damage onset and resistance of composites to low-velocity impact loadings can be determined by conducting experiments on composite plates in a controlled laboratory environment. The widely followed standard for the drop-weight impact test of composite laminates is ASTM D7136 [17], where the test setup is illustrated in Figure 1.4. Unfortunately, the complexity and variety of damage mechanisms require many tests for each material composition [18], specimen size [19], stacking sequence [20], impact energy, impactor shape [21], and boundary conditions [22]. Ultimately, the aim is to replace most of these tests with physically accurate numerical simulations.

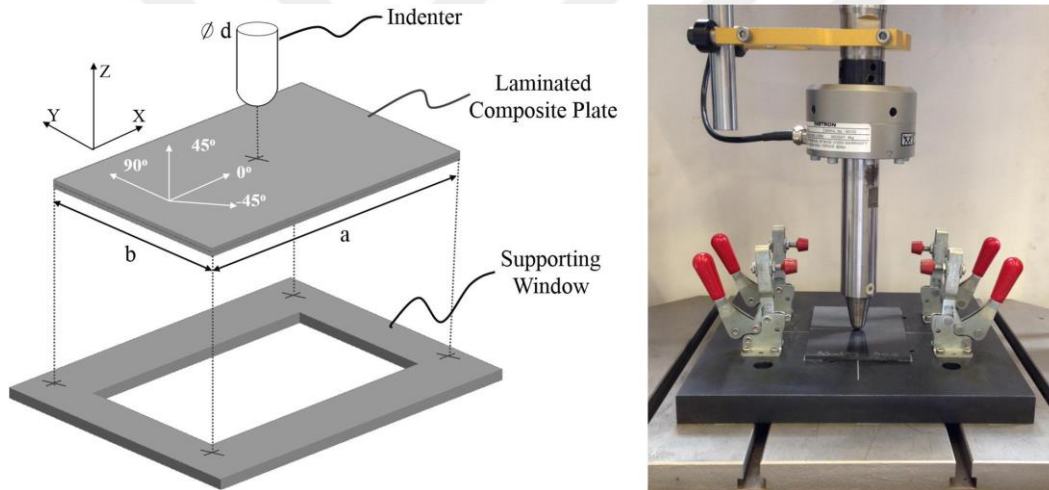


Figure 1.4 Drop-weight impact test of composite plates (ASTM D7136) [23].

Predicting the impact-induced damage within composite plates by constructing numerical simulations of standard drop-weight tests has been extensively investigated in the literature [22,24–34], where the study by Falcó et al. [34] showing the evolution and post-mortem damage predictions is presented in Figure 1.5 as an example. In these simulations, ply damage was modeled using either continuum damage models (CDM) [24,25,27–29,31,34] or discrete cracking models [22,26,28,30,32,33], while delamination damage was predominantly addressed using the cohesive zone method (CZM). Notably, in-depth investigations conducted by

Lopes et al. [24,27], Gonzalez et al. [25], and Sun et al. [22] successfully predicted the final damaged state of their specimens, demonstrating an accurate representation of the global impact response and damage footprints. Soto et al. [29], Sun et al. [30], and Thorsson et al. [33] further validated their modeling capabilities by capturing permanent deformations resulting from impact and simulating the compressive behavior of damaged specimens through compression-after-impact (CAI) tests. In these studies, the prediction accuracy was validated by comparing the simulation results with experimental observations obtained from post-mortem inspections, utilizing techniques such as optical microscopy, ultrasonic C-scan, X-ray CT-scan, and others. However, it should be noted that the comparisons between simulations and experiments have remained limited to global impact responses and the final damaged states.

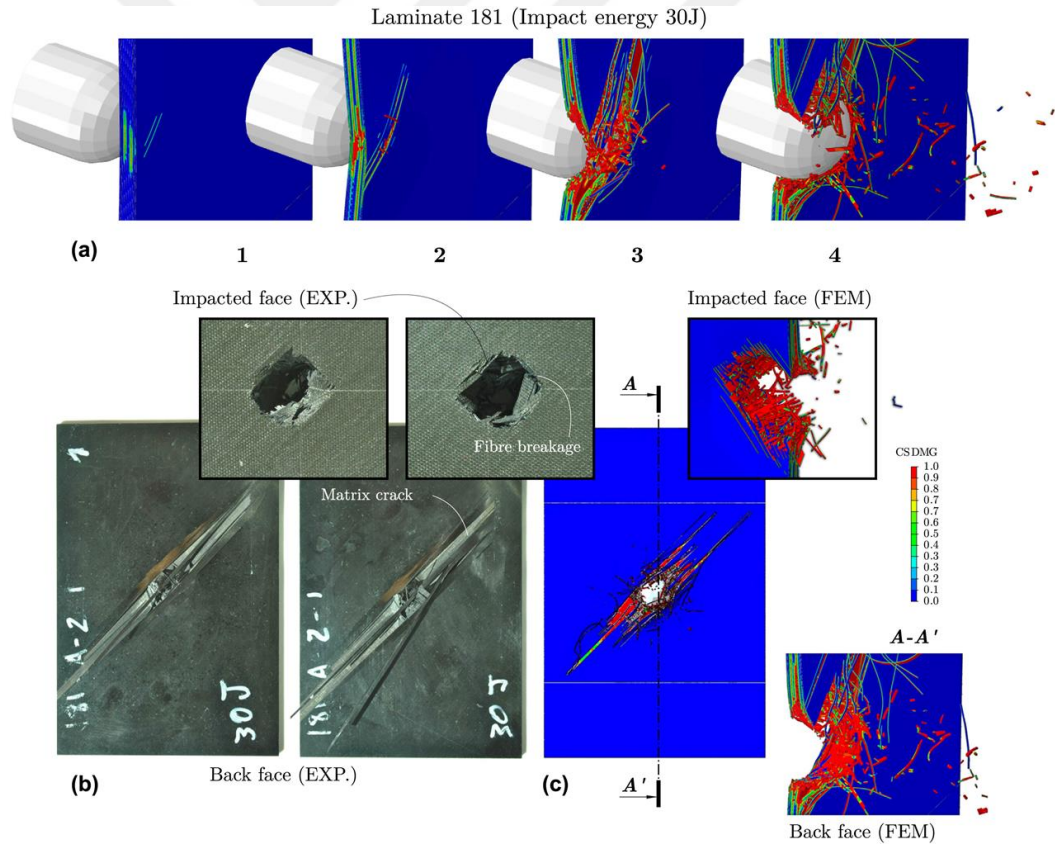


Figure 1.5 (a) Evolution of the LVI-induced damage for a composite plate, (b) experimental observation of failure after full penetration, and (c) numerical predictions of failure mechanisms from the study of Falcó et al. [34].

Although a good agreement between experiments and simulations would be shown globally, uncertainties associated with input parameters, material constitutive laws, and actual experimental conditions may hinder the modeling of the true physics of failure, since simulations incorporating a fiber-reinforced material failure framework require a considerable number of parameters (typically +20), which are either obtained through experiments or being exclusively virtual. Therefore, for the validation of physically sound damage models, direct observation of the dynamic evolution process of damage becomes necessary.

Direct observation of damage sequences enhances the understanding and identification of the underlying physics of composite damage mechanisms, especially when simpler layups are utilized. In this regard, beam tests have an advantage over plate tests as they enable direct observation of the damage processes, and information is not restricted to the post-mortem examinations of failed specimens [35]. One of the pioneering studies on composite beam LVI was conducted by Choi et al. [36], who employed a line-loading approach in Figure 1.6 to reduce the impact event to an almost two-dimensional scenario. Their experiments on cross-ply laminates with clamped boundaries showed the occurrence of impact-induced diagonal shear cracks and delaminations. The numerical studies that simulated these experiments [36–38] elucidated the failure sequence, revealing that shear matrix cracks serve as a trigger for subsequent delaminations.

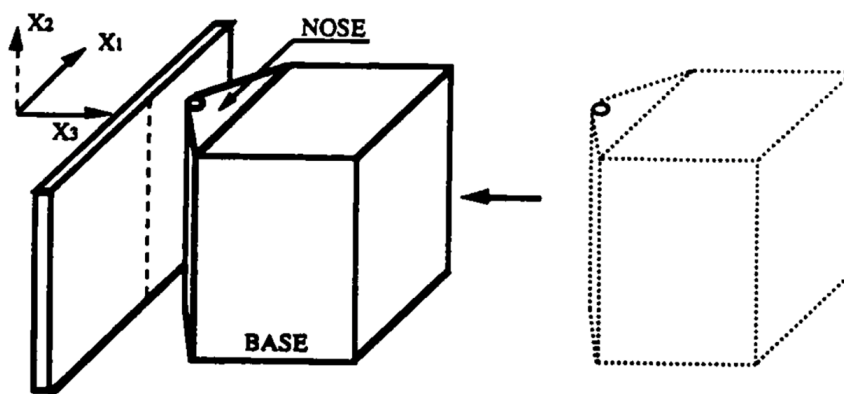


Figure 1.6 The line-loading approach proposed by Choi et al. [36]. This approach allows *in situ* observations on the free edge of the beam specimen by reducing the impact event to an almost two-dimensional scenario.

Several investigations have examined the flexural behavior and failure of composite beams with various layups in a three-point configuration [35,39–41]. The observed matrix cracks beneath the impacted region, as depicted in Figure 1.7, vary between vertical and diagonal orientations. The specific crack patterns depend on several factors, such as boundary conditions and composite layup configurations, with the failure type being influenced by bending or shear dominance.

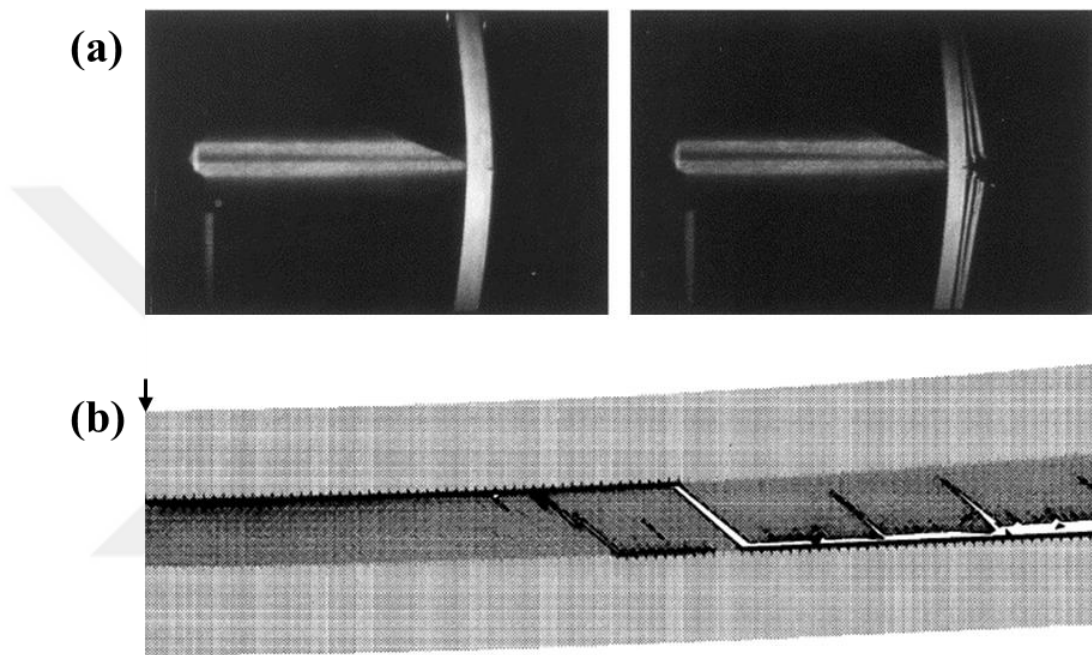


Figure 1.7 Line-impact studies on composite beams show the formation of **(a)** bending-induced vertical cracks [35] and **(b)** diagonally oriented shear-cracks [38].

Recently, Topac et al. [42] performed line impact experiments on composite beams using high-speed photography in conjunction with digital image correlation (DIC). They successfully predicted the observed damage pattern by employing a CDM-based ply damage model with the LaRC04 initiation criterion [43]. Their simulations showed the damage sequence is matrix-cracking followed by delamination, and delaminations grow in a three-dimensional pattern. More recently, Bozkurt and Coker [44] expanded on the line impact study of Topac et al. [42] by performing additional *in situ* experiments on  $[0_5/90_3]_s$  carbon fiber-reinforced beams, where the sequence of matrix crack-induced delamination is explicitly captured using an ultra-

high-speed camera in conjunction with the digital image correlation method. They measured the delamination crack tip speeds and suggested that crack tip speed is an experimental outcome that can be used as a benchmark for fine-tuning damage models.

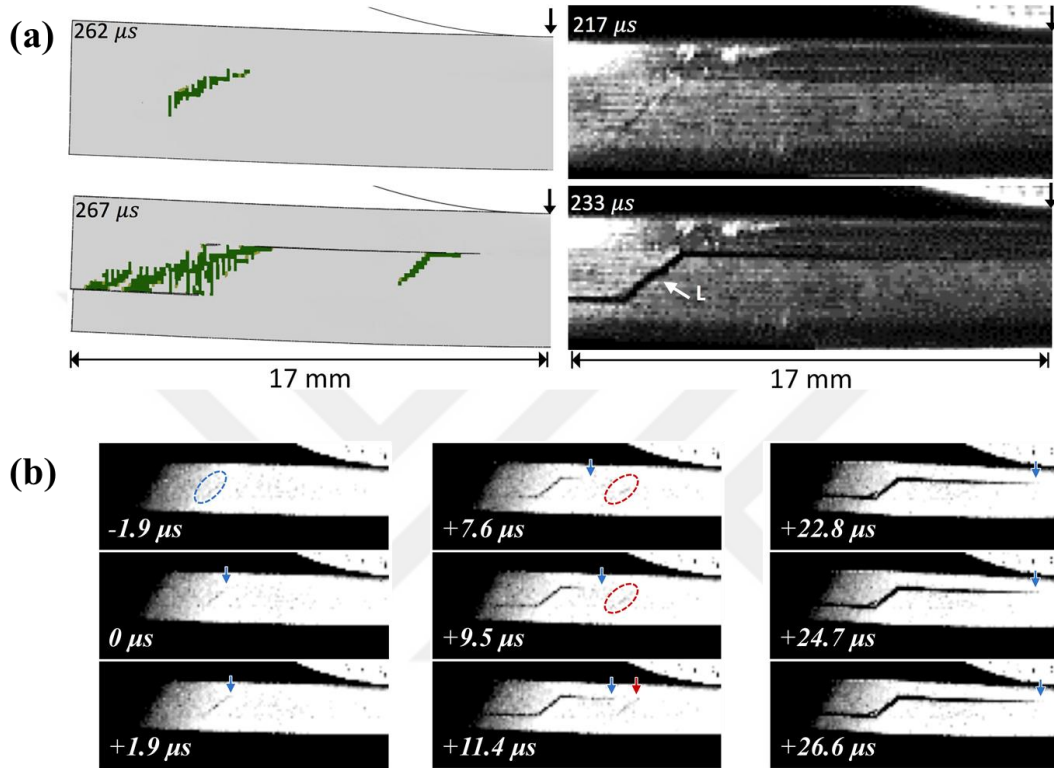


Figure 1.8 Low-velocity line-impact studies on  $[0_5/90_3]_s$  composite beams: **(a)** Numerical and experimental observations of LVI-induced damage progression by Topac et al. [42], and **(b)** experiments for measuring delamination crack tip speeds by Bozkurt and Coker [44].

### 1.3 Scope of this Study

The thesis consists of two parts that are independent yet connected.

The objective of the first study is to model the *in situ* observations of low-velocity line impact experiments conducted by Bozkurt and Coker on [0<sub>5</sub>/90<sub>3</sub>]s CFRP laminate. The focus is on capturing strain and deformation fields, damage initiation time, location, damage pattern and progression, delamination speeds. For this purpose, a high-fidelity finite element model is constructed accounting for composite damage and delamination, and experimental boundary conditions. The simulations successfully replicate the *in situ* observed details of the composite failure, thereby validating the capabilities of the numerical model.

In the second part of the thesis, the effect ply thickness on damage mechanisms under low-velocity line impact is investigated. This is accomplished by conducting high-fidelity *in situ* experiments and employing the numerical model constructed in the first study. First, low-velocity impact and quasi-static experiments are conducted on [0<sub>4</sub>/90<sub>4</sub>/0<sub>2</sub>]s and [0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>]s CFRP laminates. Second, simulations are performed fine-tuning the composite damage parameters by comparing the damage pattern and progression with the experiments. Our experiments revealed that the damage mechanisms differ for different ply thicknesses, with the failure mechanism changing from only matrix cracking induced delamination to matrix cracking induced delamination with fiber breakage. Simulations show that in-situ approach in determining strength properties of multidirectional layups need to be incorporated to capture this change in failure mechanisms with thickness.

Finally, this thesis demonstrates the necessity of high-fidelity modeling to accurately capture the high-fidelity *in situ* observations of dynamic failure mechanisms.



## CHAPTER 2

### **HIGH-FIDELITY FINITE ELEMENT SIMULATIONS OF LOW-VELOCITY IMPACT INDUCED MATRIX CRACKING AND DYNAMIC DELAMINATION PROGRESSION IN $[0_5/90_3]_s$ CFRP BEAMS**

A high-fidelity finite element (FE) model is constructed to simulate the low-velocity line impact experiments conducted by Bozkurt and Coker on  $[0_5/90_3]_s$  CFRP beams. The experiments captured the impact-induced matrix cracking and dynamic delamination progression using an ultra-high-speed camera in conjunction with the strain measurements via the digital image correlation method. To simulate these experiments, we utilize a user-implemented three-dimensional continuum damage model with LaRC05 initiation criterion for matrix cracking, and a built-in cohesive zone model at the  $0^\circ/90^\circ$  interfaces for delamination damage in the ABAQUS/Explicit software. The influence of boundary supports on the global impact response is found to be significant, therefore, a heuristic boundary conditions approach that replicates the experiment boundaries through the assembly of spring elements at the corresponding boundary nodes is proposed. The simulation results then demonstrated excellent agreement with the experiments in terms of global impact response, strain fields, damage initiation time, location, form, and sequence. Simulations reveal intersonic delamination propagation, with crack tip speeds around  $\sim 5000$  m/s, accompanied by shear Mach waves emanating from the crack tip. In contrast, experimental delamination crack tip speeds were measured to be sub-Rayleigh speeds reaching  $\sim 1000$  m/s. However, when a crack tip definition based on the crack opening is introduced, its adaptation yields improved agreement with the experiments, implying that the sliding mode might be physically hidden in the experiments. Increasing the effective interface toughness to incorporate the

macroscopic effects of experimentally observed microscopic cracks at the interfaces demonstrated the potential use of delamination crack tip speeds as a benchmark for refining high-fidelity numerical simulations.

## 2.1 Introduction

Damage onset and resistance of composites to low-velocity impact loadings are commonly assessed by conducting standard drop-weight impact tests (ASTM D7136 [17]) on composite plate specimens. Considerable effort in the literature has been devoted to constructing high-fidelity numerical models capable of accurately predicting impact-induced damage within composites [22,24–34], ultimately aiming to reduce the reliance on physical tests. In these studies, the simulations were validated through comparisons with experimental results, and comparisons were typically limited to global impact responses and post-mortem inspections since standard test setups with plate specimens restrict observations during tests without modifications. Only a few studies [23,35,42,44–47] investigated the impact-induced damage mechanisms by monitoring experiments *in situ*, either employing high-speed cameras [35,42,44] or by simulating impact-induced damages through quasi-static experiments where applied loading is interrupted at every damage monitoring interval [23,45–47]. The lack of observations on the growth of local damages and the interaction of different damage modes may impede the modeling of the actual physics of composite damage mechanisms, even when there is good agreement between experiments and simulations at a global level. Therefore, for the validation of physically sound numerical models, direct observation of the dynamic evolution of damage becomes necessary.

A limited number of studies in the literature specifically explore the dynamic evolution of impact-induced damages. Zehnder and Rosakis [48] experimentally investigated the dynamic crack propagation in a steel material using a configuration consisting of a three-point bending specimen impacted in a drop-weight tower. They measured crack tip speeds using high-speed photography and concluded that the

crack tip velocity is an indirect material property. Similarly, several researchers have examined the dynamic characteristics of impact-induced interface cracks on bonded homogenous materials [49,50], bimetals [51–53], multi-layered heterogeneous materials [54,55], and fiber-reinforced composites [56–60] by measuring crack tip speeds. In the specific context of these studies with computational focus [50,53,56–59], experimental crack tip data were utilized to validate the interface damage models or to explore influencing parameters.

For the investigation of dynamic damage growth in composites, beam tests have an advantage over plate tests as they enable direct observation of the damage processes, and information is not restricted to the post-mortem examinations of failed specimens [35]. Recently, Bozkurt and Coker [44] expanded on the line impact study of Topac et al. [42] by performing additional experiments on  $[0_5/90_3]_s$  carbon fiber-reinforced beams, where the sequence of matrix crack-induced delamination is explicitly captured using an ultra-high-speed camera in conjunction with the digital image correlation method. They measured the delamination crack tip speeds and suggested that crack tip speed is an experimental outcome that can be used as a benchmark for fine-tuning damage models.

In this study, we constructed a high-fidelity finite element (FE) model of the LVI experiments conducted on  $[0_5/90_3]_s$  CFRP beams by Bozkurt and Coker [44]. The objectives are to accurately reproduce matrix cracking and dynamic delamination observations and to investigate hypotheses beyond the limitations of direct experimental observation. The numerical simulations are constructed in the commercially available FE package ABAQUS/Explicit [61] and include the following characteristics. For the matrix cracking damage, a three-dimensional CDM featuring the LaRC05 damage initiation criterion proposed by Pinho et al. [62] and the damage progression procedure from the study of Topac et al. [42], which takes into account the three-dimensional stress states and in-situ effects, is implemented through a user-defined subroutine (VUMAT) with an explicit integration scheme. For the delamination damage, the CZM with built-in cohesive elements inserted at the  $0^\circ/90^\circ$  interfaces is employed. The influence of boundary supports on the global

impact response is found to be significant, therefore, a heuristic boundary conditions (BCs) approach that replicates the experiment boundaries through the assembly of spring elements at the corresponding boundary nodes is proposed. The dynamic nature of impact-induced matrix cracking and delamination events is explored through global impact response, *in situ* damage progression and damage mechanisms, the large-scale contact and friction between crack surfaces, and delamination crack tip speeds.

The originality of this work lies in i) revisiting the importance of accurate modelling of BCs in LVI regime, ii) elucidating the dynamic characteristics of sudden delamination growth in a cross-ply composite subjected to transverse LVI, iii) demonstrating the potential use of crack tip speeds in high-fidelity numerical modelling, and iv) highlighting the limited reliability of relying solely on global impact responses and damage footprints (as commonly practiced in literature) without analyzing the dynamic progression of damage, emphasizing the need for a more comprehensive approach to validation.

This chapter is organized as follows: In the subsequent section, we provide a brief summary of the relevant findings presented in the previous experimental work by Bozkurt and Coker [44], which we aimed to construct the high-fidelity simulation. Section 2.3 presents the numerical method with descriptions of FE model, composite ply damage and delamination models. In Section 2.4, the implementation of flexural BCs with the proposed approach is introduced. Section 2.5 presents the numerical results of the high-fidelity FE model in terms of the global response, *in situ* damage progression and damage mechanisms. These numerical results are discussed further in Section 2.6 and compared with the experimental observations. Finally, Section 2.7 concludes the study, summarizing the key findings and highlighting the contributions.

## 2.2 Summary of the Experiments of Bozkurt and Coker [44]

In the previous experimental work of Bozkurt and Coker in [44], 9.15 J line impact tests were carried out on CFRP beam specimens with  $[0_5/90_3]_s$  stacking sequence, which was ideally chosen to facilitate clear visualization of the failure sequence. In this regard, real-time *in situ* observations of the initiation and dynamic progression of the matrix damage and succeeding delaminations were performed at the edge of the beam via an ultra-high-speed camera at capturing rates varying between 20,000 and 525,000 fps. In order to elucidate the observed failure mechanisms, full-field strain measurements were conducted through digital image correlation (DIC) analyses. Further characterization of the failure pattern was performed in post-mortem micrographic observations of beam specimens. In this section, we provide a summary of the relevant aspects of the experimental methodology and observations, directing interested readers to the original paper [44] for more comprehensive details.

### 2.2.1 Experimental Method

Line impact experiments, originally proposed by Choi and co-workers [36], were carried out in an in-house built non-standard drop-weight impact test setup instrumented with a 20-mm radius semi-cylindrical impactor, as shown in Figure 2.1(a). To impose encastre (or fixed-end) boundary conditions, the composite beam specimen was clamped along 25-mm at both ends by top and bottom steel plates, which compress the beam by means of preloaded bolts passing through them. The steel plates were mounted on linear guideways, and they squeezed the composite beam with preloaded bolted joints. The bottom steel plates of opposite ends were positioned to create an unsupported span length of 50-mm.

The beam specimens were comprised of Hexcel 913C/HTS unidirectional (UD) prepregs stacked in  $[0_5/90_3]_s$  laminate configuration. They had in-plane dimensions of  $100 \times 17 \text{ mm}^2$ , and a resulting laminate thickness of 4.8-mm. The ply-level elastic and strength properties of the UD prepreg material are presented in Section 2.3.2.

### 2.2.2 Experimental Observations

The failure sequence was reported to be a diagonal matrix crack in  $90^\circ$  layers, followed by delaminations in the upper and lower  $0^\circ/90^\circ$  interfaces. This evolution of LVI-induced damage was captured through a sequence of high-speed camera images with an interframe time of  $1.9\ \mu\text{s}$ , as partly shown in Figure 2.1(b). Detailed examination of the high-speed image sequence revealed that the initial diagonal matrix crack, occurring approximately 16 mm away from the impact line (or mid-span), immediately triggered delaminations upon reaching the  $0^\circ/90^\circ$  interfaces. About  $7.6\ \mu\text{s}$  later, a second matrix crack initiated 9 mm away from the impact line, leading to the nucleation of a new delamination front at the upper  $0^\circ/90^\circ$  interface. These initial and secondary upper delaminations eventually merged into a single delamination at approximately  $15.2\ \mu\text{s}$ , propagating beneath the impactor. Delamination crack tip positions were determined from the high-speed images, and the corresponding crack tip speeds were calculated by numerically differentiating the crack tip positions with respect to time. The histories of crack tip position and speed are presented in Figure 2.1(b), indicating subsonic but dynamic crack tip propagation with speeds ranging from 250 to 1000 m/s.

The mechanism of matrix crack formation was elucidated through detailed examination of strain fields obtained via digital image correlation (DIC) analyses. Contours of the transverse shear ( $\gamma_{xy}$ ) and longitudinal ( $\epsilon_{xx}$ ) components of the strain just before the initial diagonal matrix crack formation are presented in Figure 2.1(c). High  $\gamma_{xy}$  values were reported to indicate that the major diagonal matrix cracks were shear matrix cracks. Based on the coincidence of  $\gamma_{xy}$  hotspots and tensile  $\epsilon_{xx}$  regions with the actual diagonal crack locations, it was postulated that a three-dimensional (3-D) failure criterion should be employed for analyzing failure under transverse loading scenarios.

In addition to the major diagonal matrix cracks and delaminations observed in the high-speed images, two additional types of cracks, namely hairline cracks with lengths ranging between 0.1-to-1.0 mm and micro-matrix cracks with lengths up to

0.1 mm, were identified during post-mortem examination of the failure patterns using a digital microscope, as depicted in Figure 2.1. Hairline cracks were reported to occur near 0°/90° interfaces, regardless of whether the interface is failed or intact, and exhibit directionality along the beam. The authors deduced that the observation of hairline cracks at the intact interfaces might be attributed to the sudden longitudinal wave propagation resulting from the major matrix cracking. On the other hand, micro-matrix cracks were observed at the failed interfaces and were associated with secondary opening mode cracks resulting from the contact and friction of crack surfaces behind a dynamic crack tip. Furthermore, the formation of debris within the delaminated interfaces was reported, which were sheared-off particles trapped between the delaminated surfaces.

Finally, for the purpose of providing additional benchmark data to our simulations, we performed further investigation on the existing experimental materials of all tested specimens. Locations, initiation times, and orientation angles of diagonal matrix cracks on the left-hand side (LHS) and the right-hand side (RHS) of the specimens are summarized in Table 2.1. Average quantities are also reported with standard deviation values.

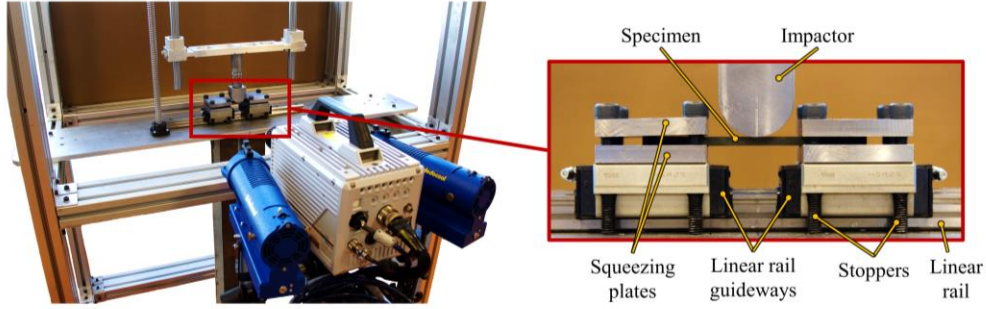
Table 2.1 Locations, initiation times and orientation angles of matrix cracks in all tested specimens.

| Specimen | Location (mm)  | Initiation time ( $\mu$ s)                                                  | Orientation (°) |
|----------|----------------|-----------------------------------------------------------------------------|-----------------|
|          | LHS / RHS      | LHS / RHS                                                                   | LHS / RHS       |
| IMP-1    | 11 / 10        | 807 $\pm$ 7 / 350 $\pm$ 7                                                   | 42 / 48         |
| IMP-2    | NC* / 12       | NC* / 393 $\pm$ 7                                                           | NC* / 49        |
| IMP-3    | 16 / 24        | NA** / NA**                                                                 | 43 / 51         |
| IMP-4    | 17 / 19        | 325 $\pm$ 25 / 875 $\pm$ 25                                                 | 45 / 45         |
| IMP-5    | 14 / 13        | 625 $\pm$ 25 / 275 $\pm$ 25                                                 | 45 / 42         |
| Mean     | 15.1 $\pm$ 4.4 | 1 <sup>st</sup> crack: 336 $\pm$ 43<br>2 <sup>nd</sup> crack: 769 $\pm$ 106 | 45.5 $\pm$ 3.2  |

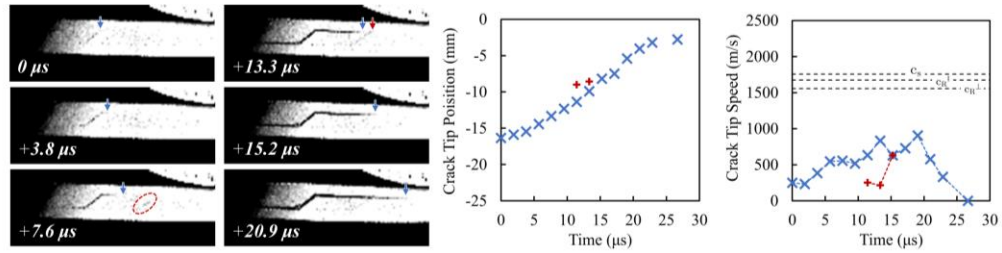
\*NC: Not cracked

\*\*NA: Observation not available

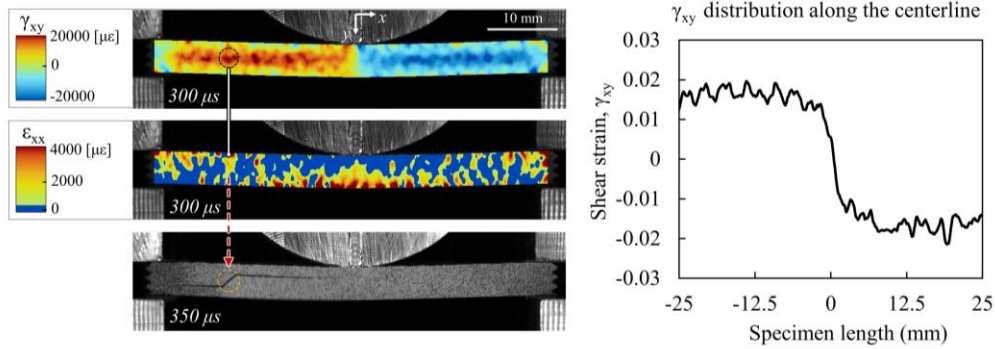
**(a) Non-standard drop-weight impact setup**



**(b) Delamination propagation**



**(c) Strain fields by DIC analysis**



**(d) Post-mortem examination**

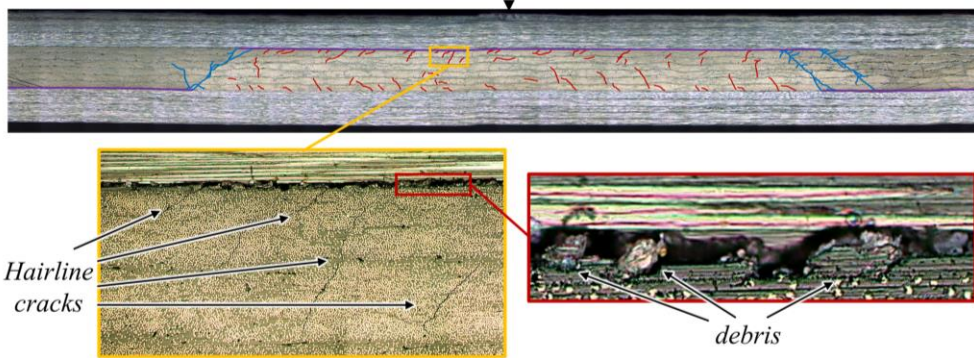


Figure 2.1 Summary of the experimental study conducted by Bozkurt and Coker [44]. **(a)** Experimental setup for low-velocity impact, **(b)** delamination propagation with corresponding delamination crack tip position and speed histories, **(c)** transverse shear and longitudinal strain fields prior to the damage, and **(d)** post-mortem examination showing hairline cracks and debris formation.

## 2.3 Numerical Method

This section presents the methodology followed for constructing the numerical model. The objectives of numerical simulations are (i) to reproduce the experimental observations through high-fidelity computations and (ii) gain further insights into the dynamic failure mechanisms beyond the limitations of direct observation. In this regard, a virtual test setup is constructed to simulate the low-velocity line impact experiments summarized in Section 2.2. This virtual test setup is modelled in 3-D using the commercial finite element (FE) package ABAQUS/Explicit [61]. Details of the modelling strategy are provided in the following sections.

### 2.3.1 Geometry and Boundary Conditions

A schematical illustration of the FE model is presented in Figure 2.2. The semi-cylindrical steel impactor is idealized as an analytical rigid body with a mass of 1.865 kg. It is positioned just above the beam providing that the initial contact happens right at the mid-span of the composite beam. To simulate the free fall from a height of 0.5 m, an initial downward velocity of 3.13 m/s is assigned to the impactor. The composite beam specimen is modelled as a three-dimensional deformable body. To account for large deflections that result in stiffness changes, the nonlinear geometry option is activated.

A representative meshed section of the composite laminate model is depicted in Figure 2.2. For the ply material, 8-noded linear brick elements with reduced integration, enhanced hourglass control, and second-order accuracy (denoted as C3D8R in the ABAQUS library) are employed. For the purpose of modelling delamination, 8-noded three-dimensional cohesive elements (denoted as COH3D8 in the ABAQUS library) are inserted only at  $0^\circ/90^\circ$  interfaces. Additional information regarding the element sizing is provided in Section 2.3.3.3.

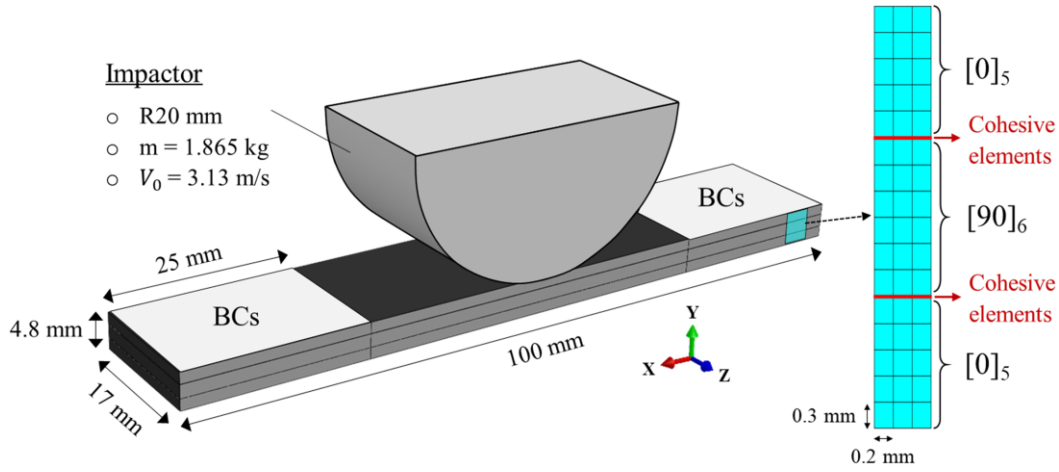


Figure 2.2 FE model and a representative section of the mesh.

Recalling that the specimen was clamped along the  $25 \times 17 \text{ mm}^2$  region from both ends in experiments, a straightforward approach to idealize these experimental boundary conditions could be made by constraining displacement degrees of freedom of the corresponding nodes, as practiced by Topac et al. [42]. Specifically, the end portions of the bottom surface are constrained in both longitudinal and transverse directions, while those of the top surface are fixed only in the longitudinal direction to allow contraction of the specimen due to Poisson's ratio effect. Although experimental failure patterns can be accurately reproduced with the implementation of these ideal boundary conditions, a detailed comparison of *in situ* experimental and computational deformation and strain fields reveals that such rigid constraining led to a stiffer flexural response, which significantly affects dynamic initiation and progression characteristics of the damage. Instead, we propose a heuristic approach that replicates the experiment boundaries through the assembly of spring elements at the corresponding boundary nodes. Details of the proposed boundary conditions approach are provided in Section 2.4.

### 2.3.2 Material Properties

The complete list of properties for Hexcel 913C/HTS unidirectional (UD) prepreg used in the simulations is provided in Table 2.2, along with the corresponding

references. These properties consist of the ply-level elastic, strength and interface properties. The interface properties include strengths, toughness, penalty stiffness and mixed-mode interaction parameter.

Laminates exhibit increased strength when they are constrained by laminates with different fiber orientations, compared to the strength of the unidirectional laminates. This increased strength is referred as “in-situ strength” and is influenced by various factors: *i*) the positioning of the laminates (outer or embedded), *ii*) the orientation of the adjacent laminates, and *iii*) the ply thickness. In our case, the 90° laminate where damage occurred is categorized as a thick-embedded laminate.

Camanho et al. [63] derived the linear in-situ transverse matrix strength relation as,

$$Y_T^{is} = 1.12\sqrt{2}Y_T \quad (2.1)$$

where  $Y_T$  is the transverse tensile strength. The same study also provides the in-situ in-plane shear strength  $S_L^{is}$  relationship, taking into account shear non-linearity, as given in Equation (2.2)

$$S_L^{is} = \sqrt{\frac{(1 + \beta\phi G_{12}^2)^{1/2} - 1}{3\beta G_{12}}} \quad (2.2)$$

where parameters  $\beta$  equals to  $3.6 \times 10^{-8} \text{ MPa}^{-3}$ ,  $\phi$  can be computed through the relation  $\phi = 12(S_L)^2/G_{12} + 18\beta(S_L)^4$ , and  $G_{12}$  is the in-plane shear modulus. For the in-situ transverse shear strength  $S_T^{is}$ , Catalanotti [64] provided the relation as,

$$S_T^{is} = \frac{(2 \sin^2 \alpha_0 - 1)}{\eta_L \sin 2\alpha_0} S_L^{is} \quad (2.3)$$

where  $\eta_L$  is the internal friction parameter in the longitudinal direction. Experimental studies on composites with similar fiber and resin systems have reported a value of 0.082 for  $\eta_L$  [65,66]. Therefore, in this study, we adopt the same value of 0.082. The friction parameter in the transverse direction,  $\eta_T$ , is calculated by the relation  $\eta_T = -1/\tan(2\alpha_0)$  where  $\alpha_0$  is the fracture angle under pure transverse compression tests which consistently reported as 53° in the literature [62,64,66,67].

Table 2.2 Material properties used in simulations.

| Property                                 | Value                                  | Ref.    |
|------------------------------------------|----------------------------------------|---------|
| <u>Elastic Properties:</u>               |                                        |         |
| $E_1$                                    | 135 GPa                                | [44]    |
| $E_2 = E_3$                              | 9.2 GPa                                | [44]    |
| $G_{12} = G_{13}$                        | 5.5 GPa                                | [44]    |
| $G_{23}$                                 | 4.5 GPa                                | [44]    |
| $\nu_{12} = \nu_{13}$                    | 0.30                                   | [44]    |
| $\nu_{23}$                               | 0.45                                   | [44]    |
| <u>Ply-level Strengths:</u>              |                                        |         |
| $Y_T = Z_T$                              | 60 MPa                                 | [42]    |
| $Y_C = Z_C$                              | 205 MPa                                | [42]    |
| $S_L$                                    | 62 MPa                                 | [42]    |
| <u>Density:</u>                          |                                        |         |
| $\rho$                                   | 1780 kg/m <sup>3</sup>                 | [44]    |
| <u>Interface Strengths:</u>              |                                        |         |
| $T_{o,I}$                                | 87.6 MPa                               | [68]    |
| $T_{o,II} = T_{o,III}$                   | 83.7 MPa                               | [68]    |
| <u>Interface Toughness:</u>              |                                        |         |
| $G_{I,c}$                                | 0.26 kJ/m <sup>2</sup>                 | [42]    |
| $G_{II,c} = G_{III,c}$                   | 0.84 kJ/m <sup>2</sup>                 | [42]    |
| <u>Mixed-mode interaction parameter:</u> |                                        |         |
| $\eta$                                   | 1.45                                   | [24,25] |
| <u>Penalty stiffness:</u>                |                                        |         |
| $K_N$                                    | $2.88 \times 10^{14}$ N/m <sup>3</sup> | [69]    |
| $K_S$                                    | $8.14 \times 10^{13}$ N/m <sup>3</sup> | [70]    |

The density of the composite material  $\rho$  is reported as 1780 kg/m<sup>3</sup> in [42,44]. Within the explicit FEA framework, it is necessary to assign density to all elements, including cohesive elements. In this study, we assume that the interface density is equal to the composite material density. Notably, the cohesive density, denoted as

$\rho_{coh}$ , can be calculated using the formula  $\rho_{coh} = \rho t_{coh}$ , where  $t_{coh}$  represents the thickness of the cohesive elements [61].

The plain strain longitudinal wave speeds parallel and perpendicular to the fibers, and shear wave speed [60] are calculated through the equations provided in Equation (2.4)

$$c_l^{\parallel} = \sqrt{\frac{c_{11}}{\rho}} \quad , \quad c_l^{\perp} = \sqrt{\frac{c_{22}}{\rho}} \quad , \quad c_s = \sqrt{\frac{c_{66}}{\rho}} \quad (2.4)$$

where  $c_{11}$ ,  $c_{22}$  and  $c_{66}$  are the components of the three-dimensional stiffness matrix of the material. The Rayleigh wave speeds parallel ( $c_R^{\parallel}$ ) and perpendicular ( $c_R^{\perp}$ ) to the fibers are calculated by solving the relation provided in Equation (2.5) [71]. The solution gives  $c_R^{\parallel}$  value if  $c_l = c_l^{\parallel}$  and  $c_R^{\perp}$  value if  $c_l = c_l^{\perp}$ .

$$4 \sqrt{\left(1 - \frac{c_R^2}{c_l^2}\right) \left(1 - \frac{c_R^2}{c_s^2}\right)} - \left(2 - \frac{c_R}{c_s}\right)^2 = 0 \quad \left. \vphantom{\sqrt{\left(1 - \frac{c_R^2}{c_l^2}\right) \left(1 - \frac{c_R^2}{c_s^2}\right)}} \right\} \text{Solve for } c_R \quad (2.5)$$

The calculated values of plain strain material wave speeds are given in Table 2.3.

Table 2.3 Material wave speeds for Hexcel 913C/HTS UD prepreg [44].

| $c_l^{\parallel}$ | $c_l^{\perp}$ | $c_s$    | $c_R^{\parallel}$ | $c_R^{\perp}$ |
|-------------------|---------------|----------|-------------------|---------------|
| 8807 m/s          | 2567 m/s      | 1758 m/s | 1675 m/s          | 1556 m/s      |

The effect of stochastic distributions of material properties, specifically the dominant strength of  $Y_T^{is}$ , on the damage patterns is presented in Appendix B.

### 2.3.3 Damage Modelling

This section describes the numerical methodology employed to model two different types of damage observed in experiments: intralaminar damage within the composite

plies and interlaminar delamination at the interfaces between plies. These two failure modes are modelled separately in numerical simulations. Intralaminar failure is characterized by matrix cracking and is the only failure mode observed in experiments due to the cross-ply layup sequence. Therefore, the numerical model only includes matrix failure modes to simulate intralaminar damage. Interlaminar failure, on the other hand, is simulated using the Cohesive Zone Method (CZM) to model crack growth at the interfaces between plies with different fiber orientations, such as the  $0^\circ$  and  $90^\circ$  interfaces.

To capture matrix damage initiation and progression within the composite plies, a continuum damage model is implemented using a VUMAT user-subroutine. For the delamination damage, built-in cohesive elements of the ABAQUS software are utilized at the  $0^\circ/90^\circ$  interfaces. More specific details and formulations of the damage models employed in the numerical simulations are provided in the subsequent sections.

#### **2.3.3.1 Transverse Matrix Cracking**

The experimental findings, summarized in Section 2.2, emphasize the importance of employing three-dimensional (3-D) failure criteria in transverse impact events. In this regard, the LaRC05 damage initiation criterion [62] is chosen over the LaRC04 criterion as in [42]. This decision is based on several factors: *i*) The LaRC05 criterion is a combined criterion that considers tensile and compressive damage with a single relation, *ii*) it is a complete 3-D criterion including out-of-plane stress components, which have been identified as critical in transverse impact events [25], and *iii*) it provides a critical fracture plane orientation (as in Puck's criterion [72]).

The bilinear equivalent stress ( $\sigma_{eq}$ ) vs. equivalent displacement ( $\delta_{eq}$ ) relation that an integration point obeys is illustrated in Figure 2.4. The equivalent displacement and the corresponding equivalent stress governing the progressive matrix damage model are defined as,

$$\delta_{eq} = L^c \sqrt{\langle \pm \epsilon_{22} \rangle^2 + \langle \pm \epsilon_{33} \rangle^2 + \epsilon_{12}^2 + \epsilon_{13}^2 + \epsilon_{23}^2} \quad (2.6)$$

$$\sigma_{eq} = \frac{\langle \pm \sigma_{22} \rangle \langle \pm \epsilon_{22} \rangle + \langle \pm \sigma_{33} \rangle \langle \pm \epsilon_{33} \rangle + \tau_{12} \epsilon_{12} + \tau_{13} \epsilon_{13} + \tau_{23} \epsilon_{23}}{\delta_{eq}/L^c} \quad (2.7)$$

where  $L^c$  is the characteristic length defined to alleviate mesh dependency, and  $\langle \rangle$  represents the Macaulay bracket. The material response is linear up to the damage initiation point (labelled as A), which is predicted by the LaRC05 matrix failure criterion, shown in Equation (2.8). This criterion is a modified version of Mohr-Coulomb's transverse compression criterion, incorporating an additional term to account for tensile failures. The internal friction parameters ( $\eta_L$  and  $\eta_T$ ) reflect the pressure effects on the failure response by either increasing or decreasing respective shear strengths depending on whether the fracture plane tractions are compressive or tensile.

$$FI = \left( \frac{\tau_T}{S_T - \eta_T \sigma_N} \right)^2 + \left( \frac{\tau_L}{S_L - \eta_L \sigma_N} \right)^2 + \left( \frac{\langle \sigma_N \rangle_+}{Y_T} \right)^2 = 1 \quad (2.8)$$

The traction components on the fracture plane ( $\tau_T$ ,  $\tau_L$ ,  $\sigma_N$ ) are obtained by stress transformation relations given in Eqns. (2.9), (2.10) and (2.11). The illustration of the traction components on the matrix fracture plane is depicted in Figure 2.3.

$$\tau_T = -\frac{\sigma_2 - \sigma_3}{2} \sin(2\theta) + \tau_{23} \cos(2\theta) \quad (2.9)$$

$$\tau_L = \tau_{12} \cos(\theta) + \tau_{31} \sin(\theta) \quad (2.10)$$

$$\sigma_N = \frac{\sigma_2 + \sigma_3}{2} + \frac{\sigma_2 - \sigma_3}{2} \cos(2\theta) + \tau_{23} \sin(2\theta) \quad (2.11)$$

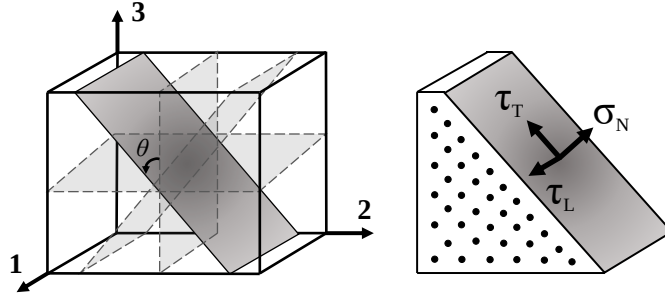


Figure 2.3. Illustration of critical plane search and traction components on the critical plane.

After the damage initiation criterion is satisfied, the damage in the material progresses by degrading the stiffness of the elements following the continuum-damage modelling approach [73]. Stiffness degradation is implemented by introducing degraded values of transverse and shear moduli into the compliance tensor. Here, the 3-D damaged compliance tensor  $S$  is given in Equation (2.12) [74].

$$S = \begin{bmatrix} \frac{1}{E_{11}} & -\frac{\nu_{21}}{E_{11}} & -\frac{\nu_{31}}{E_{33}} & 0 & 0 & 0 \\ -\frac{\nu_{12}}{E_{11}} & \frac{1}{E_{22}(1-d_m)} & -\frac{\nu_{32}}{E_{33}} & 0 & 0 & 0 \\ -\frac{\nu_{13}}{E_{11}} & -\frac{\nu_{23}}{E_{22}} & \frac{1}{E_{33}(1-d_m)} & 0 & 0 & 0 \\ 0 & 0 & 0 & \frac{1}{2G_{12}(1-d_s)} & 0 & 0 \\ 0 & 0 & 0 & 0 & \frac{1}{2G_{23}(1-d_s)} & 0 \\ 0 & 0 & 0 & 0 & 0 & \frac{1}{2G_{13}(1-d_s)} \end{bmatrix} \quad (2.12)$$

where the matrix damage variable  $d_m$  varies from 0 (undamaged) to 1 (complete damage) along the line A-C in Figure 2.4, and the shear damage variable becomes  $d_s = d_m$  since there is no distinction between tension and compression failure in LaRC05. The irreversibility of the damage process is taken into account by the non-linear saturation type behavior provided in Equation (2.13).

$$d_m = \frac{\delta_{eq}^f (\delta_{eq} - \delta_{eq}^0)}{\delta_{eq} (\delta_{eq}^f - \delta_{eq}^0)} \quad (2.13)$$

where  $\delta_{eq}^0$ ,  $\delta_{eq}$  and  $\delta_{eq}^f$  are the equivalent displacements at the initiation, progression and complete damage states, respectively. The equivalent displacement at the completely damaged state is calculated by equating the dissipated energy (i.e. the area under the triangle in Figure 2.4) to the fracture toughness,  $G_c$ , of the respective mode, i.e.,

$$\delta_{eq}^f = \frac{2G_c}{\sigma_{eq}^0} \quad (2.14)$$

$G_c$  is considered equivalent to the Mode-I fracture toughness ( $G_{I,c}$ ) for a tensile matrix failure ( $\sigma_N > 0$ ), and approximated by the relation  $G_{II,c}/\cos(\alpha_0)$  [75] for a compressive matrix failure ( $\sigma_N < 0$ ). If an element becomes completely damaged, it is not deleted due to its compressive load-transferring capabilities since only matrix failure occurs. The matrix damage variables are limited to a threshold of 99.99%.

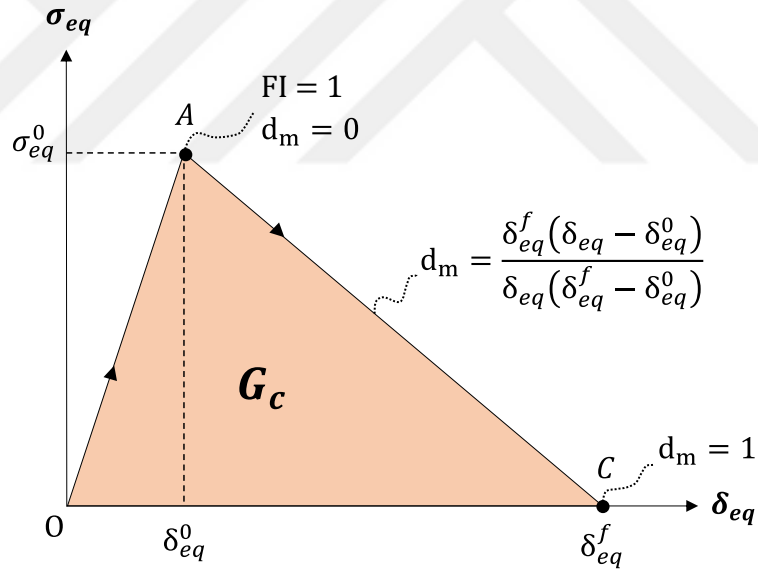


Figure 2.4. Constitutive material model that predicts the intralaminar damage initiation and progression.

### 2.3.3.2 Delamination

Delamination growth between the adjacent ply interfaces having different fiber orientations is simulated utilizing the Cohesive Zone Method (CZM) through the use of built-in cohesive elements of ABAQUS.

A bilinear traction response to the respective separation at the interfaces for both opening and combined shear fracture modes is employed in this study, as illustrated in Figure 2.5. Considering the multiaxial nature of transverse impact loading, the interaction of opening and shear fracture modes is taken into account (mixed-mode CZM). The region prior to the delamination onset enforces a linear elastic response with penalty stiffnesses of  $K_N$  and  $K_S$  for the opening and shear modes, respectively. Turon et al. [69,70] proposed Eqns. (2.15) and (2.16) to determine  $K_N$  and  $K_S$ ,

$$K_N = c \frac{E_3}{t} \quad (2.15)$$

$$K_S = K_N \frac{G_{Ic}}{G_{IIc}} \left( \frac{T_{o,II}}{T_{o,I}} \right)^2 \quad (2.16)$$

where  $c$  is a coefficient suggested to be 50 in [69],  $t$  is the sublaminate thickness,  $T_{o,i}$  and  $G_{i,c}$  ( $i = I, II$ ) being interface strength and fracture toughness. The delamination initiation predicted using the quadratic nominal stress criterion, proposed by Chang and Springer [76], is provided in Equation (2.17).

$$\left( \frac{\langle T_I \rangle}{T_{o,I}} \right)^2 + \left( \frac{T_{II}}{T_{o,II}} \right)^2 + \left( \frac{T_{III}}{T_{o,III}} \right)^2 = 1 \quad (2.17)$$

where  $T_i$  ( $i = I, II, III$ ) are the surface tractions of corresponding modes, respectively. Only the positive values of  $T_I$  are included in this relation since compressive stresses have no contribution to opening mode fractures. The separation at the damage initiation is calculated with the relation  $\delta_o = T_o/E_o$  where its corresponding initiation traction is  $T_o = \sqrt{T_{o,I}^2 + T_{o,II}^2 + T_{o,III}^2}$ .

Following the prediction of damage initiation in a cohesive element, a linear softening behaviour is employed during the propagation of damage through degradation of the penalty stiffnesses of the cohesive element by the cohesive damage variable  $D$ , which is given by,

$$D = \frac{\delta_c(\delta - \delta_0)}{\delta(\delta_c - \delta_0)} \quad (2.18)$$

where the effective separation is  $\delta = \sqrt{\delta_I^2 + \delta_{II}^2 + \delta_{III}^2}$  and its critical separation at the propagation is determined by  $\delta_c = 2G_c/T_0$ . Cohesive elements are removed from the mesh when the damage parameter  $D$  reaches the predefined 99% threshold to simulate the contact and frictional events between delaminated surfaces. The mixed-mode fracture toughness,  $G_c$ , is calculated based on the Benzeggagh and Kenane (B-K) criterion in Equation (2.19) [77].

$$G_c = G_{Ic} + (G_{IIc} - G_{Ic}) \left( \frac{G_{II} + G_{III}}{G_I + G_{II} + G_{III}} \right)^\eta \quad (2.19)$$

where  $\eta$  stands for the mixed-mode interaction parameter.

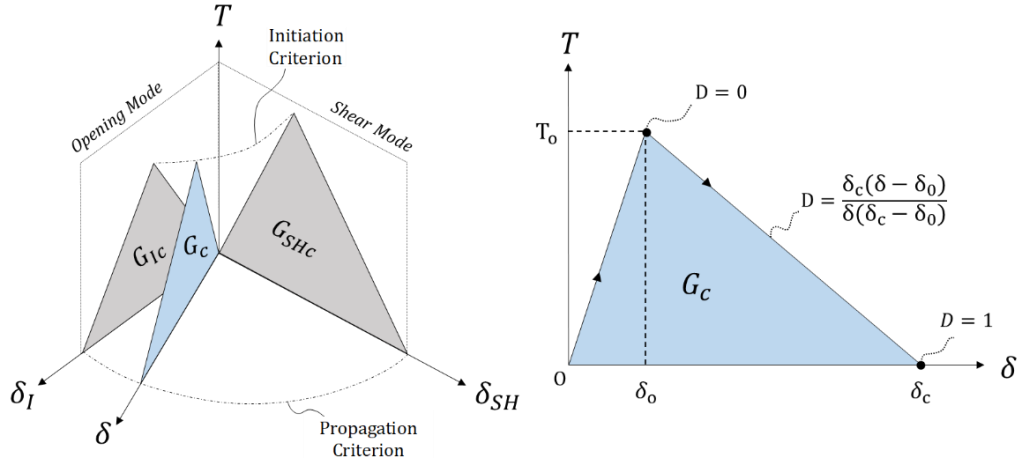


Figure 2.5. Constitutive law of the mixed-mode cohesive zone model.

### 2.3.3.3 Element Sizes

To establish a realistic energy dissipation during fracture, the determination of appropriate element sizes plays a crucial role in achieving accurate results. The greatest element size is determined by comparing the allowable sizes of intralaminar and interlaminar damage models.

For composite continuum elements, the greatest in-plane size  $l^* = \sqrt{l_x l_y}$  is suggested to be less than the maximum in-plane characteristic length  $l_{max}^*$  to avoid the snap-back behavior of the constitutive softening branch [25]. In the case of linear softening law, the adopted criterion to determine the  $l_{max}^*$  length is given in Equation (2.20) [25].

$$l^* = \sqrt{l_x l_y} < \frac{2E_M G_M}{X_M^2} \quad \text{where } M = 2^+, 2^-, 6 \quad (2.20)$$

where  $E_M$ ,  $G_M$  and  $X_M$  are elastic modulus, fracture toughness and ply strength values.  $M = 2^+, 2^-, 6$  corresponds to tensile, compressive and shear failure modes of the matrix. Accordingly, the maximum in-plane characteristic length using the properties given in Table 2.2 is 0.61 mm.

For cohesive elements, an estimation for the interlaminar fracture process zone can be acquired using the mesh stability criteria proposed by Yang and Cox for slender beams [78]. The fracture process zones for Mode-I and Mode-II delamination are estimated by  $l_{cz,I} = l_{ch,I}^{1/4} h^{3/4}$  and  $l_{cz,II} = \sqrt{l_{ch,II} h}$ , respectively. In these relations,  $h$  is conservatively taken equal to the thinnest sub-laminate thickness and  $l_{ch,i}$  is the characteristic length of interfaces for failure modes  $i = I, II$  where  $l_{ch,i} = G_{ic} E' / T_{o,i}$  with  $G_{ic}$ ,  $E' = E_1 \sqrt{1 - \nu_{12} \nu_{21}}$  and  $T_{o,i}$  being fracture toughness, equivalent elastic modulus and interfacial strength. With the given parameters, the interlaminar fracture process zone is estimated as 0.71 mm, yet the maximum allowable in-plane size of a cohesive element is determined as 0.24 mm since having

at least 3 elements along the process zone is suggested for an accurate delamination initiation and propagation representation [25,69,79,80].

For element sizing along the thickness direction, mesoscale modelling [81,82] is adopted for the composite elements, where each ply is represented by a single layer of solid elements. This corresponds to a mesh size of 0.3 mm along the thickness of composite layers.

For the thickness of cohesive elements,  $t_{coh} = 5 \mu\text{m}$  is assigned, which is less than 1% of the laminate thickness. While modelling the cohesive zone with zero-thickness elements seems plausible, assigning a density value to these elements in dynamic analyses can be problematic as they theoretically have an infinite density, and such a value can significantly affect stable-increment time and wave propagation speeds in explicit analyses [61]. Nevertheless, this issue can be resolved by modelling cohesive elements with a small thickness, which allows to assignment of a finite density value. In this study, we assume that the interface density is equal to the density of the prepreg material. The cohesive density, denoted as  $\rho_{coh}$ , is defined as the mass per unit area using the formula  $\rho_{coh} = \rho t_{coh}$  [61].

Consequently, it has been determined that a maximum mesh sizes of  $0.2 \times 0.2 \text{ mm}^2$  in-plane, and 0.3 mm along the thickness are suitable for accurately simulating the damage progression in the laminate. The corresponding representative meshed section of the FE model is depicted in Figure 2.2. The effects of mesh refinement on the global impact responses and damage patterns by halving the element sizes are presented in Appendix A.

#### **2.3.3.4 Contact and Friction**

Contact and friction laws are defined between the steel impactor and composite beam, as well as between composite layers in case self-contact occurs. The contact algorithms and friction models provided by Abaqus [61] are employed for this purpose.

Self-contact between composite layers is simulated by utilizing the general contact algorithm with the penalty contact method, excluding the impactor-composite beam contact pair. The surface-to-surface contact type with penalty contact method is preferred for the contact between the steel impactor and composite beam to increase computational efficiency by defining the contact pair in advance. Friction is included at all contacting surfaces and interfaces by employing the built-in Coulomb's friction model. In this model, the tangential motion is zero up until the surface shear traction reaches the critical value that is calculated by the relation  $\tau = \mu_s p$  where  $\mu_s$  and  $p$  are the static Coefficient of Friction (COF) and normal contact pressure, respectively. Different COFs are utilized depending on the contact couples: 0.3 for the contact between the steel impactor and composite [24,42,83–85] and 0.5 for in case of self-contact of composite layers [24,86]. Although there is no consensus on values of COF along the crack surfaces for similar composites in the literature, the effects of utilizing frictionless and very high COF contacts are investigated in Section 2.6.

## 2.4 Implementation of Flexural Boundary Conditions

In the LVI regime, the role of boundary conditions (BCs) is crucial as there is sufficient time for stress waves generated from the impact zone to propagate and reflect back from the supports of the structure [87]. In our problem, a straightforward idealization approach for the BCs by constraining displacement degrees of freedom of the corresponding nodes, as practiced by Topac et al. [42], was described in Section 2.3.1. Although experimental failure patterns can be accurately reproduced by implementing such BCs, a detailed comparison of *in situ* experimental and computational deformation and strain fields reveals that such rigid constraining led to a stiffer flexural response, which significantly affects dynamic initiation and progression characteristics of the damage.

In this regard, we propose a heuristic approach that replicates the experiment boundaries through the assembly of spring elements at the corresponding boundary

nodes using a Python script, where we determine the stiffness of these springs based on the minimization of differences between the full-field displacements and strains obtained from the experiment and simulation. The aim of this approach is to eliminate the artificial effects that influence the aspects of the global and local impact-induced failure processes caused by the idealization of BCs. In the FE model, translational spring elements (denoted as CONN3D2 in the ABAQUS library) connect boundary nodes to the rigid ground where each spring element has two translational degrees of freedom, horizontal and vertical. The following considerations are made in modelling of spring configurations:

- The horizontal response of top boundary springs is associated with the tangential contact between the beam and the top fixture plates. This implies the tension-compression symmetric definition of a finite horizontal spring constant ( $k_{h,t}^+ = k_{h,t}^- \neq 0$ ).
- The vertical response of top boundary springs is associated with a normal contact or separation between the beam and the top fixture plates and should be treated differently under compression and tension. Under compression, springs are modelled to simulate the normal contact situation with a finite vertical spring constant ( $k_{v,t}^- \neq 0$ ). Under tension, on the other hand, springs are modelled to simulate the separation of the mating surface by means of the definition of zero vertical spring constant ( $k_{v,t}^+ = 0$ ).
- The horizontal response of bottom boundary springs is associated with the adhesive frictional contact between the beam and bottom fixture plates as this interface was reported to have adhesive regions [44]. This also implies the tension-compression symmetric definition of a finite horizontal spring constant ( $k_{h,b}^+ = k_{h,b}^- \neq 0$ ).
- The vertical response of bottom boundary springs is associated with the vertical compliance of the test fixture under compression, and adhesive contact of mating surfaces under tension. These imply the definitions of a finite vertical spring constant under compression ( $k_{v,b}^- \neq 0$ ) and tension ( $k_{v,b}^+ \neq 0$ ).

A flowchart for the construction of flexural boundary conditions through the proposed approach is depicted in Figure 2.6. The first FE analysis was initiated by employing a spring configuration with randomly assigned spring constants guided

by the aforementioned considerations. Horizontal and vertical displacements along a vertical path near boundaries shown with a dashed line in Figure 2.6 and shear strains along the beam's centerline were extracted from simulations and compared with the corresponding DIC results. Considering the differences between displacements, a new FE model is constructed with either increased or decreased spring constants. Differences are calculated for the same impactor position with respect to the undeformed state in experiments and simulations, which results in indirectly considering the impactor boundary also. The process of adjusting the spring constants continued iteratively until the error between the simulations and DIC results fell below a predetermined threshold. For an error threshold of 5%, the spring constants for  $k_{h,t}$ ,  $k_{v,t}^- = k_{v,b}^-$ ,  $k_{h,b}$ ,  $k_{v,b}^+$  are determined as 1040, 130, 1517,  $1084 \times 10^4$  kN/mm, respectively.

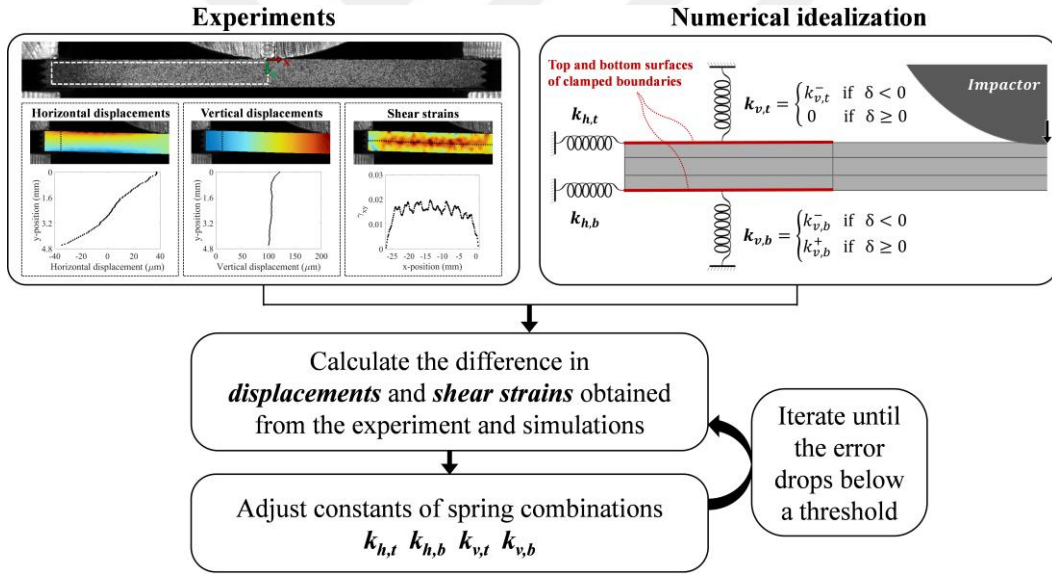


Figure 2.6. A heuristic approach to replicate the experiment boundaries through the assembly of spring elements at the corresponding boundary nodes. The stiffness of the springs is determined based on the minimization of differences between the full-field displacements and strains obtained from the experiment and simulation.

The proposed BCs approach is validated through detailed comparisons of displacement and shear strain fields between simulation with optimum spring constants and experiment. Figure 2.7 presents the impactor translation history with plots showing the distributions of horizontal ( $\delta_x$ ) and vertical displacements ( $\delta_y$ )

along the boundary edges from experiment and both FE models with fixed and proposed BCs. The displacement distributions are extracted from the experiment frame prior to damage (labelled as frame A) and corresponding time frames from FE simulations. The experiment and FE frames are plotted transparently on top of each other in the bottom left corner for visual comparison. The results obtained from the experiment and FE model with proposed BCs are in good agreement in both horizontal and vertical displacement distributions. In contrast, the discrepancy between the experiment and FE model with fixed BCs shows the inadequacy of this BCs modelling approach.

Furthermore, the transverse shear strain fields ( $\gamma_{xy}$ ) obtained from experiments are compared to the FEA results with fixed and proposed BCs at the identical impactor translation (0.84 mm) frame in Figure 2.8. On the right,  $\gamma_{xy}$  data acquired along the centerlines (shown with white dashes) are plotted.  $\gamma_{xy}$  contours of experiments (top) and FEA with proposed BCs (bottom) are in excellent agreement except for the hotspot regions observed in experiments. These slightly high localized values, also reported in [42], were attributed to the tested specimen's stochastic distribution of elastic properties causing a relaxed strain response locally, which is beyond the scope of this study to model. In contrast, the FE model with fixed BCs overpredicts  $\gamma_{xy}$  values along most of the test section and underpredicts near the boundaries due to rigid constraining.

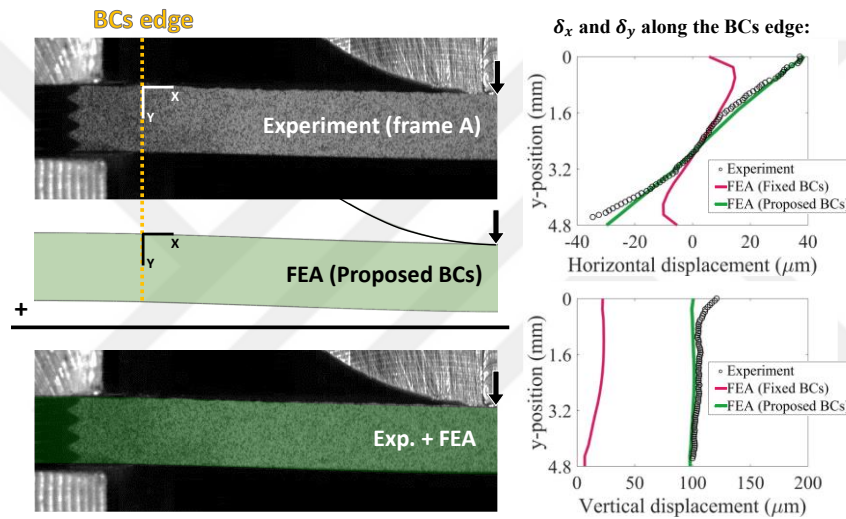
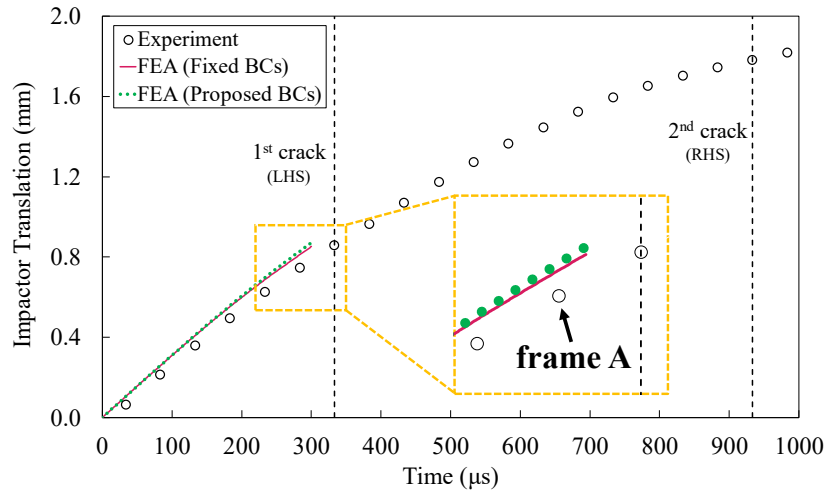


Figure 2.7. Impactor translation history (top) and plots showing the distributions of horizontal ( $\delta_x$ ) and vertical displacements ( $\delta_y$ ) along the boundary edges from the experiment and FE models with fixed and proposed BCs (bottom). Displacement distributions are extracted from the experiment frame prior to damage (labelled as A) and corresponding frames of FE simulation results.

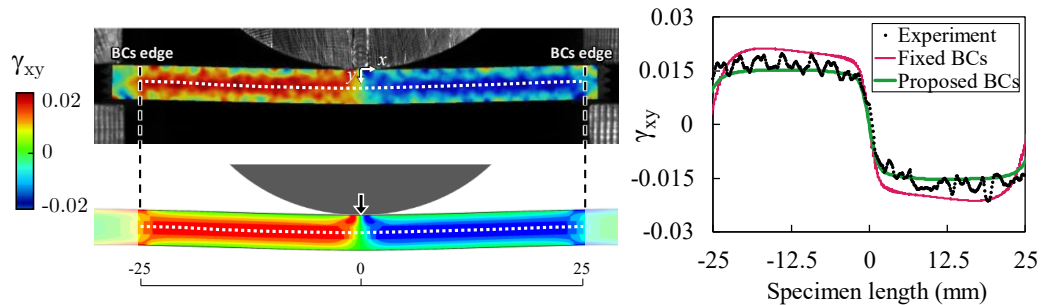


Figure 2.8. Comparison of experiment and FEA results in terms of transverse shear strain fields ( $\gamma_{xy}$ ).  $\gamma_{xy}$  data acquired along the centerlines (shown with white dashes).

## 2.5 Results

In this section, the FE simulation results of the low-velocity impact experiments performed on  $[0_5/90_3]_s$  CFRP beam [44] are presented. The section begins with an overview of the global behavior of the composite beam. Subsequently, a detailed examination is carried out on the *in situ* impact-induced matrix cracking and delamination progression, damage mechanisms, and dynamic characteristics.

### 2.5.1 Global Impact Response

The global impact response of the composite beam is investigated through the analysis of force history, force-displacement curves and energy balance of FE simulations.

The impactor force history and the force-displacement response of the FE model are plotted in Figure 2.9 for the entire impact process, from the initial contact of the impactor to its rebound. The material response is elastic up to the sudden load drop (denoted with A) at time and displacement of  $375\ \mu\text{s}$  and  $1.04\ \text{mm}$ , respectively. The periodic oscillations in the elastic regime occur due to the dynamic coupling of impactor, specimen and BCs [20]. A steep load drop occurs when delamination is propagated, as the effect of delamination on the load-bearing capacity of the specimen is greater than that of matrix cracking. At  $1.2\ \text{ms}$ , the impactor reaches its maximum deflection point of  $2.1\ \text{mm}$  and subsequently starts to rebound back (denoted with B). Eventually, it completely disengages from the laminate at around  $2.5\ \text{ms}$  (denoted with C).

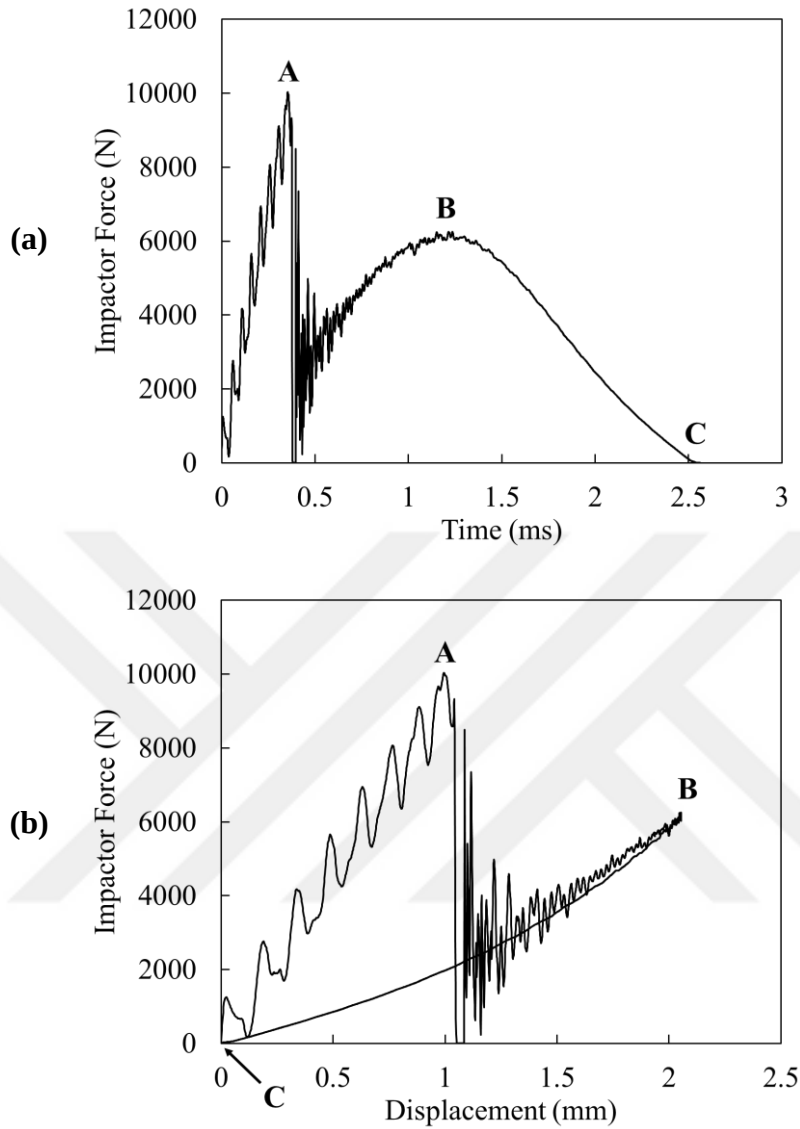


Figure 2.9. **(a)** Impactor force history and **(b)** impactor force-displacement response of the model.

The energy transformation of the FE model is plotted in Figure 2.10 for the entire impact process. Initially, the kinetic energy of the impactor is converted into elastic energy within the laminate. Upon the occurrence of matrix cracking and delamination at approximately 0.4 ms, energy is dissipated in the form of intralaminar and interlaminar damages. Frictional dissipation becomes evident following the occurrence of damage, indicating that self-contacting of the delaminated layers plays a significant role in energy dissipation. Impactor

disengages from the laminate at around 2.5 ms with a kinetic energy of 4.9 Joules, which corresponds to rebound velocity of 2.3 m/s. In the experiments, the rebound velocity of the impactor was measured 2.1 m/s, indicating that the simulations predicted the dissipated energy by an error of less than 15%. The total energy throughout the simulation remains practically constant, indicating that the simulation is free of significant artificial disturbances.

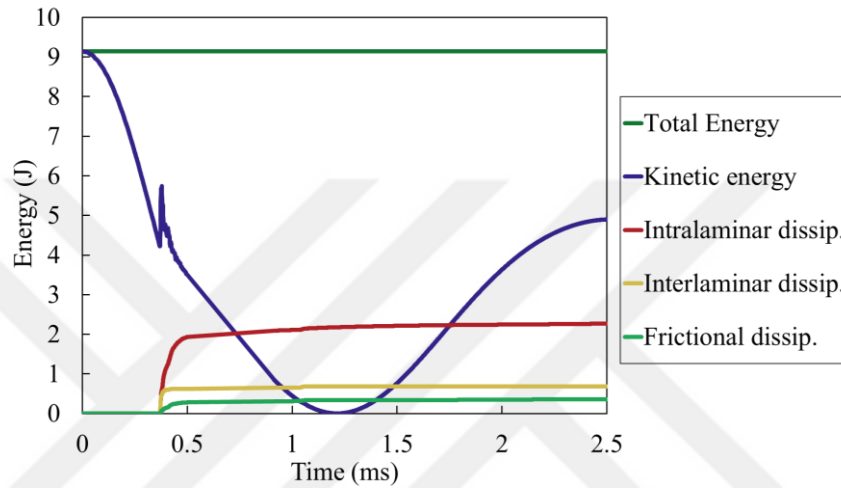


Figure 2.10. Energy transformation of the FE model for entire impact process.

### 2.5.2 In situ Damage Progression

The entire sequence of the damage observed during the low-velocity impact event is presented in this section. Figure 2.11 displays seven consecutive frames from the first damage initiation to the final state of the damaged beam. In the figure, finite elements are depicted in black to indicate complete composite element damage, while thin black lines represent cohesive failure. The key observations regarding the damage progression can be summarized as follows:

- The major matrix damage initiates inside the embedded 90° plies at around 368  $\mu$ s. This initiation region is located  $\sim$ 18 mm away from the impactor and near the upper 0°/90° interface, where the stiffness mismatch between adjacent layers is significant.
- The matrix damage grows diagonally with an inclination angle of about 48 degrees and reaches the lower 0°/90° interface at 369.4  $\mu$ s.

- When the matrix damage reaches interfaces, it immediately triggers delamination damage. The upper interface delamination propagates towards the beam's midspan, while lower delamination to the clamped boundaries.
- At 370.8  $\mu\text{s}$ , a secondary diagonal matrix crack develops near the upper interface in front of the delamination as it propagates toward the impactor.
- The primary and secondary delamination, which was triggered by the secondary matrix crack as it reaches the interface, merge and form a single delamination.
- Delaminations reach the region underneath the impactor at around 376  $\mu\text{s}$  but are momentarily arrested due to the compressive stress state in this region.
- The final state of the damaged beam is captured at 425.0  $\mu\text{s}$ . Delaminations propagating from the opposite sides of the beam are observed to be merged into one in the middle.

These damages occur simultaneously on both sides of the beam due to having a completely symmetric model with respect to the center where the impactor collides.

### 2.5.3 Damage Mechanisms

The underlying damage mechanisms of impact-induced matrix cracking and delamination are explained in this section. The dynamic characteristics of delamination are elucidated.

#### 2.5.3.1 Matrix Cracking

The formation of major matrix cracking observed inside the embedded 90° plies was presented in the previous section. The mechanism of matrix cracking is investigated through transverse shear stress ( $\tau_{23}$ ) contours where the transverse normal stress ( $\sigma_{22}$ ) state is tensile, with the material coordinate system aligned with the fibers in the 1-direction.

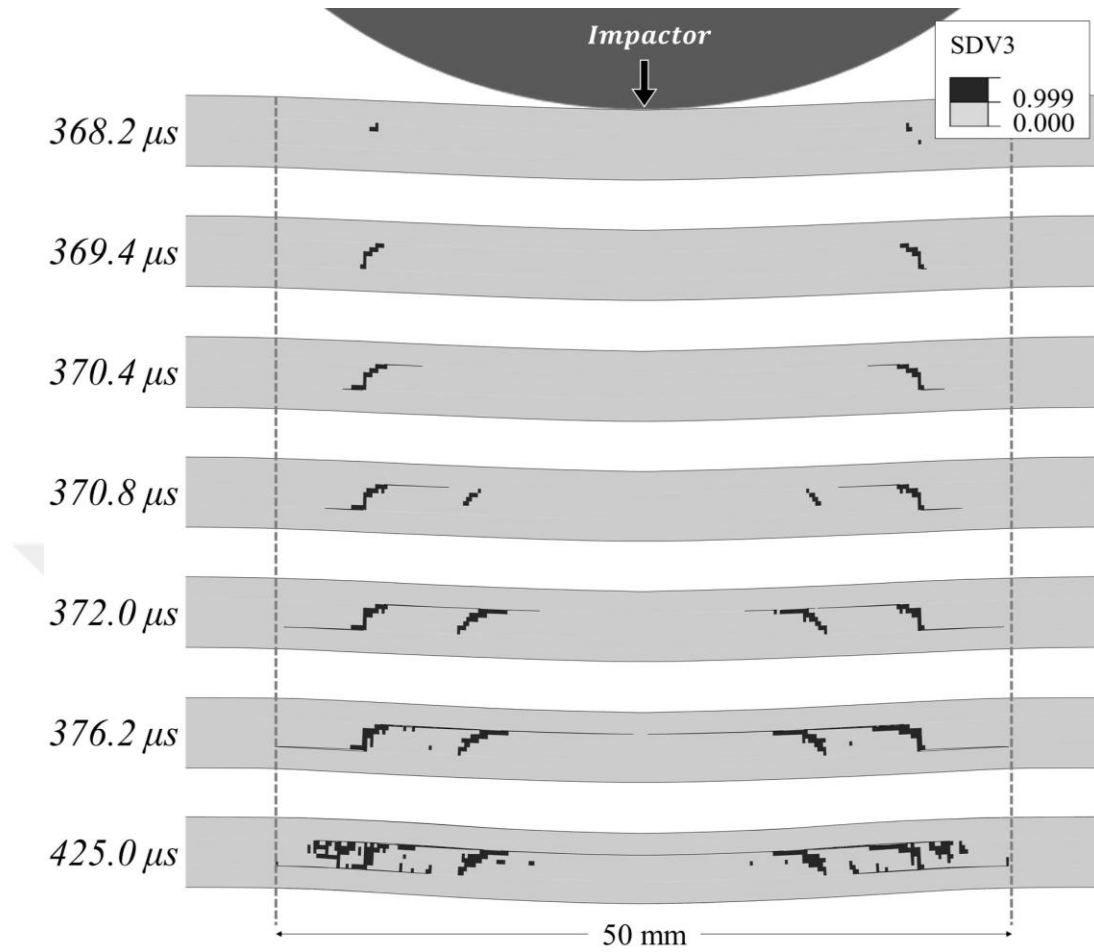


Figure 2.11. Damage formation sequence and resulting damaged state.

In Figure 2.12, five consecutive  $\tau_{23}$  contours coincident with tensile  $\sigma_{22}$  regions are plotted on top of images showing damaged elements for the  $90^\circ$  plies, on the left half of the beam. The first frame shows the stress state of the beam where upper layers near boundaries, lower layers underneath the impactor, and interconnection between these two regions are in tension, while the entire embedded  $90^\circ$  plies experience transverse shear. The first major matrix crack initiates at a location inside the  $90^\circ$  plies where  $\tau_{23}$  becomes maximum, specifically 368  $\mu$ s after the initial contact of the impactor. The high  $\tau_{23}$  values and diagonal orientation of approximately  $48^\circ$  indicate that these matrix cracks can be characterized as shear cracks, as also emphasized in [44]. After the first matrix crack occurs, the maximum  $\tau_{23}$  location shifts towards the impactor, but it has not caused any damage in the immediate vicinity due to

compressive waves emanating from the sudden cracking. The second matrix crack develops due to waves generated by the impactor amplifying the shear stresses before compressive waves emanating from the first matrix crack reach the damage location. The orientation of the second matrix crack is nearly identical to that of the first matrix crack, as both experience a similar  $\tau_{23}$  dominated stress state.

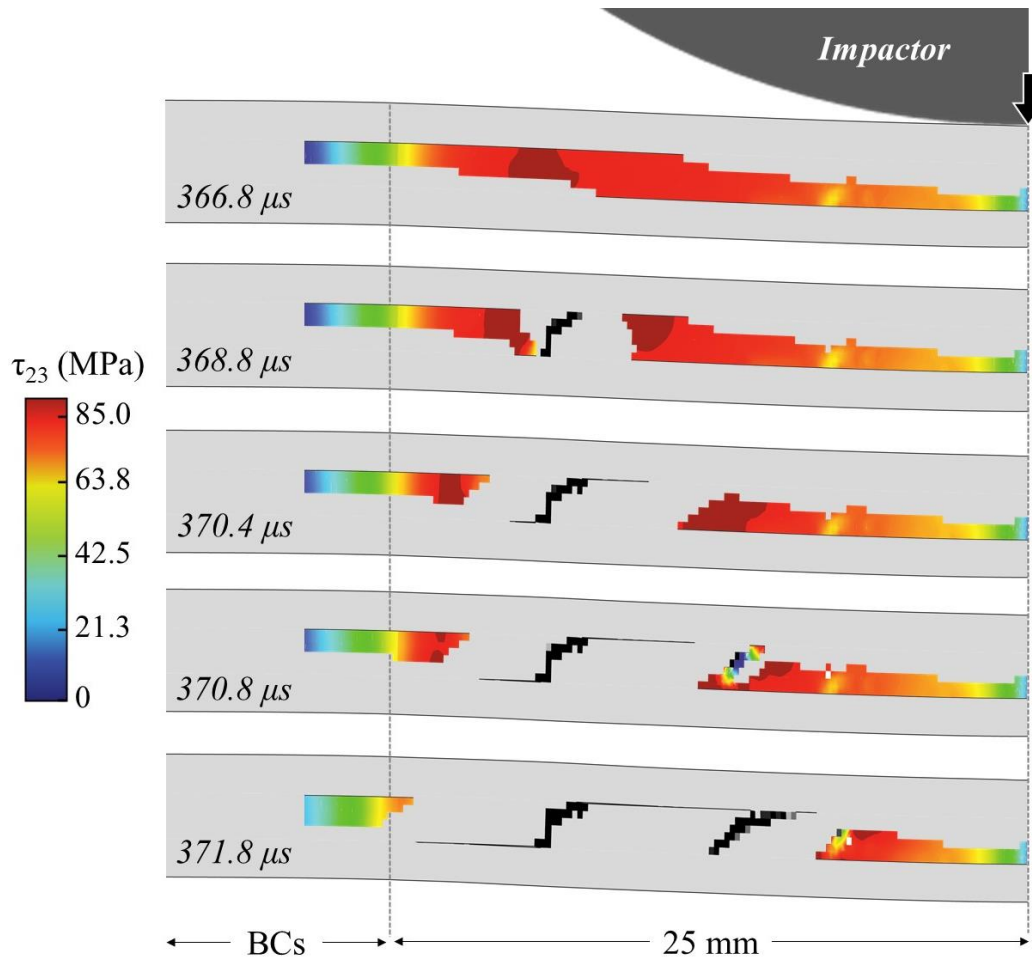


Figure 2.12. Transverse shear stress contours ( $\tau_{23}$ ) coincident with the tensile transverse normal stress ( $\sigma_{22} > 0$ ) regions. Black elements and lines respectively show completely damaged elements and delamination.

### 2.5.3.2 Delamination Propagation

The three-dimensional progression of delamination is investigated through the cohesive damage variable distributions of upper interface elements depicted in Figure 2.13. The first frame, captured at time 368.4  $\mu\text{s}$ , shows the locations of cohesive damage initiation at the edges near matrix damages. Initial frames demonstrate the free-edge effect [88] induced 3-D damage formation seen at the  $0^\circ$ - $90^\circ$  interface. The subsequent three frames reveal that the delamination propagation near edges occurs more rapidly than those in the mid-width. When delamination reaches the region underneath the impactor around 375.0  $\mu\text{s}$ , the increased compressive stresses retard the propagation momentarily. Eventually, the delamination cracks from the opposite sides of the beam are merged into one, as depicted in the last frame of Figure 2.13, which shows the final form of the delamination.

Cohesive damage variables are analyzed along the visible edge of the upper interface to investigate the delamination propagation characteristics further. In Figure 2.14(a), the development of the cohesive damage variables is plotted from 1  $\mu\text{s}$  before the initial occurrence of delamination damage at 369.6  $\mu\text{s}$  (denoted as 0  $\mu\text{s}$ ) until its completion. The curves indicate that shear loading across the entire interface leads to a sudden accumulation of delamination damage following initiation. For instance, the element located approximately 3 mm ahead of the delamination already experiences 80% damage when the initial delamination crack tip forms.

This sudden delamination propagation is further investigated by evaluating the contributions of the opening (Mode-I), in-plane shear (Mode-II), and transverse shear (Mode-III) loading modes on the critical strain energy release rates (SERR) in mixed-mode delamination. Figure 2.14(b) presents the critical SERR components

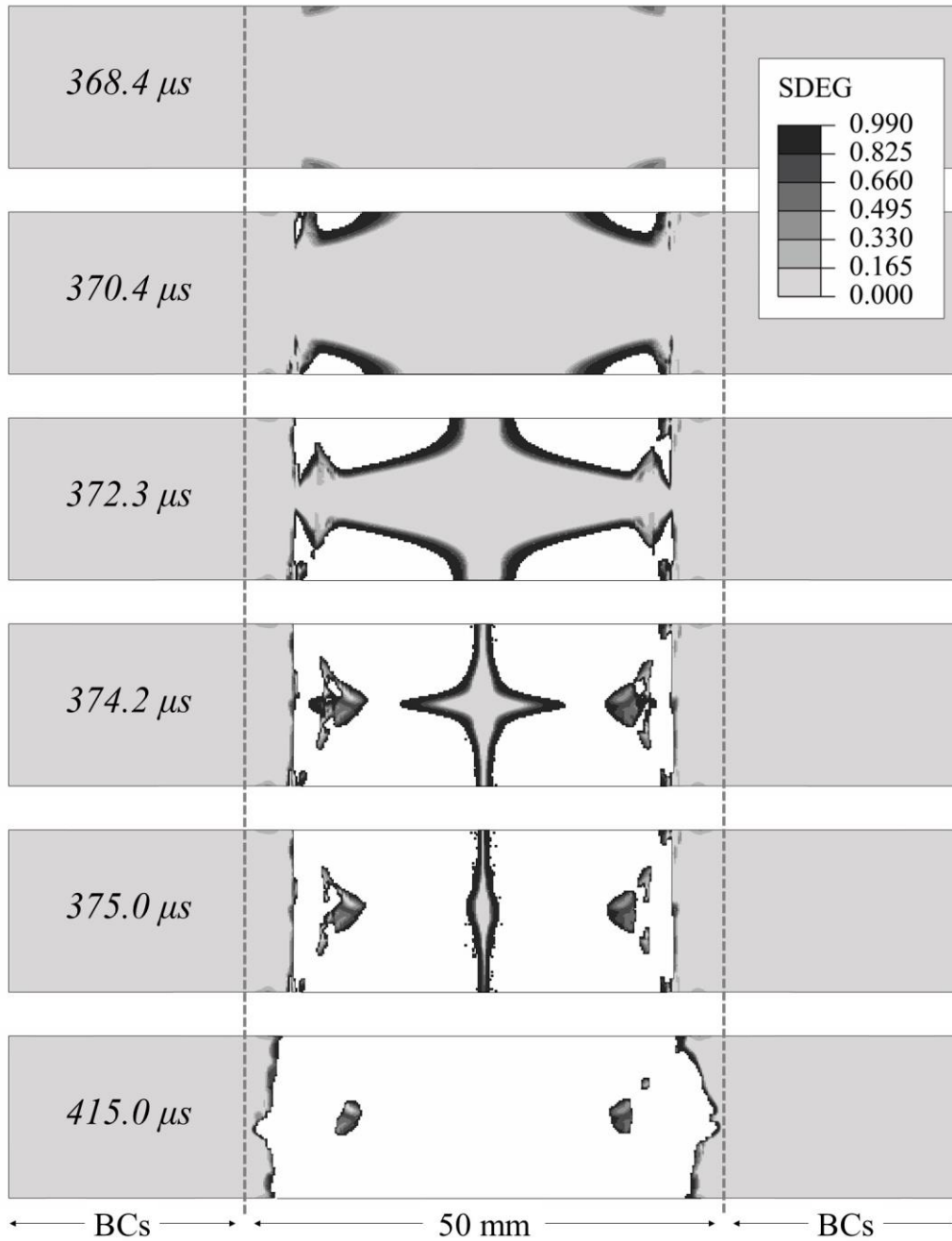


Figure 2.13. Top view of the upper interface elements showing 3-D dynamic delamination effects.

for the elements along the visible edge of the upper  $0^\circ$ - $90^\circ$  interface. The contribution of Mode-I and Mode-III fracture is found to be significant near the initiation region. However, the Mode-II type fracture becomes dominant after the delamination crack propagates approximately 2 mm, explaining the unstable and

sudden propagation characteristics of delamination through a pure Mode-II fracture [51,56,60]. At the final stages of delamination, just before the crack reaches the impactor, the fracture mode shifts to a mixed-mode propagation with significant contributions of Mode-I and Mode-III.

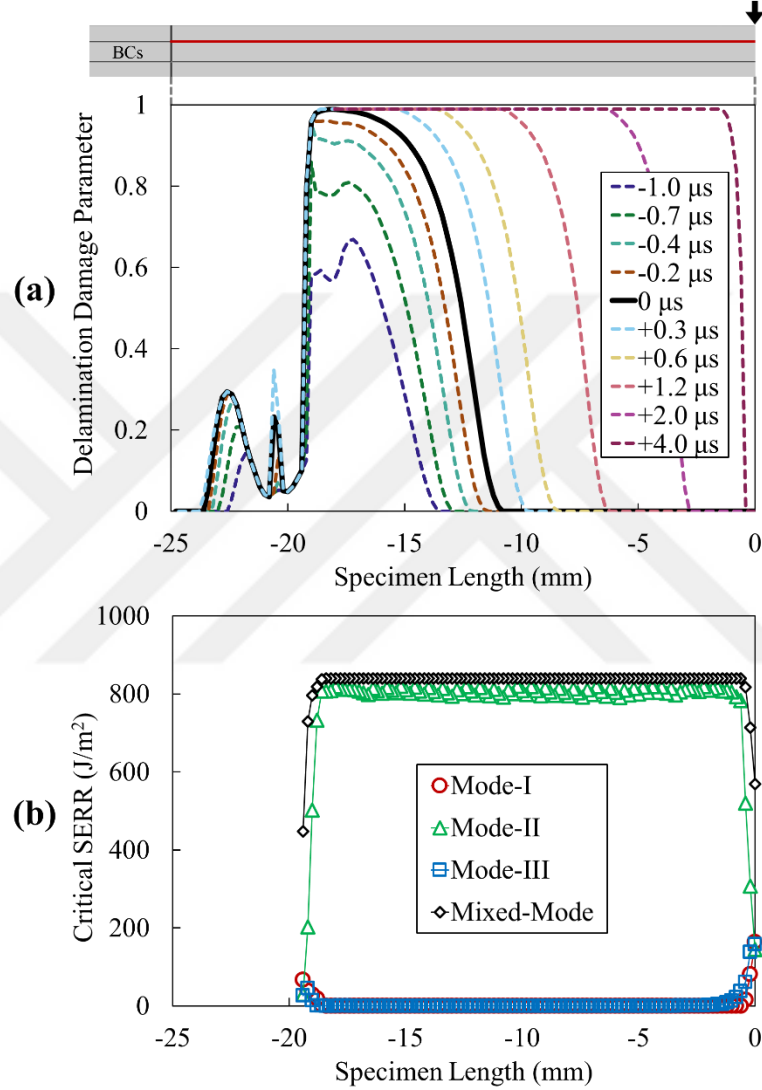
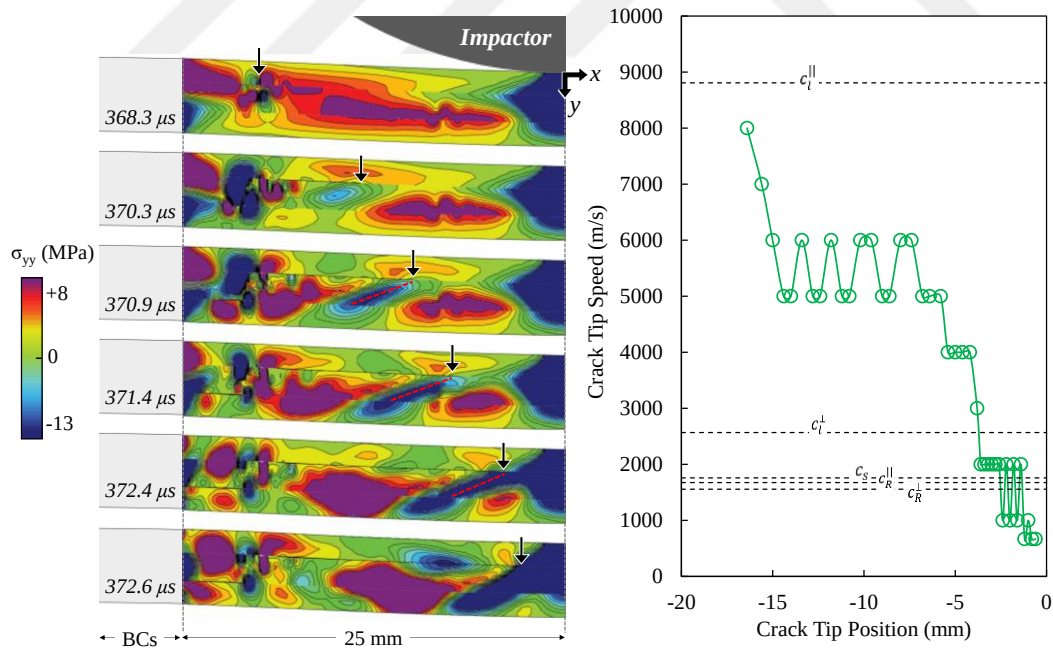


Figure 2.14. **(a)** Development of the cohesive damage variable over time and **(b)** critical strain energy release rate (SERR) components of mixed-mode delamination for the elements along the visible edge of the upper  $0^\circ$ - $90^\circ$  interface. Delamination grows suddenly and unstably under shear-mode dominant fracture.

The dynamic behavior of delamination is further analyzed by examining the distribution of normal stresses and the associated crack tip speeds. Figure 2.15 presents the distribution of normal stresses ( $\sigma_y$  in the problem axis) with the crack

tip speeds measured along the beam. The delamination process initiates at around  $368 \mu\text{s}$  and rapidly propagates at crack tip speeds ranging between 5000-8000 m/s. This is consistent with prior theoretical [89] and experimental [52,60] investigations, which have shown that Mode-II cracks propagate at speeds between the shear and longitudinal wave speed of the material (i.e., intersonic crack speed). At approximately  $370 \mu\text{s}$ , shear Mach waves emanating from the crack tip (highlighted with red dashed lines) become apparent, which are indicative of intersonic crack propagation. These waves are observed during intersonic crack propagation and are analogous to the aerodynamic shock waves observed in supersonic flight [90]. Delamination slows down to speeds of 1000-2000 m/s as it approaches the compressive region underneath the impactor. Consequently, the crack tip speeds are mostly intersonic and vary between 500 and 8000 m/s throughout its propagation. After the interface is completely delaminated, incoming bending waves generate contacting regions with increased compressive stress behind the delamination front that can be seen at  $372.6 \mu\text{s}$ .



The contact region that appears behind the delamination crack tip is examined further. In Figure 2.16, the normal crack opening profiles and the corresponding contact regions are plotted for the upper 0°/90° interface. The results show that the first contact region is observed when the delamination crack tip reaches the longitudinal position of -3 mm at 372.6  $\mu$ s. Subsequently, additional contact regions trail the crack tip until it reaches the center. Once the delamination surface is fully formed, the region beyond 5 mm from the center experiences contact pressures resulting from the compressive loading applied by the impactor, as shown in the capture at 379.6  $\mu$ s. The simultaneous examination of the opening profiles and the contact images for the same time frames reveals that the normal opening of the crack occurs outside the cohesive failure zone, indicating that mostly shear openings are effective. This finding supports the previous result regarding the dominance of the shear mode in delamination.

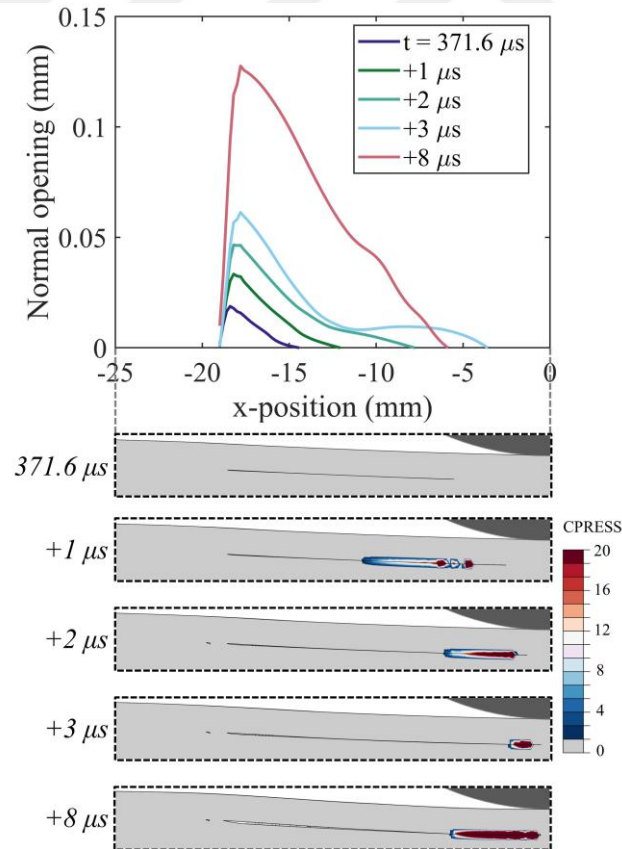


Figure 2.16. Normal opening profiles and corresponding contact regions behind the upper 0°/90° delamination crack tip for consecutive time frames.

## 2.6 Discussion

### 2.6.1 Influence of Boundary Conditions on the Global Impact Response

The influence of the proposed boundary conditions (BCs) on the global impact response is investigated through force history and force-displacement curves in Figure 2.17, in comparison with the results of the FE model with fixed BCs. The results demonstrate that the proposed BCs model yields a more accurate impactor force-displacement response compared to experimental results in terms of failure initiation time or displacement. Specifically, the failure initiation time is postponed from 257  $\mu\text{s}$  to 376  $\mu\text{s}$  when proposed BCs are employed as seen in Figure 2.17(a), where the average value from experiments was reported as 336  $\mu\text{s}$ . The corresponding impactor displacements are 0.85-mm and 1.04-mm for the fixed and proposed BCs, respectively, as depicted in Figure 2.17(b). The model with proposed BCs overpredicted the impactor displacement at failure, which was reported as 0.95-mm in the reference experiment. However, this overprediction in the simulation correlates with the slight delay in the failure initiation time, which arises due to the stochastic distribution of material properties in the experiments. Moreover, the compliance effect of the BCs leads to a decline in global stiffness response from 11903 N/mm to 8994 N/mm, where a linear fit is used for the curves up to the load drop for comparison purposes. Despite the changes in BCs and, hence, the stiffness of the system, the maximum failure load level is found to be an ineffective metric for validating the FE model's global response as it yields similar values for fixed and proposed BCs models (10801 N and 10036 N, respectively), with a difference of less than 8%. It is worth noting that the critical load levels have been found to be approximately the same for impacts occurring at locations offset from the symmetry axes of the specimens and hence different BCs [22], and even for different type of BCs of clamped and supported [91] under LVI loading in literature.

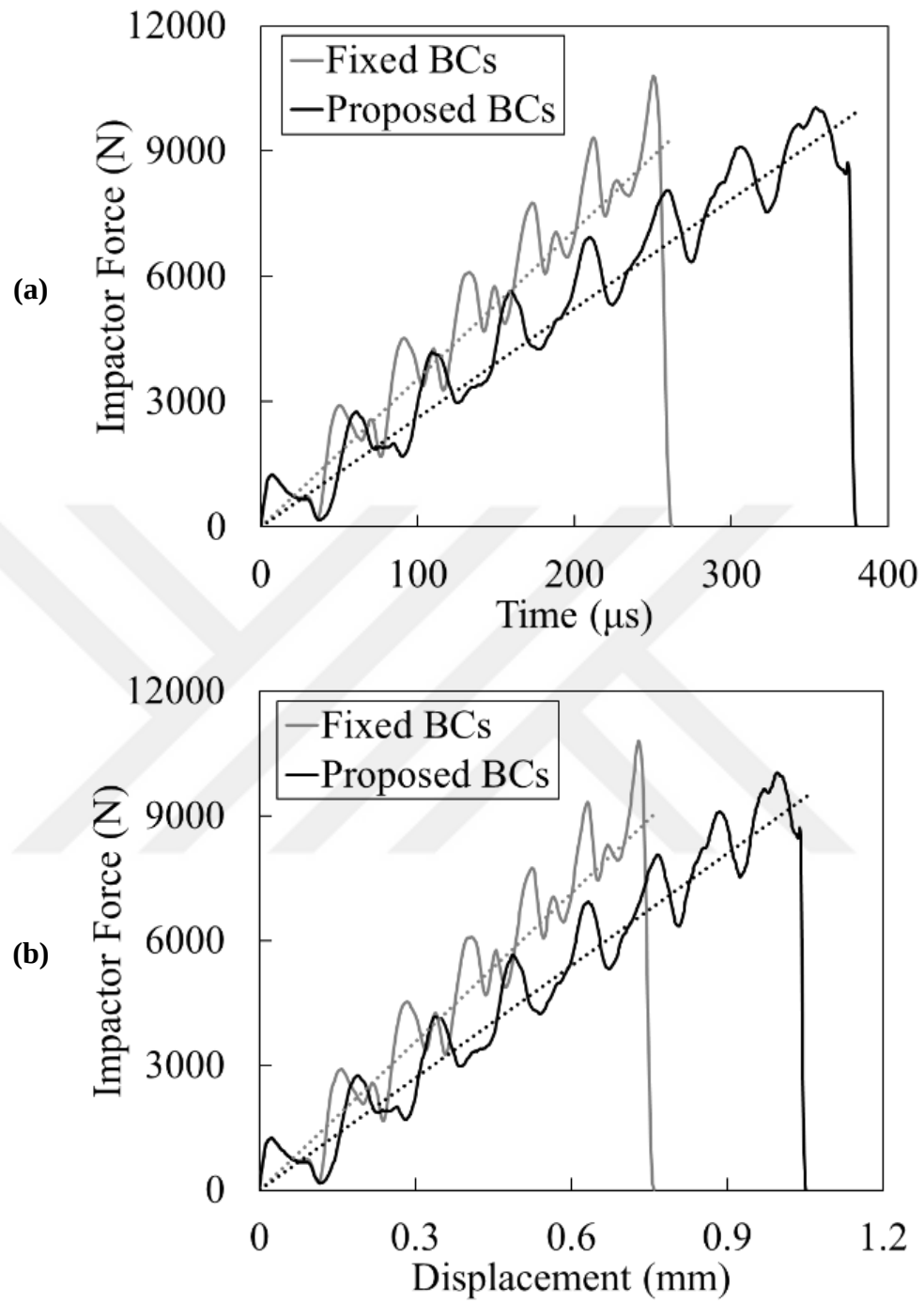


Figure 2.17. Influence of boundary conditions on the **(a)** impactor force history and **(b)** force-displacement response.

### 2.6.2 Comparison of Dynamic Failure Progression with Experiments

The failure progression captured in the *in situ* impact experiment is compared to the results obtained from the FE simulation. Figure 2.18 presents side-by-side images showing the similar damaged states of the experiment and FE simulation, focusing on the left half of the specimen. Both in the experiment and simulation, damage initiates as a diagonal matrix crack and propagates as delamination to the mid-section of the beam where the impactor collides. As delamination propagates, a secondary matrix crack initiates in front of the delamination crack tip. The first and second matrix cracks located away from the impactor approximately at 16-mm and 9-mm in experiments, 17-mm and 11-mm in simulations. The numerical results are in good agreement with the experiments in terms of damage form, sequence, and location. However, there exists a notable discrepancy in time between each image, and consequently the time elapsed for the crack to reach the mid-section of the beam. In the experiment, this time is approximately 20  $\mu\text{s}$ , whereas in the simulations, it is less than 7  $\mu\text{s}$ . This indicates that the delamination speed is much higher in simulations, which is supported by the comparison of crack tip speed curves for the experiment (Figure 2.1) and simulation (Figure 2.15). We conjecture that such inconsistency arises from the different definitions of crack tips used in the experiments and simulations, as further discussed in the following section.

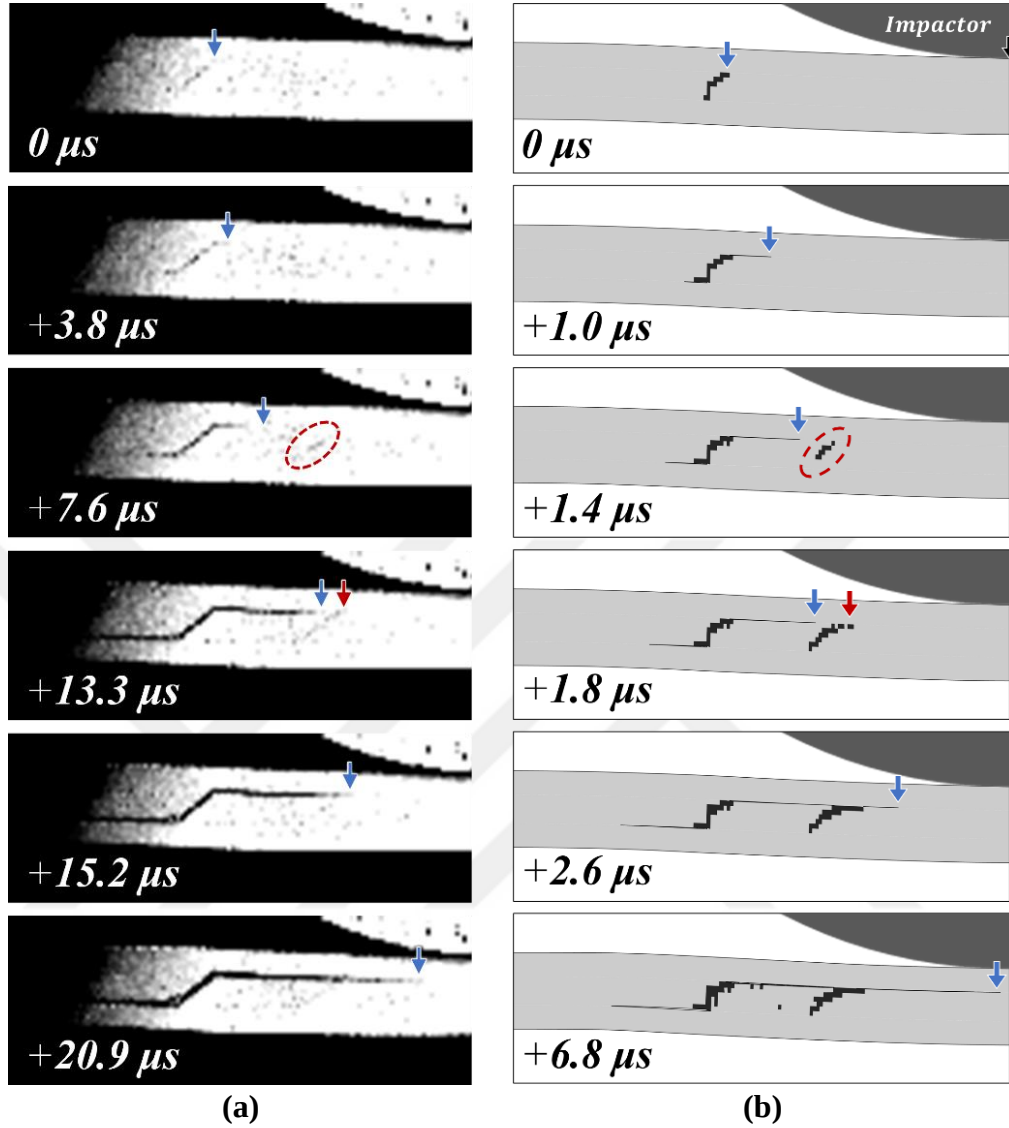


Figure 2.18. Dynamic failure progression of **(a)** reference experiment [44] and **(b)** FE simulation.

### 2.6.3 Definition of Delamination Crack Tip

The definitions of crack tips in experimental and numerical studies lack uniqueness leading to possible discrepancies. In experimental investigations, crack tip locations are typically identified using visual instruments, such as cameras or travelling microscopes, which inherently contain some degree of uncertainty [92]. On the other hand in CZM simulations, there are three distinct crack tip definitions according to

[93]. The first definition is “mathematical crack tip”, which identifies the crack tip location where the damage parameter  $d$  becomes 1 at  $\delta = \delta_c$ . The second definition identifies the location where the maximum traction at  $\delta = \delta_o$  is seen as the crack tip. The last definition is referred to as the “specified crack tip” which attains any crack opening value  $\delta$  between  $\delta_o < \delta < \delta_c$  as the crack tip. Geubelle and Baylor [38] used the mathematical crack tip definition in a similar line-impact loading configuration but they did not compare their crack tip speed results with experiments. For the mixed-mode delamination, Gozluklu et al. [57] proposed a specified crack tip definition when a location experiences 60% of damage ( $d = 0.6$ ), which provided a satisfactory correlation with their experimental results. For the Mode-II dominated fracture, Coker et al. [60] utilized a specified crack tip definition regarding the cohesive separation, given the possibility of the crack tip being physically hidden. They defined the crack tip at a location where  $\delta$  becomes  $5\delta_o$ .

The substantial discrepancy in crack tip speeds between experimental and mathematical simulations led us to investigate a numerical crack tip definition consistent with the experiment. In this regard, we visually examined the normal and shear components of the delamination crack face displacements at the upper interface. The initial shear mode crack face displacement at  $3 \mu s$ , and the subsequent normal opening at  $6 \mu s$  are depicted in Figure 2.19(a). Histories of normal and shear components of crack face displacements were extracted from six different node pairs along the composite beam and are presented in Figure 2.19(b). Initially, the delamination crack forms purely in shear mode, and shear displacements progressively increase with a negligible amount of normal opening until the mathematical delamination tip reaches the impactor as a result of sudden delamination growth at intersonic speeds. Normal openings became apparent  $\sim 5 \mu s$  after the crack initiation, with slower propagation speeds than the shear component.

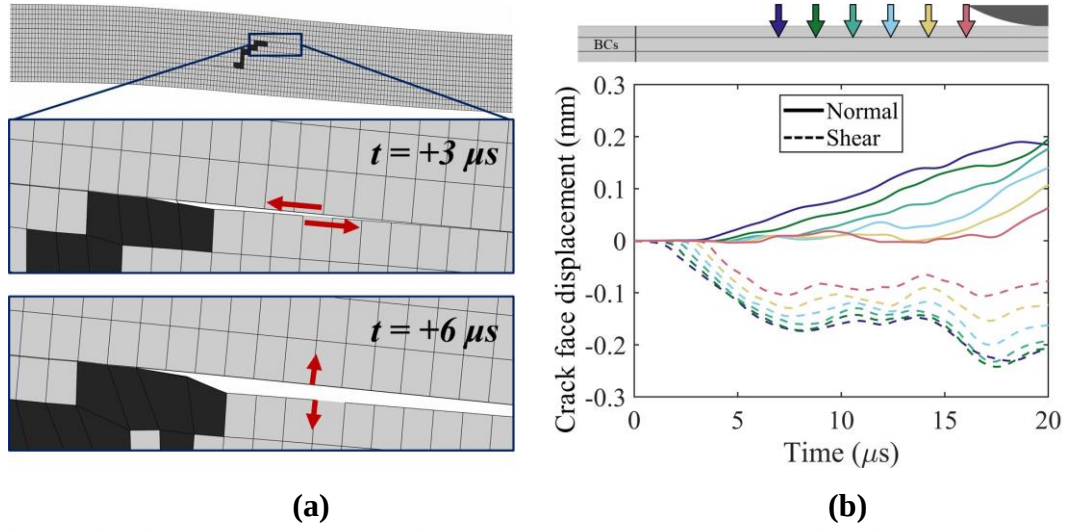


Figure 2.19. **(a)** Close-up view of the delamination showing the sequence of crack face displacements, starting with shear followed by normal opening, and **(b)** crack face displacement histories of the normal and shear components for the node pairs indicated with markers (marker colours match with the data curve colours).

From these investigations, we also conjecture that the shear-dominated delamination crack tip may be physically concealed due to the limited amount of normal opening ( $\delta_n$ ) in experiments. Therefore, we aimed to incorporate the effect of the  $\delta_n$  component in the numerical crack tip definition to facilitate coherent comparisons with experiments. In reference experiments, crack tip identification relies on the camera resolution as the visibility of a crack depends on the opening distance that exceeds the minimum pixel size. Considering the resolution of HSC used in experiments ( $128 \times 48 \text{ px}^2$ ), a single pixel corresponds to  $\sim 0.1 \text{ mm}$ . Consequently, we propose a “opening crack tip” definition that takes into account the resolution of the visual instrument employed in the experiments, considering  $\delta_n$  to be  $0.1 \text{ mm}$  between interfaces.

Delamination crack tip position histories and crack tip speeds are plotted for the reference experiment, and FEA using both mathematical and proposed “opening crack tip” definitions in Figure 2.20. Delamination initiation positions of the experiment and FE simulations are almost identical, but the mathematical crack tip reaches the region underneath the impactor less than  $5 \mu s$ , opening crack tip about  $15 \mu s$ , and experiment about  $25 \mu s$ , as shown in Figure 2.20(a). The crack tip speeds

obtained from the experiment vary between 250-1000 m/s, whereas the FE simulation with mathematical crack tip definition shows intersonic speeds of 500 to 8000 m/s throughout its propagation, as shown Figure 2.20(b). However, when the opening crack tip definition is employed in FE simulations, crack tip speeds slow down to the sub-Rayleigh speeds vary between 490-1500 m/s, which agrees better with the experimental results. Although opening crack tip speeds differ more at the delamination initiation and completion regions, the similarity in the data and the shape of the curves, which display three peaks and two valleys in between these regions, gives a strong impression that the proposed crack tip definition is more suitable for comparing with the crack tip data obtained from experiments.

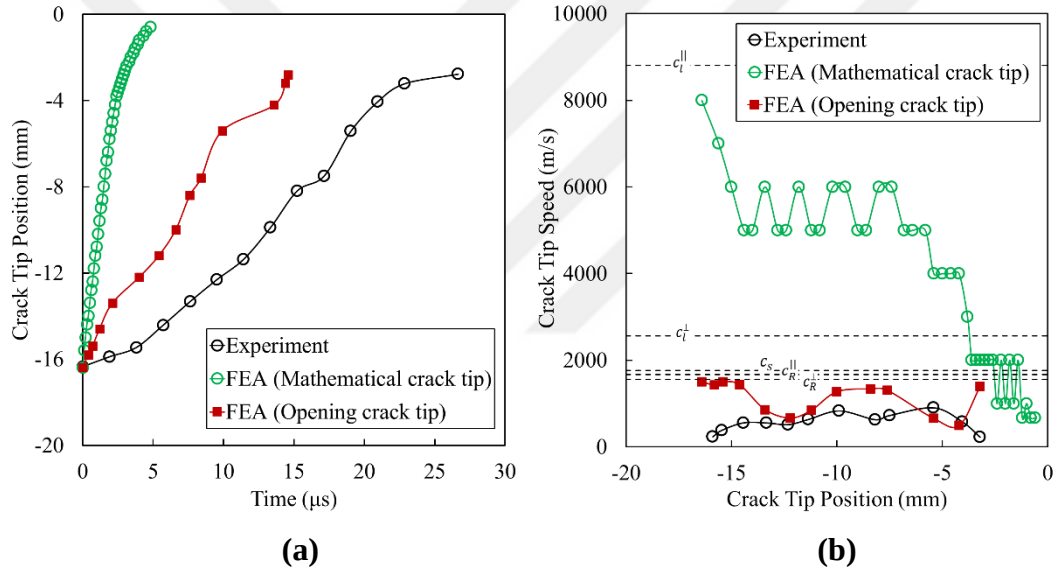


Figure 2.20. (a) Crack tip position history and (b) crack tip speeds along the beam from experiment and FE simulation.

#### 2.6.4 Effect of Varying Fracture Toughness and COF on Delamination Dynamics

Dissipative processes, such as microcracking, can lead to effective fracture toughness values that differ from those obtained from standard characterization tests. In the literature, it has been suggested that the presence of microcracks may increase the effective toughness [94,95], and decrease the effective wave speed and thus the

crack speed through reduced stiffness of the material in the vicinity of microcracks [96]. In the reference experimental study of our problem, it was reported that numerous hairline cracks are formed along the delamination interface, as presented in Figure 2.1(d) also. Although these microscopic cracks could not be captured with our mesoscale FE model, their apparent effect is reflected in the simulations through an increase in the shear mode interface toughness  $G_{IIc}$ . Figure 2.21(a) demonstrates the influence of increasing  $G_{IIc}$  to 1.5 and 2.5 times its base value on the global response of the laminate and delamination crack tip speeds. The global response of the beam did not show a significant difference; however, the crack tip speeds consistently slowed down with increasing  $G_{IIc}$ . Specifically, increasing  $G_{IIc}$  to 2.5 times its base value decreases the crack tip speed by 33%, except for the compressive region underneath the impactor where crack speeds converge to similar values.

The other phenomenon reported in the experimental study was debris formation along the delamination interfaces, as presented in Figure 2.1(d). The effect of debris formation on the coefficient of friction (COF) remains uncertain: it may lead to the behavior of interface contact as almost frictionless, preventing crack closure and enabling interfaces to slide, or as an extremely rough contact, inducing mechanical interlocking. Therefore, we investigate its effect numerically by defining frictionless and very high COF between the contact of delaminated composite layers. Figure 2.21(b) presents the influence of the COF on the global response of the beam and delamination crack tip speeds. The base COF value taken from previous research was 0.5. We performed two additional analyses, one with a COF value of 0 (frictionless), and the other with an extreme value of 10, which was selected as the upper limit of COF in [59]. The variations in COF yielded almost identical results with no apparent effect on the global response. For the model with COF equals 10, delamination initiated slower with a speed of 6000 m/s instead of 8000 m/s, as it was the case for frictionless and base models. This reduction in initial crack speeds for increased COF was also reported in the results of [59]. Except for the initiation phase, the results of the present study are found to be almost insensitive to variations in the COF due to several reasons. Firstly, the contact regions between the delaminated

composite layers form relatively late, i.e., delamination has already propagated  $\frac{3}{4}$  of its path when first contact is occurred (see Figure 2.16). Secondly, the composite under investigation has a simple layup configuration with fewer interfaces. Finally, the lack of a debris model that permanently blocks crack closure, akin to the model in [97], and its possible contact effects further contribute to the negligible effect of COF selection on the results.

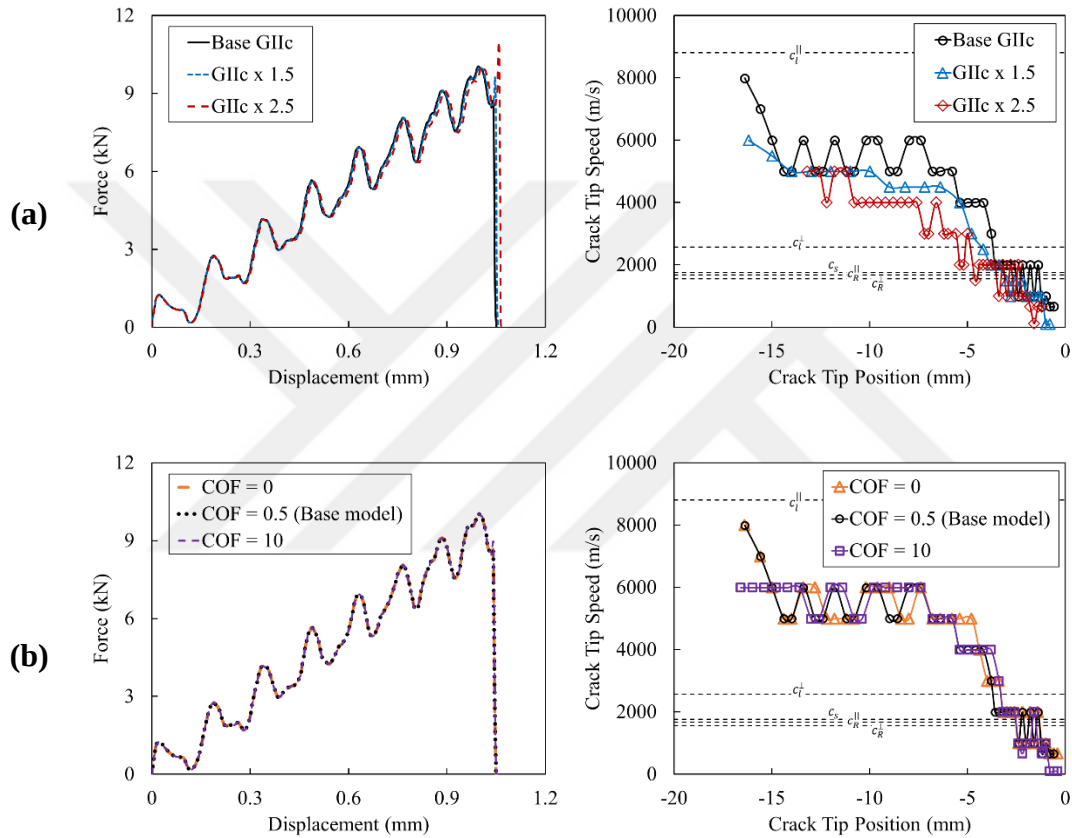


Figure 2.21. Influence of **(a)** increased shear mode fracture toughness ( $G_{IIc}$ ) and **(b)** coefficient of friction (COF) on the global force-displacement response and delamination crack tip speeds.

## 2.6.5 Cohesive and Matrix Strength Pairs

There are three possible situations for an interface encountering crack to experience:

- the crack reaches the interface and penetrates adjacent material along its original path (i.e., crack penetration) [98],
- the crack reaches the interface, deflects from its

original path, and propagates along the interface (i.e., crack kinking or deflection) [99], iii) before crack reaches the interface, it initiates debonding failure at the interface (i.e., Cook-Gordon mechanism) [100].

Regarding the present study, the reference experiments have shown that diagonal matrix cracking triggered the onset of delamination damage almost instantaneously. Such failure is consistent with the definition of crack deflection at the interfaces between two orthotropic materials, which is dictated by the interface and material strengths [98,101]. Accordingly, we aimed to determine the limiting values of the cohesive shear ( $T_{o,II}$ ) and transverse matrix ( $Y_T^{is}$ ) strength pairs, which are the decisive parameters in the present impact configuration, that would preserve the experimentally observed failure mechanisms. In this regard, a parametric analysis is conducted by investigating the damage progression of FE models with  $Y_T^{is}/T_{o,II}$  ratios of 0.95, 1.05, 1.08, 1.15, and 1.25, while keeping  $T_{o,II}$  constant. The model with  $Y_T^{is}/T_{o,II}$  ratio of 1.08 is the base model to simulate the experiments. The corresponding force-displacement curves and images depicting the damaged states are presented in Figure 2.22. Force-displacement response of the models with  $Y_T^{is}/T_{o,II}$  ratio higher than the base model follow the same behaviour until failure, whereas smaller ratios (i.e., lower  $Y_T^{is}$ ) demonstrate failure sooner.

In Figure 2.22(a), it was observed that the model with a strength ratio of 0.95 exhibited excessive matrix cracking with suppressed delamination damage (recall that damaged composite elements turn to black while thin black lines represent cohesive failure) due to the interaction of CDM and CZM [102,103]. However, the suppression of CZM resulted in the prediction of matrix cracks at the adjacent elements parallel to the upper  $0^\circ/90^\circ$  interface corresponding to a sudden load drop in the force-displacement plot. The prediction of delamination-type matrix failure together with a load drop is a consequence of employing a complete 3-D damage initiation criterion, LaRC05, which searches for possible fracture planes in all directions. In Figure 2.22(c), the model with a strength ratio of 1.05 showed a similar matrix damage pattern to the base model in Figure 2.22(b), but the damage location

shifted to the center due to the dynamic translation of stress fields [42]. Increasing the strength ratio to 1.15 in Figure 2.22(d) caused the oblique matrix crack pattern to vanish, but the matrix cracking followed by delamination sequence remained. Further increasing the ratio to 1.25 resulted in the matrix crack pattern and damage sequence being lost and demonstrated pure delamination damage observed in Figure 2.22(e). Consequently, it was determined that a  $Y_T^{is}/T_{o,II}$  ratio between 1.00 and 1.15 is appropriate for preserving the experimentally observed damage form and sequence.

The matrix cracking patterns and damage sequences do vary with changes in the matrix and cohesive strength pairs. However, these variations have no noticeable effect on the global responses due to the dominant influence of delamination on the load-bearing capacity. As a result, it was concluded that relying solely on the global load-displacement response and delamination footprints could be misleading for predicting intralaminar damages.

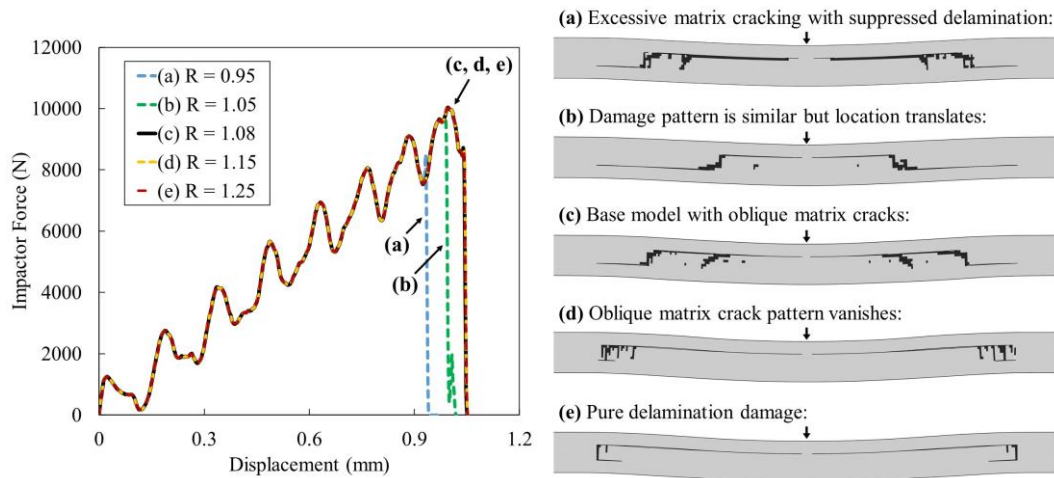


Figure 2.22. Force-displacement curves and images of damaged states for models with transverse matrix strength to cohesive shear strength ratio ( $R = Y_T^{is}/T_{o,II}$ ) of (a) 0.95, (b) 1.05, (c) 1.08, (d) 1.15 and (e) 1.25.

## 2.7 Conclusions

In this study, we have reproduced the *in situ* experimental observations on the low-velocity impact-induced damage progression in  $[0_5/90_3]_s$  CFRP beams from [44] through high-fidelity computations and gained further insights into the dynamic failure mechanisms beyond the limitations of direct observation. The simulations employ a user-implemented constitutive model featuring the LaRC05 damage initiation criterion for matrix cracking, and a built-in cohesive zone model at the  $0^\circ/90^\circ$  interfaces for delamination damage in ABAQUS/Explicit software. Our main conclusions are outlined as follows:

- The proposed boundary condition modelling approach provides a more accurate representation of the experimental conditions compared to fixed BCs. Results of the FE simulation with the proposed BCs show excellent agreement with experiments in terms of global impact response, damage initiation time, location, form, and sequence.
- Matrix cracking initiates near the  $0^\circ/90^\circ$  interface, propagates diagonally and immediately induces delamination. Simulations reveal intersonic delamination propagation, with crack tip speeds around  $\sim 5000$  m/s, accompanied by shear Mach waves emanating from the crack tip.
- A crack tip definition based on the normal component of the crack opening is introduced for consistent comparisons with experiments where only sub-Rayleigh crack growth, in the 250-1000 m/s range, was captured. The introduced crack tip definition yields consistent crack tip speeds with the experimental observations, in the 490-1500 m/s range, implying that sliding mode might be physically hidden in the experiments.
- Increasing the effective interface toughness values, motivated by microcrack formation near interfaces, led to decreasing crack tip speeds with no noticeable effect observed in the global responses, which demonstrated the potential use of delamination crack tip speeds as a benchmark for refining high-fidelity numerical simulations.
- The damage patterns and sequences vary with the matrix and cohesive strength pairs. However, these variations have no noticeable effect on the global responses. Therefore, relying solely on the global load-displacement response and post-mortem delamination footprints is shown to be misleading for high-fidelity predictions of impact-induced damage in composite laminates.

## 2.8 Publications and Presentations

The work presented in this chapter resulted in the following publications:

1. Batmaz OA, Bozkurt MO, Gurses E, Coker D. High-fidelity simulations of low-velocity impact induced matrix cracking and dynamic delamination progression in CFRP beams. *Composites Part A: Applied Science and Manufacturing* 2024;177:107960. <https://doi.org/10.1016/j.compositesa.2023.107960>.
2. Batmaz OA, Bozkurt MO, Gurses E, Coker D. Numerical Simulation of Low-Velocity Impact on  $[0_5/90_3]_S$  CFRP Beam Considering Accurate Experimental Boundary Conditions. In press, *Procedia Structural Integrity*.

The work in this chapter is presented at the following international conferences:

1. Batmaz OA, Bozkurt MO, Gurses E, Coker D. Numerical Simulation of Low-Velocity Impact on  $[0_5/90_3]_S$  CFRP Beam Considering Accurate Experimental Boundary Conditions. 3<sup>rd</sup> International Workshop on Plasticity, Damage and Fracture of Engineering Materials (IWPDF 2023), İstanbul, Türkiye, 4<sup>th</sup> – 6<sup>th</sup> October 2023.
2. Batmaz OA, Bozkurt MO, Gurses E, Coker D. Numerical Investigation of Impact-Induced Damage Progression in Composite Wind Turbine Blade Structures. 18<sup>th</sup> EAWC PhD Seminar on Wind Energy, Bruges, Belgium, 2<sup>nd</sup> – 4<sup>th</sup> November 2022.
3. Batmaz OA, Bozkurt MO, Gurses E, Coker D. Numerical Simulations of Dynamic Delamination in  $[0_5/90_3]_S$  CFRP Beams Subjected To Transverse Impact. 8<sup>th</sup> ECCOMAS Thematic Conference on the Mechanical Response of Composites (COMPOSITES 2021), Online, 22<sup>nd</sup> – 24<sup>th</sup> September 2021.

## CHAPTER 3

### **DYNAMIC DAMAGE EVOLUTION IN THICK-PLY AND THIN-PLY CFRP COMPOSITE BEAMS UNDER LOW-VELOCITY IMPACT AND QUASI-STATIC INDENTATION LOADING**

This study investigates the influence of ply-group thicknesses on low-velocity impact (LVI)-induced damages in cross-ply CFRP beams with stacking layups  $[0_4/90_4/0_2]_s$  and  $[0_2/90_2/0_2/90_2/0_2]_s$  through *in situ* LVI experiments. In addition to the LVI experiments, where high-speed photography is employed to observe the damage progression, quasi-static indentation (QSI) experiments are conducted to reduce the difficulties in monitoring the damage in the short period of impact loading. QSI experiments allow us to have magnified *in situ* observations on one free edge of the beam with a traveling digital microscope, while a high-resolution camera is employed on the opposite edge to obtain full-field strain distributions. Furthermore, the LVI experiments are numerically modeled using the finite element method in the ABAQUS/Explicit platform. An orthotropic constitutive material model, incorporating fiber and matrix damage, is implemented via a VUMAT subroutine. Delamination damage is simulated using the cohesive zone method with built-in cohesive elements inserted at the interfaces. Our experimental findings reveal that clustering a smaller number of  $90^\circ$  plies ( $[0_2/90_2/0_2/90_2/0_2]_s$ , thin-ply specimen) leads to a complex damage mechanism involving central matrix cracking followed by delamination and fiber breakage. On the other hand, clustering of a greater number of  $90^\circ$  plies ( $[0_4/90_4/0_2]_s$ , thick-ply specimen) demonstrates off-center matrix cracking followed by delamination, without any discernible fiber breakage. Numerical simulations of these experiments are in good agreement in terms of failure loads, sequences, and patterns seen in experiments. The comparison between numerical simulations and experimental observations provides evidence supporting the so-called in-situ strength concept, where employing it played a very important role not only in global response but also in failure mechanism choice.

### 3.1 Introduction

The desire for lightweight yet reliable load-carrying composite structures necessitates an understanding of the mechanisms governing the damage within these materials. A critical loading scenario leading to damage is the transverse low-velocity impact (LVI) on composites. The damage mechanism primarily responsible for the loss of stiffness during LVI events was reported to be delamination between the adjacent layers having different fiber orientations [104]. The interaction of delamination with the translaminar damages, specifically transverse matrix cracking [105,106], has been extensively studied, given the potential for these damages to trigger delamination [3,107,108]. The most apparent matrix cracking at initiation occurs within the cross-ply configurations, where the angle between fiber directions of neighboring plies is  $90^\circ$ , since the stiffness mismatch of adjacent layers is the highest. Consequently, many researchers have chosen to study damage mechanisms of cross-ply configurations [105,109–113].

Regarding damage mechanisms inside the cross-ply configurations, Parvizzi [109] and Flaggs and Kural [110] have reported that the strengths of carbon and glass fiber-reinforced plastic (FRP) laminates influenced by the number of embedded  $90^\circ$  plies between other plies. This observation has been linked to the restrictive effect of the  $0^\circ$  plies on the characteristics of  $90^\circ$  plies. Further studies have indicated that the actual strength of a laminate depends not only on the individual properties of its constituent lamina, but also on the layup configuration including the location of angled plies, the orientation of adjacent layers, and the thickness of the plies [63,109]. In the literature, this increased strength is referred to as in-situ strength.

The observed size effect, dependence of the actual strength on the number of plies clustered together with the same fiber orientations, has motivated investigations to give an explanation to this effect by experiments or numerical models [22,25,114–116]. Wisnom et al. demonstrated that stacking more plies with the same fiber orientation in a quasi-isotropic laminate type exhibits a reduction in the tensile [114] and compressive [115] strengths under uniaxial loadings. For the LVI loading,

González et al. [117] found that increasing the thickness of ply clusters, which results in decreasing the number of interfaces, leads to larger delamination areas and reduces the resistance of composites to LVI damage by decreasing the delamination threshold and peak loads. Sun and Hallett [22] also reported the increased delamination threshold load with delayed delamination propagation in thin-ply specimens. Amacher et al. [116] observed a change in predominant failure mode from delamination with limited transverse cracking and fiber breakage to a large translaminar crack along the width, as the ply-cluster thickness is decreased. Although the thin-ply laminates showed a brittle failure, they exhibited delayed damage growth with higher resistance to impact damage than the thick-ply specimens. It should be noted that, in these studies, the experimental investigations and comparisons with simulations have remained limited to global impact responses and the final damaged states since direct observation of damage during LVIs is challenging.

To address the complications of the observation of damage during the impact events, many researchers have adopted quasi-static indentation (QSI) experiments to qualitatively identify the damage mechanisms occurring during low-velocity impact (LVI) events [23,44,46,47,118–120] since the global impact response in terms of failure load, deflection at failure, and failure modes have been shown to correlate well with the quasi-static test configuration. In fact, Olsson [120] showed that the ratio of impactor-coupon masses governs the global impact response, where the impact duration of large-mass impactors on small coupons is long enough to exhibit a deformation mode similar to the quasi-static loading. Furthermore, Bozkurt and Coker [44] demonstrated that the similarities between LVI and QSI experiments are not limited to the global response but extend to the dynamic characteristics including pre-damage deformation fields and *in situ* delamination crack tip speeds. Consequently, utilizing QSI experiments to capture damage mechanisms that may be experienced during LVI events has been found to be adequate.

The appearance of size effect under LVI loadings, with thin-ply laminates exhibiting a significant delay of the damage compared to thick-ply laminates, has been further

investigated through QSI experiments and simulations in the literature [23,83,121]. Fuoss et al. [121] reported that ply clustering degrades the impact damage resistance of laminates, leading to an increased delamination area. Abisset et al. [23] performed interrupted QSI experiments on thick-ply and thin-ply composite plates, assessing the damage evolution through non-destructive techniques. Sun et al. [83] numerically modeled these QSI experiments using high-fidelity FE models. They captured the sequence of matrix cracking-induced delamination and showed the influence of ply-group thickness on interlaminar shear stress distributions and delamination load levels.

The interest in utilizing thin-ply laminates is evident, given their advantageous effect in delaying the onset of damage in the 90° plies. However, companies that manufacture composite components, mainly the aeronautics industry, are currently trending in the opposite direction, aiming to reduce manufacturing costs through the utilization of thicker plies [113]. The use of thicker is also attributed to their increased compression-after-impact strength by delaying the buckling of the total laminate due to experiencing excessive delamination rather than translaminar damage under impact [122]. Considering the significance of LVI causing undetectable damages in composites, these facts support the importance of conducting a detailed study on the effect of the thickness of ply clusters on the impact-induced damage mechanisms.

In this study, we performed LVI experiments on the CFRP beam specimens with two different cross-ply layup configurations, namely [0<sub>4</sub>/90<sub>4</sub>/0<sub>2</sub>]<sub>s</sub> and [0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>]<sub>s</sub>. These configurations have an equal number of 0° and 90° plies but vary in the number of plies clustered together within the laminate, thus enabling us to revisit the influence of ply clustering on the LVI-induced matrix cracking and delamination phenomenon. High-speed photography was utilized to observe the complete sequence of damage progression. In addition to LVI, we conducted quasi-static indentation (QSI) experiments to qualitatively identify the micro-scale damage mechanisms experienced during LVI events. These QSI experiments provided us with magnified/high-resolution *in situ* observations on one edge of the beam using a

traveling digital microscope, while simultaneously a high-resolution camera was utilized on the opposite edge to measure full-field strain distributions through the digital image correlation method. Then, we carried out numerical simulations of the LVI experiments using the finite element method in ABAQUS/Explicit platform. To account for fiber breakage and matrix cracking, a three-dimensional continuum damage mechanics approach, incorporating the maximum stress and LaRC05 matrix damage initiation criteria, is employed. The delamination damage is simulated using the cohesive zone method with built-in cohesive elements inserted at the interfaces. In conjunction with the numerical simulations, the *in situ* monitoring of damages allowed us to further elucidate the damage mechanisms seen in cross-ply laminates under LVI loading.

The significant contribution of this study lies in its experimental and numerical observations presenting the effect of the thickness of ply groups/clusters on the damage mechanisms. Clustering a smaller number of 90° plies led to a complex damage mechanism involving central matrix cracking followed by delamination and fiber breakage, whereas clustering of a greater number of 90° plies demonstrated off-center matrix cracking followed by delamination without any discernible fiber breakage. The evidence supporting the *in-situ* strength concept, which asserts that the resistance to matrix cracking increases as the ply-thickness decreases, is presented for the LVI loadings. Employing this concept in simulations played a very important role not only in global response but also in failure mechanism choice.

This chapter is organized as follows: In the subsequent section, we provide the experimental method including descriptions of materials, specimens, experimental setups and procedures. Section 3.3 presents the numerical method of the high-fidelity FE model, composite ply damage and delamination models. This section highly overlaps the respective numerical method section shown in Section 2.3, therefore, it can be regarded as an overview of Section 2.3 while highlighting differences. Section 3.4 presents the observations from LVI and QSI experiments for both laminate configurations. Section 3.5 presents the numerical results of the high-fidelity FE model in terms of the global responses, *in situ* damage progression and damage

mechanisms. These results are discussed further in Section 3.6 and the experimental and numerical observations are compared. Finally, Section 3.7 concludes the study, summarizing the key findings and highlighting the contributions.

## **3.2 Experimental Method**

Low-velocity impact (LVI) experiments are conducted on CFRP composite beams of two different layups:  $[0_4/90_4/0_2]_s$  and  $[0_2/90_2/0_2/90_2/0_2]_s$ . To reduce the complications in monitoring the damage in the short period of impact and to qualitatively identify the damage mechanisms observed during LVI events, quasi-static indentation (QSI) experiments are performed. QSI experiments allowed us to obtain magnified *in situ* observations and to measure full-field strain distributions using the digital image correlation (DIC) method. Details of the material, specimens, experiment setups and layouts of employed instruments are described in the following sections.

### **3.2.1 Materials and Specimens**

The composite specimens are manufactured from Hexcel HexPly 913/HTA unidirectional prepreg carbon fibers and epoxy by hand-layup technique. The curing is processed at  $80\pm 5^\circ$  for 30 minutes and  $125\pm 5^\circ$  for 60 minutes at an ambient pressure of 4 bars in the autoclave. Manufactured plates are cut into beam specimens of in-plane sizes of 17 mm $\times$ 150 mm with a water jet. Thicknesses are approximately 2.5 mm for all specimens.

For the LVI experiments one, and for the QSI experiments both sides of the composite beams are polished with 400, 800, 1200, 2400, and 4000-grit SiC grinding papers using Presi Minitech 233 polishing machine at 300 rpm. Polished surfaces of the beams are examined visually before tests using the Huvitz HDS-5800 digital microscope to validate that there is no visible void or manufacturing defect.

Microscope photographs showing the layup sequences of untested  $[0_4/90_4/0_2]_s$  and  $[0_2/90_2/0_2/90_2/0_2]_s$  specimens are depicted in Figure 3.1. These layups have an equal number of  $0^\circ$  and  $90^\circ$  plies in total but differ in the number of plies clustered together inside the laminate, thus enabling us to revisit the influence of ply-group thicknesses on the LVI-induced damages.

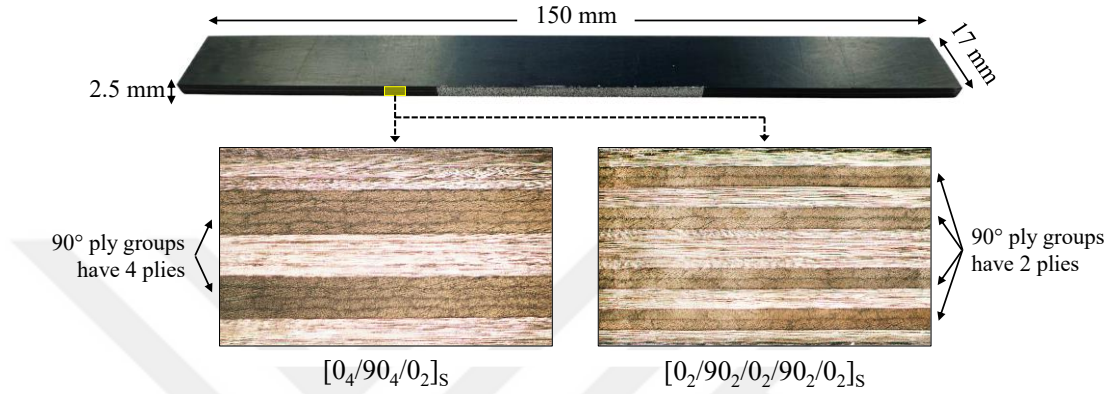


Figure 3.1. Specimen geometry and microscope photographs showing layup sequences.

### 3.2.2 Experiment Setups

The identical impactor (named as indenter in the QSI experiments) and boundary configuration are utilized in both LVI and QSI experiments, except that loading is applied quasi-statically using an electromechanical testing machine in QSI experiments. The specimen, impactor/indenter, and clamped boundary configuration employed for these experiments are depicted in Figure 3.2(a). In this configuration, a semi-cylindrical impactor/indenter made of steel with a radius of 20 mm was employed to load specimens in a uniform two-dimensional setting. This line-loading approach allowed us to observe the failure mechanisms from the visible edge of the specimens. The composite specimens were positioned under the impactor/indenter such that they came into contact with the center of the beam specimens. The specimens were fixed from both ends by clamping their upper and lower surfaces with steel plates. These steel plates were squeezed by four M6 bolts that were

preloaded to 5 Nm using a torque meter. The unsupported span length between steel plates was set to 50 mm.

The LVI experiment setup is presented in Figure 3.2(b). This is the same setup that was described in the previous chapter, thus, its relevant properties are described in this section for completeness. During the impact event, the polished visible edge of the specimen was monitored with the Photron SA5 ultra-high-speed camera. The LVI experiments were captured at 25,000 frames per second, which corresponds to the interframe time of 40 microseconds between each photograph. The high-speed camera was equipped with a 100 mm lens and 100 mm extension ring to capture a region of  $41 \times 12 \text{ mm}^2$  at  $1024 \times 296$  pixels resolution. The camera was positioned approximately 450 mm away from the visible edge of the specimen. Shutter speed and diaphragm opening were set to the highest possible values for the available lighting. To increase the visibility during high-speed imaging, two Dedocool COOLH lighting systems equipped with 250 W Osram HLX Tungsten lamps were utilized to illuminate the edge of the specimen. The whole drop-weight assembly that slides down on steel guiding shafts with the impactor weighed 1865 grams.

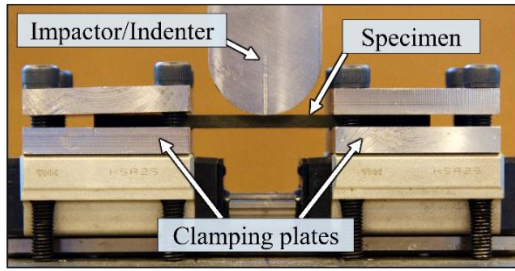
The impactor position data acquired from the high-speed camera images were utilized to determine the velocities, which were then used to calculate the kinetic energies of the impactor just prior to the collision. Accordingly, the kinetic energies at impact were calculated as 7.8 and 11.8 Joules for  $[0_4/90_4/0_2]_s$  and  $[0_2/90_2/0_2/90_2/0_2]_s$  specimens, respectively. When the  $[0_2/90_2/0_2/90_2/0_2]_s$  specimen was impacted with the same energy as the  $[0_4/90_4/0_2]_s$  specimen, no observable damage was observed in the trial experiments. Consequently, the energy level was increased to a level that resulted in significant damage. Since our focus was to capture the damage initiation patterns rather than determine the damage resistances to impact, the specimens were impacted with energy levels above their critical thresholds, although the energy levels varied for different layups.

The QSI experiment setup is presented in Figure 3.2(c). The setup employed the same clamped boundary configuration and impactor/indenter used in the LVI

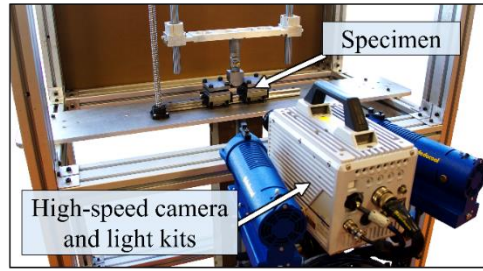
experiments, depicted in Figure 3.2(a). But this time, loading was applied quasi-statically using the Shimadzu AGS-J electromechanical testing machine equipped with a 10 kN load cell. Loading was applied quasi-statically in displacement control at the slowest crosshead speed of the machine of 0.5 mm/min. The corresponding indenter force and crosshead displacement data were obtained from the testing machine at 20 Hz. During the experiments, the visible edges of the specimen were monitored using a high-resolution Canon EOS-1D digital camera on the front side, and a Huvitz HDS-2520Z traveling microscope on the rear side.

During the QSI experiments, the visible edges of the specimen were monitored using a high-resolution digital camera of 5184×3456 pixels resolution on one side, and a traveling microscope of 1600×1200 pixels resolution on the opposite side. We painted the side where the high-resolution camera was employed with white and black spray painters to introduce a speckle pattern for facilitating full-field strain measurements using the digital image correlation (DIC) method. DIC analyses were performed using an open-source software named Ncorr [123]. The subset radius was selected as 15 pixels with no overlapping pixels. The number of threads and the subset spacing were set to 1 and 10, respectively. The traveling microscope was positioned approximately 27 mm away from the specimen edge. For  $[0_2/90_2/0_2/90_2/0_2]_s$  and  $[0_4/90_4/0_2]_s$  specimens, the microscope magnifications of 25× and 50× were utilized, respectively, to capture regions of  $15.3 \times 11.5 \text{ mm}^2$  and  $7.4 \times 5.6 \text{ mm}^2$ . Microscope and high-resolution camera images were acquired at a rate of 0.5 Hz and 1 Hz, respectively. Illumination on the microscope side was handled with the integrated light source of the microscope.

**(A) Specimen & BCs configuration**



**(B) Low-velocity impact setup**



**(C) Quasi-static indentation setup**

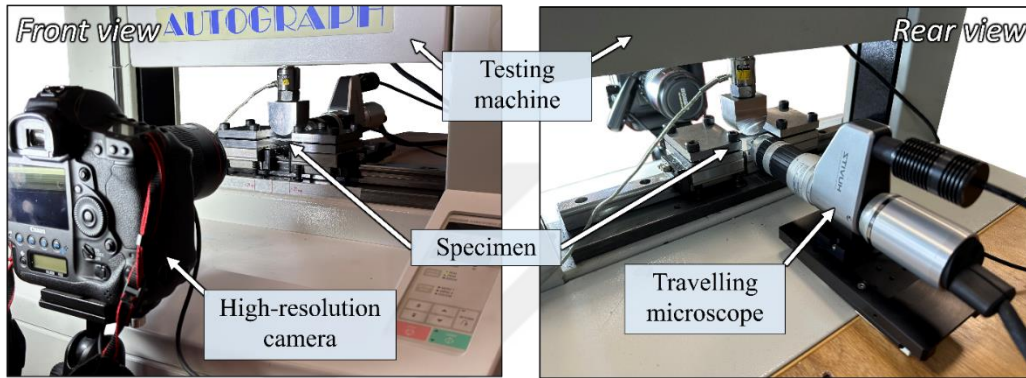


Figure 3.2. **(a)** Specimen, impactor/indenter and clamped boundary configuration in low-velocity impact (LVI) and quasi-static indentation (QSI) experiments, **(b)** the non-standard LVI setup including ultra-high-speed camera and lighting systems, **(c)** front view and rear view of the QSI setup including a high-resolution digital camera and a traveling microscope.

### 3.3 Numerical Method

The objectives of numerical simulations are to reproduce the experimental observations through high-fidelity computations, and gain further insights into the damage mechanisms beyond the limitations of experimental observations. In this regard, a 3D finite element (FE) model is constructed in ABAQUS/Explicit to simulate the low-velocity line impact experiments. Details of the computational modeling methodology are provided in the following sections.

### 3.3.1 High-fidelity Finite Element Model

A schematical illustration of the FE model is presented in Figure 3.3. The semi-cylindrical steel impactor is idealized as an analytical rigid body with a mass of 1.865 kg. It is positioned just above the beam providing that the initial contact happens right at the mid-span of the composite beam. To simulate the free fall, an initial downward velocity measured in the experiments, 3.56 m/s, is assigned to the impactor. The composite beam specimen is modeled as a three-dimensional deformable body. To account for large deflections that result in stiffness changes, the nonlinear geometry option is activated.

Representative meshed sections of the composite laminates are depicted in Figure 3.3. For the ply material, 8-noded linear brick elements with reduced integration, enhanced hourglass control, and second-order accuracy (denoted as C3D8R in the ABAQUS library) are employed. An in-plane mesh size of  $0.2 \times 0.2 \text{ mm}^2$  with an element thickness of 0.125 mm, which corresponds to the mesoscale approach [81,82] of modeling each ply with a single element, is capable of capturing the desired level of accuracy while satisfying the stability criteria for ensuring energy dissipation during damage process. For the purpose of modeling delamination, 8-noded three-dimensional cohesive elements (denoted as COH3D8 in the ABAQUS library) are inserted only at  $0^\circ/90^\circ$  interfaces. Consequently, there are four and eight layers of cohesive elements in the thick-ply and thin-ply models, respectively. A finite thickness of 5  $\mu\text{m}$  is assigned to cohesive elements.

As the specimen was clamped along 25 mm from both ends in experiments, a straightforward approach could be idealizing these experimental boundary conditions as fixed supports. However, in Chapter 2, detailed comparisons of experimental and computational deformation and strain fields reveals that such rigid constraining led to a stiffer flexural response, which ultimately influences the global impact response. To minimize such unrealistic effects, we adopt the BCs modelling approach proposed in Chapter 2, which replicates the experiment boundaries through the assembly of spring elements at the corresponding boundary nodes. The

spring constants for the top and bottom BCs surfaces are determined respectively as  $k_{h,t} = 26$ ,  $k_{v,t}^- = 33$  kN/mm, and  $k_{h,b} = 38$ ,  $k_{v,b}^- = 33$ ,  $k_{v,b}^+ = 2159 \times 10^4$  kN/mm for the specimens under investigation in this chapter.

Contact and friction models are defined between the steel impactor and composite beam, as well as between composite layers in case self-contact occurs. The contact algorithms and friction models provided by Abaqus [61] are employed for this purpose. Self-contact between composite layers is simulated by the general contact algorithm with the penalty contact method, excluding the impactor-composite beam contact pair. The surface-to-surface contact type with penalty contact method is employed for the contact between the steel impactor and composite beam to increase computational efficiency by defining the contact pair in advance. Friction is included at all contacting surfaces and interfaces by employing the built-in Coulomb's friction model with a Coefficient of Friction (COF) of 0.3 for the contact between the steel impactor and composite [24,42,83–85] and 0.5 in the case of self-contact of composite layers [24,86].

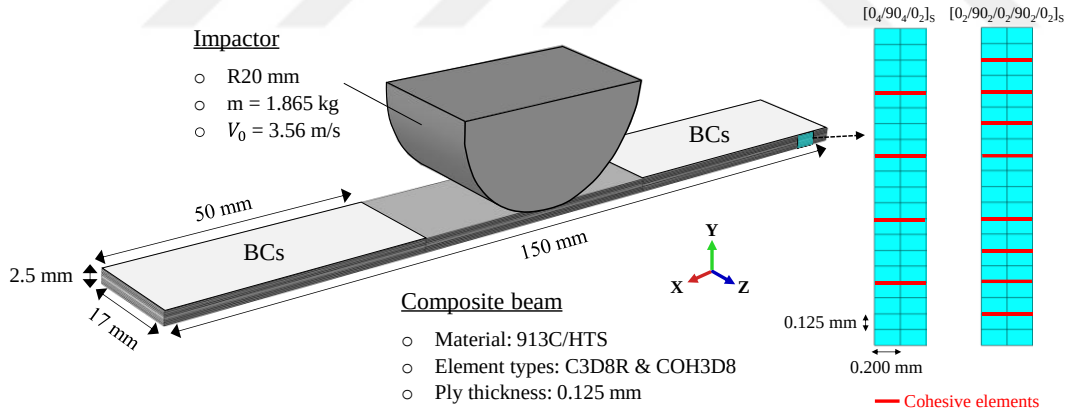


Figure 3.3. A schematical illustration of the FE model and representative meshed sections for the  $[0_4/90_4/0_2]_s$  and  $[0_2/90_2/0_2/90_2/0_2]_s$  laminates.

The elastic and strength-related properties of Hexcel 913C/HTS unidirectional (UD) prepreg used in the simulations are provided in Table 3.1. The density of the composite material  $\rho$  is reported as  $1780 \text{ kg/m}^3$  [42,44]. Within the explicit FE framework, it is necessary to assign density to all elements, including cohesive

elements. In this study, we assume that the interface density is equal to the density of the prepreg material. Notably, the cohesive density, denoted as  $\rho_{coh}$ , is defined as the mass per unit area using the formula  $\rho_{coh} = \rho t_{coh}$  where  $t_{coh}$  represents the geometrical thickness of the cohesive elements [61].

Table 3.1 Material properties used in simulations.

|                      |                                                                                                                                                                                    |
|----------------------|------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| Density              | $\rho = 1780 \text{ kg/m}^3$                                                                                                                                                       |
| Elastic properties   | $E_1 = 135 \text{ GPa}$ , $E_2 = E_3 = 9.2 \text{ GPa}$ ,<br>$\nu_{12} = \nu_{13} = 0.3$ , $\nu_{23} = 0.45$ ,<br>$G_{12} = G_{13} = 5.5 \text{ GPa}$ , $G_{23} = 4.5 \text{ GPa}$ |
| Ply strength         | $X_T = 2207 \text{ MPa}$ , $X_C = 1500 \text{ MPa}$<br>$Y_T = Z_T = 60 \text{ MPa}$ , $Y_C = Z_C = 205 \text{ MPa}$ ,<br>$S_L = 62 \text{ MPa}$                                    |
| Interface strength   | $T_{o,I} = 87.6 \text{ MPa}$ , $T_{o,II} = T_{o,III} = 83.7 \text{ MPa}$                                                                                                           |
| Fracture toughness   | $G_{I,c} = 260 \text{ N/m}$ , $G_{II,c} = G_{III,c} = 840 \text{ N/m}$                                                                                                             |
| Penalty stiffness    | $K_N = 2.88 \times 10^{14} \text{ N/m}^3$ , $K_S = 8.14 \times 10^{13} \text{ N/m}^3$                                                                                              |
| Mixed-mode parameter | $\eta = 1.45$                                                                                                                                                                      |

Laminae exhibit increased strength when constrained by laminae with different fiber orientations, compared to the strength of the unidirectional laminates [109,110]. This increased strength is referred to as “in-situ strength” and is influenced by various factors: *i*) the positioning of the sublaminates (or ply, outer or embedded), *ii*) the orientation of the adjacent plies, and *iii*) the sublaminates (or ply) thickness. In our case, the 90° sublaminates where the damage occurs is categorized as a thick-embedded laminate.

Camanho et al. [63] derived the in-situ transverse matrix strength relation as  $Y_T^{is} = 1.12\sqrt{2}Y_T$  for the thick-embedded laminates. The same study also provides the in-situ shear strength relationship for thick plies, taking into account shear non-linearity as,

$$S_L^{is} = \sqrt{\frac{(1 + \beta \phi G_{12}^2)^{1/2} - 1}{3\beta G_{12}}} \quad (3.1)$$

where a recommended value for  $\beta$  equals to  $3.6E-08 \text{ MPa}^{-3}$  and  $\phi$  can be computed through the relation  $\phi = 12(S_L)^2/G_{12} + 18\beta(S_L)^4$ . For the transverse shear strength, Catalanotti [64] provided the in-situ relation as,

$$S_T^{is} = \frac{(2 \sin^2 \alpha_0 - 1)}{\eta_L \sin 2\alpha_0} S_L^{is} \quad (3.2)$$

where  $\eta_L$  is the internal friction parameter in the longitudinal (fiber) direction. Experimental studies on composites with similar fiber and resin systems have reported a value of 0.082 for  $\eta_L$  [65,66]. Therefore, in this study, we adopt the same value of 0.082. The friction parameter in the transverse direction,  $\eta_T$ , is calculated by the relation  $\eta_T = -1/\tan(2\alpha_0)$  where  $\alpha_0$  is the fracture angle under pure transverse compression tests which was consistently reported as  $53^\circ$  in the literature [62,64,66,67].

### 3.3.2 Damage Models

The numerical methodology is employed to model two different types of damage observed in experiments: translaminar damage including fiber breakage and matrix cracking within the  $0^\circ$  and  $90^\circ$  plies, and delamination failure at the  $0^\circ/90^\circ$  interfaces. To capture translaminar damage initiation and progression, a continuum damage model (CDM)-based orthotropic constitutive material description is implemented into ABAQUS via a VUMAT subroutine. On the other hand, the built-in cohesive zone method (CZM) with the mixed-mode cohesive model is used to simulate delamination through the use of cohesive elements. Detailed formulations of the damage models are provided in the following sections.

### 3.3.2.1 Intralaminar Damage

Figure 3.4 depicts the bilinear equivalent stress ( $\sigma_{eq}$ ) vs. equivalent displacement ( $\delta_{eq}$ ) relation that an integration point obeys during complete failure. The equivalent displacement and the corresponding equivalent stress governing the progressive tensile fiber damage mode are defined as,

$$\delta_{eq}^F = L^c |\varepsilon_{11}| \quad (3.3)$$

$$\sigma_{eq}^F = |\sigma_{11}| \quad (3.4)$$

where  $L^c$  is the characteristic length of the finite element. For the matrix damage modes, the equivalent displacement and the corresponding equivalent stress are defined as,

$$\delta_{eq}^M = L^c \sqrt{\langle \pm \varepsilon_{22} \rangle^2 + \langle \pm \varepsilon_{33} \rangle^2 + \varepsilon_{12}^2 + \varepsilon_{13}^2 + \varepsilon_{23}^2} \quad (3.5)$$

$$\sigma_{eq}^M = \frac{\langle \pm \sigma_{22} \rangle \langle \pm \varepsilon_{22} \rangle + \langle \pm \sigma_{33} \rangle \langle \pm \varepsilon_{33} \rangle + \tau_{12} \varepsilon_{12} + \tau_{13} \varepsilon_{13} + \tau_{23} \varepsilon_{23}}{\delta_{eq}/L^c} \quad (3.6)$$

The material response is assumed to be linear until damage initiation (point A in Figure 3.4) which is either predicted by the maximum stress criterion [124] given in Equation (3.7) for the fiber damage, or by the LaRC05 failure criterion [62] given in Equation (3.8) for the matrix damage modes. Damage initiates when the equations become unity.

$$FI^F = \frac{\sigma_{11}}{X_T} \quad (3.7)$$

$$FI^M = \left( \frac{\tau_T}{S_T - \eta_T \sigma_N} \right)^2 + \left( \frac{\tau_L}{S_L - \eta_L \sigma_N} \right)^2 + \left( \frac{\langle \sigma_N \rangle_+}{Y_T} \right)^2 \quad (3.8)$$

$\eta_L$  and  $\eta_T$  are internal friction parameters that contribute to the initiation of damage by either strengthening or weakening the material depending on the sign of the acting normal traction at the corresponding fracture plane.  $\langle \cdot \rangle_+$  is the Macauley bracket

which returns either zero or the enclosed value depending on whether the enclosed value is negative or positive. The traction components acting on the fracture plane ( $\tau_T, \tau_L, \sigma_N$ ) are obtained by stress transformation relations given as,

$$\tau_T = -\frac{\sigma_2 - \sigma_3}{2} \sin(2\theta) + \tau_{23} \cos(2\theta) \quad (3.9)$$

$$\tau_L = \tau_{12} \cos(\theta) + \tau_{31} \sin(\theta) \quad (3.10)$$

$$\sigma_N = \frac{\sigma_2 + \sigma_3}{2} + \frac{\sigma_2 - \sigma_3}{2} \cos(2\theta) + \tau_{23} \sin(2\theta) \quad (3.11)$$

where  $\theta$  is the critical plane angle defined as the angle returning the maximum  $FI^M$  over the range of  $[0^\circ, 180^\circ]$ .

Following the damage initiation, the damage in the material progresses by degrading the stiffness of the elements following the continuum-damage modeling approach [73]. The damage progresses by linearly degrading longitudinal or transverse and shear moduli of any flagged element by the damage variable  $d$ , which varies from 0 (undamaged) to 1 (complete damage) along the line A-C in Figure 3.4. Here, the 3-D damaged compliance tensor is given in Equation (3.12) [74].

$$\begin{bmatrix} \frac{1}{E_{11}(1-d_f)} & -\frac{\nu_{21}}{E_{11}} & -\frac{\nu_{31}}{E_{33}} & 0 & 0 & 0 \\ -\frac{\nu_{12}}{E_{11}} & \frac{1}{E_{22}(1-d_m)} & -\frac{\nu_{32}}{E_{33}} & 0 & 0 & 0 \\ -\frac{\nu_{13}}{E_{11}} & -\frac{\nu_{23}}{E_{22}} & \frac{1}{E_{33}(1-d_m)} & 0 & 0 & 0 \\ 0 & 0 & 0 & \frac{1}{2G_{12}(1-d_s)} & 0 & 0 \\ 0 & 0 & 0 & 0 & \frac{1}{2G_{23}(1-d_s)} & 0 \\ 0 & 0 & 0 & 0 & 0 & \frac{1}{2G_{13}(1-d_s)} \end{bmatrix} \quad (3.12)$$

where the fiber and matrix damage variables,  $d_f$  and  $d_m$ , vary from 0 (undamaged) to 1 (complete damage) along the line A-C in Figure 3.4, and the shear damage variable becomes  $d_s = d_m$  since there is no distinction between tension and compression failure in LaRC05. The explicit formula for  $d$  reads as,

$$d = \frac{\delta_{eq}^f (\delta_{eq} - \delta_{eq}^0)}{\delta_{eq} (\delta_{eq}^f - \delta_{eq}^0)} \quad (3.13)$$

where  $\delta_{eq}^0$ ,  $\delta_{eq}$  and  $\delta_{eq}^f$  are the equivalent displacements at the initiation, progression and complete damage states, respectively. This nonlinear saturation-type formula ensures the irreversibility of the damage. The equivalent displacement at the completely damaged state is calculated by equating the dissipated energy (i.e., the area under the triangle) to the fracture toughness,  $G_c$ , of the respective mode, i.e.  $\delta_{eq}^f = 2G_c/\sigma_{eq}^0$ . For fiber damage,  $G_c$  is considered equivalent to the fracture toughness associated with longitudinal failure in tension ( $G_{1+}$ ), which can be measured using compact tension tests [125]. For the matrix damage,  $G_c$  is considered equivalent to the Mode-I fracture toughness ( $G_{I,c}$ ) for a tensile matrix failure ( $\sigma_N > 0$ ), and approximated by the relation  $G_{II,c}/\cos(\alpha_0)$  [75] for a compressive matrix failure ( $\sigma_N < 0$ ). During the evolution of the damage, stiffness degradation is implemented by introducing damage variables of longitudinal  $d_1$ , transverse  $d_2$  and shear  $d_6$  into the components of compliance tensor  $S$ , as described in [73]. Element deletion has been considered for the fiber breakage when  $d_1$  becomes unity, whereas damage variables have been limited to a maximum value of 0.9999 for  $d_2$  and  $d_6$ .

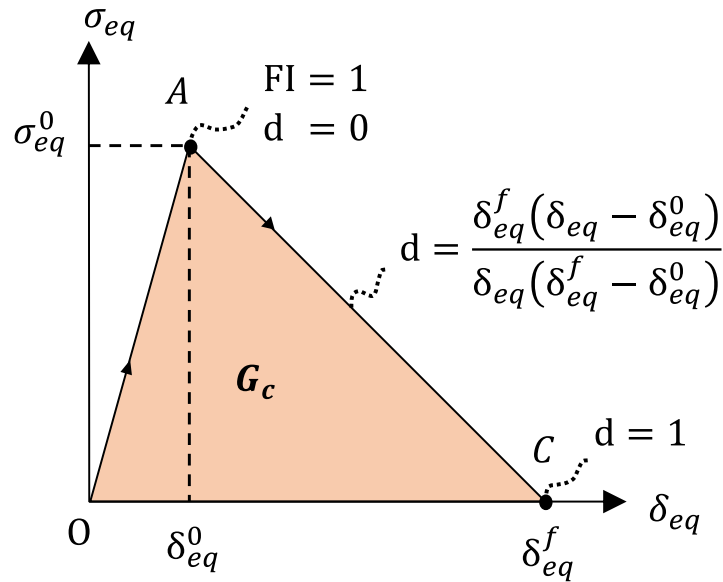


Figure 3.4. Constitutive response of the composite material with linear softening.

### 3.3.2.2 Interlaminar Damage

Delamination is simulated by utilizing the built-in cohesive zone model in ABAQUS. A bilinear traction response to the respective separation at the interfaces for both opening and combined shear fracture modes is employed in this study, as illustrated in Figure 3.5. The region prior to the delamination onset enforces a linear elastic response with penalty stiffnesses of  $K_N$  and  $K_S$  for the opening and shear modes, respectively. In this study,  $K_N$  and  $K_S$  are calculated following the approach outlined by Turon et al. [69,70] and the corresponding numerical values are given in Table 3.1.

Considering the multiaxial nature of transverse impact loading, the interaction of opening and shear fracture modes is taken into account (mixed-mode CZM). The delamination initiation is predicted using the quadratic nominal stress criterion proposed by Chang and Springer [76] as  $(\langle T_I \rangle / T_{o,I})^2 + (T_{II} / T_{o,II})^2 + (T_{III} / T_{o,III})^2 = 1$  where  $T_i$  ( $i = I, II, III$ ) are the surface tractions of corresponding modes, respectively. Only the positive values of  $T_I$  are included in this relation since compressive stresses have no contribution to opening mode fractures. The separation at the damage initiation is calculated with the relation  $\delta_o = T_o / E_o$  where the corresponding initiation traction is  $T_o = \sqrt{T_{o,I}^2 + T_{o,II}^2 + T_{o,III}^2}$ .

Following the prediction of damage initiation in a cohesive element, a linear softening behavior is employed during the propagation of damage through degradation of the penalty stiffnesses of the cohesive element by the cohesive damage variable  $D$ , which is given by  $D = [\delta_c(\delta - \delta_o)] / [\delta(\delta_c - \delta_o)]$ . The effective separation is  $\delta$  is computed by  $\delta = \sqrt{\langle \delta_I \rangle^2 + \delta_{II}^2 + \delta_{III}^2}$  and the critical separation, which is the separation at which fracture is expected, is determined by  $\delta_c = 2G_c / T_o$  where the mixed-mode fracture toughness is calculated based on the Benzeggagh and Kenane (B-K) criterion as,

$$G_c = G_{Ic} + (G_{IIc} - G_{Ic}) \left( \frac{G_{II} + G_{III}}{G_I + G_{II} + G_{III}} \right)^\eta \quad (3.14)$$

where  $\eta$  stands for the mixed-mode interaction parameter.

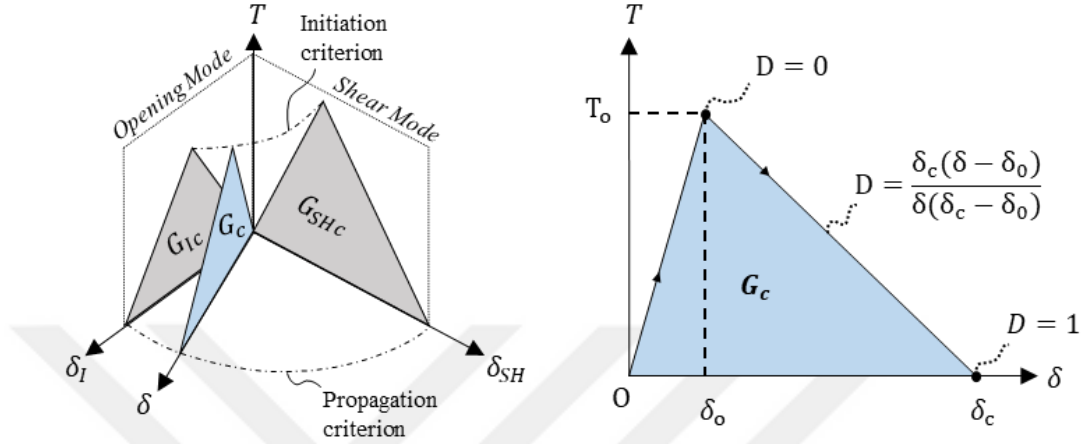


Figure 3.5. Mixed-mode cohesive zone model for delamination damage.

### 3.4 Experimental Results

This section presents the results of LVI and QSI experiments conducted on CFRP beams with layup sequences of  $[0_4/90_4/0_2]_s$  and  $[0_2/90_2/0_2/90_2/0_2]_s$  in two subsections. The findings in each subsection are organized to present the results of LVI experiments first, followed by the results of QSI experiments.

#### 3.4.1 Thick-ply Laminate: $[0_4/90_4/0_2]_s$

Figure 3.6 shows the evolution of the impact-induced matrix cracking and delamination damage in  $[0_4/90_4/0_2]_s$  specimen. The initial frame in which the impactor contacted the composite specimen was taken as the reference with a time of 0 ms. The matrix cracking is observed at 1.32 ms as a diagonal matrix crack inside the lower  $90^\circ$  group of plies on the right-hand side of the beam  $\sim 6.5$  mm away from the impactor center, with an orientation of approximately  $53^\circ$  with respect to the horizontal in Figure 3.6(c). This matrix cracking is deduced to trigger delamination

as it reaches the adjacent  $0^\circ/90^\circ$  interfaces [44], however, it could not be captured explicitly in this experiment due to the slower capturing rate compared to the rapidness of the damage growth. The delamination that is initiated from the upper tip of the diagonal matrix crack propagates toward the impactor, whereas the lower tip initiates a delamination that propagates toward the boundary region. The impactor continued to translate downwards until it reached the maximum deflection point of 3.7 mm at time 2.96 ms, as captured in Figure 3.6(d). After this point, the internal energy of the composite specimen started to be transformed into the kinetic energy of the impactor, thus, it experienced no further damage. The final state of the composite beam and rebounding impactor are shown at 5.84 ms in Figure 3.6(e). The failure patterns observed in this specimen are consistent with those observed in specimens containing middle  $90^\circ$  clusters consisting of six plies reported in [42,44]. In both cases, shear cracks, named after the diagonality of the cracks, are observed at a specific distance from the impactor region accompanied by delamination at the adjacent interfaces. Also, the reasons for asymmetrical damage occurring only at the right-hand side of the beam are deduced to be either due to differences in clamp pressures between opposite boundaries or shifting of the impactor from the symmetry plane, although the experiment setup was intended to be built as perfectly symmetric.

We aimed to investigate the micro-scale damage mechanisms experienced during LVI events qualitatively by conducting QSI experiments. In these QSI experiments, a high-resolution camera was utilized on one edge of the beam to measure full-field strain distributions through the DIC method, whereas a traveling digital microscope is employed on the opposite edge to perform magnified *in situ* observations.

The full-field strain distributions are measured from the DIC analyses for the  $[0_4/90_4/0_2]_s$  specimen in Figure 3.7. The longitudinal ( $\epsilon_{xx}$ ) and transverse shear ( $\gamma_{xy}$ ) strain fields prior to the damage, and the damaged state of the specimen are presented in Figure 3.7(a). The damaged state shows matrix cracks in three different locations, one situated underneath the indenter and the other two located in the vicinity of boundaries. Each matrix crack has a diagonal orientation and is accompanied by

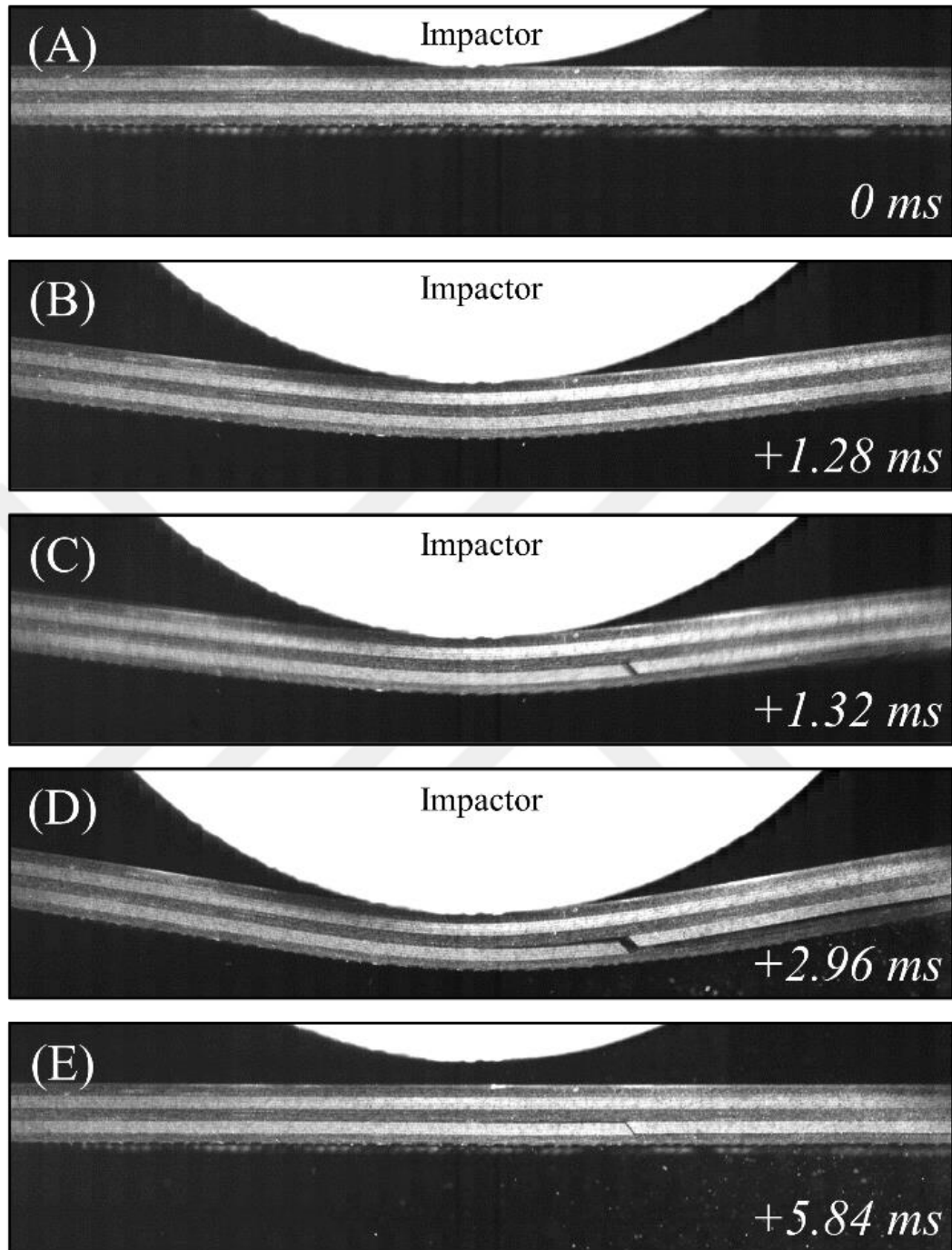
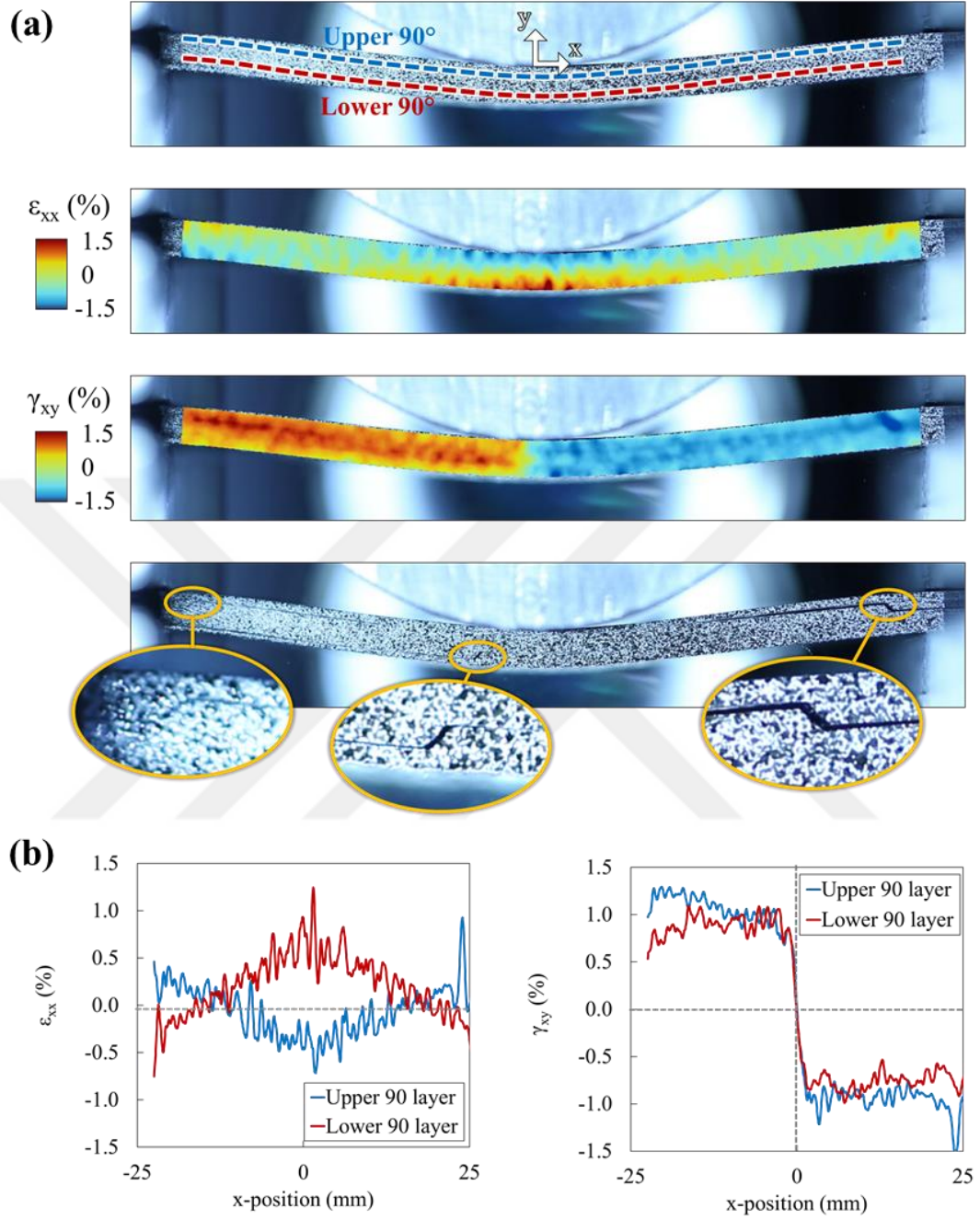


Figure 3.6. High-speed camera photographs of the thick-ply ([0<sub>4</sub>/90<sub>4</sub>/0<sub>2</sub>]<sub>s</sub>) specimen showing (a) the initial contact of impactor and specimen, (b) frame prior to matrix cracking, (c) matrix cracking and delamination formation, (d) maximum deflection point, and (e) final state of the specimen after the impactor rebounded.

delaminations at the adjacent  $0^\circ/90^\circ$  interfaces. In Figure 3.7(b),  $\epsilon_{xx}$  and  $\gamma_{xy}$  strain distributions along the upper and lower  $90^\circ$  ply groups (data paths shown in Figure 3.7(a)) are plotted.  $\epsilon_{xx}$  distributions show that the regions with higher  $\epsilon_{xx}$  values are located just underneath the indenter, where the lower side of the beam is in tension and the contacting side is under compression. Conversely,  $\gamma_{xy}$  distributions do not show any distinct region with higher values but exhibit a transition of  $\gamma_{xy}$  sign from negative (cold colors) to positive (warm colors) convention along the beam, implying that the region just underneath the impactor has smaller  $\gamma_{xy}$  values. Specifically, the matrix crack location underneath the impactor experiences approximately 0.8% normal and 1.2% transverse shear strain. Thus, strain distributions show that the matrix cracks occurred at locations where  $\epsilon_{xx} > 0$  and  $\gamma_{xy}$  values are significant.

Indenter force vs. displacement response obtained from the testing machine is presented alongside *in situ* micrographs at  $25\times$  magnification, taken from the opposite edge where DIC analyses, in Figure 3.8. The specimen exhibited linear elastic behavior with a stiffness of 1.14 kN/mm. Before any sign of global stiffness loss, the microscopic observations revealed the early formation of a diagonal matrix crack within the lower  $90^\circ$  ply group  $\sim 3.5$  mm away from the impactor center, shown in frame A. Macroscopic instruments could not detect this crack since it did not affect the force-displacement response or strain fields for the current resolution. Then in frame B, this matrix crack triggers the delamination at the adjacent interfaces, which corresponds to the initial load drop at 2.75 mm and 2.69 kN. Subsequently, frame C shows the delamination propagation that occurred within the upper  $90^\circ$  ply group, accompanied by the second major load drop. However, both matrix cracks near the left and right-hand-side boundaries, presented in Figure 3.7(a), could not be captured as they were left outside of the microscope's field of view.



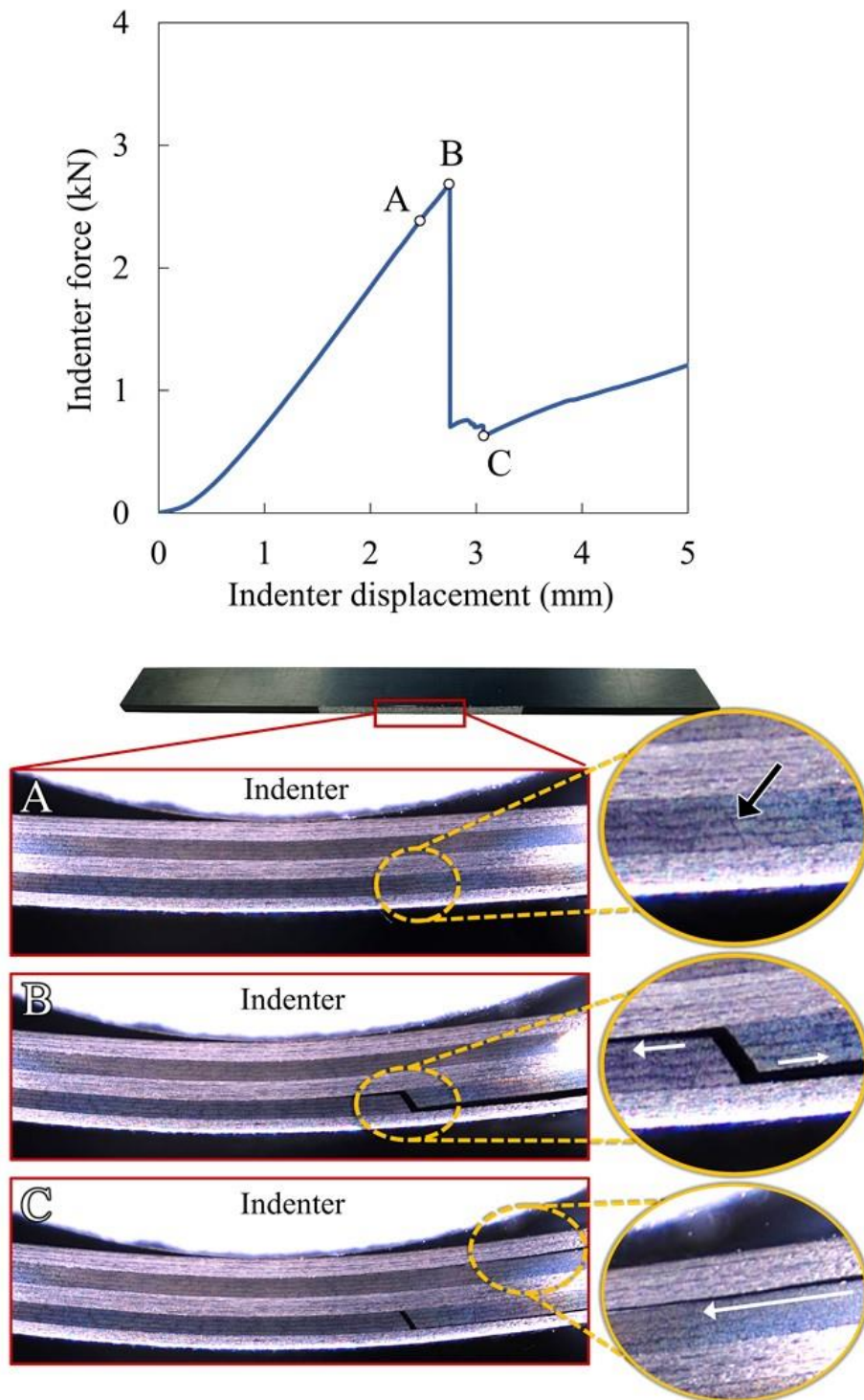


Figure 3.8. Indenter force-displacement response and corresponding *in situ* micrographs of the thick-ply specimen showing the first diagonal matrix cracking at frame (A), matrix cracking-induced delamination damage at frame (B), and delamination propagated from the outside of the field of view at frame (C).

### 3.4.2 Thin-ply Laminate: [0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>]<sub>s</sub>

For the [0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>]<sub>s</sub> specimen, the whole sequence of impact event from the initial contact to the rebound of the impactor is presented in Figure 3.9. No apparent damage is observed prior to 1.00 ms. At 1.04 ms, a sudden failure including matrix cracking, fiber breakage and delamination damages is detected within the lower 0° and 90° group of plies beneath the impactor, as displayed in Figure 3.9(b). Unfortunately, the matrix cracking, delamination or fiber breakage sequence could not be captured at this capturing rate. The failure occurred successively within the adjacent upper ply groups as the impactor translates downwards, where Figure 3.9(c) and Figure 3.9(d) respectively show these damages at 1.16 and 4.68 ms. Figure 3.9(d) also shows the asymmetric damage of matrix cracking and delamination occurred only at the right-hand side of the beam. The impactor reached the maximum deflection point of 6.2 mm at 4.68 ms after the initial contact, as shown in Figure 3.9(d). Lastly, the final state of the composite beam and rebounding impactor are given at 9.12 ms in Figure 3.9(e).

The full-field strain distributions obtained from DIC analysis for the [0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>]<sub>s</sub> specimen are presented in Figure 3.10. Figure 3.10(a) shows the longitudinal ( $\epsilon_{xx}$ ) and transverse shear ( $\gamma_{xy}$ ) strain fields prior to the damage, and the damaged state of the specimen. The hotspots with higher  $\epsilon_{xx}$  values grouped just underneath the indenter are evident where the lower and the contacting sides of the beam were in compression and tension, respectively.  $\gamma_{xy}$  field shows strain hotspots grouped near the middle layers of the laminate. From the damaged state given in Figure 3.10(a), which corresponded to the first load drop in the load-displacement response, it can be observed that the initial failure is in the form of matrix cracking, delamination and fiber breakage. Fiber breakage and perpendicular matrix cracking were observed inside the lower 0° and 90° layers, while delamination propagated along the interfaces adjacent to the lower 90° ply group. The simultaneous investigation of  $\epsilon_{xx}$  and  $\gamma_{xy}$  fields indicates that the initial damage occurred due to high values of  $\epsilon_{xx}$ , where  $\gamma_{xy}$  values are almost zero. Specifically, the center of the

lower 90° ply group, where damage is observed, experiences approximately 1.5% normal and <0.5% transverse shear strain. Consequently, the damage pattern observed in QSI experiments can be regarded as identical to the damage captured during the LVI experiments.

Indenter force vs. displacement response obtained from the testing machine is presented alongside *in situ* micrographs at 50× magnification, taken from the opposite edge where DIC analyses, in Figure 3.11. The specimen exhibited linear elastic behavior with a stiffness of 1.25 kN/mm. Similar to the previous specimen, the microscopic observations revealed the early formation of a diagonal matrix crack within the lower 90° ply group shown in frame A before any sign of global stiffness loss. Following this, a group of four matrix cracks on the right-hand side appeared in frame B. Macroscopic instruments again could not detect this crack since it did not affect the force-displacement response or strain fields for the current resolution. The formation of matrix cracks was followed by a sudden damage growth including fiber breakage and delaminations in frame C, which corresponded to the initial load drop at 3.27 mm and 3.65 kN. As the loading increased further, matrix cracking and delamination subsequently occurred inside the upper 90° plies and adjacent interfaces. Frame D presents the matrix cracks inside the second and third 90° plies with delaminations along their adjacent interfaces, and delamination at the top 0°/90° interface.

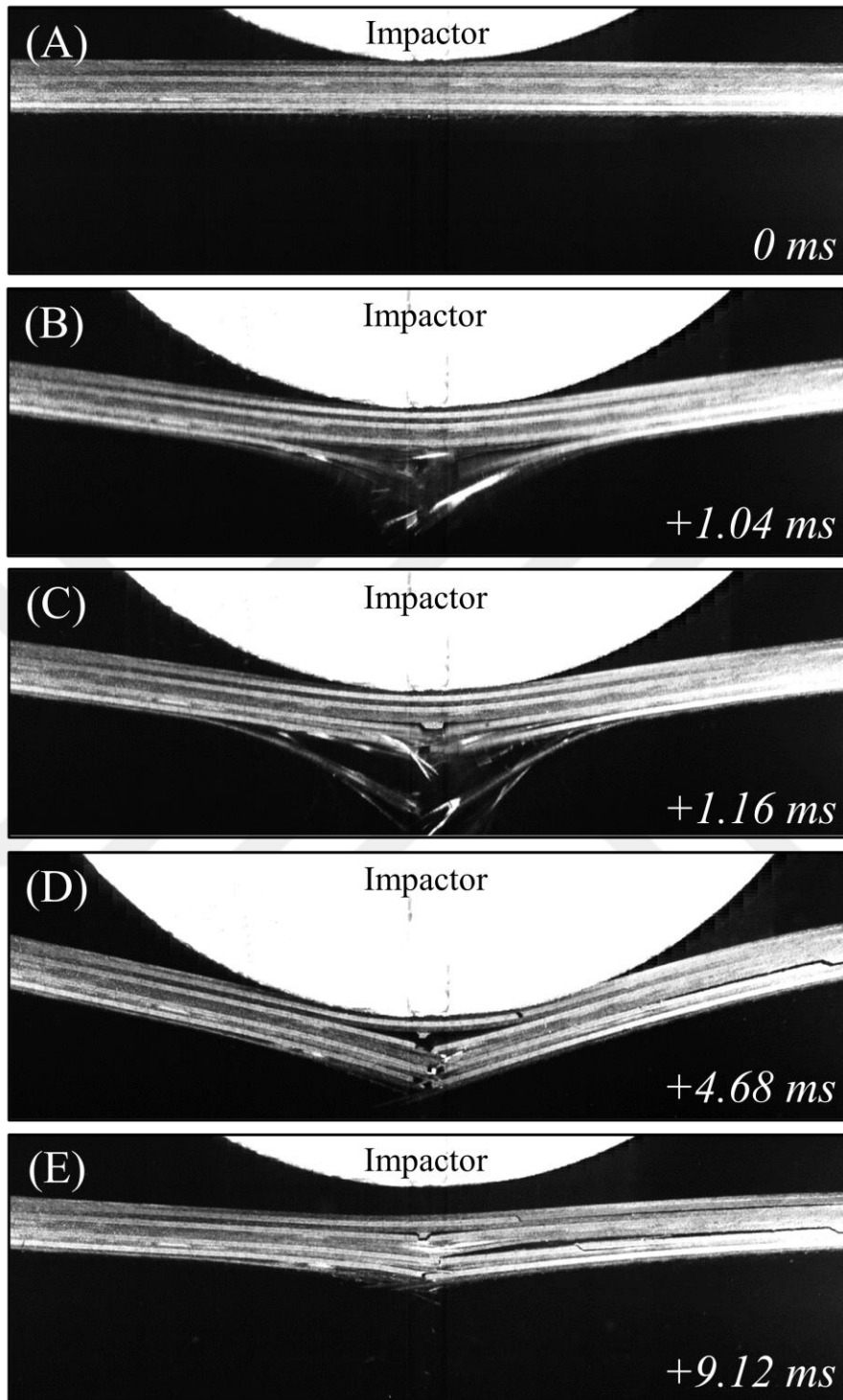


Figure 3.9. High-speed camera photographs for the thin-ply ( $[0_2/90_2/0_2/90_2/0_2]_s$ ) specimen showing (a) the initial contact of impactor and specimen, (b) initial matrix cracking, delamination and fiber breakage, (c) the successive damages within the upper ply groups, (d) point of maximum deflection and (e) final state of the specimen after the impactor rebounded.

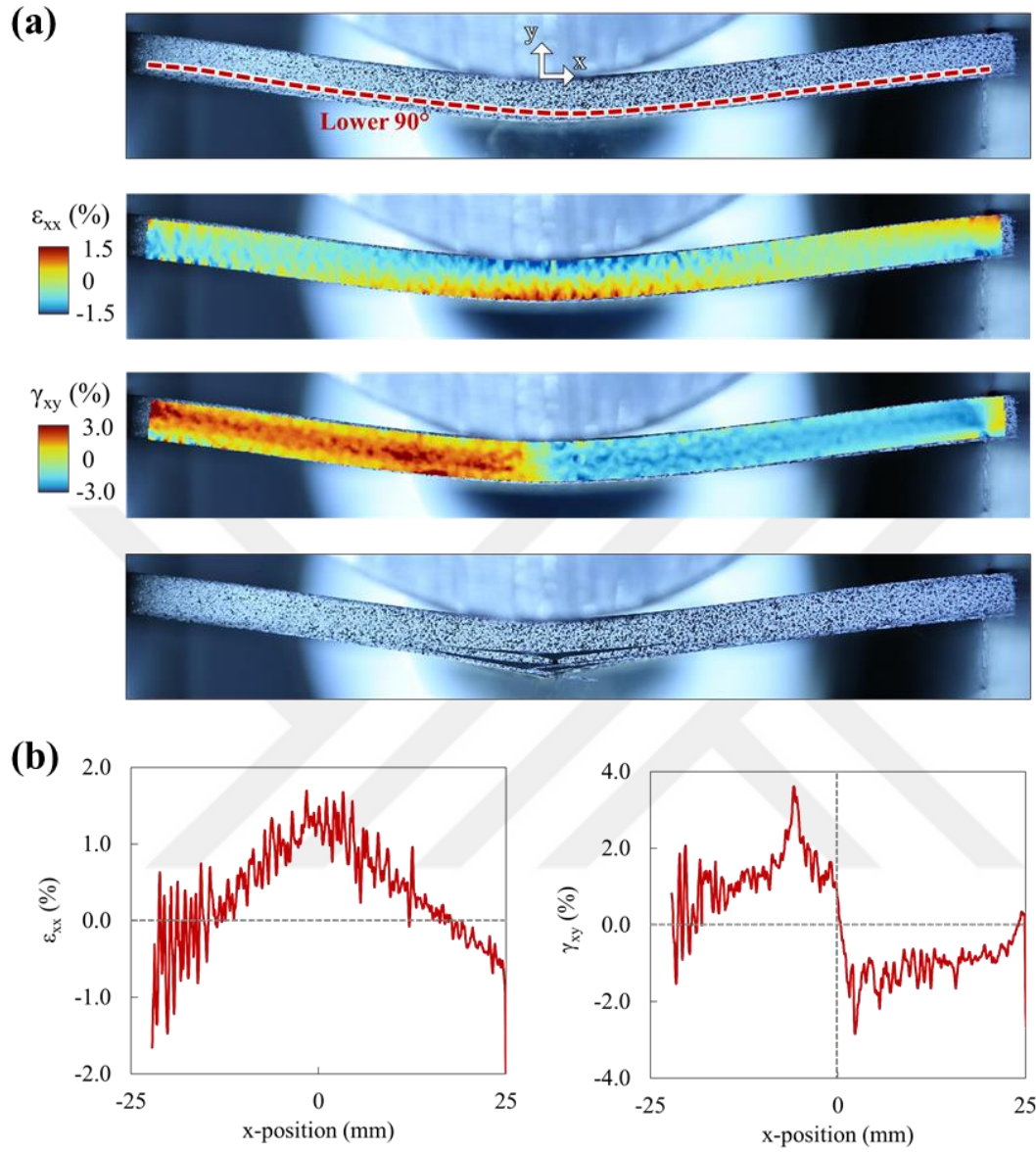


Figure 3.10. DIC results for the  $[0_2/90_2/0_2/90_2/0_2]_s$  specimen showing **(a)** longitudinal strain ( $\varepsilon_{xx}$ ) and transverse shear strain ( $\gamma_{xy}$ ) fields prior to damage, and damaged state of the beam after load drop, **(b)**  $\varepsilon_{xx}$  and  $\gamma_{xy}$  distributions extracted along the lowest  $90^\circ$  ply group.

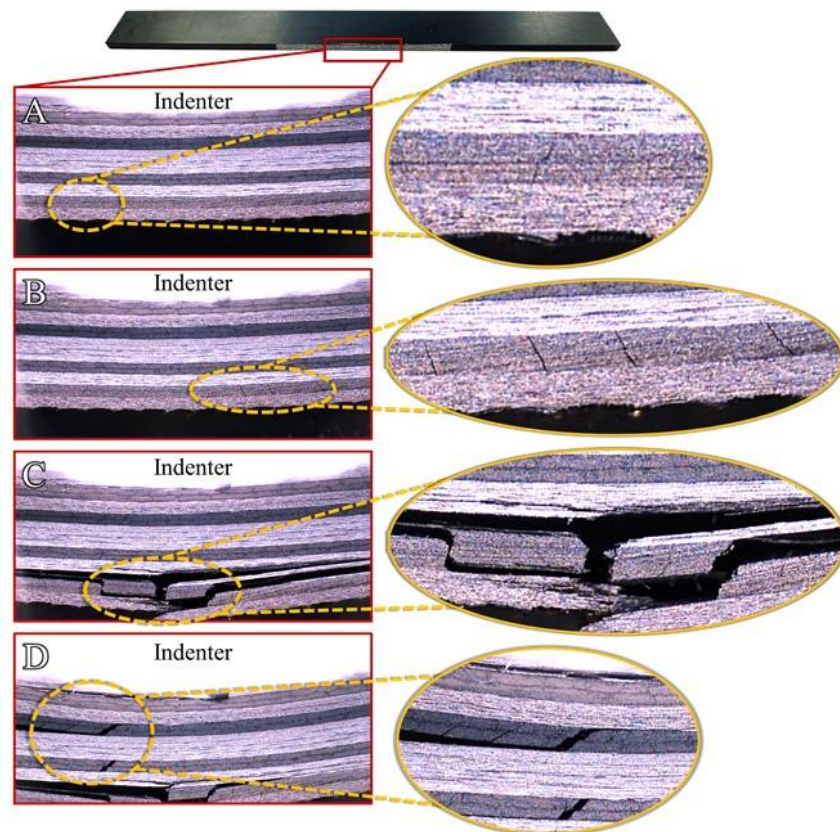
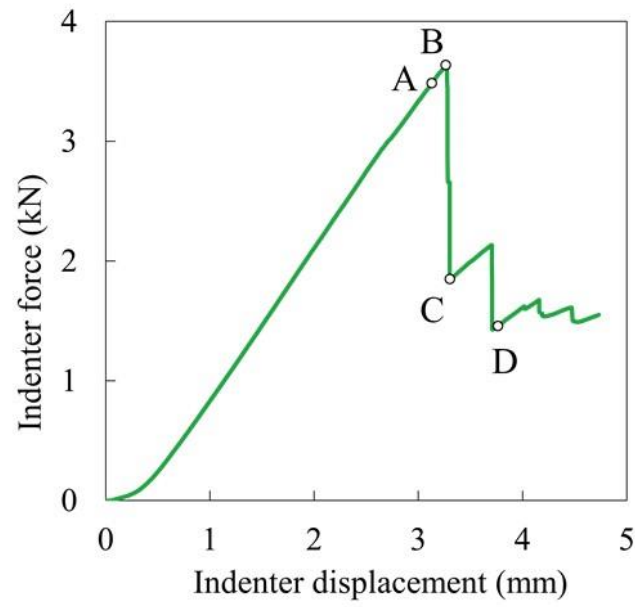


Figure 3.11. Indenter force-displacement response and corresponding *in situ* micrographs of the  $[0_2/90_2/0_2/90_2/0_2]_s$  specimen showing (a) the first diagonal matrix cracking, (b) formation of the group of four matrix cracks, (c) matrix cracking-induced delamination damage and fiber breakage, and (d) matrix crack formation inside the subsequent  $90^\circ$  ply groups.

### 3.5 Numerical Results

In this section, the FE simulation results of the low-velocity impact experiments performed on CFRP beams with layup sequences of  $[0_4/90_4/0_2]_s$  and  $[0_2/90_2/0_2/90_2/0_2]_s$  are presented in two subsections. Each subsection begins with an overview of the global behavior of the composite beams prior to damage. Subsequently, a detailed examination of the *in situ* impact-induced damage progression and mechanisms is carried out.

#### 3.5.1 Thick-ply Laminate: $[0_4/90_4/0_2]_s$

The global impact response of the composite beam is investigated through the analysis of force-displacement curves and full-field strain fields prior to damage.

The force-displacement response of the FE model is plotted in Figure 3.12. The material response is elastic up to the sudden load drop at time and displacement of approximately 600  $\mu s$  and 2 mm, respectively. The periodic oscillations in the elastic regime occur due to the dynamic coupling of the impactor, specimen and BCs [20]. A load drop occurs when delamination is propagated, as the effect of delamination on the load-bearing capacity of the specimen is greater than that of matrix cracking. The maximum force level at the point of damage is 3.12 kN.

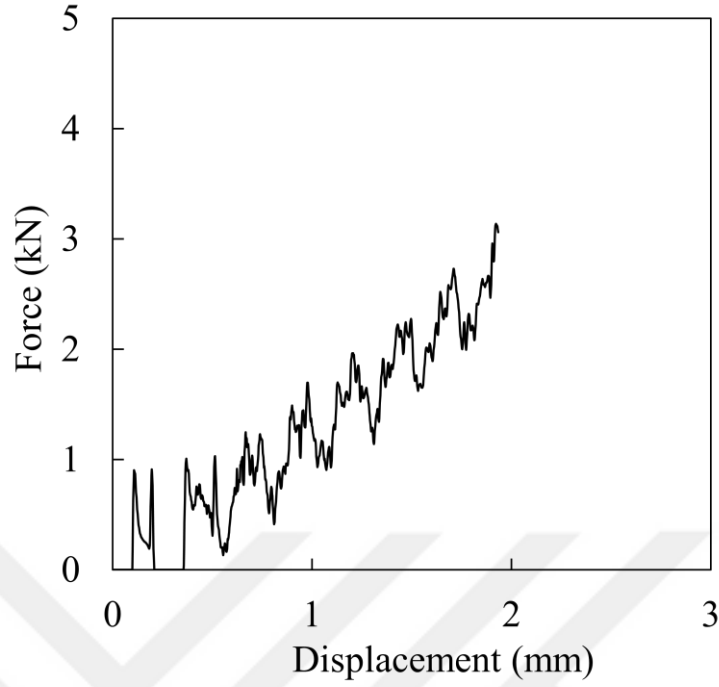


Figure 3.12. Impactor force-displacement response of the FE model.

Figure 3.13 depicts the distributions of full-field longitudinal ( $\epsilon_{xx}$ ) and transverse shear ( $\gamma_{xy}$ ) strains across the  $[0_4/90_4/0_2]_s$  specimen before any damage occurs. In Figure 3.13(a), regions with elevated tensile  $\epsilon_{xx}$  values are concentrated beneath the impactor and in the upper regions near the boundaries, whereas the contacting side of the upper layers experiences compression. The  $\gamma_{xy}$  field exhibits elevated values near boundaries and near the cone-shaped stress region beneath the impactor within the  $90^\circ$  plies. Simultaneous comparison of strain distribution curves across the specimen in Figure 3.13(b) indicates that the critical regions for the shear-dominated matrix failure are located  $\sim 5.5$  mm off-center for the lower  $90^\circ$  ply group, and  $\sim 1.0$  mm off-center for the upper  $90^\circ$  ply group, with  $\gamma_{xy}$  values approximately 1.0%.

Matrix cracks occur in planes perpendicular to the maximum principal stress direction [126]. Instead of calculating the failure index using the LaRC05 relation given in Equation (3.8) to determine critical regions for potential matrix cracking, we will examine the maximum principal stresses ( $\sigma_p$ ) reaching the tensile strength of  $90^\circ$  plies ( $Y_T$ ) for simplicity. On the other hand, for fiber failure, only normal stresses in the fiber direction ( $\sigma_{11}$ ) reaching the associated strength ( $X_T$ ) are considered.

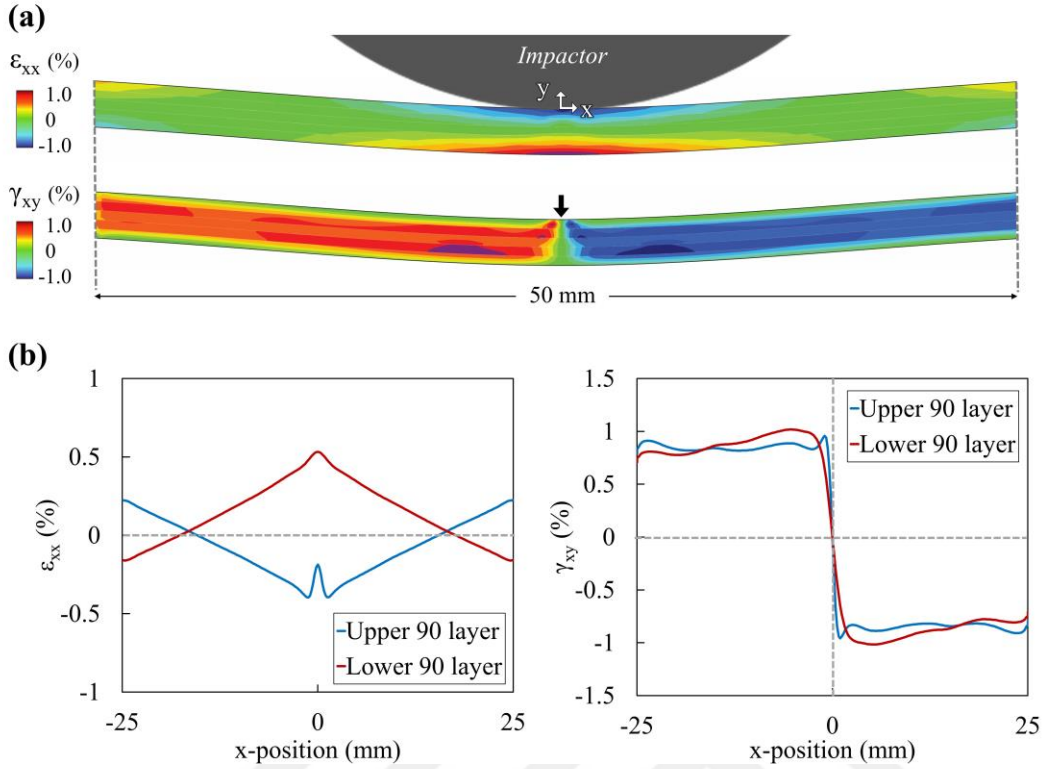


Figure 3.13. FE results of the [0<sub>4</sub>/90<sub>4</sub>/0<sub>2</sub>]<sub>s</sub> specimen showing **(a)** longitudinal strain ( $\epsilon_{xx}$ ) and transverse shear strain ( $\gamma_{xy}$ ) fields prior to damage, and **(b)**  $\epsilon_{xx}$  and  $\gamma_{xy}$  distributions extracted along the upper 90° ply group and lower 90° ply group.

In Figure 3.14(a), the normal stress ( $\sigma_{11}$ ) contour for 0° ply groups and the maximum principal stresses ( $\sigma_p$ ) contour for 90° ply groups are presented for the [0<sub>4</sub>/90<sub>4</sub>/0<sub>2</sub>]<sub>s</sub> specimen. The corresponding distributions of  $\sigma_{11}$  at  $x = 0$  and  $\sigma_p$  at  $x = -3.5$  mm are extracted through the thickness direction in Figure 3.14(b) and Figure 3.14(c). Consistent with the strain field results, Figure 3.14(a) shows higher values of  $\sigma_{11}$  at the bottom 0° ply group underneath the impactor, and  $\sigma_p$  within the lower 90° ply group but  $x = 3.5$  mm off from the center. Comparing the highest stresses, extracted from Figure 3.14(b) and (c) as  $\sigma_{11} = 1199$  MPa and  $\sigma_p = 77$  MPa, with the associated strength values,  $X_T = 2207$  MPa and  $Y_T = 80$  MPa, indicates that the matrix damage is on the verge of initiation within the lower 90° ply group, while fiber damage is not expected due to lower  $\sigma_{11}$  values compared to  $X_T$ .

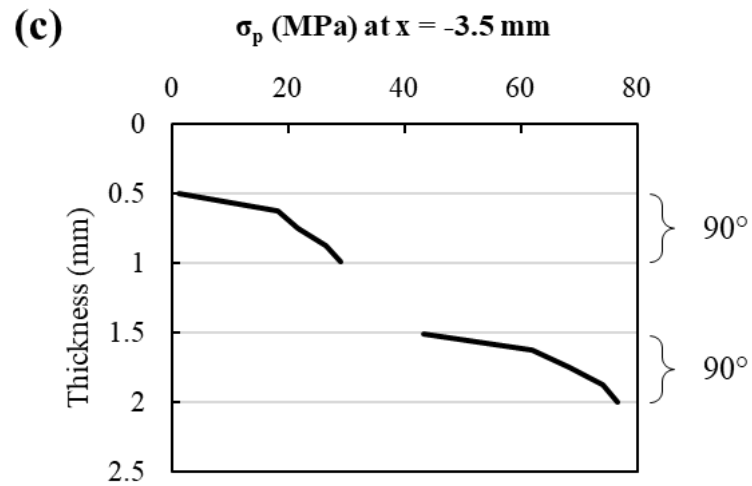
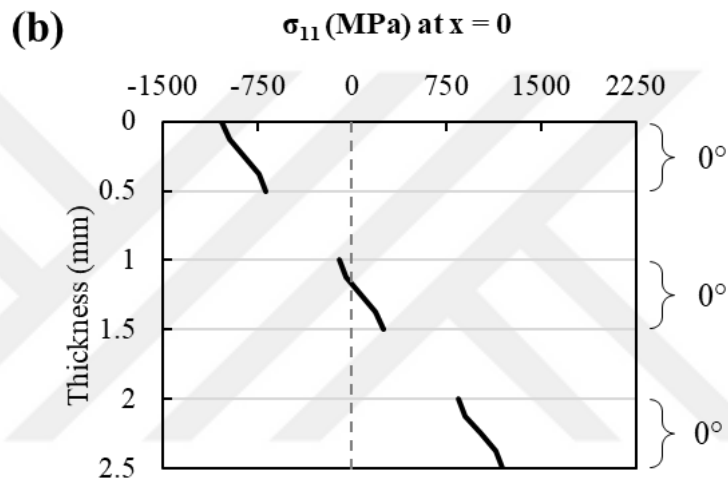
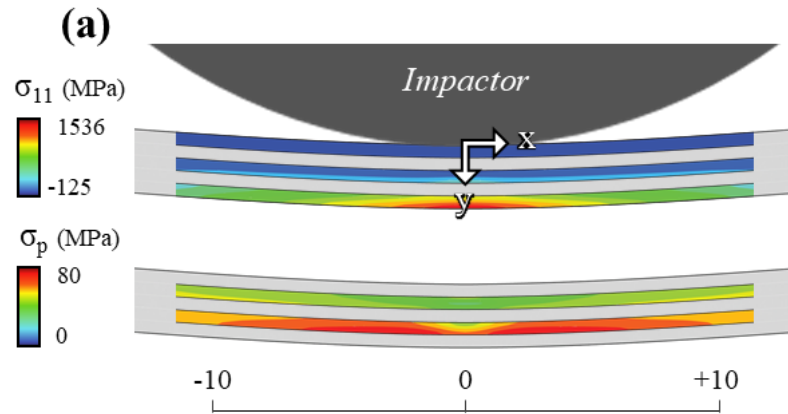


Figure 3.14. **(a)** Contours of normal stress ( $\sigma_{11}$ ) for  $0^\circ$  ply groups and the maximum principal stress ( $\sigma_p$ ) for  $90^\circ$  ply groups, and through-the-thickness distributions of **(b)**  $\sigma_{11}$  at  $x = 0$  and **(c)**  $\sigma_p$  at  $x = -3.5$  mm for the  $[0_4/90_4/0_2]_s$  specimen prior to damage.

The development of damage in the  $[0_4/90_4/0_2]_s$  specimen is presented in Figure 3.15. The first frame shows the initial off-center matrix damage located  $\sim 2$  mm away from the impactor. This matrix crack induces delamination damage at the adjacent  $0^\circ/90^\circ$  interfaces, where upper and lower delamination respectively propagates towards the impactor and the boundaries. As the delamination at the lower  $0^\circ/90^\circ$  interface reaches the boundary, damage in the form of matrix cracking accompanied by a delamination at the adjacent interfaces are observed within the upper  $90^\circ$  ply group, where matrix cracking is located  $\sim 18$  mm away from the impactor.

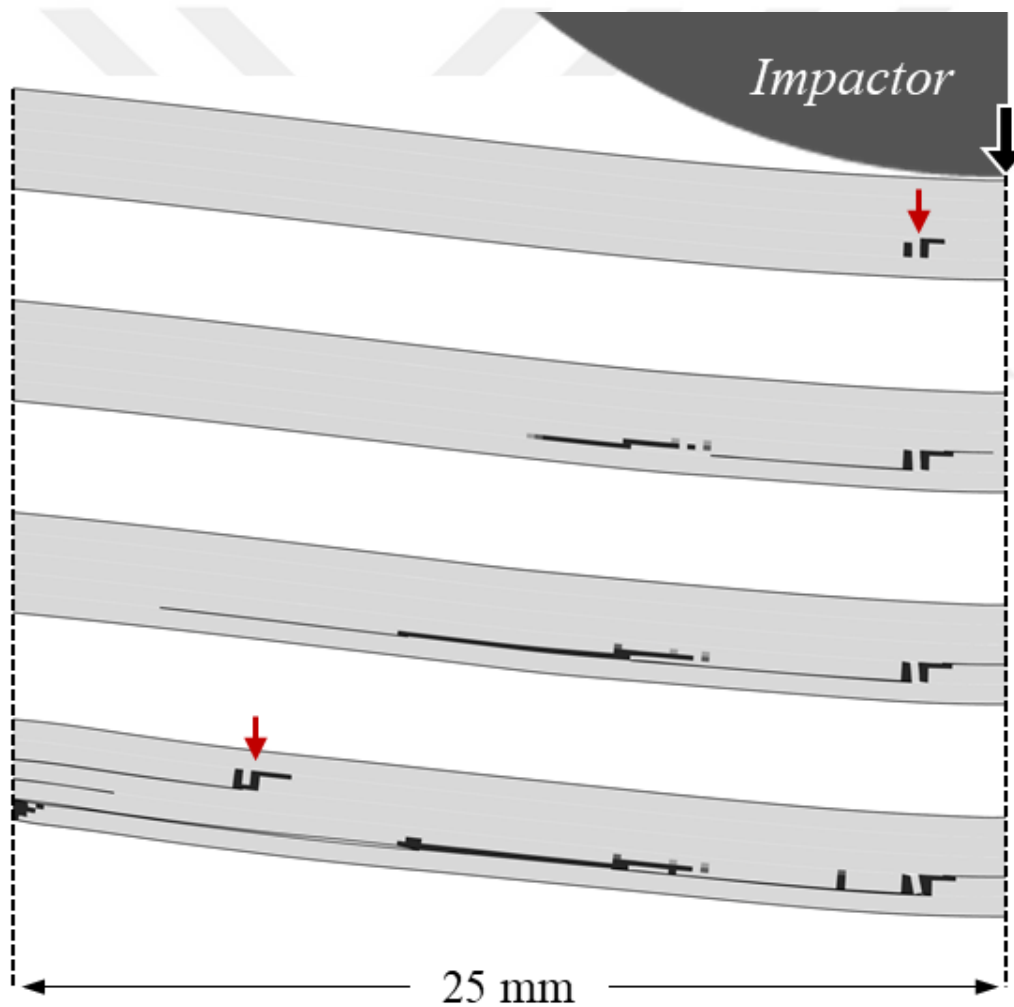


Figure 3.15. Development of the damage in the thick-ply ( $[0_4/90_4/0_2]_s$ ) specimen.

### 3.5.2 Thin-ply Laminate: [0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>]<sub>s</sub>

The force-displacement response of the FE model is plotted in Figure 3.18. The material response remains elastic up to the sudden load drop at a time and displacement of 850  $\mu$ s and 2.81 mm, respectively. As for the preceding specimen, the periodic oscillations in the elastic regime occur due to the dynamic coupling of the impactor, specimen and BCs. A steep load drop occurs when fiber breakage and delamination occur. The maximum force level at the point of damage is 3.97 kN.

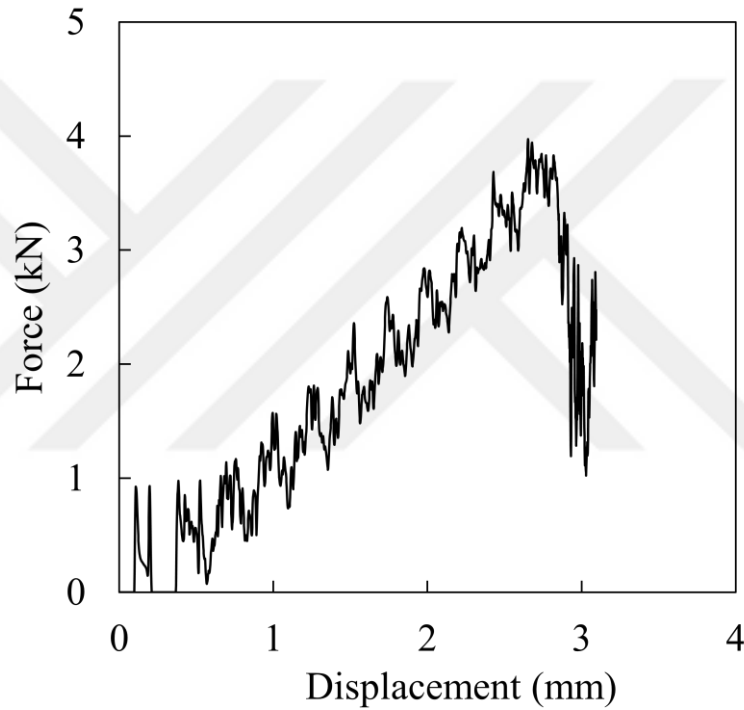


Figure 3.16. Impactor and indenter force-displacement response of the FE model and QSI experiments.

Figure 3.17 depicts the distributions of full-field longitudinal ( $\epsilon_{xx}$ ) and transverse shear ( $\gamma_{xy}$ ) strains across the [0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>/90<sub>2</sub>/0<sub>2</sub>]<sub>s</sub> specimen prior to damage. Similar to the preceding layup, Figure 3.17(a) displays areas of elevated  $\epsilon_{xx}$  values concentrated just beneath the impactor and in upper regions adjacent to the boundaries.  $\gamma_{xy}$  field exhibits elevated values near boundaries and within a cone-shaped region beneath the impactor inside the 90° plies. A simultaneous examination of strain distribution curves across the specimen in Figure 3.17(b) indicates that the critical regions are at

the center for the tensile-dominated failure, and the regions located  $\sim 3$  mm off-center for the shear failure. Specifically, the bottom  $90^\circ$  ply group experiences a maximum normal and transverse shear strain of 1.22% and 1.71% at  $x = 0$  and  $x = 3.5$  mm, respectively.

As explained for the preceding specimen, the critical regions where matrix cracking and fiber breakage may occur are examined by respectively checking the maximum principal stresses ( $\sigma_p$ ) reaching the tensile strength of  $90^\circ$  plies ( $Y_T$ ), and the normal stresses in the fiber direction ( $\sigma_{11}$ ) reaching the associated strength ( $X_T$ ).

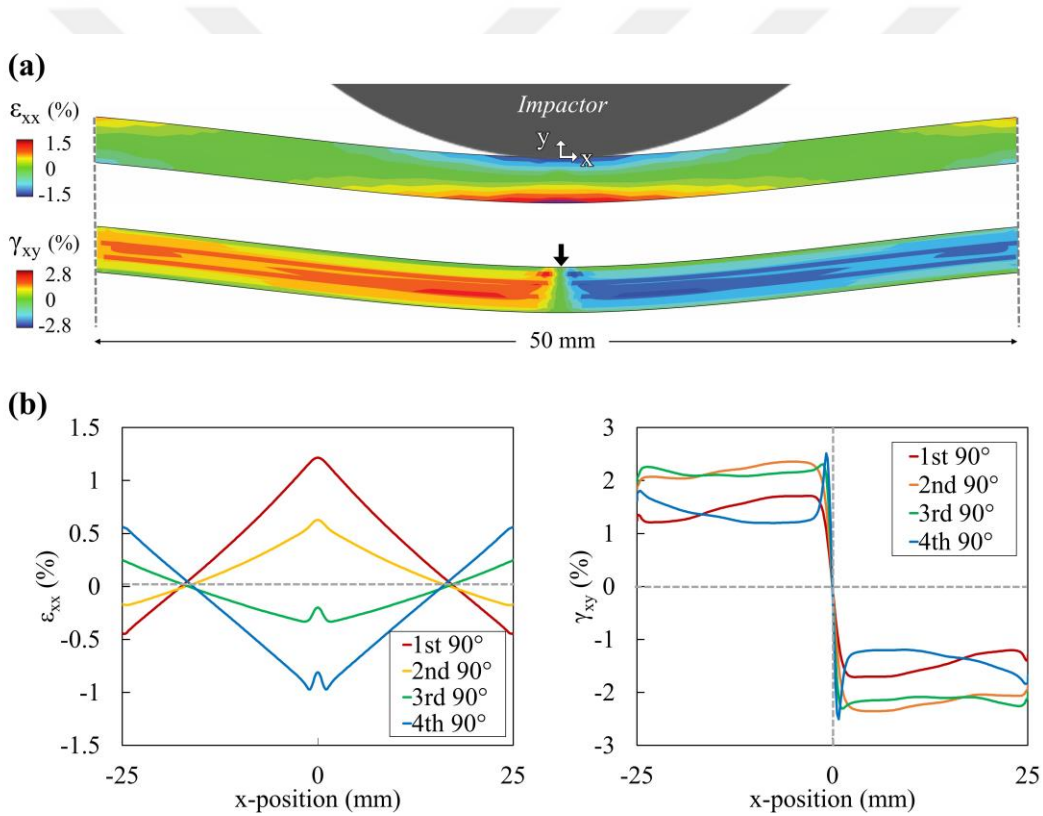


Figure 3.17. FE results of the  $[0_2/90_2/0_2/90_2/0_2]_s$  specimen showing (a) longitudinal strain ( $\epsilon_{xx}$ ) and transverse shear strain ( $\gamma_{xy}$ ) fields prior to the damage, and (b)  $\epsilon_{xx}$  and  $\gamma_{xy}$  distributions extracted along the  $90^\circ$  ply groups (1<sup>st</sup> and 4<sup>th</sup> correspond to the bottom and top  $90^\circ$  ply groups, respectively).

In Figure 3.18(a), the normal stress ( $\sigma_{11}$ ) contour for  $0^\circ$  ply groups and the maximum principal stresses ( $\sigma_p$ ) contour for  $90^\circ$  ply groups are presented for the  $[0_2/90_2/0_2/90_2/0_2]_s$  specimen. The corresponding distributions of  $\sigma_{11}$  at  $x = 0$  and  $\sigma_p$  at  $x = -3.5$  mm are extracted through the thickness direction in Figure 3.18(b) and Figure 3.18(c). Consistent with the strain field results, Figure 3.18(a) shows higher values of  $\sigma_{11}$  at the bottom  $0^\circ$  ply group underneath the impactor, and  $\sigma_p$  within the lower  $90^\circ$  ply group but  $x = 3.5$  mm off from the center. Comparing the highest stresses, extracted from Figure 3.18(b) and (c) as  $\sigma_{11} = 1772$  MPa and  $\sigma_p = 109$  MPa, with the associated strength values,  $X_T = 2207$  MPa and  $Y_T = 111$  MPa, indicates that both matrix and fiber damage are on the verge of initiation within the lower  $90^\circ$  and  $0^\circ$  ply groups, respectively.

Development of the damage in the  $[0_2/90_2/0_2/90_2/0_2]_s$  specimen is presented in Figure 3.19. The first frame reveals the occurrence of vertical matrix cracks beneath the impactor, specifically within the lower  $90^\circ$ , which lies 0.2 and 0.8 mm away from the impactor. Delamination damage is induced from these matrix cracks, and elements located at the center of the lower  $0^\circ$  ply group are deleted due to complete fiber damage. The damage sequence of delamination followed by fiber breakage is revealed in simulations, which could not be captured explicitly in the corresponding experiments. As the delamination propagates to the left and right-hand-side boundaries, subsequent vertical matrix cracks become evident within the adjacent upper  $90^\circ$  plies. Similarly, these matrix cracks trigger a sequence of events of delamination followed by fiber breakage within the subsequent  $0^\circ$  ply group. As the initial delamination propagation, this delamination-type damage also propagates to the boundaries, yet it migrates to the adjacent interface of the  $90^\circ$  ply group due to higher shear at the midplane of the specimen.

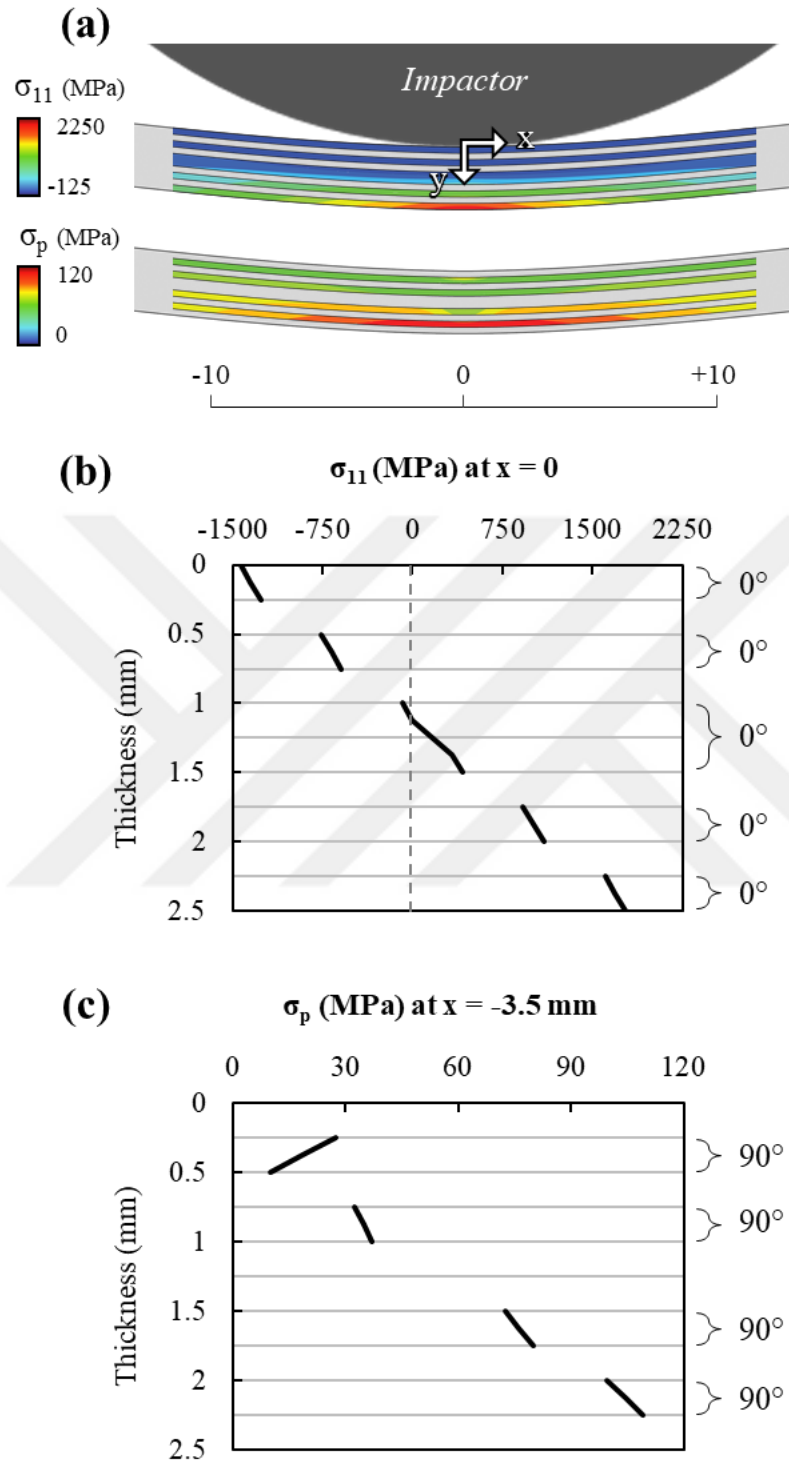


Figure 3.18. **(a)** Contours of normal stress ( $\sigma_{11}$ ) for  $0^\circ$  ply groups and maximum principal stress ( $\sigma_p$ ) for  $90^\circ$  ply groups, and through-the-thickness distributions of **(b)**  $\sigma_{11}$  at  $x = 0$  and **(c)**  $\sigma_p$  at  $x = -3.5$  for the  $[0_2/90_2/0_2/90_2/0_2]_s$  specimen prior to the damage.

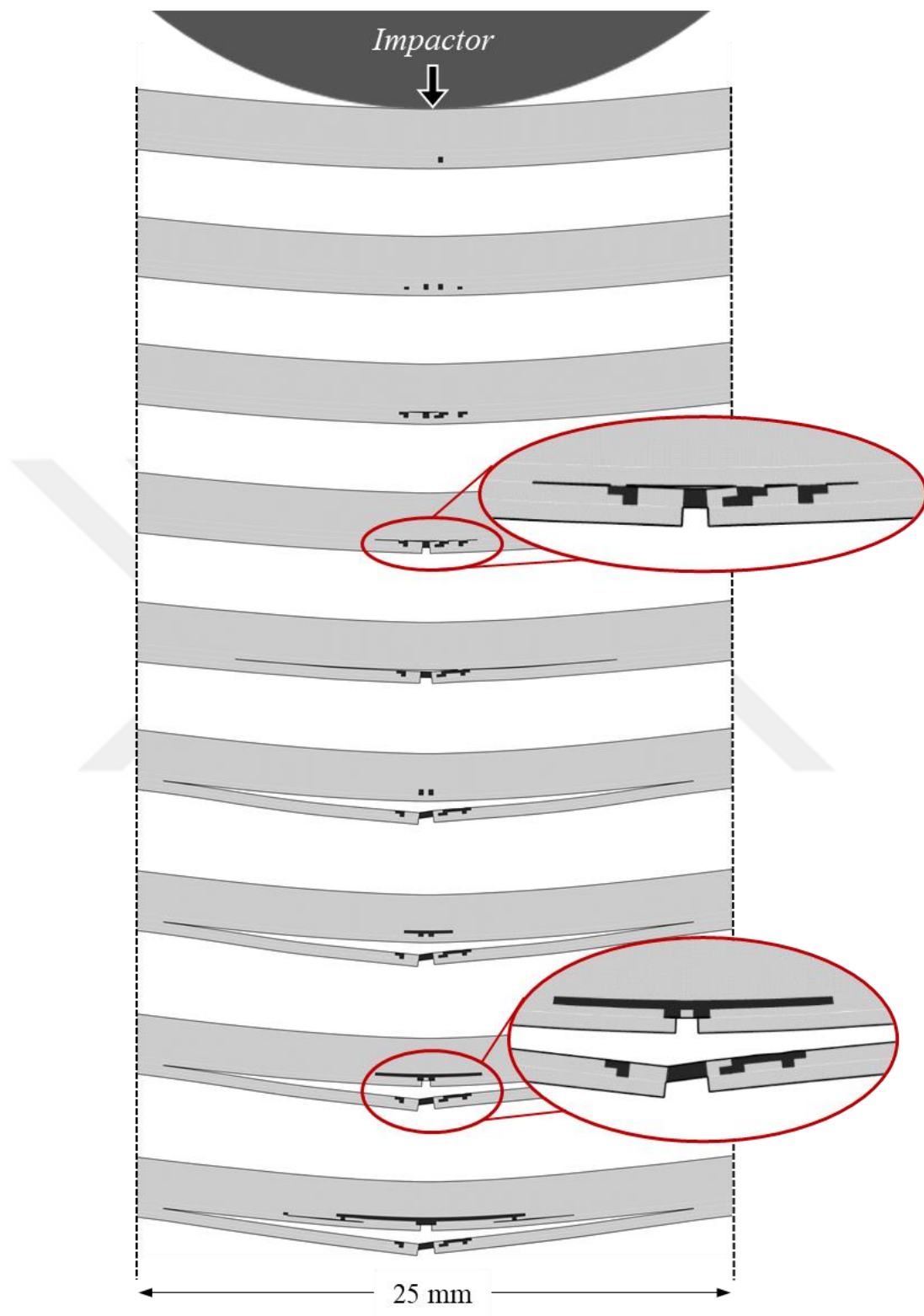


Figure 3.19. Development of the damage in the  $[0_2/90_2/0_2/90_2/0_2]_s$  specimen.

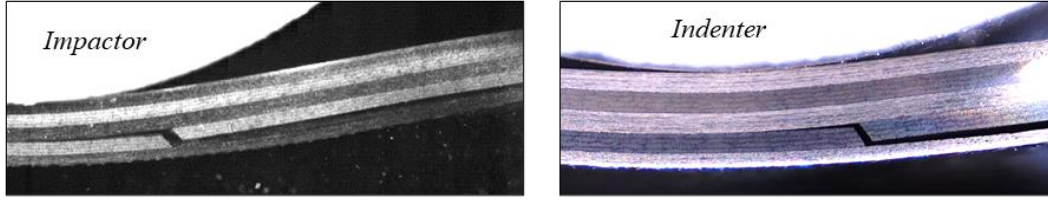
## 3.6 Discussion

### 3.6.1 Comparison of Low-velocity and Quasi-static Experiments

The damage patterns observed during the low-velocity impact (LVI) and quasi-static indentation (QSI) experiments are compared in Figure 3.20. LVI experiment results revealed different impact-induced damage patterns for the layups under investigation. Specifically, the thick-ply specimen ( $[0_4/90_4/0_2]_s$ ) experienced failure primarily due to off-center matrix cracking followed by delaminations in Figure 3.20(a). In contrast, the thin-ply specimen ( $[0_2/90_2/0_2/90_2/0_2]_s$ ), despite having the same number of  $0^\circ$  and  $90^\circ$  plies in total compared to the thick-ply specimen, experienced central matrix cracking followed by delaminations and fiber breakage within the lowest ply group in Figure 3.20(b), with damage subsequently propagating to the adjacent upper plies. The damage patterns and sequences observed in the QSI experiments, presented on the right-hand side of Figure 3.20(a) and (b), are highly consistent with the damage captured during the LVI experiments. Comparing these results shows that the loading rate dependency of material is deemed negligible for LVI on failure characteristics. These findings provide a corollary to the previous study showing consistency in dynamic damage characteristics of LVI and QSI experiments conducted on  $[0_5/90_3]_s$  specimens in [44], which can be extended to the  $[0_4/90_4/0_2]_s$  and  $[0_2/90_2/0_2/90_2/0_2]_s$  specimens under investigation in this work.

The indenter force vs. displacement responses of thick-ply and thin-ply specimens obtained from the quasi-static indentation (QSI) experiments are presented in Figure 3.21. The stiffness values obtained in the linear elastic regime were determined as 1.14 and 1.25 kN/mm for the thick-ply and thin-ply specimens, respectively. The thick-ply specimen showed a softer response compared to the thin-ply specimen in the linear elastic regime, as the  $D_{22}$  element of the D matrix (see Appendix C) of the thick-ply specimen has a smaller value. The thick-ply specimen exhibited the first load drop accompanied by delamination at 2.75 mm and 2.69 kN, while the thin-ply specimen at 3.27 mm and 3.65 kN. These displacement at failure values align with

**(a) Thick-ply specimen:**



**(b) Thin-ply specimen:**

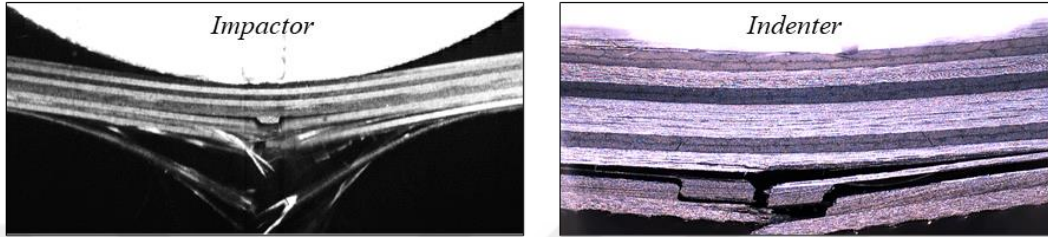


Figure 3.20. Damage patterns from low-velocity impact experiments (left) and quasi-static indentation experiments (right) of **(a)** thick-ply and **(b)** thin-ply specimens.

the values obtained from the LVI experiments, where the thick-ply specimen exhibited failure at 2.77 mm and the thin-ply specimen at 3.12 mm. The thick-ply specimen exhibits two major load drops accompanied by the delamination propagation at the adjacent interfaces of upper and lower 90° ply groups. Similarly, the load-displacement response of the thin-ply specimen showed four consecutive load drops that corresponded to the failure of four 90° ply groups and delaminations.

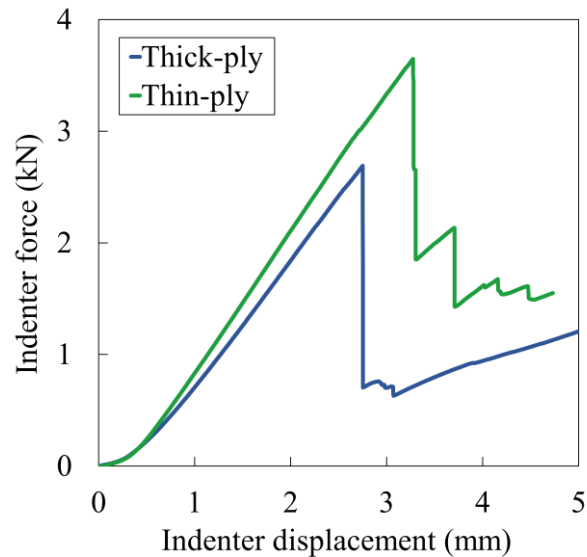


Figure 3.21. Indenter force-displacement responses of the thick-ply and thin-ply specimens from QSI experiments.

### 3.6.2 Comparison of Experiments and Simulations

The force-displacement responses of the FE models of thick-ply and thin-ply specimens are plotted in Figure 3.22. The indenter force-displacement responses from QSI experiments are included to compare global stiffnesses and maximum force levels where damage occurs. The FE responses are in good agreement with experimental data, indicating that the proposed boundary conditions approach is successful in representing the actual stiffness of the experimental setup in simulations.

Following the in-situ strength theories outlined in Section 3.3.1, the strength of 90° plies of the thick-ply specimen increases from unidirectional strength  $Y_T$  of 60 MPa to the in-situ strength  $Y_T^{is}$  of 95 MPa. However, a comparison of force-displacement responses revealed that employing such strength value leads to the overprediction of failure load. This overprediction in in-situ strengths may arise due to the assumption of linear material response, as it has been shown that the matrix could exhibit plasticity under transverse shear loading [126]. Still, the plastic material response is neglected in this study to keep the numerical modeling simple. Therefore, the  $Y_T^{is}$  value is lowered to 80 MPa to make simulation results consistent with the experiments. Conversely, for the calculated in-situ strength of the thin-ply laminate, where the unidirectional strength  $Y_T$  of 60 MPa increases to the in-situ strength  $Y_T^{is}$  of 111 MPa, simulations accurately predict the force at the onset of damage. This indicates the improved capability of in-situ strength theories for the strength calculation of bending-dominated brittle failure of thin-ply laminates compared to the shear-dominated failure of thick-ply ones.

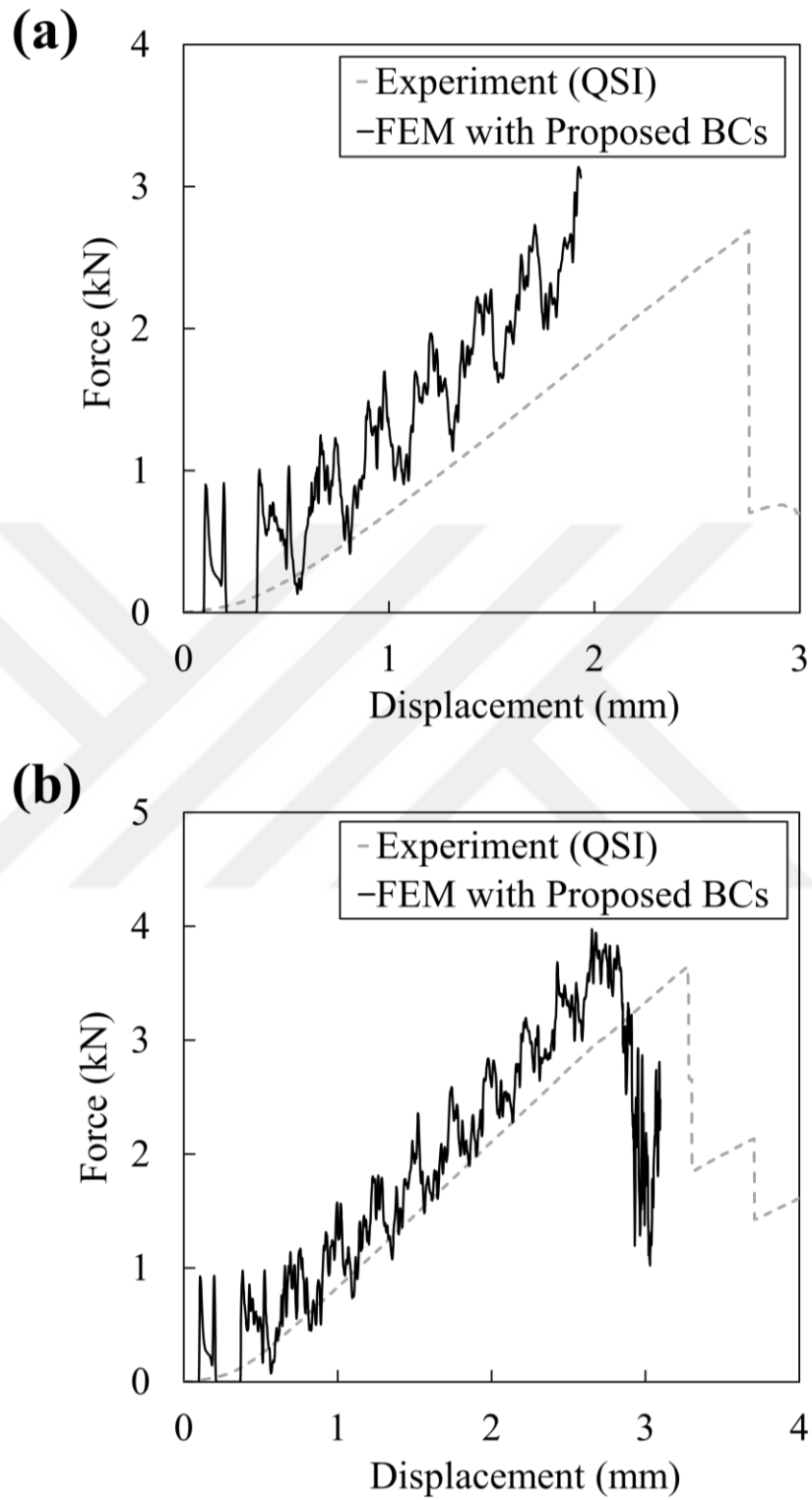
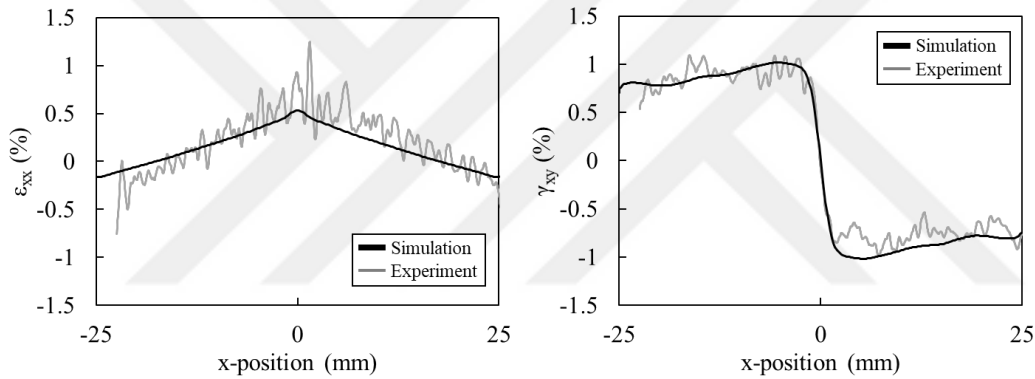


Figure 3.22. Impactor and indenter force-displacement responses of the FE model and QSI experiments for **(a)** thick-ply ( $[0_4/90_4/0_2]_s$ ) and **(b)** thin-ply ( $[0_2/90_2/0_2/90_2/0_2]_s$ ) specimens.

The experimental and numerical strain distributions along the thick-ply and thin-ply specimens, specifically longitudinal strain ( $\epsilon_{xx}$ ) and transverse shear strain fields ( $\gamma_{xy}$ ) extracted from the lower  $90^\circ$  ply groups, are plotted in Figure 3.23. For both specimens, the general trends of simulations are in agreement with the experimental ones. However, the  $\epsilon_{xx}$  distribution in Figure 3.23(a) and  $\gamma_{xy}$  distribution in Figure 3.23(b) show slightly higher local peaks in the experimental results compared to the simulation predictions, although the  $\gamma_{xy}$  and  $\epsilon_{xx}$  distributions in Figure 3.23(a) and (b) show no distinct localization. These slightly high localized values were attributed to the tested specimens' stochastic distribution of elastic properties causing a relaxed strain response locally, which is beyond the scope of this study to model.

**(a) Thick-ply specimen:**



**(b) Thin-ply specimen:**

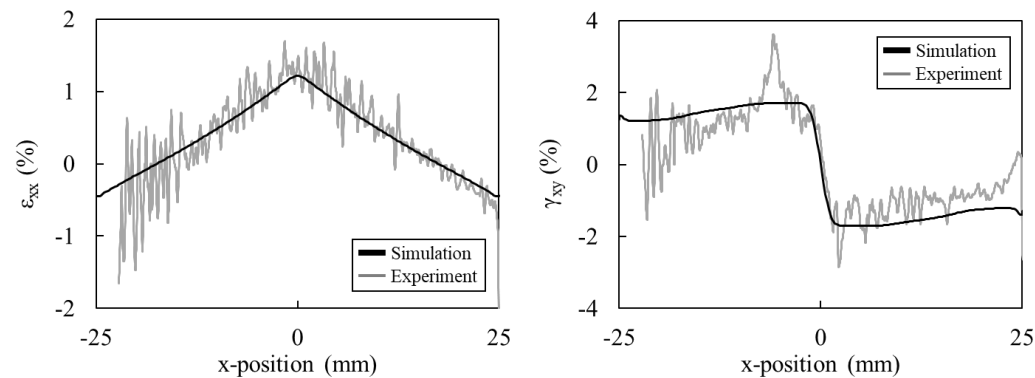


Figure 3.23. Longitudinal strain ( $\epsilon_{xx}$ ) and transverse shear strain ( $\gamma_{xy}$ ) distributions along the lowest  $90^\circ$  ply groups from simulations and experiments of **(a)** thick-ply and **(b)** thin-ply specimens.

The failure predictions from high-fidelity FE simulations are compared with the results obtained from the LVI experiments in Figure 3.24. The side-by-side images in Figure 3.24(a) compare the damaged states of thick-ply specimens from simulations on the left to the corresponding states from experiments on the right. Similarly, Figure 3.24(b) presents images of the damage at the center of the thin-ply specimen for simulations and experiments. In the thick-ply specimen, the matrix crack occurred approximately ~6 mm away from the impactor in experiments and ~2 mm in simulations, while in the thin-ply specimen it took place at the center beneath the impactor in both experiments and simulations. The variation of location and orientation of thick-ply matrix cracks are expected as the  $[0_5/90_3]_s$  specimen experienced a 4.4 mm variation in the initiation locations of matrix cracks in experiments summarized in Section 2.2.2. On the other hand, the thin-ply specimen successfully predicted central matrix cracking followed by delamination and fiber breakage. However, it could not predict the asymmetric damage seen in the upper layers in experiments since the numerical model is perfectly symmetric. Consequently, the numerical predictions are in good agreement with the experiments in terms of damage form, sequence, and location, indicating the predictive capability of high-fidelity models.

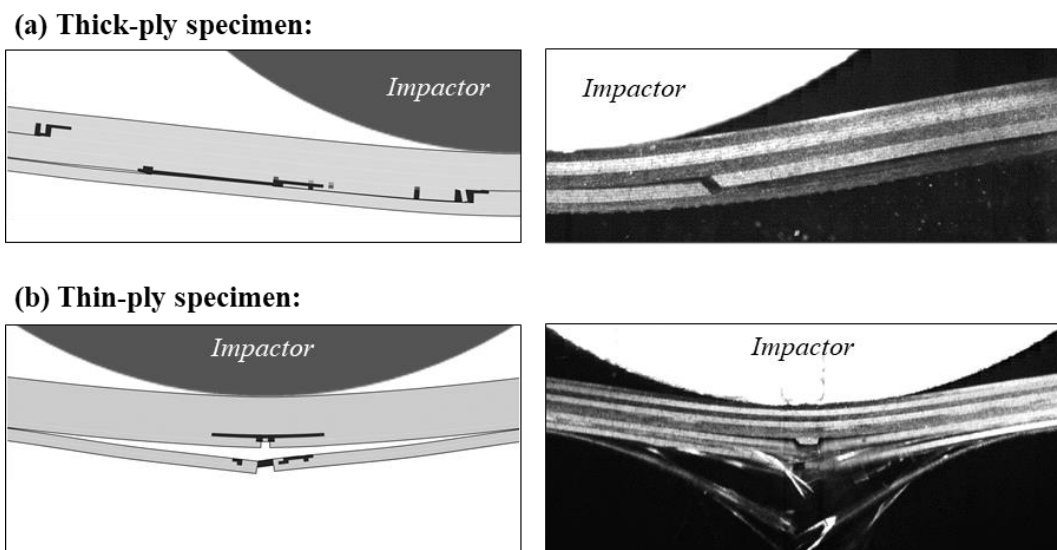


Figure 3.24. Damage patterns from simulations (left) and experiments (right) of **(a)** thick-ply and **(b)** thin-ply specimens.

### 3.6.3 Damage Mechanisms in Thick-ply and Thin-ply Laminates

The development of matrix cracking in the thick-ply specimen and its absence in the thin-ply specimen are elucidated in Figure 3.25. The figure presents fiber and matrix failure indicator stresses, specifically normal stresses ( $\sigma_{11}$ ) for  $0^\circ$  ply groups and maximum principal stresses ( $\sigma_p$ ) for  $90^\circ$  ply groups, plotted through the thickness distance for thick-ply and thin-ply specimens at the same instant. The  $\sigma_{11}$  distributions along  $0^\circ$  ply groups in Figure 3.25(a) align for both layups, except for the top and bottom plies experiencing slightly higher stresses in the thin-ply specimen. Conversely, the  $\sigma_p$  distributions along  $90^\circ$  ply groups in Figure 3.25(b) align only for the critical regions with higher stresses observed at the lower ply groups, yet both thick-ply and thin-ply layups experience similar  $\sigma_p$  levels in these regions. Consequently, both specimens are unlikely to undergo tensile fiber failure at these stress states, whereas the thick-ply specimen is on the verge of experiencing matrix damage within the lower  $90^\circ$  ply groups. The absence of damage in the thin-ply specimen is attributed to its increased in-situ strength.

To understand the occurrence of fiber damage observed within the thin-ply specimen, Figure 3.26 presents the evolution of normal stresses ( $\sigma_{11}$ ) for  $0^\circ$  ply groups and maximum principal stresses ( $\sigma_p$ ) for  $90^\circ$  ply groups are plotted across the thickness of thin-ply specimen for two different time frames:  $t = 560 \mu s$  and  $t = 840 \mu s$ . Following  $t = 560 \mu s$ , the thick-specimen experienced matrix cracking due to high shear stresses within  $90^\circ$  ply groups followed by delamination. It did not experience fiber failure as the  $\sigma_{11}$  in  $0^\circ$  ply groups remained below the tensile strength of the  $0^\circ$  plies  $X_T$ . Conversely, the thin-ply specimen exhibited no damage up to  $840 \mu s$  due to its increased in-situ matrix strength. However, as the  $\sigma_p$  approached the  $Y_T^{is}$  strength at  $840 \mu s$ , the  $\sigma_{11}$  in  $0^\circ$  ply groups also approached the  $X_T$  strength, as in Figure 3.26(a) and (b), in contrast to the observations in the thick-ply specimen. The increased values of  $Y_T^{is}$  according to the in-situ strength concept facilitated the observation of a damage pattern matching with the experiments, involving fiber breakage.

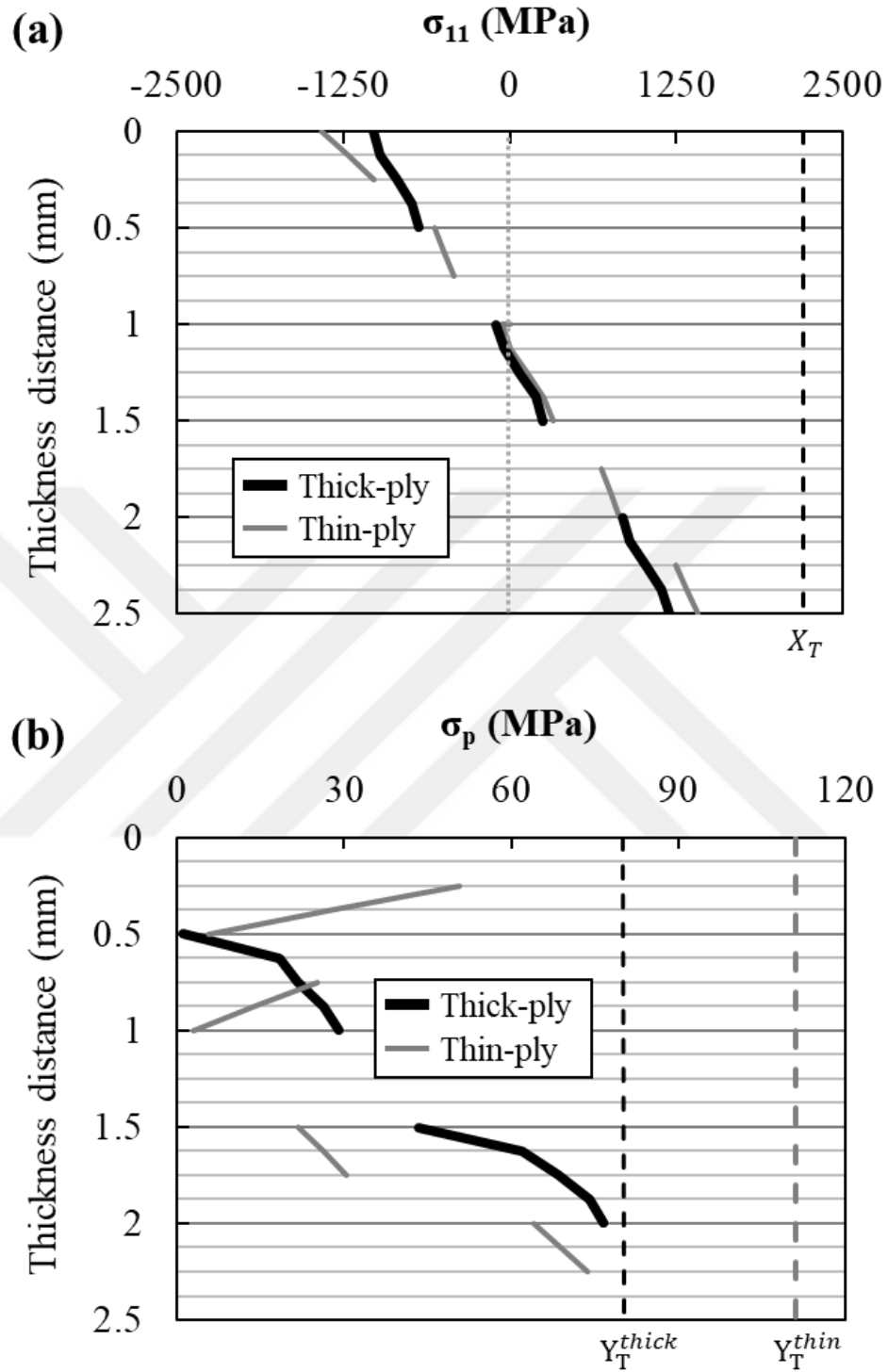


Figure 3.25. Through-the-thickness distributions of **(a)** normal stresses ( $\sigma_{11}$ ) for  $0^\circ$  ply groups and **(b)** maximum principal stresses ( $\sigma_p$ ) for  $90^\circ$  ply groups of thick-ply ( $[0_4/90_4/0_2]_s$ ) and thin-ply ( $[0_2/90_2/0_2/90_2/0_2]_s$ ) specimens at  $t = 560 \mu s$ .

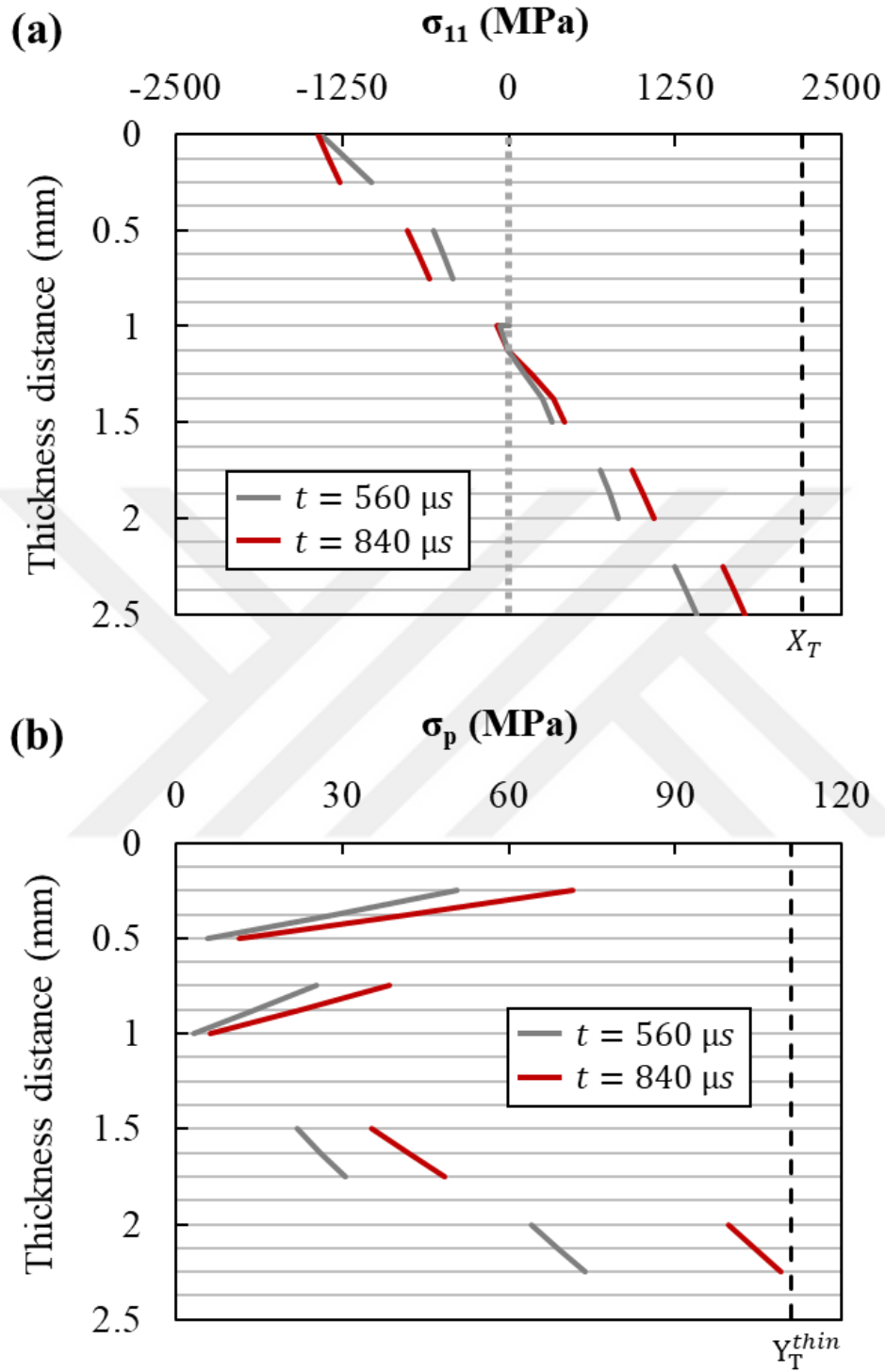


Figure 3.26. Evolution of **(a)** normal stresses ( $\sigma_{11}$ ) for  $0^\circ$  ply groups and **(b)** maximum principal stresses ( $\sigma_p$ ) for  $90^\circ$  ply groups for time frames of  $560 \mu s$  and  $840 \mu s$  for the thin-ply specimen ( $[0_2/90_2/0_2/90_2/0_2]_s$ ).

### 3.7 Conclusions

In this study, we performed *in situ* low-velocity impact (LVI) and quasi-static indentation (QSI) experiments on carbon fiber-reinforced polymer (CFRP) composite beams with two different layup configurations denoted as thick-ply ( $[0_4/90_4/0_2]_s$ ) and thin-ply ( $[0_2/90_2/0_2/90_2/0_2]_s$ ). The layup configurations had an equal number of  $0^\circ$  and  $90^\circ$  plies but differed in the number of plies clustered together inside the laminates. The LVI experiments are conducted using high-speed photography to observe the complete sequence of damage progression, while QSI experiments allow us to qualitatively identify the damage mechanisms observed during LVI events. Following the experiments, we performed numerical simulations of the LVI experiments. Our main conclusions are listed as follows:

- The experiment results on specimens with identical layup configurations reveal that the form, location, and sequence of damages observed in the specimens are highly consistent between the LVI and QSI experiments. These findings provide a corollary to the previous study conducted on  $[0_5/90_3]_s$  specimens in [44], which can be extended to the  $[0_4/90_4/0_2]_s$  and  $[0_2/90_2/0_2/90_2/0_2]_s$  specimens under investigation in this work.
- Our experimental findings indicate that clustering two  $90^\circ$  plies together within the laminate ( $[0_2/90_2/0_2/90_2/0_2]_s$  specimen) leads to a complex damage mechanism involving central matrix cracking followed by delamination and fiber breakage. Conversely, the clustering of four  $90^\circ$  plies together ( $[0_4/90_4/0_2]_s$  specimen) results in the demonstration of off-center matrix cracking followed by delamination, without any discernible fiber breakage.
- Despite macroscopic observations suggesting immediate delamination after matrix cracking, our *in situ* microscopic examinations during QSI experiments revealed the occurrence of slit matrix cracks that do not exert any substantial effect on the global response of the specimen. These cracks become macroscopically visible only when delaminations emanate from them.
- The thin-ply configuration demonstrated its initial failure at a load level 33% higher than the thick-ply laminate, accompanied by a sudden drop in the load. The occurrence of fiber breakage in thin-ply laminate resulted in a more complex damage mechanism, but also contributed to a higher load-carrying capacity. An increase in the number of interfaces exhibited an improvement in the damage-

tolerant nature of the laminate as it failed gradually with multiple load drops, akin to the outcomes reported in [117,121].

- The simulation results for both laminate configurations are in good agreement with experiments in terms of deformation fields, damage pattern and damage sequences.
- For the thick-ply laminate, the simulations accurately predicted the matrix cracking-induced delamination damage, which has a similar shear-dominated failure mechanism that was investigated in Chapter 2 in detail.
- For the thin-ply laminate, the simulations accurately predicted the matrix cracking-induced delamination followed by fiber breakage seen in experiments. Specifically, the damage sequence of delamination followed by fiber breakage is revealed in simulations, which could not be captured explicitly in the corresponding experiments.
- Our findings provide evidence supporting the in-situ strength concept under LVI loading: decreasing the ply-thickness increases the resistance to matrix cracking. We show that employing the in-situ concept in simulations plays a very important role not only in global response but also in failure mechanism choice.

### 3.8 Publications and Presentations

The work presented in this chapter resulted in the following publications:

1. Batmaz OA, Coker D. Effects of Thickness of Ply Groups on Damage Mechanisms in Cross-Ply Laminates Under Low-Velocity Impact. Manuscript to be submitted to Composites Part B: Engineering.
2. Batmaz OA, Coker D. In-Situ Damage Progression Observations in Cross-Ply CFRP Composite Beams Under Low-Velocity Impact and Quasi-Static Indentation Loading, IMECE2023-112520. In press, Proceedings of the ASME 2023 International Mechanical Engineering Congress and Exposition. Volume 13: Mechanics of Solids, Structures, and Fluids; Fracture and Failure of Reinforced Polymer Matrix Composite Materials.
3. Batmaz OA, Coker D. Effect of Ply Thickness on Dynamic Damage Progression in Cross-Ply Laminates Under Low-Velocity Impact. Proceedings of 9<sup>th</sup> ECCOMAS Thematic Conference on the Mechanical Response of Composites. <https://doi.org/10.23967/c.composite.2023.014>.

The work in this chapter is presented at the following international conferences:

1. Batmaz OA, Coker D. In-Situ Damage Progression Observations in Cross-Ply CFRP Composite Beams Under Low-Velocity Impact and Quasi-Static Indentation Loading. The ASME's International Mechanical Engineering Congress and Exposition IMECE2023, New Orleans, Louisiana, United States, 29<sup>th</sup> October – 2<sup>nd</sup> November 2023.
2. Batmaz OA, Coker D. Effect of Ply Thickness on Dynamic Damage Progression in Cross-Ply Laminates Under Low-Velocity Impact. 9<sup>th</sup> ECCOMAS Thematic Conference on the Mechanical Response of Composites (COMPOSITES 2023), Trapani, Sicily, Italy, 12<sup>th</sup> – 14<sup>th</sup> September 2023.



## CHAPTER 4

### CONCLUDING REMARKS AND FUTURE WORK

This thesis is divided into two independent yet connected parts.

In the first part of the thesis, in Chapter 2, a high-fidelity FE model is constructed to simulate the low-velocity line impact experiments conducted by Bozkurt and Coker [44] on  $[0_5/90_3]_s$  CFRP beams. The simulation incorporated a user-implemented three-dimensional continuum damage model with LaRC05 initiation criterion for matrix cracking, and the built-in cohesive zone model for delamination damage in the ABAQUS/Explicit software. The influence of boundary supports on the global impact response is found to be significant, therefore, a heuristic boundary conditions approach that replicates the experiment boundaries through the assembly of spring elements at the corresponding boundary nodes is proposed. In summary,

- An accurate numerical representation of the experimental boundary conditions is presented and the importance of modeling the complete elastic response of boundary structures under dynamic impact loading is demonstrated.
- The experimental observations on failure patterns of diagonal matrix cracks and failure sequences of matrix cracking induced delaminations are accurately simulated. In addition, simulations revealed the occurrence of intersonic delamination propagation during low-velocity impact events, which could not be observed in the corresponding experiments.
- The potential use of delamination crack tip speeds as a benchmark for refining numerical simulations is demonstrated. Increasing the effective interface toughness values, motivated by microcrack formation near interfaces, results in decreasing crack tip speeds with no noticeable effect observed in the global response.
- The limited reliability of relying solely on global impact response and post-mortem damage footprints without analyzing the dynamic progression of damage is demonstrated.

In the second part of the thesis, in Chapter 3, the influence of ply-group thicknesses on LVI-induced damage mechanisms in cross-ply CFRP beams with thick-ply ( $[0_4/90_4/0_2]_s$ ) and thin-ply ( $[0_2/90_2/0_2/90_2/0_2]_s$ ) layups and is investigated through *in situ* low-velocity impact (LVI) and quasi-static indentation (QSI) experiments, and high-fidelity simulations. LVI experiments utilized high-speed photography for observing *in situ* damage progression, while QSI experiments allowed magnified *in situ* observations on one free edge of the beam with a traveling digital microscope and full-field strain distributions on the opposite edge with a high-resolution camera. The high-fidelity simulations of LVI experiments are constructed by employing the numerical model developed in Chapter 2, including the damage models and the proposed boundary condition modeling approach. In summary,

- For thick-ply specimens ( $[0_4/90_4/0_2]_s$ ), where four  $90^\circ$  plies are clustered together within the laminate, our experiments showed off-center matrix cracking followed by delamination damage, exhibiting a shear-dominated failure mechanism similar to that observed in  $[0_5/90_3]_s$  layup, as extensively investigated in Chapter 2.
- For thin-ply specimens ( $[0_2/90_2/0_2/90_2/0_2]_s$ ), where two  $90^\circ$  plies are clustered together within the laminate, our experiments revealed a complex damage mechanism involving central matrix cracking, delamination and fiber breakage.
- From the QSI experiments, the occurrence of macroscopically undetectable slit matrix cracks prior to other damage modes, irrespective of whether the specimen is thick-ply or thin-ply, is revealed in *in situ* micrographs.
- The consistency in the aforementioned damage locations, damage patterns and sequences between the LVI and QSI experiments is demonstrated, extending Bozkurt and Coker's study on  $[0_5/90_3]_s$  specimens [44] to thick-ply and thin-ply specimens under investigation in this study.
- The comparison between experimental and high-fidelity numerical results provides evidence supporting the in-situ strength concept, where its incorporation in simulations played a very important role not only in global response but also in failure mechanism choice.

The findings of this thesis contribute to a better understanding of the impact-induced matrix cracking and dynamic delamination growth mechanisms, and the influence of ply-group thicknesses on damage mechanisms in composites subjected to low-velocity impact events. It also emphasizes the necessity of *in situ* experiments for detailed observations, and high-fidelity simulations to capture such observations without sacrificing physicality in studying the dynamic response of composite materials to impact events.

### ***Future Work***

To further explore and build upon the findings of this thesis, the following topics can be investigated:

- The boundary conditions modeling approach proposed in Chapter 2 has limitations specific to the transverse line-impact experiment setup under investigation. Developing a more generalized approach, capable of modeling any experimental boundary using digital image correlation data, would contribute significantly to the literature.
- Investigating the influence of strain-rate dependency on delamination crack tip speeds.
- Addressing the interaction between the continuum damage model and the cohesive zone model, and exploring the competition between these damage models.
- Expanding the experimental investigations on cross-ply CFRP laminates to include multi-directional laminates having plies oriented at different angles commonly used in the industry, and different ply materials from CFRP to understand material-specific behaviors.
- While the interest in utilizing thin-ply laminates is evident, given their advantageous effect in delaying the onset of damage, conducting compression-after-impact experiments on the tested specimens would provide valuable information on design considerations. Thick-ply laminates are expected to show increased compression-after-impact strength by delaying the buckling of the total laminate, and such experiments would offer insights into these design aspects.



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## APPENDICES

### A. Effect of Mesh Refinement on the Global Response and Damage

The effect of mesh refinement on the global impact response and damage patterns of the  $[0_5/90_3]_s$  layup is investigated in the FE model with a  $Y_T^{IS}/T_{o,II}$  ratio of 1.05. The in-plane dimensions of  $0.20 \times 0.20 \text{ mm}^2$ , and  $0.30 \text{ mm}$  along the thickness mesh sizes in the regular mesh model are halved to  $0.10 \times 0.10 \text{ mm}^2$  and thickness of  $0.15 \text{ mm}$  in the fine mesh model. In these models, the proposed boundary conditions modeling approach is employed, and the individual spring stiffnesses in the fine mesh model are adjusted such that the total stiffnesses of the regular and fine mesh models become equivalent. The comparison of impactor force-displacement responses and damage patterns of models with regular and fine meshes is presented in Figure A.1. From the global force-displacement responses, it is observed that the model with regular mesh experienced total collapse at an impactor displacement of  $0.99 \text{ mm}$  and the maximum force of  $9785 \text{ N}$ , whereas fine mesh model at  $0.99 \text{ mm}$  and  $9960 \text{ N}$ . Both models predicted the diagonal matrix cracking induced delamination damage accurately. The diagonal matrix crack is located at  $\sim 10.5 \text{ mm}$  and  $\sim 8.5 \text{ mm}$  away from the center of the beam in the regular and fine mesh models, respectively. Although the general trend of global responses of regular and fine mesh models is in excellent agreement, there occurred approximately  $2 \text{ mm}$  discrepancy in matrix cracking locations. Still, since the damage sequence and patterns are similar in both models, all analyses are conducted with the regular mesh sizes to decrease the computational burden.

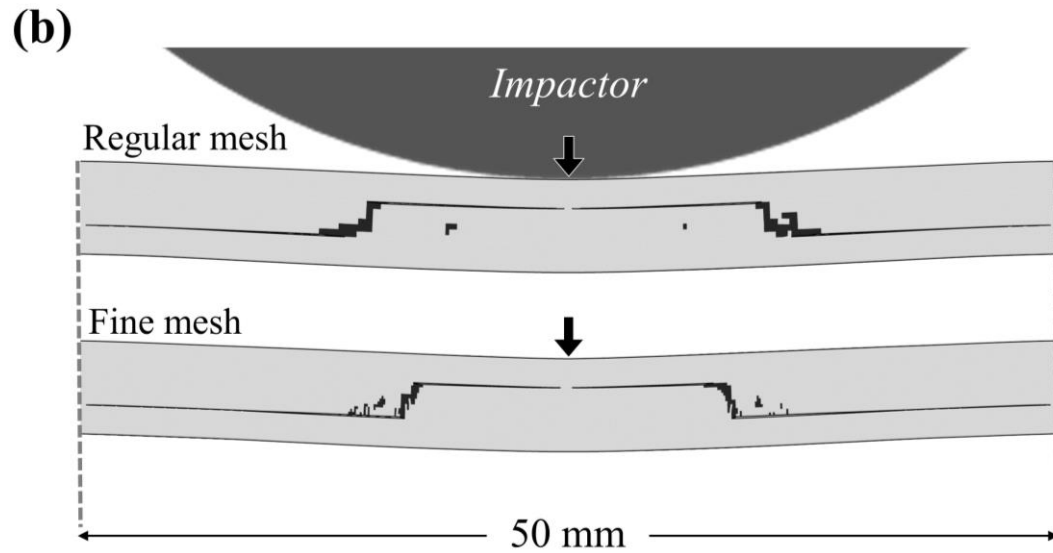
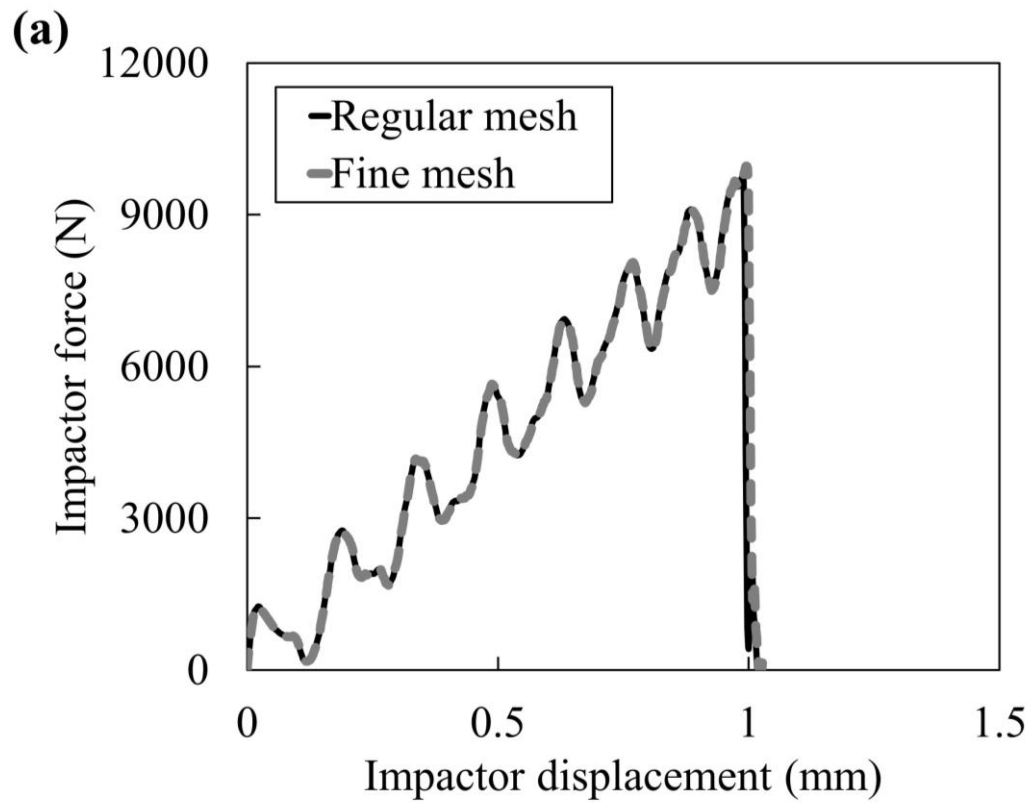


Figure A.1. **(a)** The impactor force-displacement responses and **(b)** damage patterns of models with regular and fine meshes.

## B. Effect of Stochastic Material Distribution on the Damage Patterns

The effect of stochastic material property distribution on the impact-induced damage patterns is investigated by assigning different  $Y_T^{is}$  strength values across the specimen. These values follow a Gaussian distribution with a mean of 90 MPa and a standard deviation of 4.5 MPa. This assignment is performed individually for each element within the 90° ply group along the test section of the beam using a Python script. Figure B.1(a) and (b) illustrate the stochastic distribution of strength values and a schematic representation of elements with these stochastic strength values, where each strength value is represented by a different color, respectively.

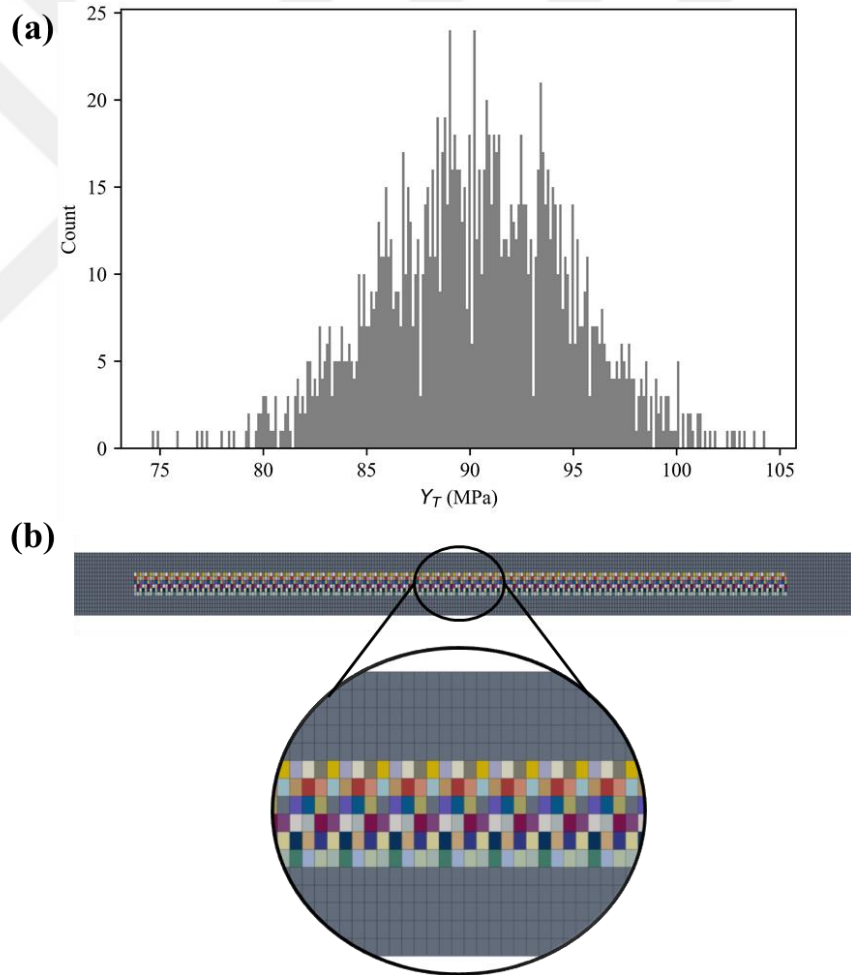


Figure B.1. **(a)** Gaussian distribution of  $Y_T^{is}$  strength values and **(b)** a schematic representation of elements within the 90° ply group with these stochastic strength values, where each strength value is represented by a different color.

The entire sequence of the damage observed in the FE model with a stochastic distribution of  $Y_T^{is}$  strength values is depicted in Figure B.2 with seven consecutive frames. The major matrix damage initiates vertically on the right-hand side of the beam at around 319  $\mu s$ , in comparison, the base model with uniform strength values exhibited a diagonal crack at 368  $\mu s$  symmetrically. When the matrix damage reaches interfaces, it triggers delamination damage. However, the stochastic distribution of strength values leads to irregular damage patterns as the delamination propagates. At 326  $\mu s$ , vertical matrix cracks occur on the left-hand side of the beam, accompanied by delaminations at the adjacent interfaces. Delaminations propagating from the opposite sides of the beam are observed to merge into one at the final state of the beam at 382  $\mu s$ .

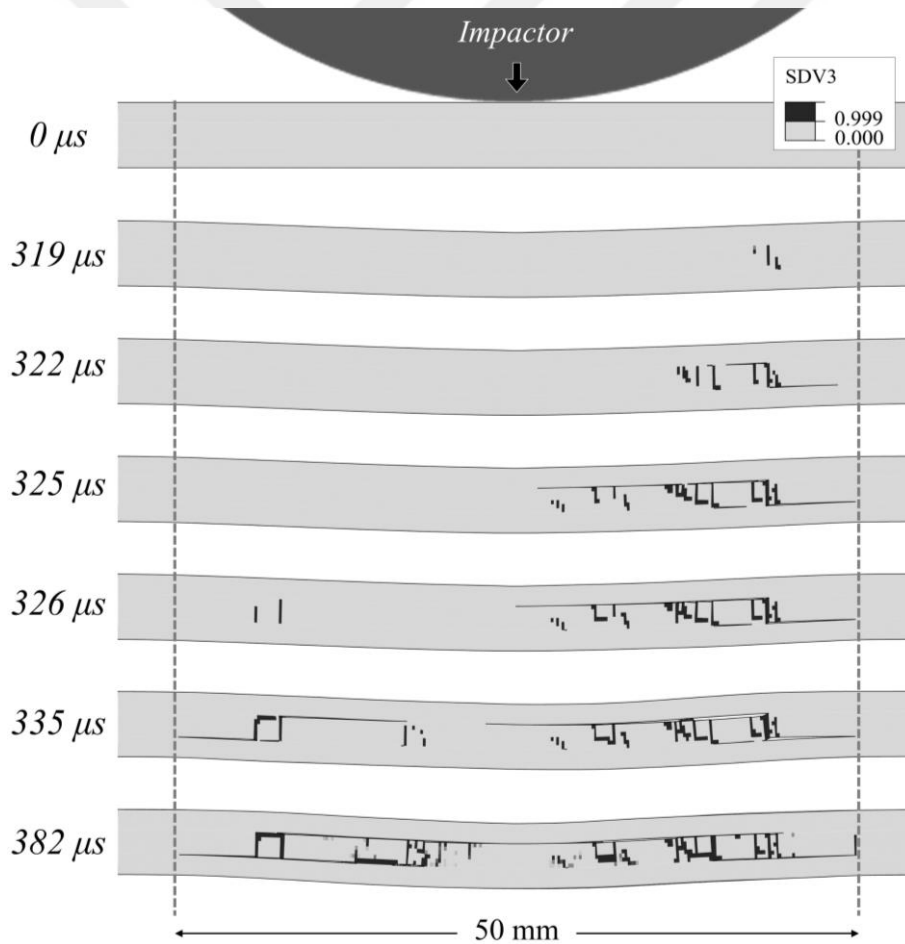


Figure B.2. Damage evolution of the FE model with a stochastic distribution of  $Y_T^{is}$  strength values.

### C. Stiffness Matrices of Thick-ply and Thin-ply Specimens

The strain-resultant relations following the Classical Lamination Theory are given by,

$$\begin{Bmatrix} N_x \\ N_y \\ N_{xy} \end{Bmatrix} = \begin{bmatrix} A_{11} & A_{12} & A_{16} \\ A_{12} & A_{22} & A_{26} \\ A_{16} & A_{26} & A_{66} \end{bmatrix} \begin{Bmatrix} \varepsilon_x^0 \\ \varepsilon_y^0 \\ \gamma_{xy}^0 \end{Bmatrix} + \begin{bmatrix} B_{11} & B_{12} & B_{16} \\ B_{12} & B_{22} & B_{26} \\ B_{16} & B_{26} & B_{66} \end{bmatrix} \begin{Bmatrix} \kappa_x \\ \kappa_y \\ 2\kappa_{xy} \end{Bmatrix}$$

$$\begin{Bmatrix} M_x \\ M_y \\ M_{xy} \end{Bmatrix} = \begin{bmatrix} B_{11} & B_{12} & B_{16} \\ B_{12} & B_{22} & B_{26} \\ B_{16} & B_{26} & B_{66} \end{bmatrix} \begin{Bmatrix} \varepsilon_x^0 \\ \varepsilon_y^0 \\ \gamma_{xy}^0 \end{Bmatrix} + \begin{bmatrix} D_{11} & D_{12} & D_{16} \\ D_{12} & D_{22} & D_{26} \\ D_{16} & D_{26} & D_{66} \end{bmatrix} \begin{Bmatrix} \kappa_x \\ \kappa_y \\ 2\kappa_{xy} \end{Bmatrix}$$

With the given material properties and laminate configurations, the results of the extensional stiffness  $[A]$ , the strain-curvature coupling stiffness  $[B]$ , and the bending stiffness  $[D]$  of the thick-ply and thin-ply laminates are as follows,

$$[A]^{thick} = \begin{bmatrix} 213.0 & 6.9 & 0 \\ 6.9 & 149.7 & 0 \\ 0 & 0 & 13.8 \end{bmatrix} \text{ GPa} \cdot \text{mm}$$

$$[A]^{thin} = \begin{bmatrix} 213.0 & 6.9 & 0 \\ 6.9 & 149.7 & 0 \\ 0 & 0 & 13.8 \end{bmatrix} \text{ GPa} \cdot \text{mm}$$

$$[B]^{thick} = \begin{bmatrix} 0 & 0 & 0 \\ 0 & 0 & 0 \\ 0 & 0 & 0 \end{bmatrix} \text{ GPa} \cdot \text{mm}^2$$

$$[B]^{thin} = \begin{bmatrix} 0 & 0 & 0 \\ 0 & 0 & 0 \\ 0 & 0 & 0 \end{bmatrix} \text{ GPa} \cdot \text{mm}^2$$

$$[D]^{thick} = \begin{bmatrix} 142.6 & 3.6 & 0 \\ 3.6 & 46.3 & 0 \\ 0 & 0 & 7.2 \end{bmatrix} \text{ GPa} \cdot \text{mm}^3$$

$$[D]^{thin} = \begin{bmatrix} 118.9 & 3.6 & 0 \\ 3.6 & 70.1 & 0 \\ 0 & 0 & 7.2 \end{bmatrix} \text{ GPa} \cdot \text{mm}^3$$