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Peeling Test of Advanced Rubber Materials – Finite Elements Modeling

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Abstract

Fiber-reinforced elastomeric isolators (FREIs), including unbonded solutions (UFREIs), are increasingly studied as cost-effective seismic isolation devices, whose performance depends strongly on the integrity of the rubber–fiber bond. Despite this relevance, dedicated numerical strategies for peel testing of rubber–textile/fiberglass assemblies remain limited, particularly when using standard commercial finite element environments. This thesis develops a finite element model of a peel test for a rubber–fiberglass system implemented in the commercial code *Straus 7*, with the primary objective of reproducing the global experimental force–displacement response available in the literature, in absence of a local interface-field measurements. A key contribution is the discrete modeling of the bonded interface through parallel cut-off bars connected between the same nodal pairs, enabling progressive loss of load transfer and controlled debonding using conventional element libraries. Model calibration is carried out through a sensitivity analysis of the cut-off bar parameters, identifying their influence on the macroscopic peel response and guiding the selection of a parameter set that best matches the experimental curve. The proposed framework provides a practical and interpretable tool for simulating peel behavior in rubber–fiber assemblies relevant to seismic isolator technology and establishes a basis for future improvements when additional interface-level data become available.

Key-words: Peeling test, Finite elements modeling, Rubber-textile assemblies, Cutoff bars.

Abstract in lingua italiana

Gli isolatori elastomerici fibrorinforzati (FREI), incluse le soluzioni non incollate (UFREI), sono sempre più studiati come dispositivi di isolamento sismico a basso costo, la cui prestazione dipende in modo significativo dall'integrità del legame gomma-fibra. Nonostante tale rilevanza applicativa, le strategie numeriche dedicate alla simulazione della prova di pelatura (peel test) su assemblaggi gomma-tessile/fibra di vetro risultano ancora limitate, soprattutto nell'ambito di codici commerciali standard agli elementi finiti. In questa tesi viene sviluppato un modello agli elementi finiti di peel test per un sistema gomma-fibra di vetro implementato nel software commerciale Straus 7, con l'obiettivo principale di riprodurre la risposta sperimentale globale forza-spostamento disponibile in letteratura, senza fare affidamento su misure locali dei campi all'interfaccia. Un contributo distintivo del lavoro è la rappresentazione discreta dell'interfaccia incollata mediante cut-off bars disposte in parallelo tra le stesse coppie nodali, che consente una perdita progressiva della capacità di trasferimento del carico e un'evoluzione controllata del distacco impiegando librerie di elementi convenzionali. La calibrazione del modello è condotta tramite una analisi di sensitività dei parametri delle cut-off bars, individuandone l'influenza sulla risposta macroscopica di pelatura e guidando la scelta del set di parametri che meglio approssima la curva sperimentale. Il framework proposto fornisce uno strumento pratico e interpretabile per la simulazione del comportamento in pelatura di assemblaggi gomma-fibra rilevanti per la tecnologia degli isolatori sismici e costituisce una base per futuri affinamenti qualora siano disponibili dati aggiuntivi a livello di interfaccia.

Parole chiave: Prova di pelatura, Modellazione agli elementi finiti, Assemblaggi gomma-tessuto, Barre a rottura.

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1 Introduction

Bonded rubber–reinforcement assemblies are increasingly employed in engineering applications where large deformations, high damping, and reliable load transfer are required. In these systems, the structural performance is often governed not only by the bulk hyperelastic response of the rubber, but also by the integrity of the bonded interface. Interfacial debonding under peeling-type loading is particularly critical because it couples severe rotations and bending of the adherends with progressive damage at the bondline, leading to rapid loss of effective stiffness and load-carrying capacity.

1.1. Motivations and scope

From a modelling standpoint, peel tests pose two main challenges. First, the global force–displacement response results from the interaction of test compliance and adherend kinematics with a highly localized process zone at the debonding front; therefore, global curves alone do not directly provide the local traction–separation law governing interfacial degradation. Second, rubber materials exhibit hyperelasticity and large strains, which introduce strong material and geometric nonlinearities and amplify sensitivity to boundary conditions and to the assumed interface formulation. As a consequence, standard cohesive-zone implementations may require user subroutines or advanced contact formulations, which are not always available or practical in all commercial FE environments.

The scope of this thesis is to develop and validate a directly implementable FE approach for predicting the global peel response of a rubber–reinforcement assembly using a discrete, spring-like interface representation. The work leverages experimental peel curves reported in the literature as a benchmark and adopts a practical modelling strategy in which the bonded region is discretized into portions and the interface at each portion is represented by a small set of parallel cut-off bars. This enables progressive debonding to be captured through element-level failure while keeping the implementation transparent and compatible with standard FE capabilities.

Within this scope, the objective is not to uniquely identify the true local interfacial traction–separation relation, but to calibrate the discrete interface parameters so that the FE model reproduces the key macroscopic features of the experimental response.

The final outcome is a calibrated modelling framework that can be used as a baseline for further works when additional local experimental measurements or mixed-mode interfacial characterization become available.

1.2. Thesis objectives and contributions

The objective of this work is to develop a finite element model using commercial software such as Straus7, to simulate the global peeling experimental response of advanced rubber materials used in the production of low-cost elastomeric seismic isolators. Specifically, the following objectives are pursued:

1. To interpret and utilize the experimental data obtained from previous peel laboratory test to calibrate the constitute material parameters and define a robust numerical model that accurately simulates the global experimental response of rubber-textile assemblies subjected to a standard peel test.
2. To implement a bonded interface in the peel-test FE model through a spring-like discrete representation using only cut-off-bar elements, leveraging their standard availability in commercial FE codes to obtain a robust, implementable interface model without custom constitutive subroutines.
3. To perform linear and nonlinear FE analyses and calibrate the FE model through a sensitivity-based analysis of the cut-off-bar parameters in the absence of an experimentally measured local interface traction–separation law, until the numerical global response matches the experimental peel curves as closely as possible.

1.3. Thesis outline

This thesis is organized into several chapters:

Chapter 2 introduces a state-of-the-art overview on elastomeric seismic isolators, describing their transition from conventional bearings to low-cost FREI/UFREI systems. It also contrasts traditional carbon-fiber solutions with emerging glass-fiber alternatives, outlines their key working mechanisms, and highlights the rubber–fiber interface, motivating peel testing as a key tool to assess material performance.

Chapter 3 presents the peel-test standards and experimental methodology relevant to this work, emphasizing ISO 36:2020 [1] as the dedicated reference for rubber–textile stripping/peel adhesion. This chapter also compiles the associated material/interface characterization and the experimental peel force–displacement curves, adopted in this

document, as the reference dataset for subsequent numerical modeling and validation stages.

Chapter 4 develops the interpretation of peel behavior in a multiscale sense, distinguishing the global force–displacement response from the local interface mechanics at the advancing peeling front. This chapter also introduces the reviews classical interface-modelling concepts and motivates the use of a practical, directly implementable discrete interface representation as a suitable modelling bridge between local constitutive assumptions and global peel observables.

Chapter 5 describes the Straus7 finite element model developed to reproduce the peel-test kinematics. It presents the adopted geometric idealization, element types, material assumptions, and boundary/loading conditions, and defines the discrete interface representation in which the bonded length is partitioned into portions, each modelled by three parallel cut-off bars.

Chapter 6 presents the calibration and sensitivity analysis. It first outlines the MATLAB-based workflow used to trilinearize the experimental peel curves and extract statistically consistent global targets. It then reports the systematic tuning of the cut-off-bar parameters to obtain the closest agreement between the FE response and the experimental global curves.

Chapter 7 summarizes the final calibrated results and evaluates the achieved agreement against the experimental targets. The chapter consolidates the main conclusions, states the limitations and outlines future developments for improving the FE formulation.

2 Literature review

This chapter presents a literature-based background on seismic isolation and elastomeric bearing technologies, starting from conventional steel-reinforced bearings (SREIs) to low-cost FREI/UFREI systems, outlining their key mechanisms and practical limitations. It then summarizes advanced rubber compounds and textile reinforcements, contrasting carbon- and glass-fiber solutions, and identifies the rubber–fiber interface as a critical issue motivating standardized peel testing.

2.1. Seismic isolation and conventional elastomeric bearing.

Seismic isolation is a widely recognized strategy to protect structures by interposing a flexible and energy-dissipating layer between the superstructure and the foundation. Among the different isolation devices, elastomeric isolators have become one of the most common solutions, owing to their ability to provide flexibility in the horizontal directions while maintaining adequate vertical load-carrying capacity and, when properly formulated, inherent damping. Conventional elastomeric bearings are typically composed of alternating layers of rubber and steel plates, vulcanized together to form a laminated unit. These devices are often referred to as steel-reinforced elastomeric isolators (SREIs) and are widely used in bridges and buildings.

Despite their proven effectiveness, traditional SREIs require thick steel laminations, anchorage plates, and bolting systems, all of which contribute significantly to the overall cost and complexity of fabrication and installation. In many regions, particularly in developing countries or for low- to mid-rise buildings, the cost of conventional isolation systems can be prohibitive, effectively limiting the widespread adoption of seismic isolation in ordinary construction. This socio-economic constraint has motivated the search for simplified and low-cost elastomeric isolators, which can offer acceptable seismic performance while being affordable, easy to manufacture, and easy to install.

2.1.1. Fiber-reinforced elastomeric isolators (FREIs) as low-cost alternatives

A major step in this direction has been the development of fiber-reinforced elastomeric isolators (FREIs), in which the internal steel plates are replaced by thin fiber-reinforcing layers. These fibers are typically arranged as woven fabrics or laminates embedded within the rubber during vulcanization (Figure 2.1(a)). By substituting steel with fiber reinforcement, FREIs can significantly reduce the amount of steel required, the weight of the device, and the need for complex anchorage details. Furthermore, when specific installation configurations are adopted, the isolators can be used without mechanical bolting to the superstructure and substructure, which further reduces cost and simplifies on-site operations.

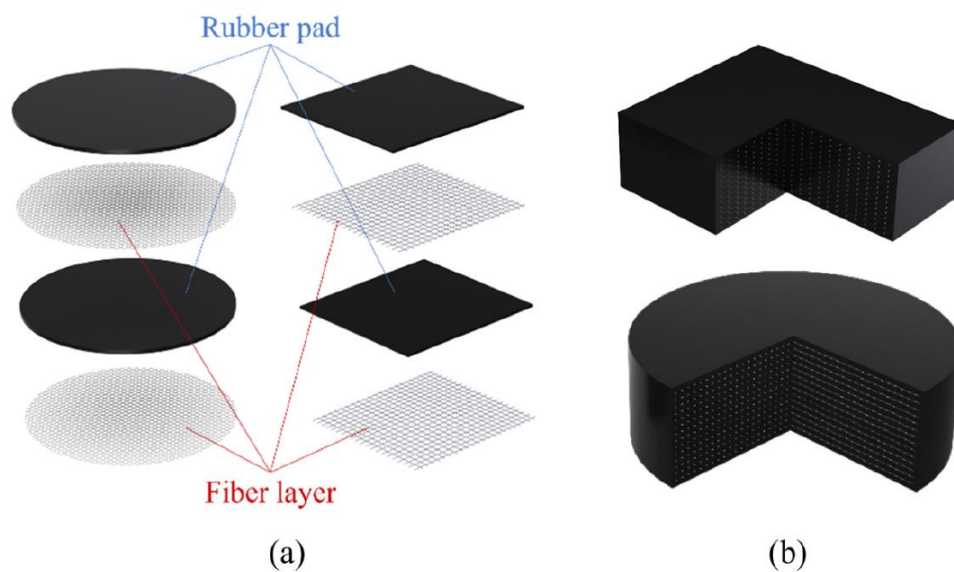


Figure 2.1 Fiber-reinforced elastomeric isolator: (a) layers, (b) unbonded type

Within the broad family of FREIs, a particularly interesting concept is the unbonded fiber-reinforced elastomeric isolator (UFREI). In a UFREI configuration (Figure 2.1(b)), the elastomeric bearing is placed between two rigid plates (such as steel or concrete) without being bonded to them through glue or mechanical anchors. Under vertical load, the rubber is confined by the fiber reinforcement and by the contact surfaces at the top and bottom; under lateral displacement, the device deforms predominantly in shear and develops a characteristic “rollover” mechanism at its edges. As lateral displacement increases, part of the bearing progressively loses contact with the supporting surface and “rolls” over, which leads to an initial softening of the lateral stiffness. At larger displacements, contact is re-established over a different region of the bearing, giving rise to a stiffening response. This softening–stiffening behavior is one of the key mechanical signatures of UFREIs.

The UFREI concept brings several advantages in terms of cost and constructability. The absence of bonding and mechanical anchorage eliminates the need for anchor bolts and thick steel end plates and allows relatively simple installation: the bearing can be placed between two flat surfaces with minimal detailing. The use of fiber reinforcement instead of steel laminations further reduces material cost and device weight. As a result, UFREIs are strong candidates for low-cost seismic isolation systems, particularly suitable for low- to medium-rise buildings and applications where moderate vertical loads are expected. However, these benefits come with important design challenges, including the control of sliding at the contact interfaces, the stability of the bearing under combined vertical and lateral loads, and the limited tensile capacity in the vertical direction.

2.1.2. Advanced rubber materials for seismic isolation

The performance of both bonded and unbonded FREIs depends critically on the mechanical properties of the rubber and on the type of fiber reinforcement. Traditional formulations rely heavily on natural rubber (NR), which offers excellent elasticity and fatigue resistance, but may be susceptible to aging and supply constraints. Recent research has therefore explored advanced rubber compounds, including blends of natural and synthetic rubbers (such as NR–EPDM, NR–BIIR, or IR–BR), as well as high-damping rubbers (HDR) specifically designed to provide increased energy dissipation under cyclic loading. These advanced elastomeric materials aim to combine adequate stiffness, durability, and damping, making them attractive for seismic isolation applications and compatible with low-cost manufacturing routes.

In parallel, different types of fiber reinforcement have been investigated in the literature, including glass, Kevlar, and carbon fibers. Among these, carbon-fiber reinforcement has often been used as a “reference” or “traditional” choice in advanced FREI systems due to its high stiffness and strength, which contribute to better confinement of the rubber, reduced bulging under vertical load, and improved overall stability. However, carbon fibers are relatively expensive compared to other textile materials, and their cost can be a limiting factor when the goal is to develop isolation systems for ordinary or low-income housing.

2.1.2.1. Glass-fiber–reinforced FREIs

Motivated by these economic considerations, recent work has proposed glass-fiber–reinforced FREIs as a low-cost alternative to carbon-fiber systems. Glass fibers are cheaper and widely available, although their elastic modulus is significantly lower (approximately one third) than that of carbon fibers, which may lead to larger rubber bulging and reduced vertical stability if not carefully accounted for. In this context, research such as that conducted in Pianese et. al [2]. has focused on the experimental investigation of fiber-reinforced elastomeric isolators that utilize woven glass fiber

fabrics as internal reinforcement, combining it with a high-damping rubber compound selected from several candidate formulations.

A crucial aspect of any fiber-reinforced elastomeric system is the quality of the bond between the rubber and the fiber reinforcement. While bonding technologies for rubber–steel interfaces in conventional SREIs are well established, the rubber–fiber interface (whether carbon, glass, or other textiles) is less standardized and must be verified experimentally. Poor adhesion at this interface can lead to delamination, premature failure, and loss of stiffness or stability of the isolator. For this reason, Pianese et al. [2] devote special attention to the characterization of the glass-fiber–rubber interface through peel tests performed according to ISO standards.

3 Peeling Test: Fundamentals and Standard Framework

This chapter presents the theoretical and standards-based framework for peel testing and defines the experimental reference dataset adopted in this research. It first introduces peel-test fundamentals and the classification of standard geometries, then summarizes the key procedures of ISO 11339 [3] and ISO 36:2020 [1], including specimen requirements, test conditions, and reported measurements. Finally, it describes the peel-test campaign reported by Pianese et al., identifies the investigated bonding systems, and motivates the selection of the experimental curves as the baseline dataset for subsequent data processing and numerical calibration of the mathematical model adopted.

3.1. Peel Test general description

Peel tests are a fundamental experimental tool to quantify the resistance of a bonded interface to progressive debonding, i.e., the force required to advance a crack-like separation front while one (or both) adherends deform as “peel arms”. In contrast to pure fracture tests on stiff arms, peel measurements are strongly influenced by the test kinematics (peel angle, fixtures) and by the bending/viscoelastic dissipation of the adherends and adhesive layer. This dependency is well established in the classical mechanics of peeling, which shows that the measured peel force reflects not only interfacial adhesion but also the elastic/bending contribution of the peeled arm and rate-dependent dissipation mechanisms.

For this reason, peel standards are most often used as comparative or qualification methods, for instance to screen surface treatments, primers and processing conditions, and to monitor manufacturing consistency, rather than as direct design values for structural joints.

From an engineering perspective, peel testing is particularly important in systems governed by delamination-type failures (progressive loss of bond). This is the case of rubber-fiberglass interfaces, as found in fiber-reinforced elastomeric seismic isolators (FREI), where the peel test is essential for assessing adhesion between high-damping rubber pads and fiberglass fabrics.

3.2. Classification and Standard Geometries

Peel testing requires at least one flexible adherend. The term ‘flexible’ refers to the ability of the adherend to bend through 90° without breaking or cracking. The result quoted for peel tests is the constant load per unit width required to continue peeling the joint after initiation, determined from the flat portion of the force–extension curve [4]. Since the measured response depends on the test geometry (peel angle, fixture configuration) and on the flexibility of the adherends, peel methods are commonly classified according to:

1. **The adherend type:** Flexible–flexible vs Flexible–rigid
2. **The nominal peel angle** (e.g., 90° or 180°).

In flexible–flexible assemblies, where both adherends are compliant, the typical standardized configuration is the T-peel test, in which the two free arms are pulled apart to form a “T”; this geometry is commonly used to assess adhesion in films, textiles, flexible laminates, and other bonded flexible sheets. In flexible–rigid assemblies, where a flexible peel arm is bonded to a rigid substrate, peel resistance is typically characterized using 90° or 180° peel configurations, often with dedicated fixtures to maintain the nominal angle during propagation. In the so called, back-peel for instance, the flexible arm is peeled back at a nominal angle close to 180° . These methods are widely applied to tapes and adhesive films bonded to rigid plates such as metals, composites, or concrete. Figure 3.1 illustrates the most common standardized peel-test configurations and lists representative standards associated with each of them.

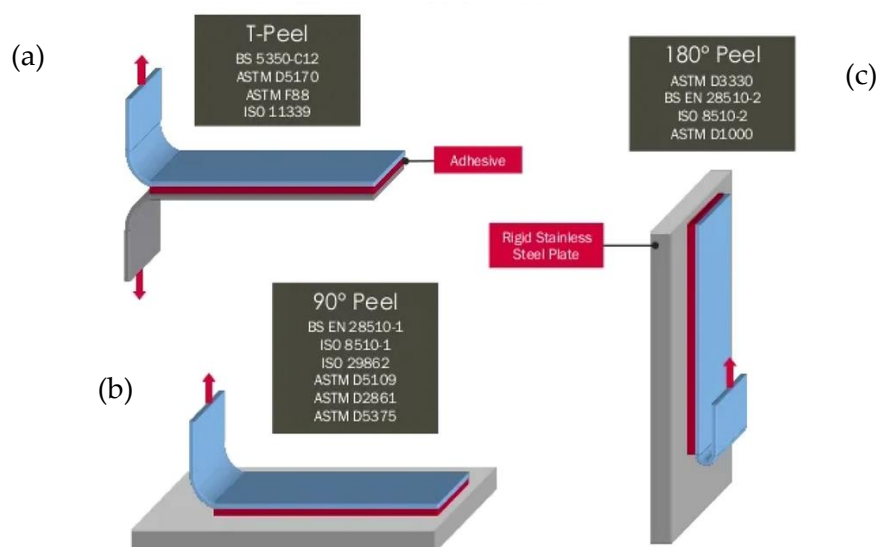


Figure 3.1 Schematic overview of common peel-test configurations and associated standards: (a) T-peel, (b) 90° Peel (c) 180° Peel.

3.2.1. “T” - peel test for flexible-to-flexible bonded assemblies – ISO 11339

The T-peel test has been adopted by most standards bodies (e.g. ISO 11339, ASTM D 1876) and is widely used to determine the peel resistance of an adhesive in flexible–flexible bonded assemblies by measuring the force required to propagate separation along the bond line in a “T”-shaped specimen. The standard clarifies that the result is primarily comparative rather than a direct design value, since the response depends on the test kinematics and on the compliance of the adherends; in a T-peel configuration, the angle between the applied force and the bond line is not constant.

Testing is performed on a universal testing machine with continuous force–displacement recording, using self-aligning grips that firmly clamp the unbonded ends (e.g., the outer 25 mm) to ensure uniform load transfer across the specimen width. A typical crosshead speed of 100 mm/min is recommended (with lower speeds such as 10 mm/min indicated for other adherends)

Specimens prepared with a standardized 25 mm wide, as shown in Figure 3.2(a), have a minimum bonded length of 150 mm and 50 mm long arms. The free ends are bent in opposite directions to form the “T” geometry Figure 3.2(b). At least five specimens should be tested after conditioning in a standard atmosphere (ISO 291), and the mean adhesive-layer thickness is also measured after curing with a resolution of ± 0.01 mm.

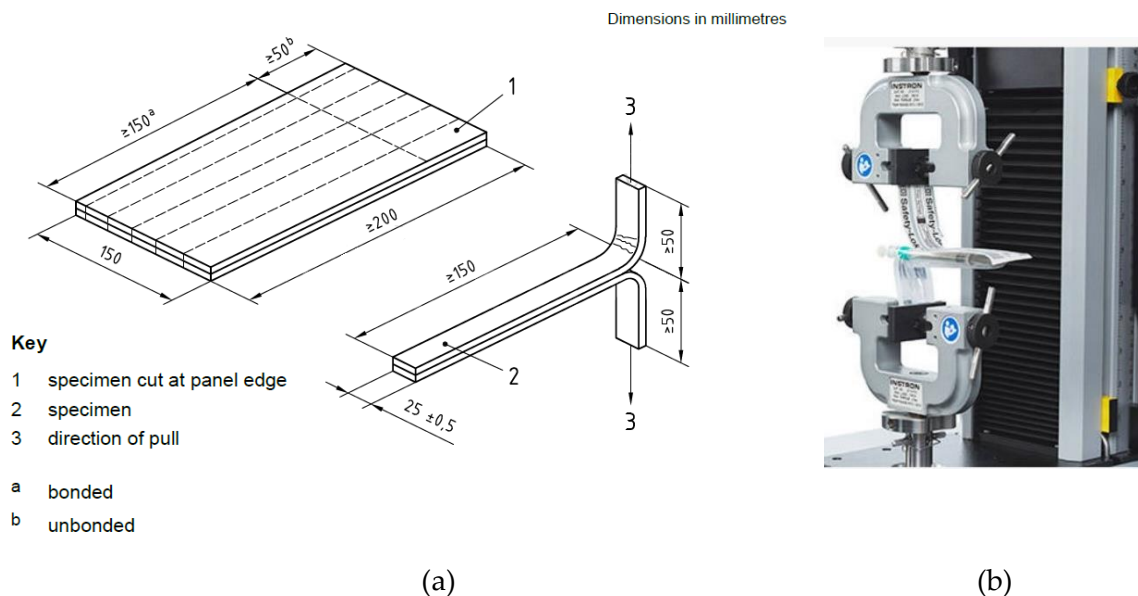


Figure 3.2 ISO 1139 Standard (a) Test specimens geometry, (b) Specimens clamped in the grips of universal machine testing.

During testing, the force curve is recorded, and peeling continues until at least 150 mm of bonded length is separated. The primary output is the force history (force–

displacement or force–peeled length), commonly normalized as force per unit width $\frac{F}{b}$ [N/mm], enabling comparison among specimens of equal width.

Results are reported as the mean peel force calculated over a minimum 100 mm peel interval, excluding the first and last 25 mm to avoid start-up/end effects, together with maximum and minimum forces and the failure mode in accordance with ISO 10365 [5], as well as full details of the adhesive, adherends, surface preparation, curing parameters, and test conditions.

3.2.2. 180° Stripped peel test for rubber, vulcanized or thermoplastic adhered to textile fabrics - ISO 36:2020

Although ISO 11339 is commonly adopted for T-peel testing of flexible-to-flexible adhesive joints, a more specialized standard is available when the objective is to characterize a rubber–textile interface. In particular, ISO 36:2020 is dedicated to rubberized fabrics and specifies a stripping/peel procedure to quantify adhesion between rubber and textile plies (or between two fabrics bonded with rubber). Therefore, for assessing the bond between rubber and fiberglass fabric reinforcement—as required in FREI/UFREI systems—ISO 36 is the most technically appropriate reference.

The method applies to approximately plane plies (or cylindrical surfaces with internal diameter $> \sim 50$ mm) and explicitly excludes coated fabrics and textile conveyor belts.

The test is performed on a constant-rate tensile machine compliant with ISO 5893, equipped with grips preventing slip. Test pieces are flat strips of $25 \text{ mm} \pm 0.5 \text{ mm}$ width and sufficient length to allow separation to be measured over at least 100 mm.

To start the test in a controlled way and to allow proper gripping, the standard requires a starter tab: approximately 50 mm at one end is intentionally kept unbonded during specimen preparation. This is achieved by masking that region with a thin plastic film or tape so that rubber and fabric do not bond there during processing (Figure 3.3(a))

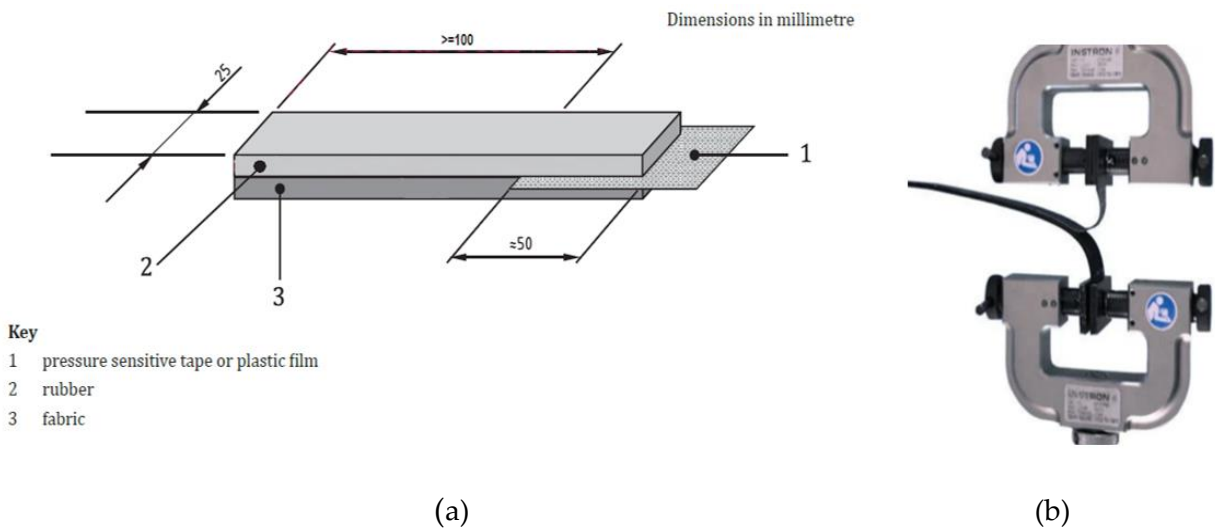


Figure 3.3 (a) Test pieces geometry ISO 36:2020 , (b) Test pieces clamped in the grips of universal machine testing.

After curing/vulcanization, the two layers are already separated over this length, forming two free tabs that can be clamped in the grips as shown in Figure 3.3 (b). During the test, the main body of the specimen is clamped in the non-driven grip, while the ply to be separated is clamped in the driven grip. The setup is adjusted to avoid twisting and to keep the clamped strips in the same plane, aiming for an approximately 180° peel configuration. Unless otherwise specified, three test pieces shall be tested for each direction of test.

The experiment is performed at a crosshead speed of $50 \text{ mm/min} \pm 5 \text{ mm/min}$, while the force is continuously recorded as peeling propagates over the required length. Adhesion strength is reported as force per unit width F/b in $[\text{N/mm}]$, obtained by evaluating the recorded force trace using the procedures specified by the standard and dividing by the specimen width b . Outputs are typically reported using the median of individual test values, together with standardized failure-mode classification (interfacial separation, cohesive rubber failure, fabric failure, etc.).

Table 3.1 summarizes the key specimen requirements, test conditions, and measurement outputs prescribed by ISO 36:2020 for rubber–textile peel (stripping) testing.

Table 3.1 Specimen requirements and test condition of peeling test for rubber-textile assemblies – ISO 36:2020.

Item	Standard ISO 36:2020
Purpose	Adhesion by stripping/peel for rubber–textile assemblies (or fabric–fabric bonded with rubber)
Test type	Nominal $\sim 180^\circ$ stripping/back-peel configuration
Specimen geometry	Rectangular strip
Width (b)	25 mm $\pm$ 0.5 mm
Length	Sufficient to measure peeling over ≥ 100 mm
Starter tab (unbonded length)	~ 50 mm (masked with film/tape or manually pre-separated)
Grip arrangement	Specimen body in non-driven grip; ply to be peeled in driven grip; minimize twist and keep strips coplanar
Test speed	50 mm/min $\pm$ 5 mm/min
Measured quantity	Force trace during peeling (force vs displacement/peeled length)
Main reported result	Adhesion strength (F/b) in [N/mm]; Failure mode observed

3.3. Experimental dataset analyzed

As highlighted by Pianese et al.[2] the adhesion between fiber fabrics and rubber remains scarcely investigated in the literature. While rubber–steel bonding is well established in industrial practice, the rubber–textile interface is less standardized and must be experimentally verified to avoid premature debonding/delamination. To address this gap, Pianese et al.[2] performed peel tests to compare different chemical treatments and select the best-performing solution. Considering this limited experimental background and the need for reliable data to analyze debonding mechanisms, the present thesis adopts the experimental peel-test results reported by Pianese et al. [2] as the reference dataset to develop and calibrate a numerical model capable of reproducing the peeling response of rubber–fiberglass assemblies.

3.3.1. Bonding systems investigated

In the study by Pianese et al. [2], the peel test is performed to evaluate the stripping force required to separate two plies of fabric bonded with rubber by following the procedure specified in the ISO 36:2020 standard. To do so, three bonding solutions are considered:

- Chemosil 231G
- Chemosil 5130 + 231G
- Chemosil 211 + NL411.

These are LORD chemical products for the adhesion of rubber to rigid and flexural substrates. The peel-test specimen is rectangular, with width 25 [mm] $\pm$ 0.5 [mm] and length 200 [mm]. The standard requires the length to be sufficient to allow ply separation over at least 100 [mm] during the measurement.

The specimens are vulcanized while masking one end (50 mm) using a plastic film so that the edge remains adhesion-free; this produces separated tabs that can be clamped in the testing machine grips. During the test, one end is securely clamped and the other end is pulled under displacement control at 50 [mm/min]. The force required to peel the rubber layer from the fabric is continuously recorded. Test results are evaluated as adhesion strength, defined as the force per unit width [N/mm] needed to cause complete separation at the interface, and the failure mode (adhesive/cohesive/mixed) is examined. as shown in Figure 3.4.

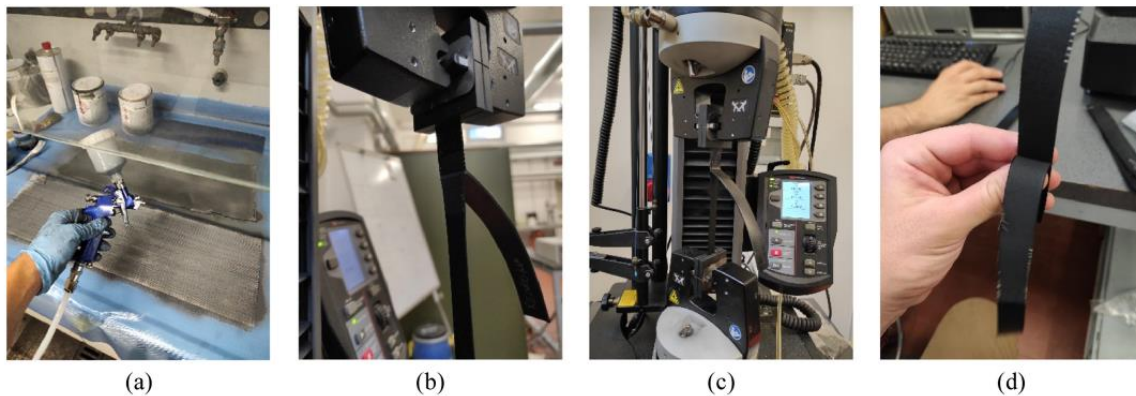


Figure 3.4 Peel test by Pianese et al. (a) preparation of the specimens, (b) specimen before, (c) during and (d) after testing.

The peel-test results reported by Pianese et al. are summarized in Figure. 3.4, with subfigure 3.4(a), corresponding to Chemosil 231G. It shows three curves—Specimen 1, Specimen 2, and Specimen 3, plotted as force per unit width [N/mm] versus displacement [mm]. These specimens are replicates, prepared and tested under the

same ISO 36 procedure and bonding treatment, with experimental scatter between the curves reflecting the inherent variability of interfacial peeling.

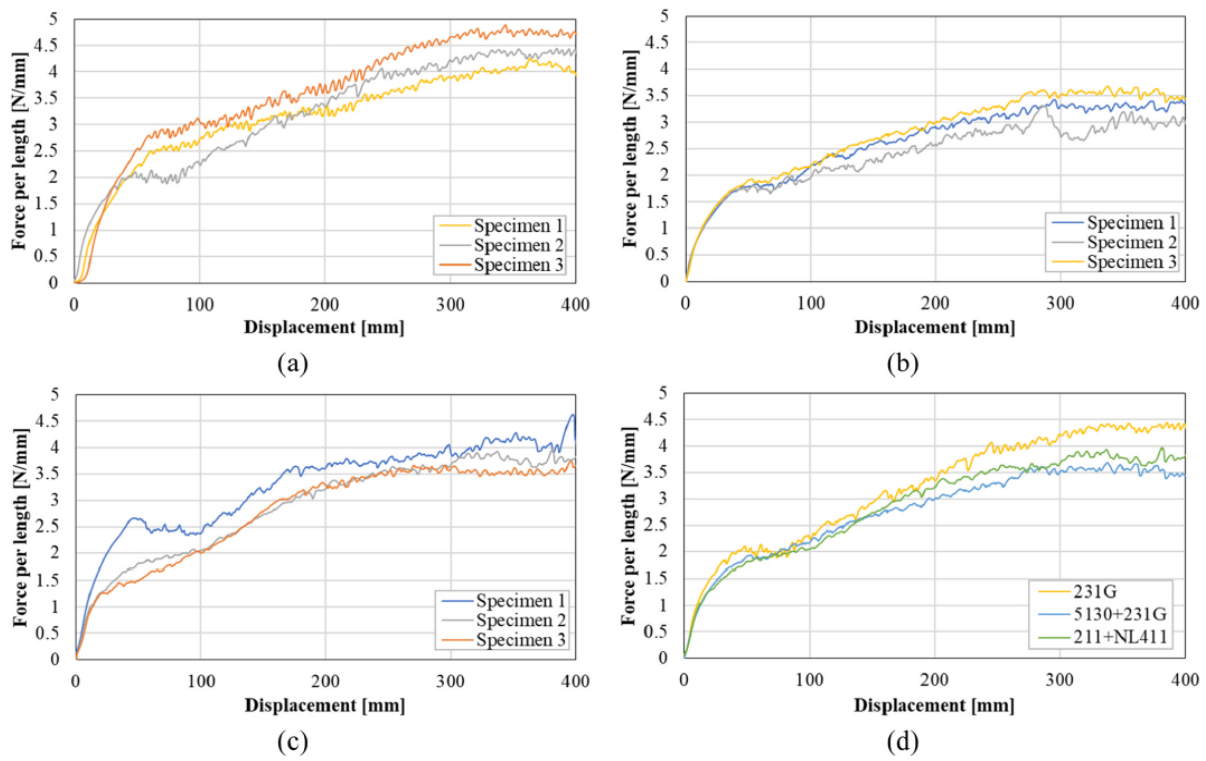


Figure 3.5 Peel test results by Pianese et al. (a) 231G, (b) 5130 + 231G, (c) 211+NL411 and (d) comparison.

Figures 3.5 (b) and 3.5 (c) show peel-test results for three specimens bonded with adhesives 5130+231G and 211+NL411, respectively. Regardless of the adhesive used, all specimens exhibit a typical peel response characterized by:

- An initial rising stage, associated with the onset of separation and progressive mobilization of the peel mechanism.
- Followed by a more stable progression, where the force per width remains relatively sustained (with fluctuations) as cracking/debonding advances along the interface.

When comparing the three bonding systems in Figure 3.5, Pianese et al.[2] conclude that glass-fiber fabrics treated with Chemosil 231G exhibit the strongest adhesion, reporting an adhesion strength close to 4.5 N/mm. They also note that the failure mode for adhesive-treated specimens is consistent, and therefore Chemosil 231G is selected for the final production stage of the glass-fiber-reinforced system.

Based on this conclusion, the present document adopts the three replicate curves of Figure 3.5(a) (Chemosil 231G), as the experimental reference dataset used to define a representative target response and calibrate the numerical peel-test model in Straus7 software.

4 Multi-Scale Peel Test Analysis: Global Response and Interface Mechanics

The present chapter introduces the theoretical framework adopted to interpret peel-test behavior and to define the calibration targets for numerical modelling. The peel response is first discussed from both global and local viewpoints, emphasizing that the measured force–displacement curve reflects the coupled contribution of adherend kinematics/test compliance and the interfacial debonding law. The main stages of a typical global peel curve are then identified, and a simplified trilinear idealization is introduced to extract a minimal set of representative parameters. Finally, the discrete cut-off-bar interface representation is presented as the practical formulation that will be implemented in the FE model developed in the subsequent chapters.

4.1. Peel test response analysis

Adhesively bonded interfaces can sustain load while allowing limited relative opening due to their finite stiffness. As the applied load increases, damage may initiate at the interface and a debonding front can propagate, ultimately leading to complete separation of the adherents. The observed global peel force–displacement response is therefore a macroscopic manifestation of the local interfacial behavior, governed by the normal tractions transmitted across the interface and by the extent of the active zone ahead of the debonding front, as shown schematically in the Figure 4.1.

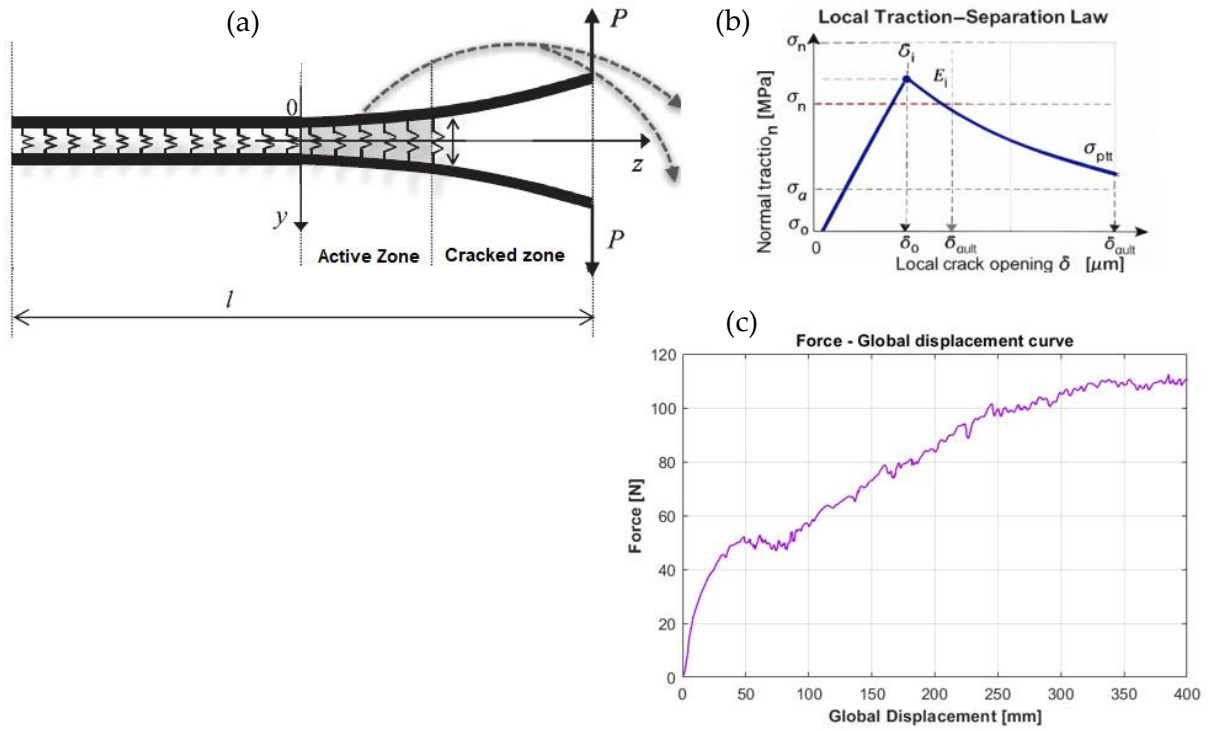


Figure 4.1 (a) Schematic representation of the detachment of two adhesively bonded materials and (b) local interfacial behavior , (c) global force-displacement response.

However, a common difficulty in modelling peeling and interfacial debonding is that experimental global curves often exhibit scatter, local oscillations, and gradual transitions that are not convenient as direct inputs for simplified numerical interface laws. From a mechanical standpoint, the global F-displacement curve is, essentially, the integrated structural response of:

1. **The kinematics and compliance of the test setup**, including adherends, grips, and the loading frame (often referred to as system compliance), and
2. **The local interfacial traction–separation relation**, $\sigma_n(\delta)$, which governs the damage evolution and the steady propagation of the debonding front within the process (active) zone.

Consequently, the interpretation of experimental peel curves requires a preliminary analysis aimed at separating, at least conceptually, the role of structural compliance from the underlying interfacial debonding mechanism. Only through this step can a consistent target response be defined for the calibration of an equivalent numerical model.

4.1.1. Global load-displacement peel response

The peel-test response of bonded flexible assemblies can be interpreted, at a global level, as the combination of an initial stage where the interface progressively mobilizes and separation initiates, and a propagation stage where peeling advances at an approximately stable resistance level.

In practice, the experimental curve is typically presented not only as the applied force F , but, more conveniently for comparison across different specimen widths, as the width-normalized peel force F/b , where b is the bonded width. Accordingly, as schematized in Figure 4.2, the experimental F/b -displacement curve can be summarized by three main segments:

1. **Initial elastic regime:** an approximately linear rise at small displacements, representing the early deformation of the system and the onset of interfacial loading. This branch is characterized by equivalent initial stiffness.
2. **Transition branch up to a normalized peak load:** a connection segment capturing the change from the initial rising response to a more stable peeling resistance.
3. **Stable peeling branch or quasi-steady plateau:** a near-constant force-per-width level representing sustained peeling propagation, typically idealized as a horizontal branch.

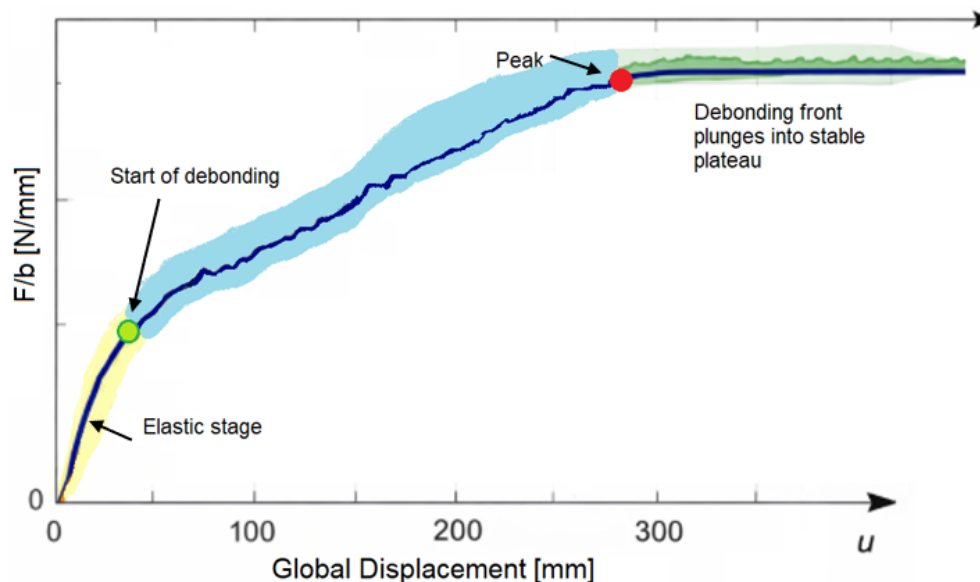


Figure 4.2 Global peel response: normalized force per unit of width (F/b) as function of the global displacement (u)

Under this idealization, the entire experimental behavior of a peeling curves set can be represented using a minimal set of parameters: an average initial stiffness in the elastic

regime; and average end of the elastic regime, identified by the point $(u_e, (F/b)_e)$; and the average onset of the plateau, identified by the point $(u_p, (F/b)_p)$, where $(F/b)_p$ represents the approximately constant plateau level.

4.1.2. Local interfacial response analysis

4.1.2.1. Interfacial detachment of bonded assemblies

Interfacial debonding in bonded assemblies is commonly described by relating the local kinematics at the interface (opening and sliding) to the corresponding tractions transmitted between adherends. Capturing this behavior requires an explicit interface constitutive description capable of representing both the onset of damage (stress-controlled) and the subsequent propagation (energy-controlled).

In this context, the Cohesive Crack Model (CCM) and Finite Fracture Mechanics (FFM) approaches are widely adopted because they naturally reconcile stress- and energy-based criteria within a unified framework for interfacial crack initiation and growth (Dimitri et al., 2017)[6].

In cohesive-type formulations, crack initiation and propagation are governed by a traction–separation law, or in shear-dominated problems, a stress–slip law; whose area is linked to an interface fracture energy.

As illustrated in Figure 4.3 , typical cohesive laws display an initial elastic branch up to a peak traction (cohesive strength), followed by a softening branch leading to complete stress release. The area under the traction–separation curve equals the fracture energy G_c , ensuring consistency with an energy balance during crack advance (Dimitri et al., 2017)[6][7]

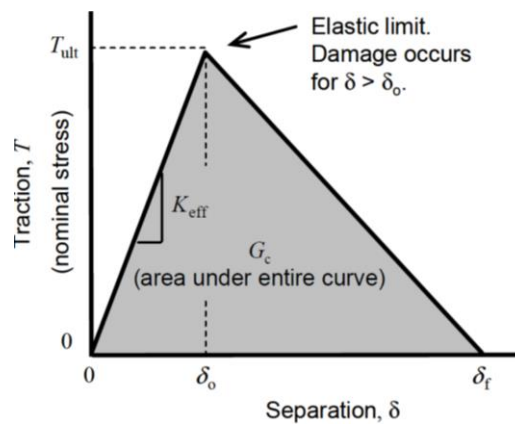


Figure 4.3 Schematization of a typical local cohesive traction–separation response $T(\delta)$ for interfacial debonding (Mode I).

4.1.2.2. Mechanical representation of an interfacial adhesive fracture

To substantiate and validate interfacial fracture models, literature commonly relies on standardized benchmark geometries. A classical reference configuration for Mode I interfacial fracture is the **Double Cantilever Beam (DCB)** test, widely used to determine the Mode I critical energy release rate G_{Ic} (interlaminar fracture toughness) of fibre-reinforced polymer composites, as standardized in ASTM D5528 and ISO 15024.

It is worth recalling that, in the terminology of fracture mechanics, Mode I (opening) corresponds to separation normal to the interface plane, where the two faces open through a normal displacement jump δ_n and the dominant transmitted traction is the normal traction T_n . In contrast, Mode II (sliding/shearing) corresponds to separation dominated by tangential sliding along the interface plane, where the relative tangential displacement (slip) δ_t prevails and the dominant traction is the shear traction τ . [8]

For the first fracture mechanism, (Dimitri et al., 2017)[6] use the DCB as a benchmark to compare CCM and FFM predictions, and show how a refined description can be obtained by modelling the adherends as **beams connected by an interface governed by an explicit traction–separation law**, representing a finite process zone and progressive degradation at the front, as shown in Figure 4.4a.

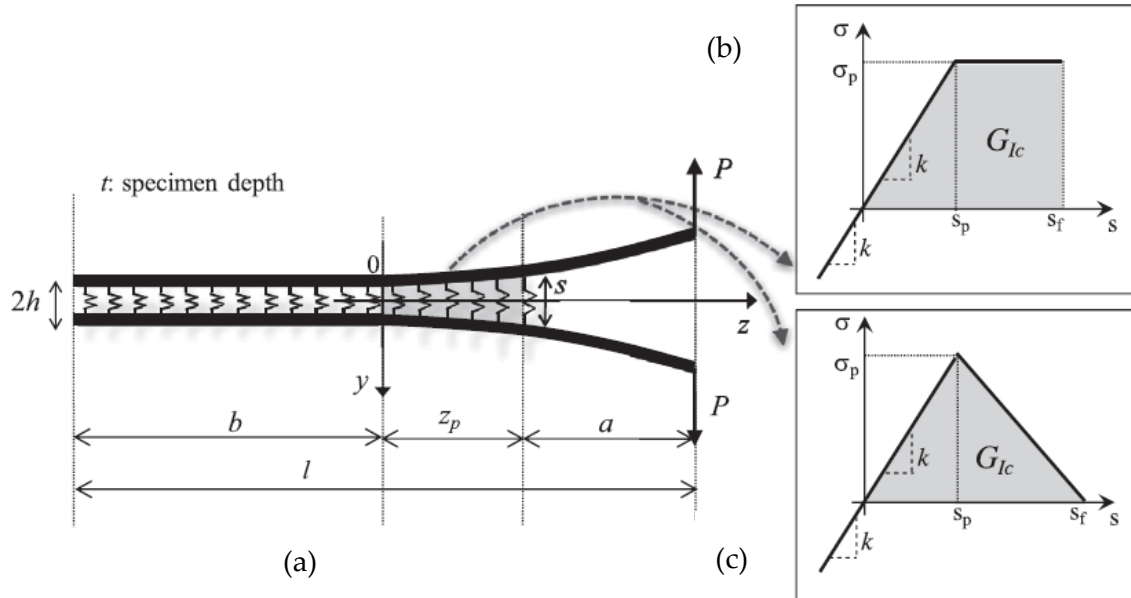


Figure 4.4 Schematic representation of the DCB specimen: (a) nomenclature used to describe the crack tip and the process-zone in Mode I interfacial fracture; (b) trapezoidal and (c) bilinear local cohesive laws.

Where:

- l : total length of each DCB arm (beam/adherend).
- a : initial crack (precrack) length, i.e., the debonded/cracked portion treated as a cantilever in the classical beam-theory interpretation.
- b : bonded length (still-adhered portion of the interface).
- z : longitudinal coordinate along the specimen/beam axis used to describe fields such as separation $s(z)$ and traction $\sigma(z)$.
- s : interfacial separation (relative opening displacement) across the interface.
- z_p : process-zone (cohesive-zone) length along the interface, i.e., the extent of the region where the cohesive law is active.

The specific shape of the cohesive traction–separation relation is not unique and may vary depending on the material system and the level of idealization required (Chandra et al., 2002). Among these, bilinear and trapezoidal cohesive laws are commonly used, (see Figure 4.4b) . In both cases, the interfacial law is completely defined by means of three parameters, e.g. the peak stress σ_P , the corresponding separation s_P , and the final separation s_f governing the complete failure of the interface. The initial (elastic) stiffness of the interface is defined as $k = \sigma_P / s_P$, while the fracture energy is equal to $G_{Ic} = \sigma_P^* s_P / (2 (2 s_f / s_P - 1))$ or $G_{Ic} = (\sigma_P^* s_f) / 2$, for a trapezoidal or bilinear cohesive law, respectively. (Dimitri et al., 2017)[6]

From a modelling perspective, the simplest possibility to analyze the mechanical behavior of adhesive joints is to model the interface as a **continuous distribution of linear springs with appropriate stiffness parameters** (Dimitri et al., 2017)[6]. In Mode I failure, the springs acting along the normal direction with respect to the interface plane, such that normal stresses are exchanged through the interface.

This representation provides a convenient basis for considering interfacial flexibility before damage and, more generally, clarifies the role of the interface law as a constitutive concept that can be integrated not only into DCB test geometries but also into peel or shear.

In other words, although the peel test involves different global kinematics with respect to the DCB, its measured F/b –displacement response still results from the coupling of adherend bending/rotation and test compliance, and the local interfacial law governing initiation and propagation at the peel front. Hence, the spring-interface concept used in DCB analyses can also be adopted to model the interface in peel-test simulations.

From a numerical perspective, implementing a continuous cohesive law (e.g., trilinear or more complex) that captures the dominant stages of interface behavior with initial linear response, softening, and residual strength; as in the case of a peel test, is not always straightforward in commercial finite element codes without user subroutines.

Pingaro et al. [9] address this limitation by proposing to approximate a target continuous interface law with a piecewise implementable law (sawtooth or multilinear discontinuous) law. This approximation can be modeled using simple elements such as parallel cutoff bars (two elasto-brittle and one elasto-ductile) thus reproducing the expected macroscopic response while maintaining high numerical robustness.

This choice is conceptually consistent with the spring-bed interpretation of an imperfect interface (Dimitri, et al. 2017)[6]. Following Pingaro et al. [9] strategy is possible discretizes a bonding interface by means of parallel cutoff bars acting as discrete spring-like connectors (see Figure 4.5) , which approximate the desired Mode I traction–separation behavior, while remaining fully compatible with standard FE element libraries.

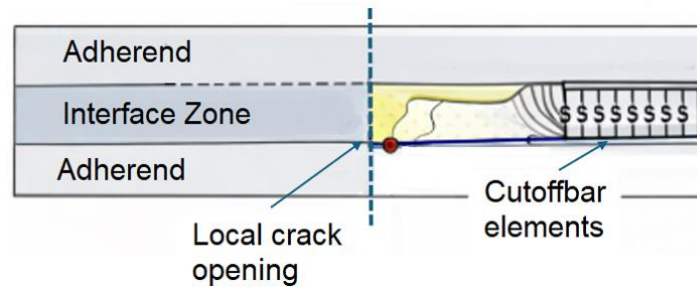


Figure 4.5 Schematic representation of an interface discretization using cutoff-bar elements acting as spring-like connectors, and local crack opening at the debonding front.

In peel testing, the available experimental information is typically limited to the global F/b –displacement response. Since no direct measurements of the local interfacial traction–separation behaviour at the front-active zone are available, the present work does not attempt to identify a continuous local cohesive law $T(\delta)$. Instead, the interface is represented through an implementable discrete model based exclusively on cutoff bars, and their parameters (stiffnesses and thresholds) are calibrated directly at the global level to reproduce the main macroscopic features of the experimental peel curve (initial stiffness, transition/onset of propagation, and plateau level). Therefore, the resulting parameters should be interpreted as effective model parameters for the specific peel configuration considered, rather than as uniquely identified intrinsic local interface properties. This choice is motivated by the work of Pingaro et al. [9], who show that a cutoff-bar assembly provides a reliable and numerically robust way to reproduce a prescribed interfacial response within commercial FE environments.

4.1.3. Numerical representation of the peel-test interface using parallel cut-off-bar elements

4.1.3.1. Derivation of the stiffness-equivalence system

Pingaro et al. [9] derive a closed-form calibration procedure to implement an interfacial constitutive law in a commercial FE code using only standard truss-like elements. Following this approach, a bonded interface can be discretized by means of a network of parallel cut-off bars (CoBs), i.e., truss elements with limited tensile strength and post-elastic behavior assumed either elasto-perfectly brittle or elasto-perfectly ductile.

The main idea is to start from a continuous interface law and obtain an **equivalent discrete nodal law** for the discretized interface, practically called as *interface station or interface portion*, which can then be reproduced by a set of springs/bars in parallel.

In the reference formulation, the local interfacial behavior is described in Mode II through a shear traction–slip relationship $\tau = \tau(s)$, where s is the relative tangential displacement (slip) across the interface and τ is the transmitted shear traction. This interfacial behavior is idealized as a trilinear interfacial law, as illustrated in Figure 4.6 (a) and considered as the target local interface law intended to be represented.

For practical implementation, the target law is approximated by a **piecewise-linear discontinuous** (sawtooth) representation. The sawtooth and trilinear models are considered equivalent, if and only if; they dissipate the same fracture energy, i.e., the area under the τ – s curve is the same in both representations. Pingaro et al. [9]

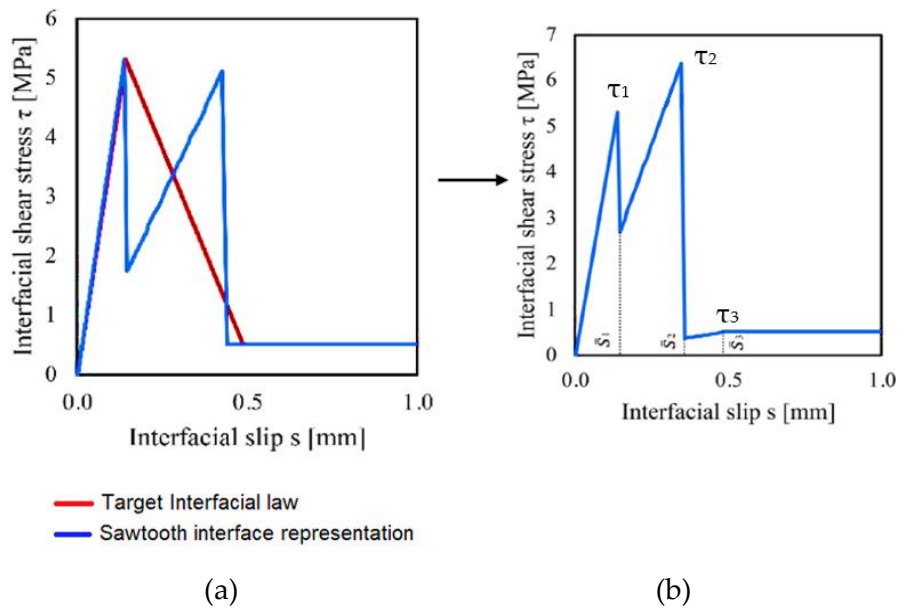


Figure 4.6 Pingaro et al (a) Shear traction-slip behavior of an interface subjected to Mode II debonding described through a target trilinear law and a sawtooth representation, (b) Sawtooth interface representation linear branches.

In each linear branch j , the traction is proportional to slip (see Figure 4.6(b)) , thus:

$$\tau = k_j s, \quad (4.1)$$

where k_j is the tangent stiffness (slope) of branch j . In terms of units,

$$k_j = \frac{\tau}{s} [\text{MPa/mm} = \text{N/mm}^3], \quad (4.2)$$

since τ is a stress [N/mm^2] and s is a displacement [mm].

To embed the continuous law into a discrete FE model, the interface is discretized into *portions* of length Δx along the bonded direction (see Figure 4.7, this zone being the region of union between two adhered materials of a known width).

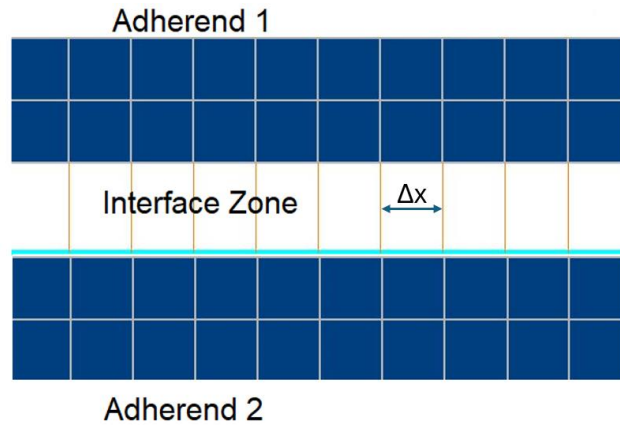


Figure 4.7 Schematic representation of a discretized interface zone bonding two adherend materials.

Considering an out-of-plane material width b , the tributary interfacial area associated with one portion is:

$$A_{\text{int,por}} = b \Delta x [\text{mm}^2] \quad (4.3)$$

Assuming that the traction is approximately uniform over this portion (standard FE lumping assumption), the resultant shear force transmitted by the interface portion is obtained by integrating the traction over the tributary area:

$$F_{\text{por}} = \int_{A_{\text{int,por}}} \tau dA \approx \tau A_{\text{int,por}}. \quad (4.4)$$

Substituting the linear branch law $\tau = k_j s$, (Eq. 4.1) one obtains:

$$F_{\text{por}} \approx (k_j s) A_{\text{int,por}}. \quad (4.5)$$

Therefore, the **equivalent nodal stiffness** of the interface portion in branch j , defined as the incremental force per incremental slip, is:

$$K_{\text{eq},j} = \frac{dF_{\text{por}}}{ds} \approx k_j A_{\text{int,por}} [\text{N/mm}]. \quad (4.6)$$

This relation is the key scaling step: a constitutive slope expressed in stress units (k_j) becomes a nodal stiffness ($K_{\text{eq},j}$) once multiplied by the tributary area of the interface portion.

Pingaro et al. [9] reproduce the staged nodal stiffness $K_{\text{eq},j}$ using three cut-off bars (springs) in parallel at each interface portion. Each bar i is characterized by:

- Cross-sectional area A_i ,
- Young's modulus E_i ,
- bar length L_{bar} (user-defined),

Leading to the axial stiffness:

$$K_i = \frac{E_i A_i}{L_{\text{bar}}} [\text{N/mm}]. \quad (4.7)$$

For springs in parallel, the total stiffness is the sum of the active stiffnesses. Pingaro's modelling choice is such that the set of active bars changes from stage to stage (because brittle bars cut off), thereby reproducing a decreasing tangent stiffness. Specifically:

- **Stage 1:** bars 1+2+3 active

$$K_1 + K_2 + K_3 = K_{\text{eq},1} = k_1 A_{\text{int,por}} \quad (4.8)$$

- **Stage 2:** bar 1 has cut off; bars 2+3 active

$$K_2 + K_3 = K_{\text{eq},2} = k_2 A_{\text{int,por}} \quad (4.9)$$

- **Stage 3:** bar 2 has cut off; only bar 3 active

$$K_3 = K_{\text{eq},3} = k_3 A_{\text{int,por}} \quad (4.10)$$

Substituting $K_i = E_i A_i / L_{\text{bar}}$ and multiplying each equation by L_{bar} , the system becomes:

$$\begin{cases} E_1 A_1 + E_2 A_2 + E_3 A_3 = k_1 A_{\text{int,por}} L_{\text{bar}} & (4.11a) \\ E_2 A_2 + E_3 A_3 = k_2 A_{\text{int,por}} L_{\text{bar}} & (4.11b) \\ E_3 A_3 = k_3 A_{\text{int,por}} L_{\text{bar}} & (4.11c) \end{cases}$$

which is the stiffness-equivalence system underlying: the elastic parameters of the three CoBs are selected so that the discrete FE package reproduces the target stiffness values in each branch of the piecewise interface law.

For the sake of convenience, $A_1 = A_2 = A_3$ can be set equal to the unitary value, so that the unknowns reduce to the moduli E_i , and the system can be solved directly.

4.1.3.2. Adaptation of the stiffness-equivalence system for a Mode I peel-test interface (effective opening law)

Although Pingaro et al. [9] formulate the procedure for a shear law $\tau(s)$, the derivation above is purely based on: (i) traction-to-force scaling via a tributary measure and (ii) stiffness matching via parallel springs. For a peel-test model, where the interface is implemented using only CoBs oriented along the opening direction, the same derivation can be applied after the substitutions:

$$\tau \rightarrow \sigma_n \quad ; \quad s \rightarrow \delta,$$

where δ is the effective opening separation and σ_n the effective normal traction. Accordingly, the branch slopes become $k_{\text{int},j} = \sigma_n / \delta$, and the stiffness-equivalence system is written identically with k_j interpreted as the slope of the effective Mode I traction–separation law. The tributary area remains $A_{\text{int,por}} = b \Delta x$, which converts the calibrated stress-level law into the nodal force levels carried by the parallel CoBs at each interface portion.

The Mode I adaptation at the portion level reads:

$$\begin{cases} E_1 A_1 + E_2 A_2 + E_3 A_3 = k_{\text{int},1} A_{\text{int,por}} L_{\text{bar}} & (4.12a) \\ E_2 A_2 + E_3 A_3 = k_{\text{int},2} A_{\text{int,por}} L_{\text{bar}} & (4.12b) \\ E_3 A_3 = k_{\text{int},3} A_{\text{int,por}} L_{\text{bar}} & (4.12c) \end{cases}$$

Dividing each equation by L_{bar} and using $K_i = E_i A_i / L_{\text{bar}}$, one obtains an equivalent and more convenient form:

$$\begin{cases} K_1 + K_2 + K_3 = k_{\text{int},1} A_{\text{int,por}} \end{cases} \quad (4.13a)$$

$$\begin{cases} K_2 + K_3 = k_{\text{int},2} A_{\text{int,por}} \end{cases} \quad (4.13b)$$

$$\begin{cases} K_3 = k_{\text{int},3} A_{\text{int,por}} \end{cases} \quad (4.13c)$$

or, solved for the interface slopes:

$$k_{\text{int},1} = \frac{K_1 + K_2 + K_3}{A_{\text{int,por}}}, \quad k_{\text{int},2} = \frac{K_2 + K_3}{A_{\text{int,por}}}, \quad k_{\text{int},3} = \frac{K_3}{A_{\text{int,por}}}. \quad (4.14)$$

These relations show explicitly how the parallel bar stiffnesses (K_i) reproduce the staged slopes of the effective Mode I traction–separation law.

4.1.3.3. Derivation of the strength/threshold system

After matching the tangent stiffness in each branch (Eq. 4.11(a);(b);(c)), Pingaro et al. [9] determine the strength thresholds of the three parallel cut-off bars by requiring that the discrete FE package reproduces the target traction levels at the prescribed kinematic transition points of the piecewise law.

Consider a piecewise interface law $\tau = \tau(s)$ with transition slips $s_1 < s_2 < s_3$ and corresponding traction levels τ_1, τ_2, τ_3 . For a discrete interface portion of tributary area $A_{\text{int,por}}$, the force transmitted at a given slip is $F(s) = \tau(s) A_{\text{int,por}}$. In the parallel-bar representation, each bar i carries an axial force $F_i = K_i s$ as long as it remains elastic and active. If bar i reaches its threshold force F_{fi} at a slip $s = s_i$, then, for smaller slips $s < s_i$, its force scales linearly as:

$$F_i(s) = F_{fi} \frac{s}{s_i} \quad (4.15)$$

Therefore, at the first transition $s = s_1$, all three bars are still active and the total force is:

$$F(s_1) = F_{f1} + F_{f2} \frac{s_1}{s_2} + F_{f3} \frac{s_1}{s_3}. \quad (4.16)$$

Imposing equivalence with the target traction level, $F(s_1) = \tau_1 A_{\text{int,por}}$, yields the first equation.

At the second transition $s = s_2$, bar #1 has cut off, while bars #2 and #3 are still active:

$$F(s_2) = F_{f2} + F_{f3} \frac{s_2}{s_3} = \tau_2 A_{\text{int,por}}. \quad (4.17)$$

At the third transition $s = s_3$, only bar #3 governs:

$$F(s_3) = F_{f3} = \tau_3 A_{\text{int,por}}. \quad (4.18)$$

Collecting these relations gives the threshold-force system:

$$\left\{ \begin{array}{l} F_{f1} + F_{f2} \frac{s_1}{s_2} + F_{f3} \frac{s_1}{s_3} = \tau_1 A_{\text{int,por}} \end{array} \right. \quad (4.19a)$$

$$\left\{ \begin{array}{l} F_{f2} + F_{f3} \frac{s_2}{s_3} = \tau_2 A_{\text{int,por}} \end{array} \right. \quad (4.19b)$$

$$\left\{ \begin{array}{l} F_{f3} = \tau_3 A_{\text{int,por}} \end{array} \right. \quad (4.19c)$$

4.1.3.4. Adaptation of the strength/threshold system for a Mode I peel-test interface

The equation system 4.19 provides the ultimate forces F_{fi} of the three bars by matching the target traction levels at given kinematic points. In a peel model, with an interface is implemented only in opening using CoBs, the same derivation applies after the substitutions:

$$\tau_i \rightarrow \sigma_{n,i}, \quad s_i \rightarrow \delta_i, \quad A_{\text{int,por}} = b\Delta x.$$

Thus, the Mode I form becomes:

$$\left\{ \begin{array}{l} F_{f1} + F_{f2} \frac{\delta_1}{\delta_2} + F_{f3} \frac{\delta_1}{\delta_3} = \sigma_{n,1} A_{\text{int,por}} \end{array} \right. \quad (4.20a)$$

$$\left\{ \begin{array}{l} F_{f2} + F_{f3} \frac{\delta_2}{\delta_3} = \sigma_{n,2} A_{\text{int,por}} \end{array} \right. \quad (4.20b)$$

$$\left\{ \begin{array}{l} F_{f3} = \sigma_{n,3} A_{\text{int,por}} \end{array} \right. \quad (4.20c)$$

where:

- $\delta_1, \delta_2, \delta_3$ are the separations defining the stage transition points,
- $\sigma_{n,1}, \sigma_{n,2}, \sigma_{n,3}$ are the corresponding target normal tractions of the staged law,
- F_{f1}, F_{f2} are the cut-off thresholds of the two brittle bars,
- F_{f3} is the yield/plateau force level of the ductile bar.

From equations systems Eqs. (4.11) and (4.19), it follows that the interface constitutive response implemented through parallel cut-off bars can be fully defined once the mechanical parameters of the bars are assigned. In particular, specifying the elastic modulus E_i (together with A_i and L_{bar} , i.e., the axial stiffness of each bar) fixes the

staged tangent stiffness of the assembled package, while specifying the threshold forces F_{fi} determines the transition points at which individual bars cut off or yield.

It is worth noting that the connector length L_{bar} appearing in Pingaro's formulations is an implementation parameter associated with the nodal separation adopted to link the adherends in the FE discretization, and it does not necessarily coincide with the physical adhesive thickness. The elastic contribution of each cut-off bar enters the formulation through its axial stiffness(Eq. 4.7) . Therefore, changing L_{bar} does not alter the intended interface response provided that the bar stiffness is preserved. In practice, for a given cross-sectional area A_i , any change in L_{bar} can be compensated by a corresponding rescaling of E_i so that K_i remains unchanged. Accordingly, the calibrated parameters ($E_i, F_{u,i}$) should always be interpreted as being identified with respect to the selected L_{bar} used in the FE implementation; the final global response is governed by the resulting axial stiffnesses K_i and thresholds $F_{u,i}$, rather than by the absolute value of L_{bar} alone.

This FE-oriented formulation significantly reduces the number of unknown variables that must be introduced to describe an interfacial debonding, which is advantageous for nonlinear simulations such as those required for peel-test modelling. Accordingly, in the present work the interfacial response is introduced by adopting Pingaro's discretization principle: the bonded length is partitioned into interface portions, each represented by three parallel cut-off bars, whose elastic and threshold parameters are calibrated so that the resulting simulated global F/b -displacement curve reproduces as closely as possible the experimental peel curves available for the rubber-fiberglass system analyzed.

5 T-Peel Test: Finite Element Model

The present chapter describes the finite element (FE) model developed to simulate the peel test in Straus7 software [10]. It introduces the adopted geometric idealization and meshing strategy, the constitutive models used for the rubber and fiberglass adherends, and the discrete cut-off-bar interface representation implemented along the bonded length. The chapter also details the boundary conditions, loading procedure, and nonlinear solution settings required to reproduce the peel kinematics.

5.1. Model description

5.1.1. Geometry & FE idealization

The peel-test specimen geometry follows ISO 36:2020 and the configuration described in Section 3.2.2 of this document, with a rectangular strip of width $b = 25$ [mm] and total length $L = 200$ [mm]. The bonded assembly consists of two adherends: a ply of high damping rubber, more precisely identified as natural rubber + ethylene-propylene-diene (NR-EPDN) and a ply of fiberglass. Both materials have the same plan dimensions but markedly different thicknesses: the rubber layer thickness is $t_r = 4$ mm, while the fiberglass ply thickness is $t_f = 0.5$ [mm]. One end of the specimen includes an initial unbonded region to enable gripping, whereas the remaining region defines the bonded length where interfacial separation propagates during the test, as shown in Figure 5.1.

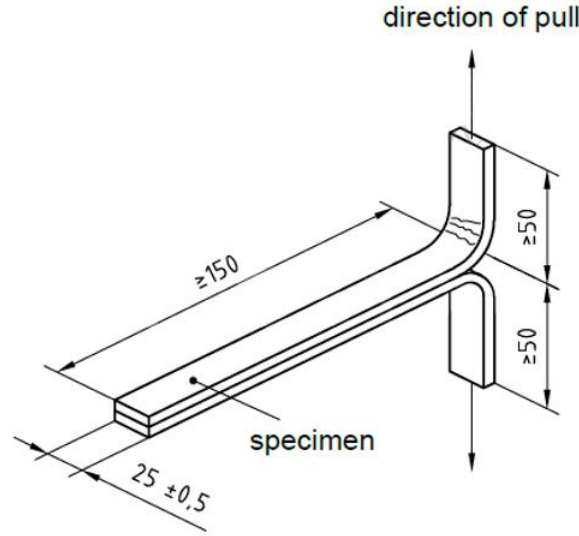


Figure 5.1 Geometry of the NR-EPDM rubber and fiberglass plies in the peel-test configuration.

The FE model is constructed in Straus 7 software by idealizing the specimen as a 2D representation coupled with an explicit out-of-plane width $b = 25 \text{ [mm]}$ used to compute width-normalized quantities and interface tributary areas. The geometry and dimensions of the finite element model agree with the configuration show in Figure 5.1.

The rubber adherend is modelled using 4-node quadrilateral (QUAD4) isoparametric plate elements in 2D plane stress, which is appropriate for a thin-strip representation where out-of-plane stresses are negligible compared to in-plane stresses and where large rotations occur during peeling.

The fiberglass adherend is represented using beam elements with a rectangular cross-section defined directly from the specimen dimensions, i.e. $t_f \times b = 0.5 \text{ mm} \times 25 \text{ mm}$. This choice preserves the correct bending stiffness of the stiff arm while keeping the model computationally efficient.

The adherend tabs were modelled with an initial 90° curvature, consistent with the showed peel-test configuration. The rubber strip was represented with plate elements and thickness $t_r = 4 \text{ mm}$; therefore, the curved region was defined by an outer and an inner edge. The outer edge corresponds to the interface side and is the one connected to the fiberglass strip through the discrete interface elements. In the curved zone, the outer radius was set to $R_{\text{out}} = 14 \text{ [mm]}$ and the inner radius to $R_{\text{in}} = 12 \text{ [mm]}$.

The corresponding arc length of the outer edge over 90° is:

$$L_{\text{arc}} = R_{\text{out}} \theta = 14 \left(\frac{\pi}{2} \right) \approx 22 \text{ [mm]}, \quad (5.1)$$

This curved contribution was included within the unbonded length so that the total unbonded length (curved part + straight free arm) equals 50 mm, matching the experimental tab configuration. For simplicity and to ease model set-up and post-processing, the specimen was drawn with the tabs oriented vertically in the (x - y) plane (as in Figure 5.2), while preserving the same local curvature and effective unbonded/bonded lengths.

The two adhesives are coupled along the bonded region through a discrete interface zone, so the tabs are modeled with a separation of 6 mm between them, initially defined for purely visualization purposes.

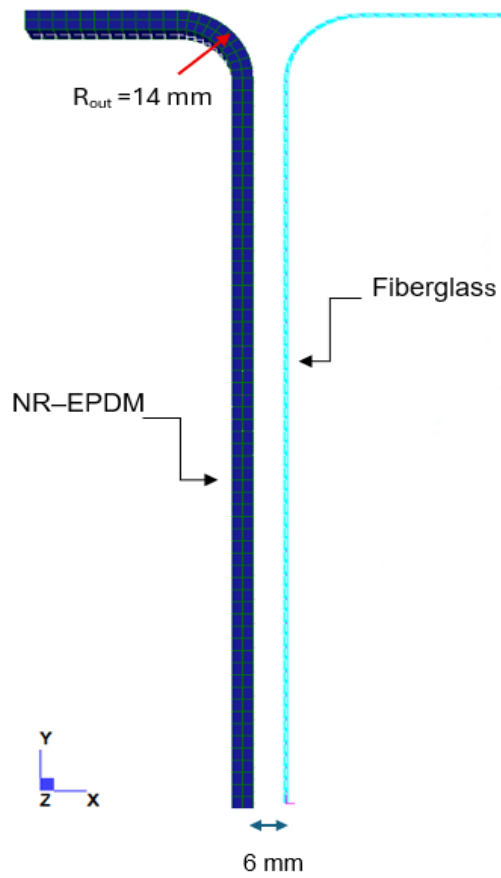


Figure 5.2 Configuration of the NR-EPDM rubber and fiberglass plies in the peel FE model.

5.1.2. Material Definitions

5.1.2.1. NR-EPDM- high damping compound

The establishment of an accurate FE model requires information on the elastic-plastic behavior of the materials involved. In the case of the NR-EPDM-high damping compound, this information was taken from Pianese et al [2], who characterized this specific material through the following standard tests on vulcanized specimens:

- Shore A hardness: To provide a measure of surface hardness
- Uniaxial tensile tests: define the tensile response and give tensile strength, elongation at break, and an initial Young's modulus,
- Tear-resistance tests: quantify the resistance to crack propagation under tearing
- Cyclic shear tests: provide the shear modulus (and damping-related response).

The resulting values reported by Pianese et al [2] are summarized in Table 5.1

Table 5.1 Mechanical Properties NR-EPDM [2]

Parameter	Symbol	Value	Unit
Shore A hardness	ShA	59 ± 1	[ShA]
Shear modulus	G	0,77	[MPa]
Tensile strength	TSbreak	16,06	[MPa]
Elongation at break	Ebreak	620	[%]
Initial Young's modulus	E	1,63	[MPa]
Tear strength	Tstr	22,8	[kN/m]

The rubber NR-EPDM is modelled as a hyperelastic material, consistent with its large-strain response. Because peel tests involve pronounced rotations and large deformations of the flexible arm, both material nonlinearity (hyperelastic constitutive response) and geometric nonlinearity (large displacement/rotation) must be included to obtain a physically meaningful model.

Since the present work relies on the experimental characterization, the uniaxial stress-strain data were adopted as reference input for parameter identification. In practice, this experimental curve was imported into the FE software code Abaqus CAE (Smith, 2009)[11], and its optimization routine was used to determine the set of coefficients that best reproduces the measured experimental response. The optimization process involves the use of the built-in material evaluation/curve-fitting tool available in the software. The fitting process iteratively updates the parameters until a convergence criterion is satisfied based on the minimization of the discrepancy between the model prediction and the experimental data (least-squares error).

The experimental uniaxial stress–strain for NR–EPDM high-damping compound is shown in Figure 5.3.

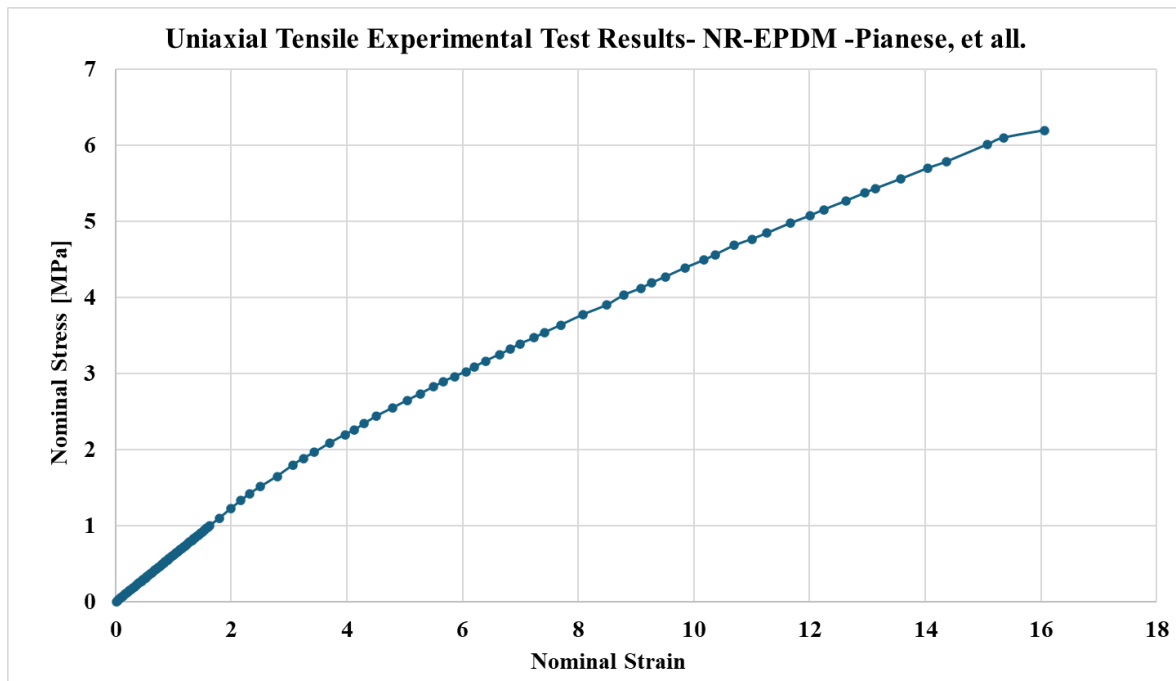


Figure 5.3 Experimental uniaxial stress–strain curve for NR–EPDM high-damping compound

Several hyperelastic families were tested during this procedure, including Ogden and Mooney–Rivlin / Polynomial forms. The resulting fits were checked through Abaqus' stability indicators, which confirmed a stable response for both an Ogden model and for the Mooney–Rivlin Polynomial model with $N = 2$, as reported by the "stable for all strains" messages, (see Figure 5.4).

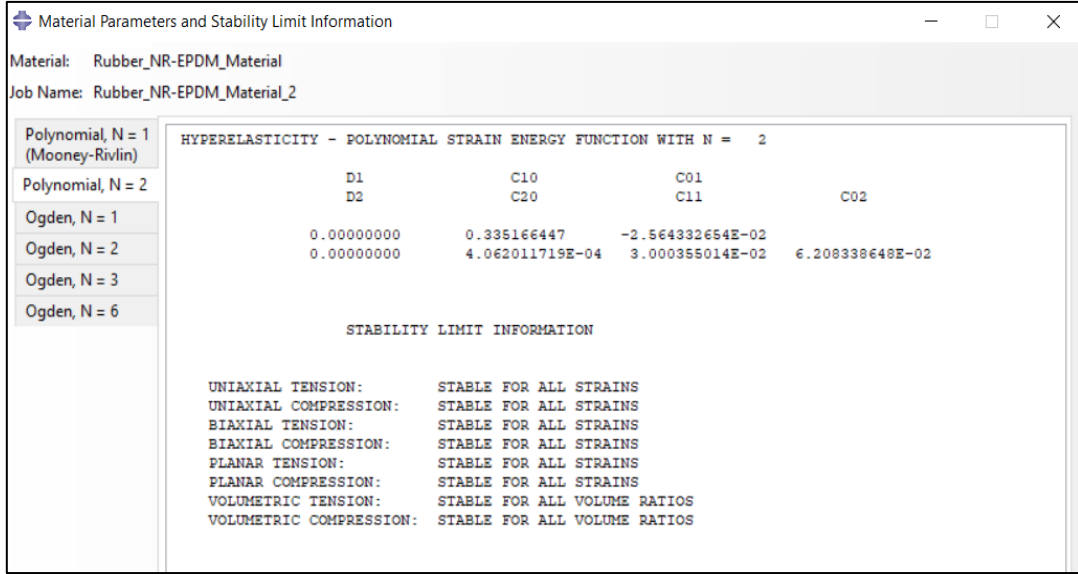


Figure 5.4 Abaqus stability limit information for the Mooney–Rivlin (Polynomial, $N = 2$) hyperelastic fit.

However, the Mooney–Rivlin representation was selected for the FE simulations because Straus 7 does not provide an Ogden formulation, whereas the Mooney–Rivlin/Polynomial form is directly available and therefore ensures a consistent transfer of the identified material parameters into the final peel-test model.

For the adopted Mooney–Rivlin Polynomial model ($N = 2$), the strain-energy density is expressed as a deviatoric (isochoric) polynomial in the invariants plus a volumetric penalty term,

$$W = \sum_{i+j=1}^2 C_{ij} (\bar{I}_1 - 3)^i (\bar{I}_2 - 3)^j + \sum_{k=1}^2 \frac{1}{D_k} (J - 1)^{2k}, \quad (5.2)$$

where $\bar{I}_1, \bar{I}_2$ are the deviatoric invariants (removing volume change), J is the volume ratio, and the coefficients C_{ij} (reported by Abaqus as $C_{10}, C_{01}, C_{20}, C_{11}, C_{02}$) control the isochoric shear response. In many engineering applications, the Mooney–Rivlin law is used in a reduced two-parameter form, often denoted C_1 and C_2 , which correspond to the first-order coefficients C_{10} and C_{01} (i.e., $C_1 \equiv C_{10}$, $C_2 \equiv C_{01}$). These parameters primarily govern the initial stiffness and curvature of the uniaxial response at moderate strains. The coefficients D_k define the compressibility through the volumetric term: they penalize changes in J and are related to the effective bulk modulus (for nearly incompressible rubber, D_k are chosen very small so that $J \approx 1$). In the present identification, Abaqus returned D_k values consistent with a nearly incompressible response, as reflected by the stability report, and the resulting Mooney–Rivlin parameters were therefore adopted as the rubber material definition used in Straus 7 for the peel simulations.

The resulting Mooney–Rivlin constants $C_1 = 0.335$ and $C_2 = 4.062 \times 10^{-4}$ were then used in the Straus 7 FE model to characterize the hyperelastic response of the rubber adherend.

5.1.2.2. Fiberglass strip

The fiberglass adherend is modelled as linear-elastic, with fiber optional yielding/failure criterion enabled to prevent unrealistically high bending stresses in extreme configurations. Since Pianese et al. do not report a unique elastic modulus for the specific glass-fiber fabric ply used in the peel specimens, a representative value is adopted. A modulus of $E \approx 20$ GPa is consistent with typical ranges reported for GFRP laminates, which can vary widely depending on architecture and fiber volume fraction [12][13]; the chosen value lies in the mid-range and provides a reasonable bending stiffness for a thin strip idealized as an equivalent homogeneous beam. A Poisson ratio $\nu = 0.27$ is assumed as a standard engineering approximation for glass-fiber composite laminates, since typical values commonly fall in the 0.25–0.30 range, depending on layup and homogenization.

5.1.3. Discrete interface FE model

The bonded region is represented through a discrete interface zone in which the interfacial constitutive response is implemented using cutoffbar elements cut-off-bar (CoB) elements, acting as spring-like connectors.

The interface is discretized along the bonded length into portions of size $\Delta x = 2.5$ mm. All CoBs have unit cross-sectional area $A = 1$ mm² and a common bar length equal to the separation between the rubber and fiberglass reference lines, $L_b = 6$ mm, as illustrated in Figure 5.5

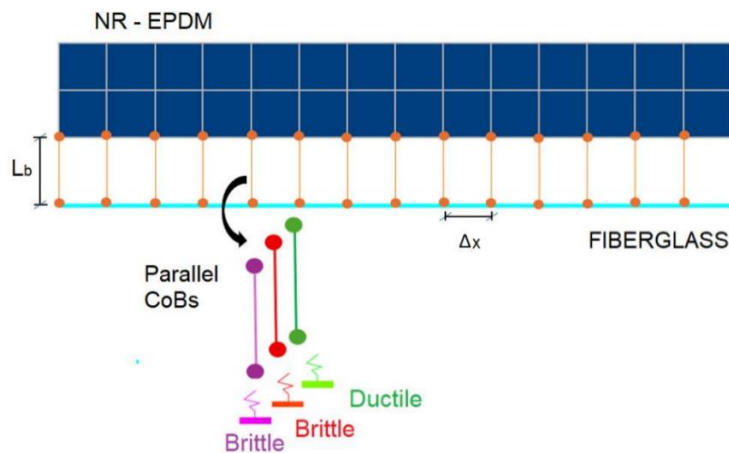


Figure 5.5 Interface model implemented in Straus 7 FE code using cutoff bars disposed in-parallel (two brittle and one ductile).

For each portion, three cutoff bars are placed in parallel and share the same pair of nodes, one node on the rubber mesh and one on the fiberglass mesh. Two CoBs are

assigned an elasto-perfectly brittle behavior (linear elastic response up to a tensile cut-off, followed by complete loss of load-carrying capacity), while the third CoB is assigned as elasto-perfectly ductile to provide residual resistance during propagation.

This enforces a single interfacial opening for the portion while allowing the transmitted force to be the sum of the forces carried by the three bars, i.e.:

- same local opening: $\delta_1 = \delta_2 = \delta_3 = \delta$ (same nodes),
- additive force transfer: $F_{\text{por}} = F_1 + F_2 + F_3$.

This discrete “parallel package” is used to approximate a piecewise interface law (sawtooth / multilinear).

The elastic modulus E_i and the corresponding maximum tensile forces/stresses defining the cut-off thresholds are treated as calibration parameters and are determined through a sensitivity-based tuning procedure, as detailed in the following sections. The CoB interface discretization is applied only over the bonded region, with CoBs distributed along a bonded length of 150 mm, while the remaining length is left unbonded to reproduce the experimental peel configuration. (see Figure 5.6)

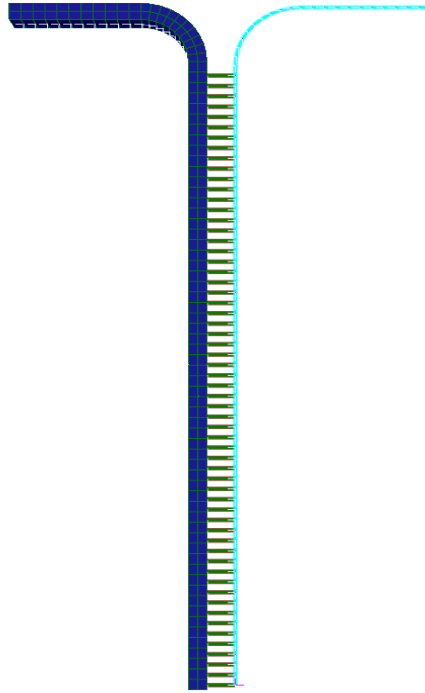


Figure 5.6 Cutoff bars disposed in-parallel (two brittle and one ductile) along a bonded length of 150 mm - Peel FE model.

5.1.4. Boundary conditions and loading

5.1.4.1. Kinematic constraints for 2D plane-stress modelling

The rubber adherend was modelled using a 2D plane-stress formulation; therefore, the physically relevant degrees of freedom are the in-plane translations D_X and D_Y . To enforce a strictly planar response and suppress spurious out-of-plane mechanisms, the following constraints were applied to the rubber nodes:

- $D_Z = 0$: prevents out-of-plane translation and enforces the plane-stress kinematic assumption.
- $R_X = 0, R_Y = 0$: suppress out-of-plane rotational modes that are not representative of the intended 2D kinematics and that may otherwise introduce numerical mechanisms and convergence issues under large rotations typical of peel loading.
- $R_Z = 0$: removes in-plane drilling rotation, which is not a physical degree of freedom for the adopted plate formulation in this context and may lead to spurious rigid-body rotation modes when the rubber arm undergoes large bending/rotation.

Accordingly, for the rubber nodes the in-plane translations were left free (D_X, D_Y free), while D_Z, R_X, R_Y , and R_Z were fixed.

The fiberglass adherend was modelled with beam elements, for which the physically meaningful kinematics include in-plane translations and in-plane rotation. Therefore, at fiberglass nodes D_X, D_Y and R_Z were kept free to allow bending and rotation of the stiff arm, while D_Z, R_X and R_Y were fixed to prevent out-of-plane motion, consistently with the 2D idealization.

5.1.4.2. Fixed and moving grip

To reproduce the peel-test kinematics, the specimen ends were coupled to rigid grips representing the steel clamps of the experimental apparatus. In Straus 7, each grip was modelled as a short plate block (QUAD4, plane-stress) with in-plane length 10 mm, thickness 4 mm, matching the rubber thickness, and out-of-plane width equal to the specimen width $b = 25\text{mm}$ (see Figure 5.7). The grip plates were assigned steel properties ($E = 210 \times 10^3 \text{ MPa}$) to ensure negligible compliance compared to the rubber arm. Since the grips are intended to behave as rigid fixtures, the use of a linear-elastic steel model is sufficient for the present purpose; plasticity is not required unless one aims to model clamp yielding, which is not expected in the test.

The right grip, connected to the fiberglass arm, represents the fixed clamp of the test. All nodes of the corresponding grip plate were fully restrained, so that this end remains stationary during the simulation (Figure 5.7(b)).

The left grip, connected to the rubber arm, represents the moving clamp. To apply the imposed displacement in a controlled manner and avoid local over-constraints, a master node was selected at the center of the grip plate edge (Figure 5.7(a)). A prescribed displacement history was applied to the master node $D_X = -400$ [mm], which corresponds to the global displacement range reported in the experimental F/b -displacement curve. At the master node, the remaining degrees of freedom were fixed ($D_Y = D_Z = 0$, $R_X = R_Y = R_Z = 0$) so that the grip motion is purely translational along the loading direction, consistent with the experimental set-up.

The remaining edge nodes of the grip plate were linked to the master node through rigid links (slave nodes), enforcing that the entire grip edge follows the prescribed motion as a rigid boundary segment. This master–slave coupling prevents artificial edge distortion and ensures a uniform transfer of the imposed displacement to the rubber tab.

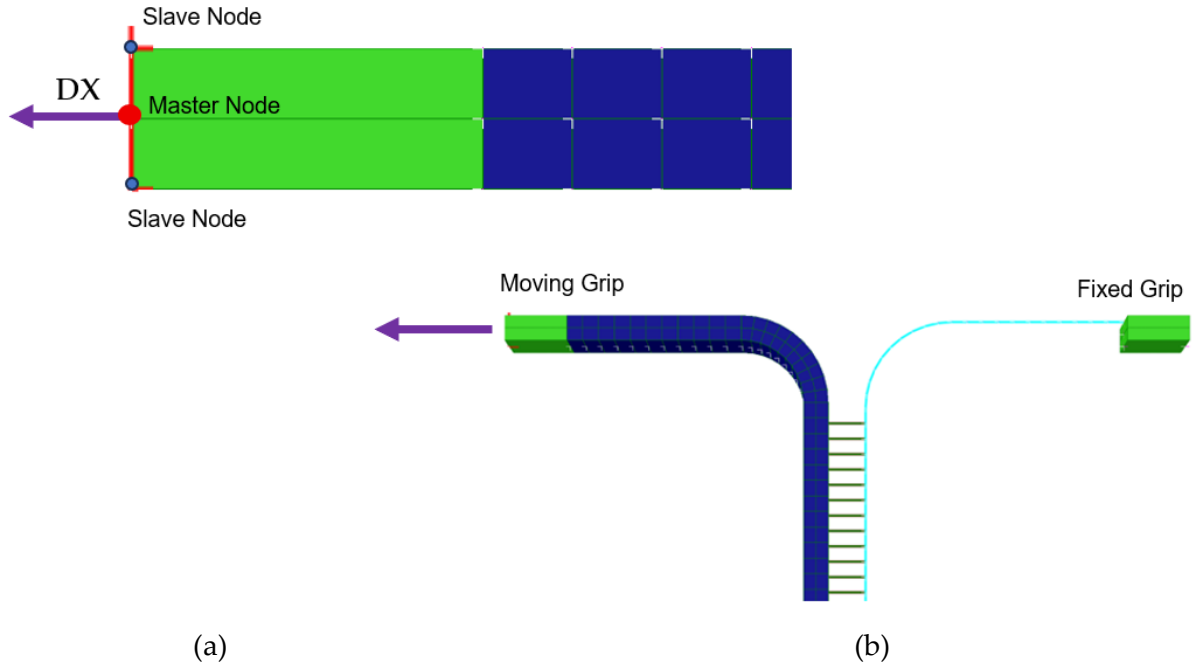


Figure 5.7 Peel-test FE model in Straus 7: (a) displacement-controlled moving grip with master–slave nodes coupling (b) moving and fixed steel grips.

To enhance numerical stability and prevent spurious lateral drift during the early stages of loading, the bottom nodes of the rubber arm in the still-bonded region were additionally constrained in D_Y . This restriction suppresses non-physical rigid-body motion in the transverse direction while the interface is still largely intact, i.e., when the last portion of the bonded length has not yet debonded.

5.1.4.3. Incremental loading strategy

Because the peel problem involves both material nonlinearity (hyperelastic rubber) and geometric nonlinearity (large rotations), the global equilibrium path was solved through an incremental–iterative procedure. The full prescribed displacement was applied in 1000 increments, providing sufficiently small step sizes (automatic substepping strategy) to capture progressive interfacial degradation (cut-off events) and to maintain robust convergence as the stiffness changes locally during debonding. This is consistent with general peel FE guidance: peel simulations typically require large-displacement formulations because the flexible arm rotates significantly and the stiffness matrix must be updated as geometry evolves.

5.1.4.4. Mesh and convergence considerations

Mesh dependence is a key issue in peel simulations, particularly near the debonding front where high gradients develop. Mesh convergence is therefore pursued by locally refining the mesh in the vicinity of the interface and by selecting an interface discretization length Δx such that the debonding front advances progressively from one discrete interface portion to the next as the applied displacement increases.[14].

In the present model, convergence is addressed by adopting a Δx reasonable small (2.5 mm) and verifying that the nonlinear solver remains stable and achieves equilibrium at each load increment, i.e., no loss of convergence associated with the advancing debonding front or distortion of plate elements. Moreover, an additional refinement study is used to demonstrate that this discretization is also sufficient in terms of global response invariance under consistent re-scaling.

6 FE model: Calibration Strategy

In this chapter, the experimental peel dataset is processed in MATLAB to obtain a reduced set of trilinear reference parameters that serve as calibration benchmarks. These targets are then mapped into initial estimates of the cut-off-bar stiffnesses and tensile thresholds used to run the first FE simulations. A structured sensitivity study is subsequently presented, showing how variations in E_i and $F_{u,i}$ affect the global response and guiding the successive parameter updates. The chapter concludes with the final calibrated configuration and its comparison against the experimental reference curves.

6.1. Calibration targets

As discussed in Section 4.1.1, the global peel response of bonded flexible assemblies can be conveniently interpreted in terms of three characteristic branches—an initial approximately linear regime, a transition region, and a quasi-steady plateau—so that the experimental F/b –displacement curve can be represented by a trilinear global idealization. This reduced description is particularly useful for calibration purposes, because it isolates the key macroscopic features that a numerical model should reproduce.

In the present work, the available experimental dataset from Pianese et al. consists of three peel curves. Rather than selecting a single curve as reference, a MATLAB [15] post-processing script was developed to extract the trilinear parameters from each individual curve and then compute average target values representative of the test repeatability. This averaging procedure is adopted because peel experiments often exhibit scatter and local oscillations, while the FE interface model employed in this thesis is intentionally simplified and is calibrated to reproduce the dominant macroscopic response rather than test-specific fluctuations. The final outcome of the MATLAB processing is therefore a single global trilinear target curve, characterized by an average initial stiffness and an average plateau level (and their associated global

displacement markers), which is taken as the calibration objective for the first parameter set of the discrete cut-off-bar interface model introduced in Chapter 5.

6.1.1. Trilinear idealization of experimental peel global curves dataset using MATLAB algorithms

Based on the trilinear idealization criterion, the coordinates of the three global experimental curves (Specimen 1–3) showed in Figure 3.5 (a) are transferred into an Excel file as paired columns (x,y), where:

- (x) is the displacement [mm]
- (y) is the force per unit width (F/b) [N/mm]

This format supports automated processing and consistent comparison among replicates. The MATLAB script, named *Trilinear Idealization*, performs four main tasks:

1. Import data sets from the Excel file, and plot the replicate curves;
2. Estimate an equivalent initial elastic stiffness from the early-stage response;
3. Identify a stable peel level (idealized plateau) from the later-stage response;
4. Build a mean trilinear idealization used as a calibration target.

To obtain an objective estimate of the initial slope for each specimen curve, the code applies a mild smoothing (*movmean*) to reduce local noise and then evaluates progressively larger initial segments of the curve. For each candidate segment, a linear regression is performed, and its coefficient of determination (R^2) is computed, where R^2 measures how well the linear model explains the variability of the data. Therefore, values of R^2 closer to 1 indicates a better fit.

The algorithm then selects the longest segment that satisfies a high linearity requirement, using a target threshold of $R^2 = 0.995$ and a fallback threshold of $R^2 = 0.98$ when necessary, and calculates:

$$k_n = \frac{\sum x_i y_i}{\sum x_i^2} \quad (6.1)$$

Where:

- k_n : Equivalent initial stiffness of the peel curve in normalized units [N/mm²];
- x_i : displacement value at the i -th data point of the selected initial linear segment [mm];
- y_i : normalized force (force per unit width) at the i -th data point of the selected segment [N/mm].

This yields $k_{n,1}$, $k_{n,2}$, $k_{n,3}$ and the mean:

$$k_{n,\text{avg}} = \text{mean}(k_{n,j}) \quad (6.2)$$

The code also stores the endpoint displacement of the selected segment for each replicate and defines an average elastic-limit displacement $\bar{x}_{el}$ used in the trilinear model.

Although the experimental curves may show gradual trends and fluctuations, the later modelling step benefits from a simplified representation of a stable peel resistance level. The script detects a plateau-like region for each replicate using:

- The local derivative dy/dx (via gradient) of a smoothed signal;
- A small-slope condition $|dy/dx| \leq \text{slopeTol}$;
- A low-variability condition based on a moving-window coefficient of variation CV ;
- A minimum displacement threshold to avoid the start-up region.

It searches for the longest continuous run satisfying the conditions and computes, for each curve:

- P_i : mean plateau level $[N/mm]$
- σ_i : local standard deviation in that window

A global plateau level is then computed as:

$$P_{\text{Global}} = \text{mean}(P_i) \quad (6.3)$$

and the scatter among replicates is stored as σ_{Global} . The code also averages the plateau start/end displacements $(\bar{x}_{P1}, \bar{x}_{P2})$.

Graphically, the script overlays:

- A solid plateau line over the detected mean range.
- An optional dotted extension to the end of the test as a modelling idealization.

Using the extracted parameters, the code constructs a trilinear target curve in F/b units, plotting the following linear branches:

1. **Elastic branch:** from $(0,0)$ to $(\bar{x}_{el}, k_{n,\text{avg}}\bar{x}_{el})$.
2. **Transition branch:** straight segment connecting the elastic endpoint to plateau start $(\bar{x}_{P1}, P_{\text{Global}})$.
3. **Plateau:** a horizontal line at P_{Global} , optionally extended to the end displacement.

The same construction is also shown in total-force units by multiplying by the width of the tested specimens (25 mm). Finally, the script prints a summary report of stiffness values, R^2 , segment endpoints, and plateau metrics for traceability and reproducibility.

For the three experimental peel curves reported by Pianese et al., the MATLAB algorithm yields the parameters summarized in Tables 6.1 and 6.2 .

Table 6.1: Initial elastic slope k_n and elastic limit displacement extracted from MATLAB script.

Specimen	R^2	$k_n \left[\frac{N}{mm^2} \right]$	$k_{n,avg} \left[\frac{N}{mm^2} \right]$	$\bar{x}_{el} [mm]$
1	0.9595	0.056	0.0716	23.110
2	0.9750	0.103		
3	0.9496	0.054		

Table 6.2 Plateau detection per curve and global plateau target extracted from MATLAB script.

Specimen	Plateau range in x [mm]	Plateau level $(F/b)_p$ [N/mm]	P_{Global} [N/mm]	$\bar{x}_{onset} [mm]$
1	241.958 – 397.022	3.951	4.356	291.55
2	319.094 – 358.368	4.362		
3	313.597 – 400.316	4.755		

Graphically these results are the ones illustrated in Figure 6.1 and 6.2. They are considered the target objectives to reproduce with the FE model and translated into initial estimates of the cut-off-bar stiffnesses and strength thresholds, as is described in the following section.

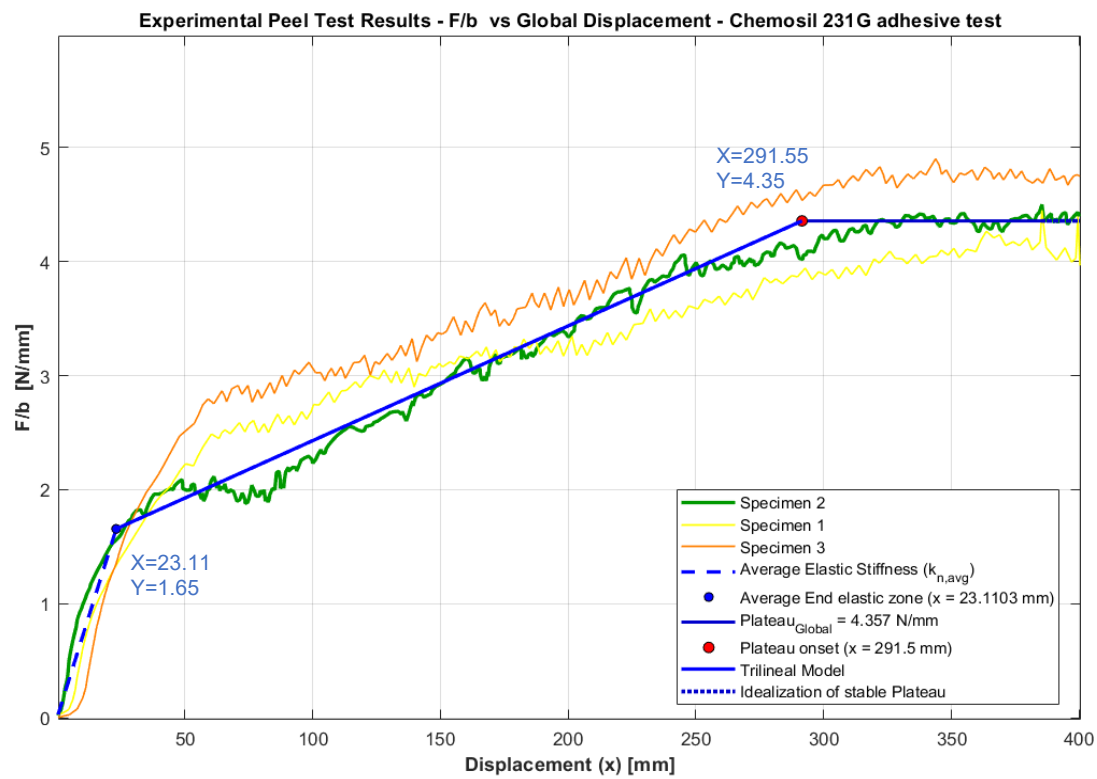


Figure 6.1 Peel Test: F/b vs Global Displacement curves and their average trilinear idealization.

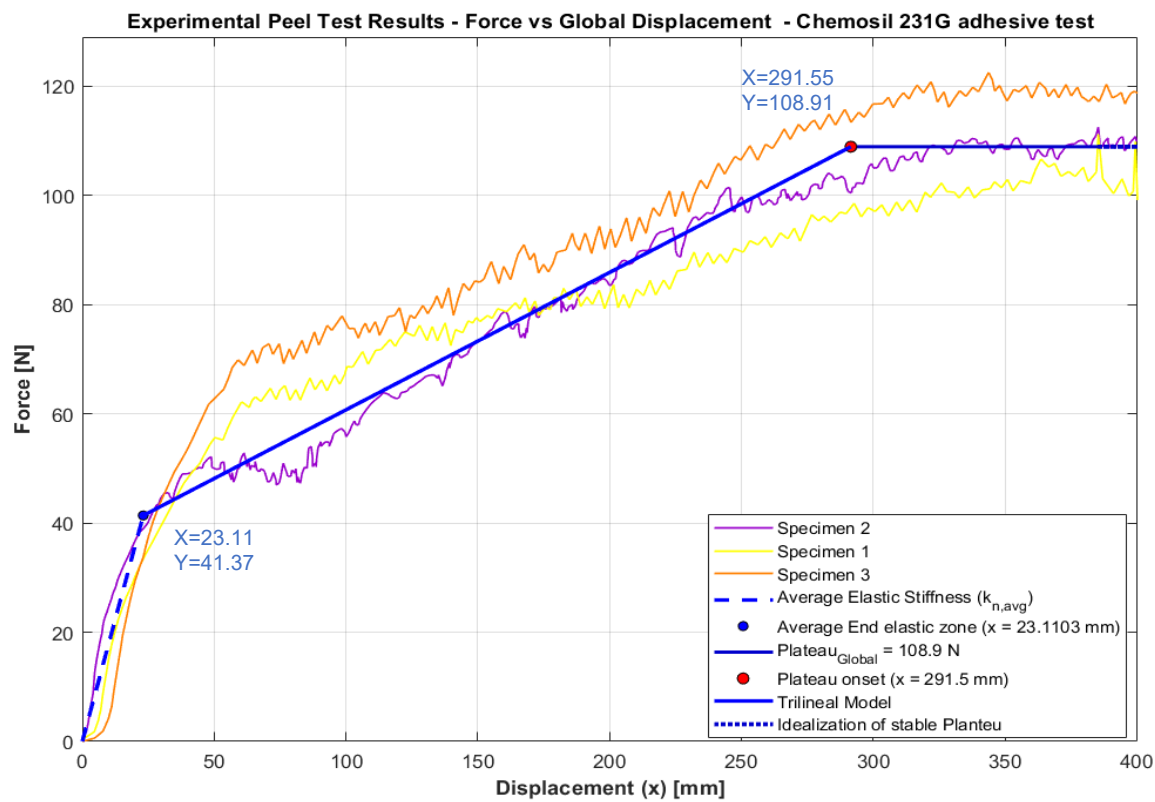


Figure 6.2 Peel Test: F vs Global Displacement curves and their average trilinear idealization.

6.2. Initial estimates of cut-off-bar parameters ($E_i, F_{u,i}$)

To initialize the discrete interface model, the first set of cut-off-bar parameters $\{E_i, F_{u,i}\}$ was estimated from the target quantities.

In this initialization step, $\delta_1, \delta_2, \delta_3$ denote **global displacement markers** read from the global $F/b-u$ curve (e.g., end of the initial linear regime and onset of the plateau). They are not interpreted as the local separation across the interface at the debonding front. In a peel test, the measured global displacement x represents the integrated response of adherend bending/rotation and test compliance coupled with progressive interfacial degradation. Therefore, the δ_i are used here only as practical global reference points to place the cut-off events in the initial sawtooth-like approximation; the final interface parameters are subsequently refined by calibration.

6.2.1. Global initial slope and cut-off bars elastic modulus

From the average initial stiffness per unit width provides by the MATLAB script: $k_n = 0.0716 \text{ [N/mm}^2\text{]}$. For specimen width $b = 25 \text{ mm}$, the corresponding global stiffness in force–displacement form is:

$$K_{\text{tot}} = k_n b = 0.0716 \times 25 = 1.79 \text{ [N/mm]}. \quad (6.4)$$

This K_{tot} is taken as the target initial stiffness of the three cut-off bars acting in parallel at each interface station. Since the three cut-off bars are connected at the same pair of nodes in each interface portion, they undergo the same opening displacement and their forces add. Consequently, their axial stiffnesses add:

$$K_{\text{tot}} = K_1 + K_2 + K_3. \quad (6.5)$$

To obtain a response considering that two bars controlling the early stiffness reduction and one bar sustaining the long-range response, the total stiffness was distributed using the non-dimensional weights:

$$K_1 = \alpha_1 K_{\text{tot}} \quad ; \quad K_2 = \alpha_2 K_{\text{tot}} \quad ; \quad K_3 = \alpha_3 K_{\text{tot}}, \quad (6.6)$$

with:

$$\alpha_1 = 0.45, \alpha_2 = 0.45, \alpha_3 = 0.10. \quad (6.7)$$

These coefficients are stiffness fractions (dimensionless), selected so that:

$$\alpha_1 + \alpha_2 + \alpha_3 = 0.45 + 0.45 + 0.10 = 1.00, \quad (6.8)$$

which ensures that $K_1 + K_2 + K_3 = K_{\text{tot}}$ and therefore preserves the target initial global stiffness. The adopted distribution assigns most of the initial stiffness to two “fast”

bars (bars 1–2), while the third bar is significantly softer so that—once bars 1–2 have reached their cut-off limits—the remaining stiffness is reduced and the response is sustained mainly by bar 3.

It is noted that the triplet (0.45, 0.45, 0.10) is not unique. Any set $(\alpha_1, \alpha_2, \alpha_3)$ satisfying $\alpha_i \geq 0$ and $\sum \alpha_i = 1$ would preserve K_{tot} . Different partitions were later explored during the sensitivity analysis to tune the shape of the global response (transition smoothness and serration amplitude), while maintaining the same initial stiffness anchor.

With $K_{\text{tot}} = 1.79 \text{ N/mm}$, the initial stiffnesses become:

$$K_1 = K_2 = 0.8055 \text{ N/mm} \quad (6.9)$$

$$K_3 = 0.179 \text{ N/mm} \quad (6.10)$$

Each cut-off bar is modelled as an axial truss with unit cross-sectional area $A = 1 \text{ mm}^2$ and effective length $L_b = 6 \text{ mm}$.

The axial stiffness of bar i is:

$$K_i = \frac{E_i A}{L_b} \Rightarrow E_i = \frac{K_i L_b}{A}. \quad (6.11)$$

Therefore:

$$E_1 = E_2 = 4.833 \text{ MPa}, \quad E_3 = 1.074 \text{ MPa}, \quad (6.12)$$

These values provide the initial elastic module to be assigned to the three cut-off-bar materials in Straus 7.

6.2.2. Plateau anchor and cut-off bars maximum tension forces

From the MATLAB script, the plateau level per unit width was estimated as:

$$P_{\text{Global}} = 4.3565 \text{ N/mm}. \quad (6.13)$$

Hence, the corresponding total plateau force is:

$$F_{\text{plat}} = P_{\text{Global}} b = 108.9 \text{ N}. \quad (6.14)$$

The cut-off forces $\{F_{u,i}\}$ were then selected to (i) satisfy the global plateau capacity and (ii) generate staggered stiffness drops. Using the MATLAB-extracted global marker at the end of the initial linear zone $\delta_1 = 23.11 \text{ mm}$ and placing the third cut-off at the plateau onset $\delta_3 = 291.5 \text{ mm}$, the first and third thresholds were defined as:

$$F_{u,1} = K_1 \delta_1 = 18.61 \text{ N}, \quad (6.15)$$

$$F_{u,3} = K_3 \delta_3 = 52.18 \text{ N}. \quad (6.16)$$

The remaining force capacity was assigned to bar 2 by balance to satisfy the plateau total:

$$F_{u,2} = F_{\text{plat}} - F_{u,1} - F_{u,3} = 108.9 - 18.61 - 52.18 = 38.11 \text{ N}. \quad (6.17)$$

The implied second cut-off displacement marker, still interpreted only as a global anchor, is:

$$\delta_2 = \frac{F_{u,2}}{K_2} = \frac{38.11}{0.80} = 47.31 \text{ mm}. \quad (6.18)$$

The initial values of E_i and $F_{u,i}$ adopted calculated are reported in Table 6.3.

Table 6.3 Initial cut-off-bar parameters ($E_i, F_{u,i}$) used for the first FE simulation.

Cut-off bar i	Type	Ei [MPa]	Fu,i [N]
1	Brittle	4.833	18.61
2	Brittle	4.833	38.11
3	Ductile	1.074	52.18

Overall, this set $\{E_i, F_{u,i}\}$ constitutes an internally consistent **initial estimate** anchored to global response metrics. These values were used as starting inputs in Straus 7 software and were subsequently refined through a sensitivity-based calibration against the full experimental $F/b-x$ curves.

6.3. Cut -off -bar stiffness and strength sensitivity analysis

Once the initial estimates of the cut-off-bar elastic moduli E_i and cut-off force thresholds $F_{u,i}$ had been obtained from the trilinear idealization of the experimental peel response (Section 6.2), these values were assigned to the interface elements in Straus 7 and a first numerical simulation was performed (run FEM-R001). At this stage, the parameters $\{E_i, F_{u,i}\}$ are intended only as a first-order guess, used to assess the

baseline response of the discrete interface model and to identify which parameters primarily control the initial stiffness and the propagation plateau.

In the present FE implementation, the interface is represented exclusively by cut-off-bar elements placed along the bonded length. Since the peel test is dominated by opening of the interface during debonding, the cut-off bars are intended to carry tensile tractions only. To prevent non-physical compressive load transfer, which could artificially stiffen the assembly during local contact/closure, a very large compressive capacity was assigned (e.g., $F_c \sim 10^6$ N), effectively ensuring that compression does not govern the response.

Although peeling is generally a mixed-mode process (Mode I + Mode II), the present modelling strategy focuses on a Mode I-driven idealization by placing the cut-off bars in the opening direction, thus treating the interface as a bed-of-springs (imperfect interface) acting primarily in tension. This choice is consistent with the objective of reproducing the dominant macroscopic response with a simplified, directly implementable interface law. In the fracture-mechanics literature, the double cantilever beam (DCB) provides the classical Mode I benchmark, where the adherends can be modelled as beams connected by an interface law; for isotropic adherends, the contribution of adherend shear deformability is often reported as negligible compared with bending in the standard formulation (Dimitri, et al. 2017)[6][16][17]. In the present peel configuration, the rubber adherend is modelled as an isotropic hyperelastic continuum, and it largely governs the global deformation due to its much lower stiffness compared with the fiberglass strip, which is idealized as a stiff, equivalent homogeneous beam. Under these assumptions, adopting a Mode I-driven spring-like interface is a reasonable first approximation for the sensitivity study, while acknowledging that a more refined model could incorporate mixed-mode coupling.

For each run, the numerical global response is extracted as the reaction force component F_x at the master node where the prescribed global displacement is applied (moving grip). This reaction is plotted versus the imposed global displacement x , producing the numerical force–displacement curve.

To compare with experiments, the experimental curves originally reported as F/b are converted into total force by multiplying by the specimen width b :

$$F(x) = \left(\frac{F}{b}\right)(x) * b \quad ; \quad b = 25 \text{ mm.} \quad (6.19)$$

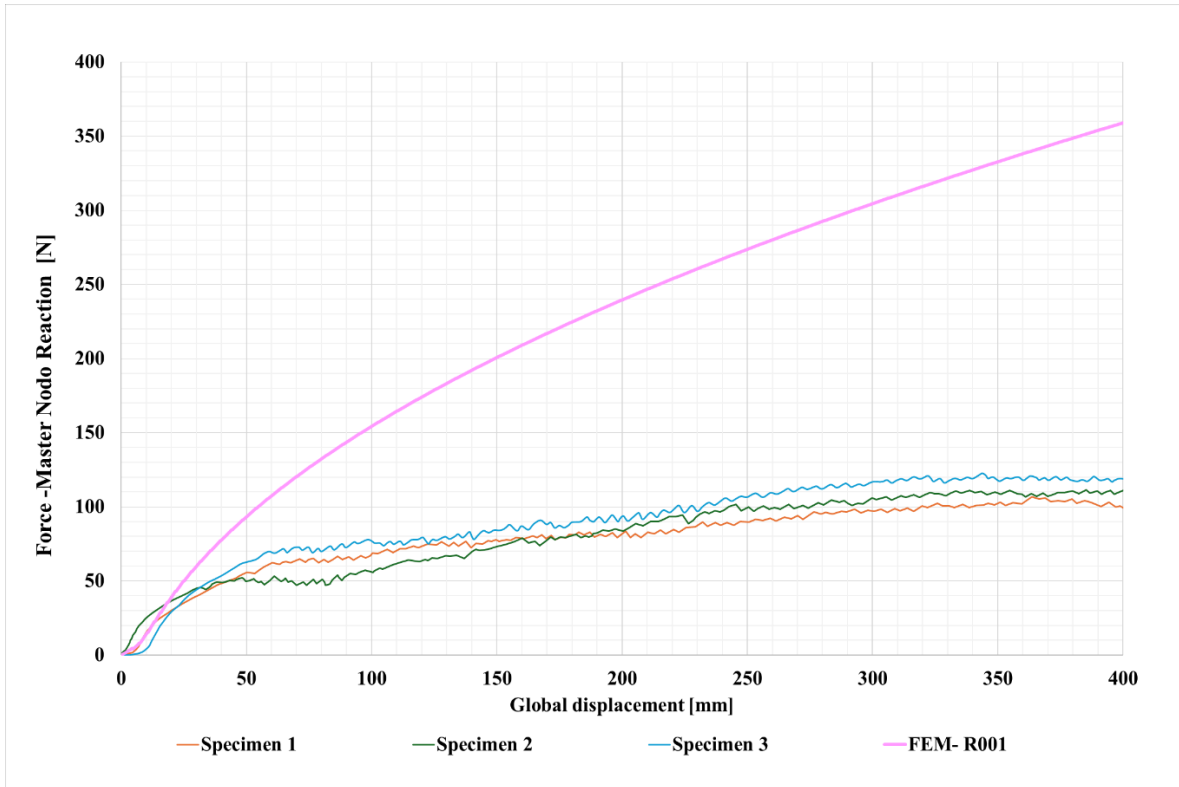
This conversion allows the experimental and numerical responses to be expressed in consistent units (N vs. mm), despite the 2D nature of the FE idealization.

Figures 6.3a–6.3b show the comparison between the three experimental curves and first run of the model, (FEM-R001). The former shows the F/b displacement curve across the full 400 mm displacement range, while the latter only shows the first 25 mm

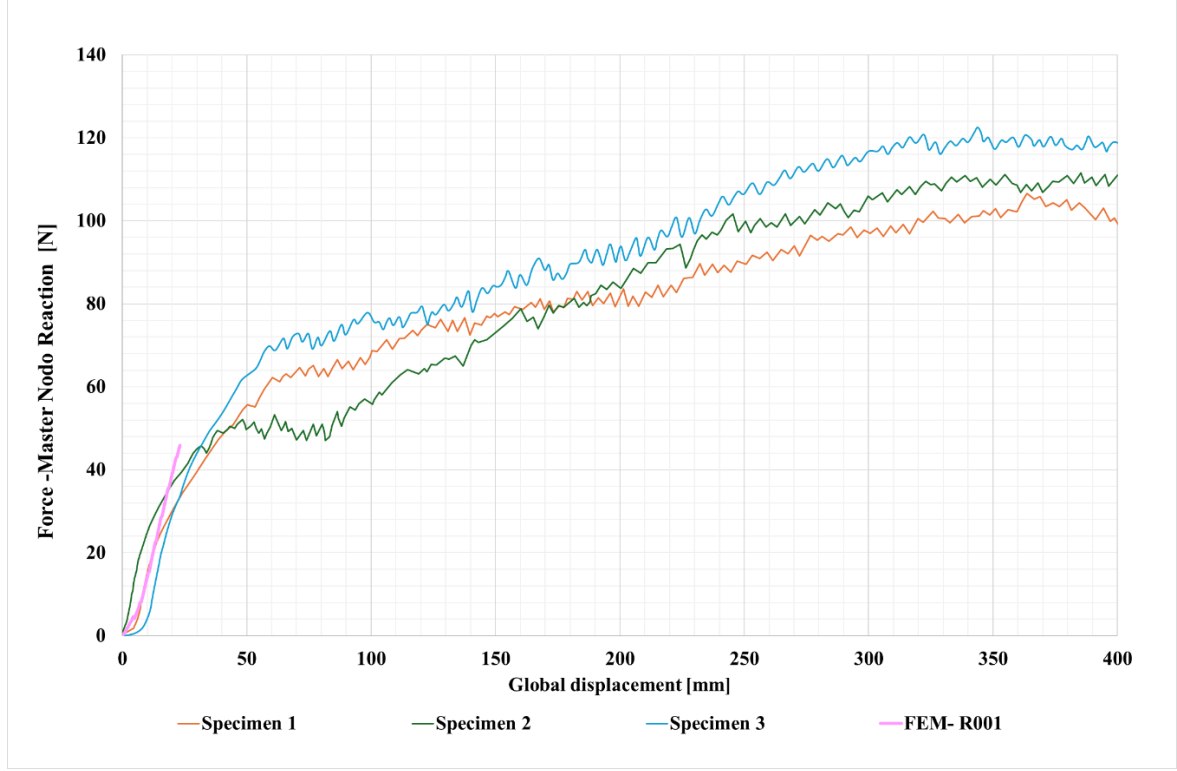
of recorded displacements, to improve the visualization of the initial elastic stiffness range achieved with the FE model on a comparable scale.

The initial portion of FEM-R001 reproduces the experimental initial stiffness reasonably well (see Figure 6.3(b)). This is consistent with the initialization procedure, since the cut-off-bar elastic moduli E_i were computed so that the sum of the three parallel bar stiffnesses matches the target initial slope derived from the trilinearization $K_{\text{tot}} = k_{\text{avg}} b = 1.79 \text{ N/mm}$. (Eq. 6.4)

However, FEM-R001 strongly overpredicts the force level at large displacements (see Figure 6.3a). At $x = 400 \text{ mm}$, the reaction reaches approximately 359 [N], whereas the target plateau force derived from the experimental average is $F_{\text{plat,target}} = 108.9 \text{ [N]}$ (Eq. 6.14).



(a)



(b)

Figure 6.3 Peel-test Experimental curves vs FE model run R001 results along: (a) full 400 mm displacement range , (b) 25 mm displacements range.

This discrepancy is expected from the way the initial cut-off forces $F_{u,i}$ were assigned. The preliminary values $\{F_{u,i}\}$ were computed so that, at a single interface station, the sum of the three bars reproduces the target plateau force (because the bars act in parallel and their forces add). In the actual FE peel model, however, the plateau is not governed by a single station: debonding advances through a finite active zone near the peel front where multiple interface portions are simultaneously engaged and carry tension. Therefore, the global reaction force during propagation is closer to:

$$F_{\text{global}} \approx \sum_{j \in \Omega_{\text{active}}} \sum_{i=1}^3 F_{i,j}, \quad (6.20)$$

where Ω_{active} denotes the set of interface portions (stations) currently active in the process zone (pz), and i , the cutoffbar number acting in that portion.

If the strength level is assigned per portion without accounting for the number of simultaneously active portions, the model naturally overestimates the global plateau by approximately a factor proportional to the effective number of active stations.

In other words, the plateau level is controlled not only by the per-bar tensile thresholds $F_{u,i}$, but also by the **effective process-zone length** and its discretization Δx , i.e., by how many interface portions (N_{active}) contribute at the same time:

$$N_{\text{active}} \approx \frac{\ell_{\text{pz}}}{\Delta x} \quad (6.21)$$

The overprediction observed in FEM-R001 therefore indicates that the initial $F_{u,i}$ values must be scaled down per portion to ensure that the sum of the contributions of all simultaneously active portions matches the target plateau force $F_{\text{plat,target}}$. This is the main motivation for the subsequent sensitivity analysis, where the cut-off thresholds are reduced (while keeping the target plateau force unchanged) to reflect the fact that only a limited region near the peel front is active at any time.

Finally, it is noted that the cut-off thresholds were initially computed using global displacement markers δ_1 and δ_3 extracted from the experimental $F/b-x$ idealization curve. These quantities do not represent local bar elongations; consequently, the initial $F_{u,i}$ values are expected to be less reliable than the stiffness estimates and must be refined through calibration.

Consequently, in the subsequent trial, the cut-off force thresholds were rescaled to account for the fact that, during steady peeling, the global reaction is sustained by a finite active (process) zone rather than by a single interface station. Specifically, an effective number of simultaneously engaged interface portions N_{active} was introduced, so that the per-portion strength is reduced while the global target plateau $F_{\text{plat,target}} = 108.9 \text{ N}$ is preserved.

Based on the adopted interface discretization $\Delta x = 2.5 \text{ mm}$, an initial estimate $N_{\text{active}} = 7$ was assumed, corresponding to an effective active length $\ell_{\text{pz}} \approx N_{\text{active}} \Delta x = 17.5 \text{ mm}$. Accordingly, the tensile thresholds $F_{u,i}$ assigned to each cut-off bar were divided by 7 for the second run. This scaling does not imply a change in the experimental plateau objective; rather, it enforces that the sum of the contributions of the concurrently active portions within the process zone matches the target plateau level, providing a more realistic starting point for the subsequent sensitivity analysis.

6.3.1. Cutoff bars' strength scaled considering a finite active interface process zone

With the initial cut-off forces adopted in RUN 001, inspection of the deformed configuration in Straus 7 shows that the debonding front does not effectively advance. The tensile cut-off thresholds assigned to the cut-off bars are sufficiently high that neither the elasto-brittle bars (bars 1–2) nor the elasto-ductile bar (bar 3) reaches its

limit; consequently, no bar failure is triggered and the interface remains essentially intact.

This is consistent with the smooth, continuous shape of the FEM-R001 curve (pink line) in Figure 6.3a. Since no discrete bar failures occur, the response does not exhibit the expected stepwise load drops (“sawtooth” signature) associated with progressive station-by-station debonding.

In Run R002, the cut-off thresholds were reduced by dividing the original $F_{u,i}$ values by $N_{\text{active}} = 7$, i.e., the assumed number of simultaneously engaged interface portions in the active zone. Although the global reaction force still reaches a high value at large displacement (peak = 350.73 N), the qualitative failure mechanism becomes physically meaningful: the debonding front can now be tracked visually as it propagates progressively along the bonded length, as is shown in Figure 6.4

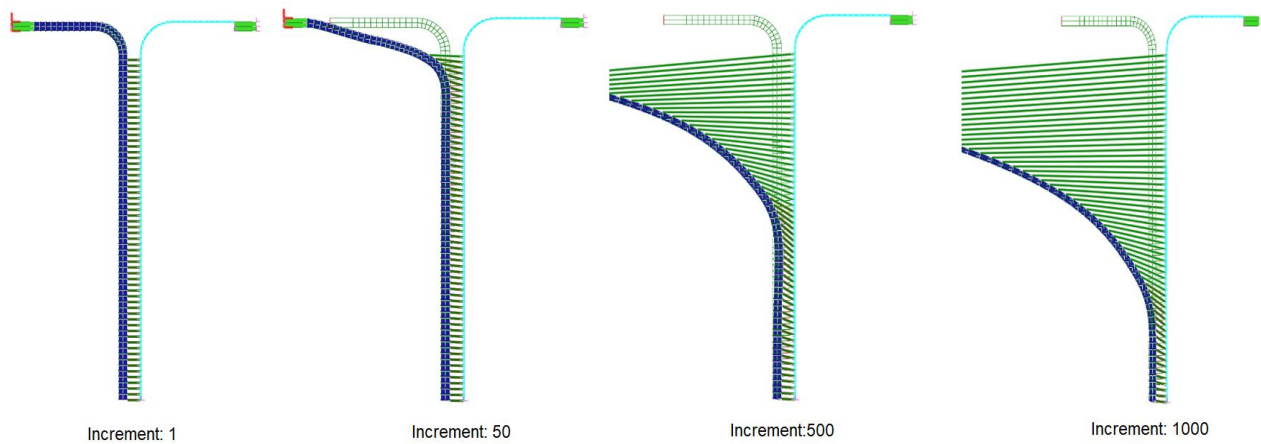
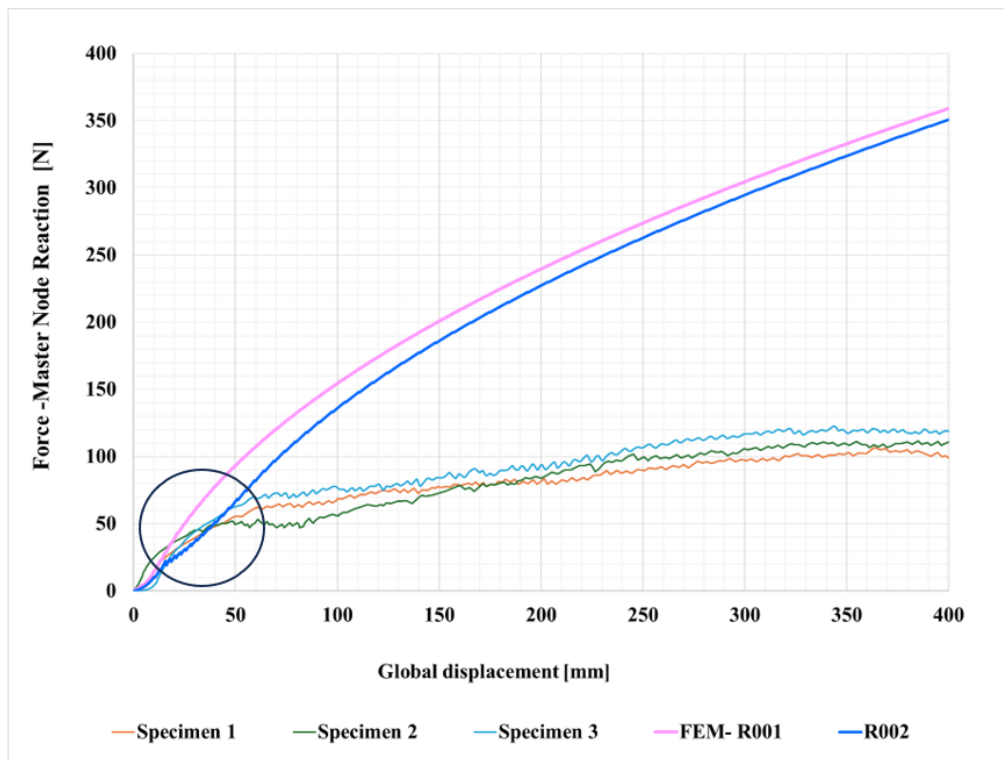
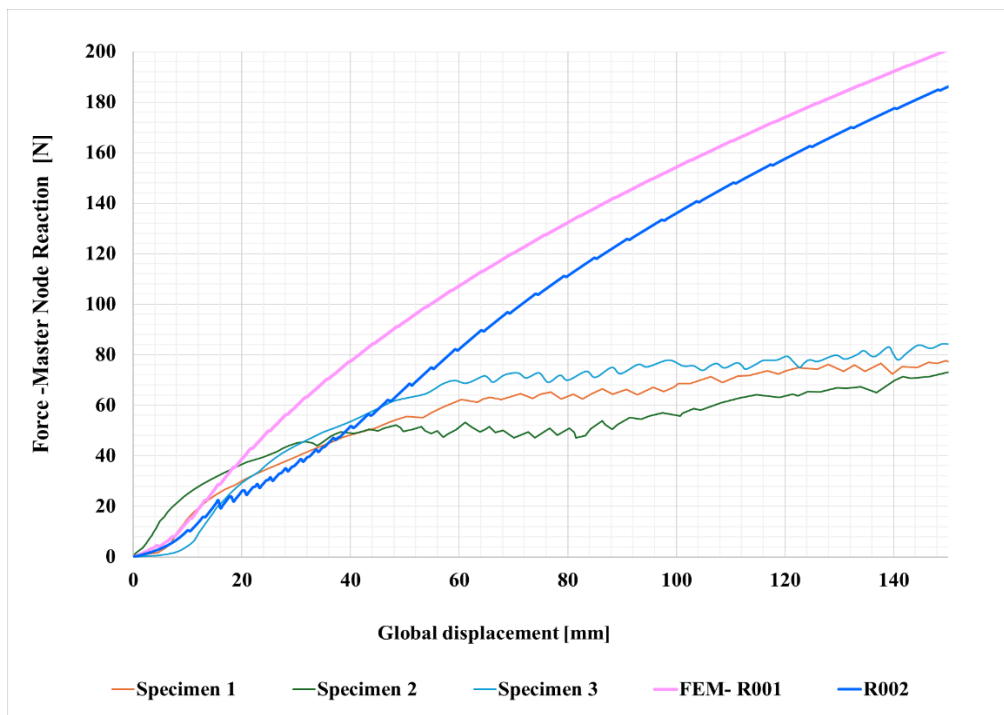


Figure 6.4 Tracking of the debonding peel front along the bonded length while increasing the increment of prescribed displacement.

This propagation is also evident in the corresponding numerical force–displacement curve, which displays the characteristic serrations at the beginning of the response, as is shown in Figure 6.5(a) and zoomed until $x=140$ mm displacement in Figure 6.5(b). These oscillations arise because, when a given interface station reaches its tensile threshold, one or more cut-off bars fail, producing a sudden local stiffness reduction and a temporary drop in the global reaction, followed by reloading as adjacent stations become engaged.



(a)



(b)

Figure 6.5 Peel-test Experimental curves vs FE model RUN R001 and R002 results along: (a) full 400 mm displacement range (b) 140 mm displacement range.

For R002, the per-bar tensile thresholds become $F_{u,1} = 2.659$ N, $F_{u,2} = 5.446$ N, and $F_{u,3} = 7.454$ N, giving:

$$\sum_{i=1}^3 F_{u,i} = 15.56 \text{ N.} \quad (6.22)$$

This value represents the maximum tensile capacity of the three parallel cut-off bars at a single interface station (i.e., for one discretized interface portion of length Δx), not the global plateau force. The global plateau emerges from the collective contribution of multiple stations that are simultaneously active within the process zone; therefore, the overall reaction level remains higher than $\sum_i F_{u,i}$ and depends on the effective process-zone length, the interface discretization Δx , and the evolving kinematics of the peel arms.

Although RUN 002 correctly activates progressive debonding, the predicted propagation level is still above the experimental plateau, indicating that further calibration of the cut-off thresholds is required. In particular, since bar 3 is defined as elasto-ductile and is intended to sustain the propagation stage, its threshold $F_{u,3}$ plays a dominant role in controlling the steady peeling resistance. For this reason, subsequent runs were performed by reducing $F_{u,3}$ while compensating through an increase of $F_{u,1}$ (and/or $F_{u,2}$) so as to keep the station-level total strength $\sum_i F_{u,i}$ approximately constant (around 15.56 N), while modifying the sequence and extent of stiffness loss. This redistribution strategy allowed the global plateau level to be progressively reduced and brought closer to the experimental target.

6.3.1.1. Sensitivity of the global response to the cut-off thresholds $F_{u,i}$

Starting from RUN 002, where progressive debonding was obtained after scaling the thresholds to account for a finite active zone, a parametric study was performed to assess the influence of $F_{u,3}$. In RUN 003, $F_{u,3}$ was reduced to 3 [N] in order to directly visualize its effect on the global response. At the same time, $F_{u,1}$ and $F_{u,2}$ were increased with respect to RUN 002, as shown in Table 6.4, so that the station-level strength (sum of the three parallel bar thresholds at one interface station) remained of the same order of magnitude. The resulting curve, R003 illustrated through the green line in Figure 6.6, shows a clear reduction of the global force level compared with RUN 002, with a maximum reaction of 230.29 [N], confirming that decreasing the ductile threshold $F_{u,3}$ effectively lowers the propagation resistance predicted by the model.

A further effect of increasing $F_{u,1}$ and $F_{u,2}$ becomes evident in R003. The curve exhibits a change in slope around $x = 26.42$ [mm], at which the reaction reaches approximately 58.64 [N]. This point can be interpreted as the end of the fully elastic stage and the onset of interface damage. This trend is mechanically consistent with the role of the brittle bars: since bars 1–2 are linear-elastic up to their cut-off thresholds and then fail abruptly, raising $F_{u,1}$ and $F_{u,2}$ delays the first discrete failures and shifts the global

elastic-limit point to a higher force/displacement. Notably, the displacement at which this transition occurs (26.42 mm) is close to the experimental marker obtained from the trilinear idealization ($x_e = 23.11$ mm), indicating that the chosen $F_{u,1} - F_{u,2}$ levels provide a reasonable initialization for the onset of damage.

Because $F_{u,1}$, $F_{u,2}$, and $F_{u,3}$ jointly define the station-level response, simultaneous changes in all three parameters can obscure parameter attribution. Therefore, in the subsequent runs the strategy was to keep $F_{u,1}$ and $F_{u,2}$ constant at the values adopted in RUN 003, so as to approximately preserve the experimentally consistent end of the fully elastic regime, and to vary only the ductile threshold $F_{u,3}$, which primarily governs the propagation stage. This approach is illustrated by RUN 004 and RUN 005, in Figure 6.6 and Table 6.4.

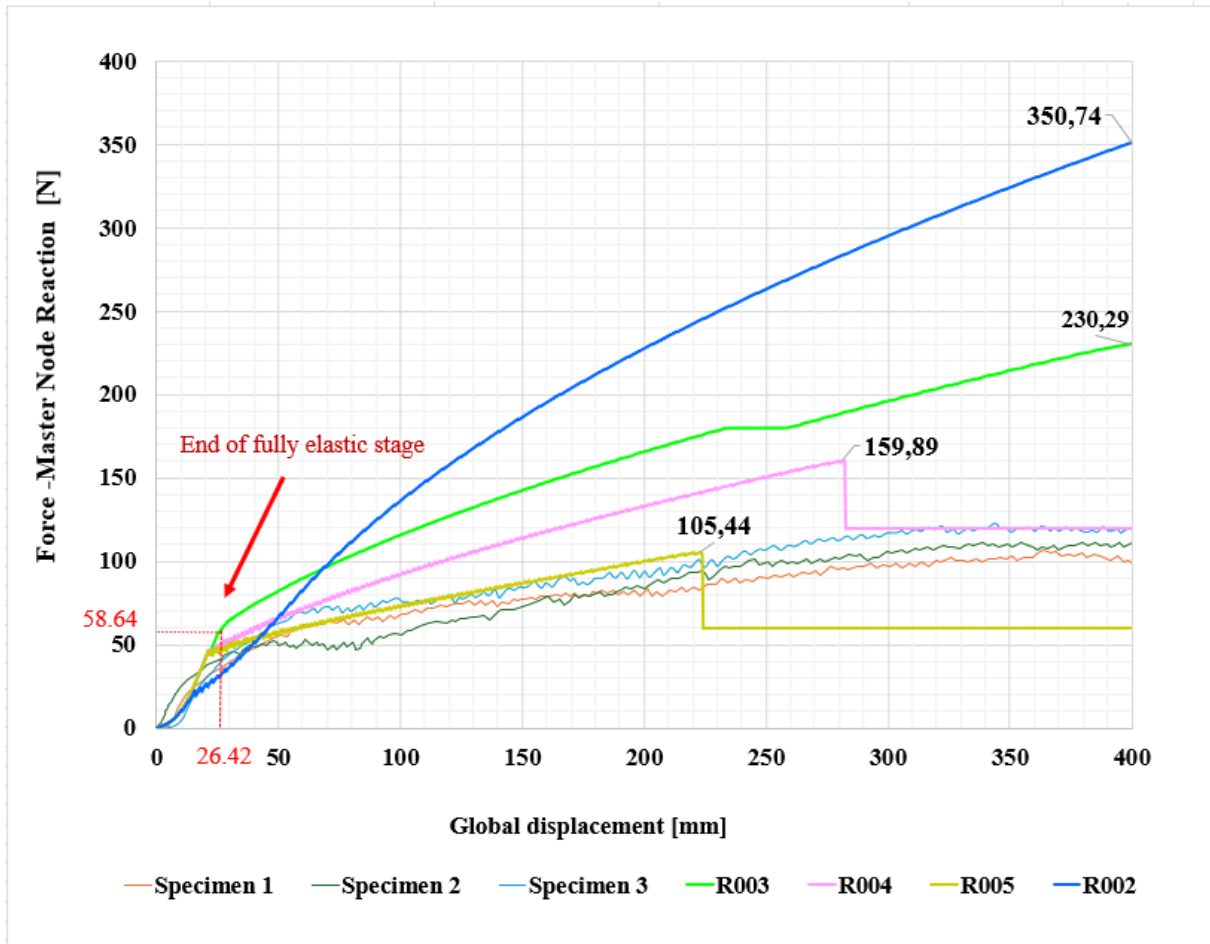


Figure 6.6 Peel-test Experimental curves vs FE model runs, showing the sensitivity of the global response to the cut-off thresholds $F_{u,i}$.

Table 6.4 Cut-off bar parameters E_i $F_{u,i}$ used in the FE first set of simulations and maximum reaction force (Fx) reached.

RunID	E1 [MPa]	E2 [MPa]	E3 [MPa]	$F_{u,1}$ [N]	$F_{u,2}$ [N]	$F_{u,3}$ [N]	F_{tot} [N]	Max (Fx) [N]
R002	4,833	4,833	1,074	2,65	5,44	7,45	15,55	350,74
R003	4,833	4,833	1,074	8,0	7,0	3,0	18,00	230,29
R004	4,833	4,833	1,074	8,0	7,0	2,0	17,00	159,89
R005	4,833	4,833	1,074	8,0	7,0	1,0	16,00	105,44

In RUN 004, the global curve shows an abrupt force drop at a reaction level of approximately 159.89 [N]. This drop is associated with complete debonding of the discretized bonded length, i.e., the debonding front reaches the end of the modeled interface region. The reaction does not drop to zero because, in Straus 7, an elasto-ductile cut-off bar does not necessarily vanish after reaching the tensile limit; rather, once the bar reaches its maximum tension force, it enters a yield-like regime where the axial force remains essentially capped near the assigned threshold in subsequent increments. As a result, a residual force level may persist even after full interface separation, reflecting the post-yield behavior of the ductile connectors remaining in the model.

In RUN 005, a further reduction of $F_{u,3}$ produces an additional decrease in the global force level reached before the final drop, consistent with the interpretation of bar 3 as the main contributor to the propagation resistance. Moreover, the transition from the fully elastic stage occurs at a lower force level than in RUN 004, reflecting the fact that, although $F_{u,1}$ and $F_{u,2}$ are kept fixed, the reduced contribution of the ductile bar lowers the overall station-level force capacity once damage is initiated and redistribution takes place among the parallel bars during progressive failure.

Overall, the trends observed in Figure. 6.6 and summarized in Table 6.4 confirm that: (i) $F_{u,1}$ and $F_{u,2}$ primarily control the displacement/force level at which the interface departs from a purely elastic response (damage initiation), while $F_{u,3}$ controls the post-initiation force level and, therefore, the predicted propagation resistance.

6.4. Second calibration set

Building on the previous parametric study, RUN 005 provided the first configuration for which the numerical response approached the experimentally derived plateau target. As shown in Figure 6.6, RUN 005 reaches a quasi-steady force level of approximately 105.44 [N], which is close to the target value $F_{\text{plat,target}} = 108.9$ [N] obtained from the MATLAB trilinearization. Starting from this case, only small adjustments were then explored on the ductile connector threshold $F_{u,3}$ (cut-off bar 3), since this parameter primarily controls the sustained resistance during propagation.

Despite the improved plateau level in RUN 005, the simulation still exhibited premature complete debonding, with the abrupt force drop occurring at $x = 225.03$ [mm]. This displacement is significantly lower than the global plateau onset identified from the experimental idealization, indicating that the model still debonds too early. From the perspective of the discrete interface law, this behavior suggests that the interface loses load-carrying capacity too rapidly within the transition stage (from the end of the purely elastic regime to the propagation regime). Since cut-off bars 1 and 2 are defined as elasto-perfectly brittle, their thresholds $F_{u,1}$ and $F_{u,2}$ strongly influence the onset and accumulation of discrete failures and therefore the overall evolution of stiffness degradation during this transition. For this reason, the subsequent runs focused on modifying $F_{u,1}$ and $F_{u,2}$, while maintaining the same elastic moduli (E_1, E_2, E_3) and keeping $F_{u,3}$ in the low range required to reproduce the plateau.

Accordingly, in RUN 006 and RUN 007 the thresholds of the brittle bars were redistributed as reported in Table 6.5.

Table 6.5 Cut-off bar parameters E_i $F_{u,i}$ used in the FE second set of simulations and maximum reaction force (Fx) reached.

RunID	E1 [MPa]	E2 [MPa]	E3 [MPa]	$F_{u,1}$ [N]	$F_{u,2}$ [N]	$F_{u,3}$ [N]	F_{tot} [N]	Max (Fx) [N]
R005	4,833	4,833	1,074	8,000	7,000	1,000	16,00	105,44
R006	4,833	4,833	1,074	6,500	12,000	0,800	19,30	117,47
R007	4,833	4,833	1,074	6,600	12,000	0,900	19,50	112,21

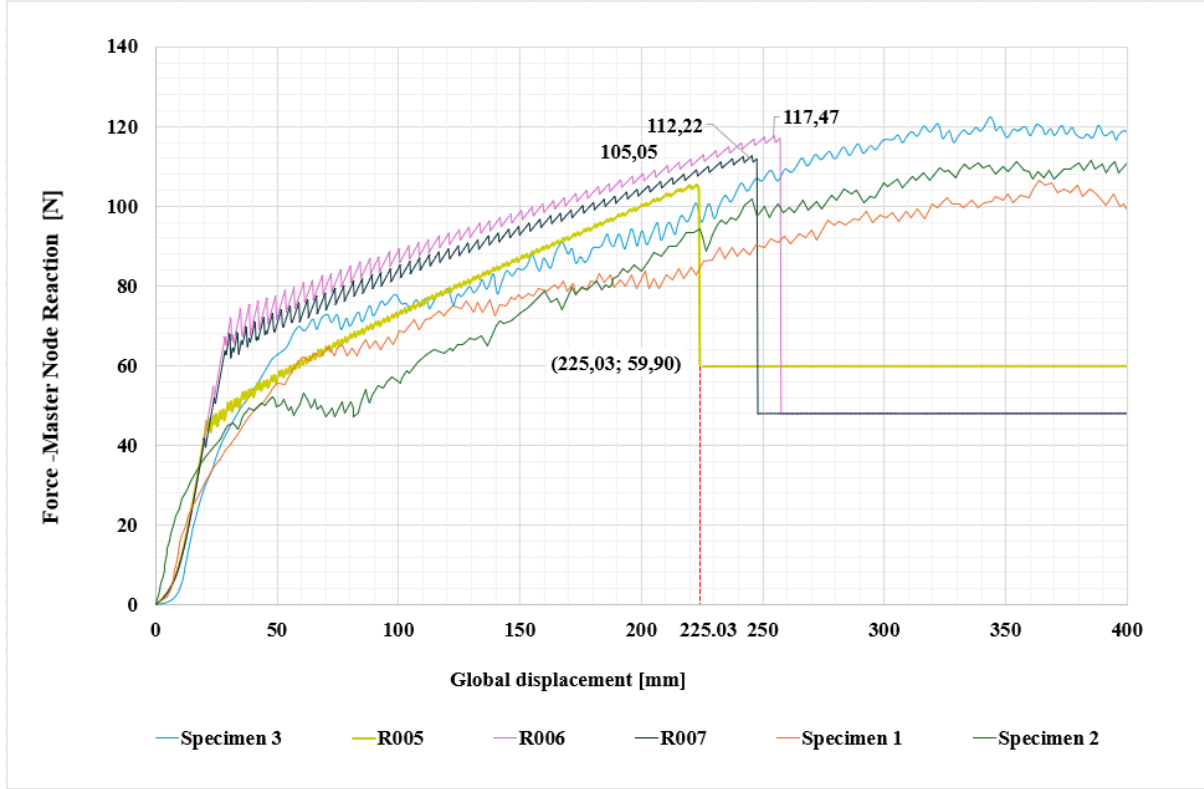


Figure 6.7 Peel-test Experimental curves vs FE second set of simulations, showing the sensitivity of the global response to the cut-off thresholds F_{u1} and F_{u2} .

Compared with RUN 005, RUN 006 increased the capacity of bar 2 to $F_{u,2} = 12.0$ [N] while slightly reducing bar 1 to $F_{u,1} = 6.5$ [N] and decreasing the ductile threshold to $F_{u,3} = 0.8$ [N]. In RUN 007 a similar configuration was tested, with $F_{u,1} = 6.6$ N, $F_{u,2} = 12.0$ N, and $F_{u,3} = 0.9$ [N]. The rationale behind these changes is that increasing $F_{u,2}$ delays the brittle failure of bar 2 at each interface station, thereby postponing the loss of local stiffness and contributing to a slower degradation of the interface resistance as the front advances. Consistently with this mechanism, the simulations show that increasing the tensile capacity of the brittle bars tends to delay the abrupt load drop occurs at a larger global displacement than in RUN 005, because the interface stations can sustain higher tractions before the brittle components fail.

However, these runs also confirm an important coupling: although $F_{u,1}$ – $F_{u,2}$ primarily govern the early-stage/transition response, they also influence the measured propagation level. Increasing $F_{u,2}$ modifies the sequence and extent of stiffness loss in the active zone, which changes how the load is redistributed among the three parallel bars and among neighboring interface stations. As a consequence, the global reaction level in the propagation stage may increase together with the delayed failure.

In other words, $F_{u,2}$ does not act exclusively on the elastic limit; it affects the evolving process-zone mechanics, and therefore it can indirectly shift the apparent plateau level as well. This trend can be observed in Figure 6.7, where the configurations with increased $F_{u,2}$ exhibit delayed debonding but also a higher global force level than the RUN 005 case.

On the basis of these observations, the sensitivity study was continued by systematically tuning $F_{u,1}$ and $F_{u,2}$, with small compensating adjustments to $F_{u,3}$ where needed, to obtain a balanced response: (i) a plateau close to the experimental target and (ii) a debonding progression that occurs at a global displacement closer to the target, postponing complete separation beyond the premature values observed in RUN 005.

6.5. Final calibrated model

After calibrating the tensile strength limits of the cut-off bars, minor refinements were also introduced to the elastic module of Bars 1 and 2 in order to achieve the closest agreement with the target trilinear parameters extracted from the experimental dataset. The final set of E_i and $F_{u,i}$ values adopted for the final calibrated FE configuration is summarized in Table 6.6.

Table 6.6 Cut-off bar parameters E_i $F_{u,i}$ used in the final calibrated model

RunID	E1 [MPa]	E2 [MPa]	E3 [MPa]	$F_{u,1}$ [N]	$F_{u,2}$ [N]	$F_{u,3}$ [N]	F_{tot} [N]
Final FE calibrated model	4,296	5.37	1,074	6,60	10,00	0,90	17,50

The resulting comparison between the final calibrated FE curve and the experimental reference curves, plotted as force vs global displacement, is presented in Figure 6.8, while Table 6.7 contrasts the corresponding numerical metrics with the targets extracted from the trilinear idealization.

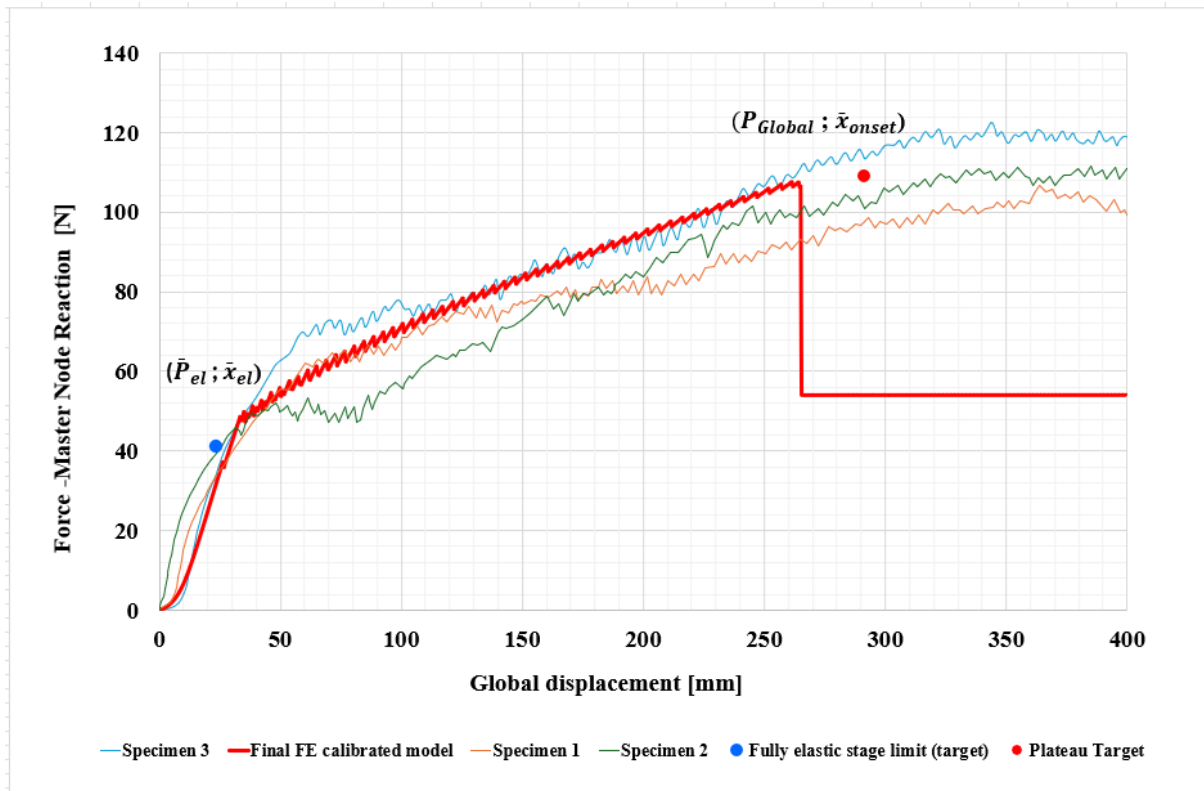


Figure 6.8 Force vs global displacement : Peel-test experimental curves vs FE final calibrated model, showing trilinear targets.

Table 6.7 FE final calibrated model versus trilinear targets -Force vs Global Displacement metrics

Curve	Fully elastic limit			Steady-state plateau stage	
	Global Displacement x [mm]	Force F [N]	kn avg [N/mm]	Global Displacement x [mm]	Force F [N]
Trilinear Target	23,11	41,37	1,79	291,55	108,91
Final Calibrated - FE Model	26,64	37,15	1,39	261,46	107,43
Relative Error [%]	15,27	10,20	22,10	10,32	1,36

Generally, the calibrated model reproduces the main global features of the experimental response: an initial rising branch, a transition toward a quasi-steady

propagation stage, and a stable force level in the plateau range. This agreement is obtained despite the fact that the calibration is performed without direct access to the local interfacial traction–separation law and therefore relies exclusively on matching global observables through the discrete cut-off-bar representation of the interface.

The FE curve exhibits an initially elastic response up to the end of the fully elastic stage, beyond which interfacial damage is assumed to initiate; from this point onward, a characteristic serrated pattern develops, reflecting the progressive advance of the debonding front as successive cut-off bars reach their tensile limits and fail, producing local stiffness drops and corresponding transient reductions in the global reaction force.

Both the experimental and numerical curves exhibit a small curvature at the beginning of the response before the approximately linear trend becomes established. This behavior is consistent with the geometric nonlinearity intrinsic to peel tests: at very small imposed displacements the peeled arm rotates and progressively aligns with the loading direction, and the bending/rotation field near the peel front evolves rapidly. As a consequence, the measured global compliance is not constant in the first increments and the response departs from a perfectly linear trend. The FE model reproduces this effect because it includes large rotations and the corresponding kinematic accommodation of the rubber strip and the grip region. In this sense, the initial curvature should not be interpreted as a deficiency of the interface law alone, but rather as the coupled effect of peel kinematics, the compliance of the test setup and adherends, and the progressive mobilization of the interface.

The fully elastic stage limit extracted from the experimental trilinearization occurs at $x_e = 23.11$ [mm] and $P_e = 41.37$ [N], while the FE model predicts $x_e^{FE} = 26.64$ [mm] and $P_e^{FE} = 37.15$ [N]. This corresponds to relative errors of 15.27 % in the displacement marker and 10.20% in the force marker, while in terms of the initial linear-elastic slope, the average relative error reaches 22.10%. as shown in Table 6.7.

These discrepancies are considered expected for two main reasons.

- x_e is not a directly measured material parameter but an identified point obtained by fitting a simplified trilinear idealization to curves that display oscillations and scatter; therefore, the resulting target value is inherently sensitive to the chosen detection window and regression criterion.
- The trilinear target itself is an average over three specimens, which smooths specimen-to-specimen variability; hence, the comparison should be interpreted as agreement with an averaged reference rather than a strict pointwise match for a single test.

The model underestimates the initial equivalent stiffness $k_{n,avg}$. This is coherent with the slightly lower predicted F_e at a slightly larger x_e , and reflects the fact that the global

stiffness is not controlled by the interface alone. In the present formulation the initial slope results from the combined compliance of:

- rubber bending/stretching under large rotations
- grip idealization and boundary constraints
- the elastic part of the cut-off-bar assembly.

Consequently, a perfect match of the initial global slope would generally require an additional identification of test-machine compliance and/or a more detailed characterization of adherend stiffness and boundary conditions, as discussed in the following sections. Within the adopted simplified framework, the model achieves a good qualitative match of the early response and a quantitatively reasonable agreement with the identified elastic-limit markers.

In the other hand, a key outcome of the calibration is that the model reproduces the plateau force level with high accuracy. The experimental target is $F_{\text{plat}} = P_{\text{global}} = 108.91 [\text{N}]$ while the FE model gives $F_{\text{plat}}^{\text{FE}} = 107.43 [\text{N}]$, corresponding to a relative error of 1.36 %. Since the plateau reflects the propagation resistance during sustained peeling, this result indicates that the chosen cut-off-bar thresholds and their interaction during progressive failure provide an effective representation of the global propagation stage.

The onset of the plateau occurs at $x_p = 291.55 [\text{mm}]$ in the trilinear target and at $x_p^{\text{FE}} = 261.46 [\text{mm}]$ in the FE model (relative error = 10.32 %). Unlike the plateau level, the onset displacement is strongly influenced by the length and evolution of the active zone near the peel front (i.e., the number of interface *stations/portions* simultaneously engaging, damaging, and unloading), as well as by rubber kinematics and geometric nonlinearity. In the present modelling strategy, the active-zone length is not directly measured experimentally and cannot be prescribed a priori from Pianese. et al. data, which are limited to the global force–displacement curve. Instead, it is inferred indirectly from the interaction of the discrete interface, and the nonlinear deformation of the rubber strip. Therefore, an offset in x_p is expected even when the plateau force level is matched accurately.

In other words, the model is calibrated primarily to match the global resistance level in propagation, while the precise displacement coordinate at which the response becomes quasi-steady remains more sensitive to secondary modelling assumptions as front-tracking discreteness, grip idealization, and the absence of an experimentally identified local law.

Overall, the final calibrated FE model represents the closest match achievable within the adopted direct implementation strategy—i.e., using only cut-off bars and calibrating against global response targets, while remaining mechanically consistent with the observed peel kinematics, the progressive nature of interfacial debonding, and a stable numerical solution.

6.5.1. Influence of grip idealization and test-machine compliance on the global peel response

Besides the intrinsic sensitivity of onset of the plateau (x_p) to the evolving active zone and rubber geometric nonlinearity, the global displacement coordinate is also affected by the way the experimental setup is represented numerically. Grip constraints and machine compliance influence the early kinematics of the rubber tab and, consequently, the displacement spent on initial accommodation before the interface starts to damage. For this reason, the effect of the grip idealization is investigated through a dedicated sensitivity check.

According to ISO 36:2020, in an experimental peel test, a finite length of the rubber tab is clamped to the grip of the test machine and thus moves approximately as a rigid body. However, the effective clamping length and associated initial accommodation are not explicitly quantified in the reference experimental description. To evaluate this sensitivity, an alternative FE configuration was introduced by coupling a portion of the rubber-grip region to the prescribed-displacement node through rigid links, as shown in Figure 6.9, enforcing a more rigid motion of the tab in the vicinity of the grip.

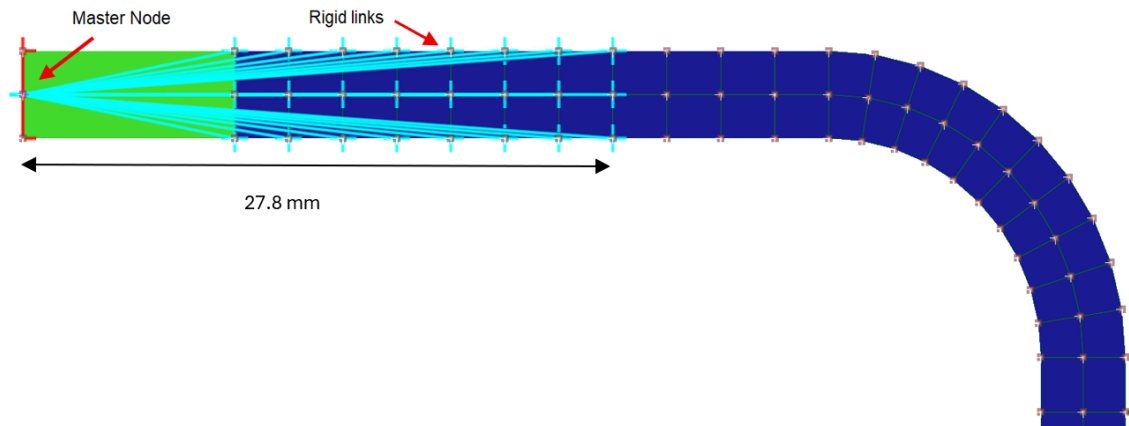


Figure 6.9 FE grip model: rubber nodes within the gripped length kinematically coupled to the master node via rigid links

Specifically, the nodal degrees of freedom of the coupled strip segment are tied to the reference/master node where the prescribed displacement is applied, enforcing an approximately rigid-body motion of that segment. This approach is intended to mimic the fact that, in the physical test, the portion of the strip inside (and immediately adjacent to) the grip does not deform freely, so that the global displacement is not artificially consumed by local reconfiguration of the free strip before the interface becomes effectively loaded. In the configuration adopted, the idealized constrained length is approximately 27.8 mm, which is of the same order as typical clamping lengths used in peel fixtures and is used here as a modelling assumption for sensitivity assessment.

Figure 6.10 and Table 6.8 compare the calibrated FE model with the model including the rigid-link coupling at the grip region. This modification affects the early-stage global response by reducing the initial geometric/compliance accommodation of the rubber ply; consequently, the model reaches the fully elastic-stage limit at a global displacement closer to the trilinear target, reducing the relative error in the corresponding displacement and force.

As reported in Table 6.8, the target values from the trilinear idealization are $x_{el} = 23.11$ [mm] and $P_{el} = 41.37$ [N], while the FE model predicts $x_{el}^{FE} = 23.22$ [mm] and $P_{el}^{FE} = 39.49$ [N], corresponding to relative errors of 0.48% and 4.54%, respectively. The initial stiffness also approaches the target, from $k_{n,avg} = 1.79$ [N/mm] to $k_n^{FE} = 1.70$ [N/mm] existing a relative error of 5.00% between them.

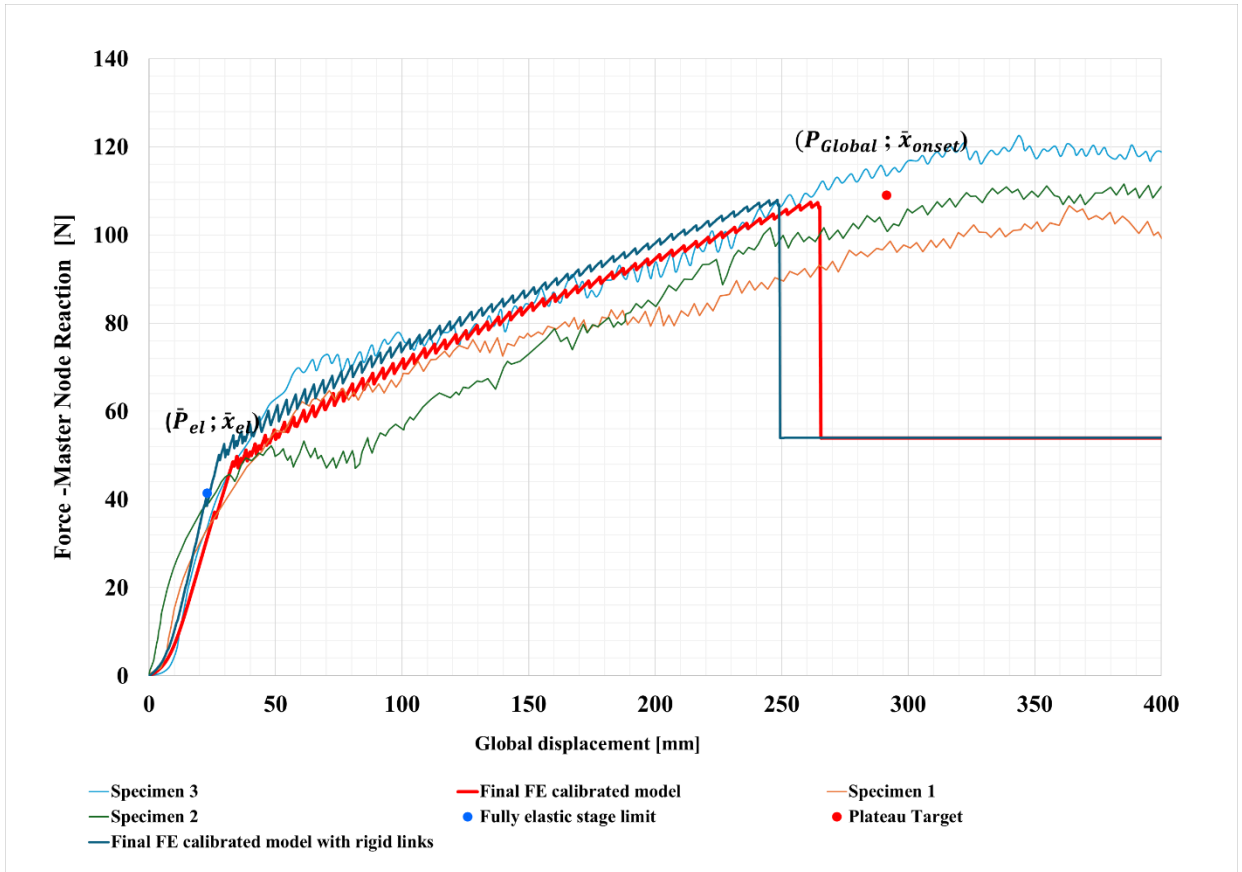


Figure 6.10 Force vs global displacement : Peel-test experimental curves vs FE final calibrated model with and without rigid links in the mobile grip, showing trilinear targets.

Table 6.8 FE final calibrated model with rigid links versus trilinear targets -Force vs Global Displacement metrics

Curve	Fully elastic limit			Steady-state plateau stage	
	Global Displacement x [mm]	Force F [N]	kn avg [N/mm]	Global Displacement x [mm]	Force F [N]
Trilinear Target	23,11	41,37	1,79	291,55	108,91
Final Calibrated - FE Model with rigid links	23.22	39.49	1.70	248.25	107,94
Relative Error [%]	0.48	4.54	5,0	14.85	0.86

Regarding the propagation stage, the plateau force level remains well matched: $P_{\text{Global}} = 108.91$ [N] (target) versus $P_{\text{Global}}^{\text{FE}} = 107.94$ [N] (relative error 0.89%). However, the plateau onset displacement still shows a marked shift, with $x_p = 291.55$ [mm] (target) versus $x_p^{\text{FE}} = 248.25$ [mm] (relative error 14.85%). This behaviour is consistent with the effect of grip idealization on the early specimen kinematics: by reducing the initial compliance of the ply, a larger fraction of the prescribed global displacement is transferred into opening at the peel front from the beginning of the analysis, rather than being absorbed by local deformation. This leads to an earlier onset of interface damage in terms of global displacement. Since debonding propagation in the model is driven by the progressive activation and failure of the discrete interface stations, an earlier damage onset also shifts the subsequent response along the displacement axis: the peel front begins to advance sooner, and therefore both the displacement at which the response approaches a quasi-steady propagation regime (plateau onset) and the displacement at complete separation can occur earlier. In this sense, the grip representation does not primarily change the calibrated plateau force level, but it indirectly affects the displacement coordinates associated with damage initiation and plateau onset. Therefore, some mismatch in the plateau onset displacement between the FE prediction and the idealized target are expected within these modelling and experimental uncertainties.

6.5.2. Interface discretization (Δx) sensitivity

To complete the validation of the Final calibrated model a last verification is conducted to confirm that the selected interface station/portion size (Δx) is adequate for representing the global peel response. The proposed interface representation is discrete by construction, since debonding propagation is captured through the progressive failure of station-wise cut-off bars. For this reason, a mesh-discretization sensitivity check was conducted by reducing the station length from $\Delta x = 2.5$ [mm] to $\Delta x = 1.25$ [mm], which doubles the number of interface portions and, consequently, increases the total number of cut-off-bar elements.

Since the station size was halved ($\Delta x: 2.5 \rightarrow 1.25$ mm), the corresponding scaling factor is $s = \Delta x_{\text{new}}/\Delta x_{\text{old}} = 0.5$. The axial stiffness of each cut-off bar is proportional to EA/L , thus, the same stiffness scaling can be obtained either by scaling the elastic modulus ($E_{\text{new}} = sE_{\text{old}}$) or, equivalently, by scaling the bar cross-sectional area ($A_{\text{new}} = sA_{\text{old}}$) while keeping E unchanged. In this study, the latter approach was adopted (area scaling), and the maximum tensile thresholds were scaled proportionally ($F_{u,\text{new}} = sF_{u,\text{old}}$) so that the force capacity per portion remained consistent when the number of stations doubled, as reported in Table 6.9.

Table 6.9 Interface discretization sensitivity: cut-off-bar parameters for $\Delta x = 2.5$ mm and refined $\Delta x = 1.25$ mm (strengths scaled by $s = 0.5$).

RunID	Δx [mm]	E1 [MPa]	E2 [MPa]	E3 [MPa]	$F_{u,1}$ [N]	$F_{u,2}$ [N]	$F_{u,3}$ [N]
Final FE calibrated model	2.5	4,296	5.37	1,074	6,60	10,00	0,90
Final FE calibrated model with refined interface discretization	1.25	4,296	5.37	1,074	3,30	5,0	0,45

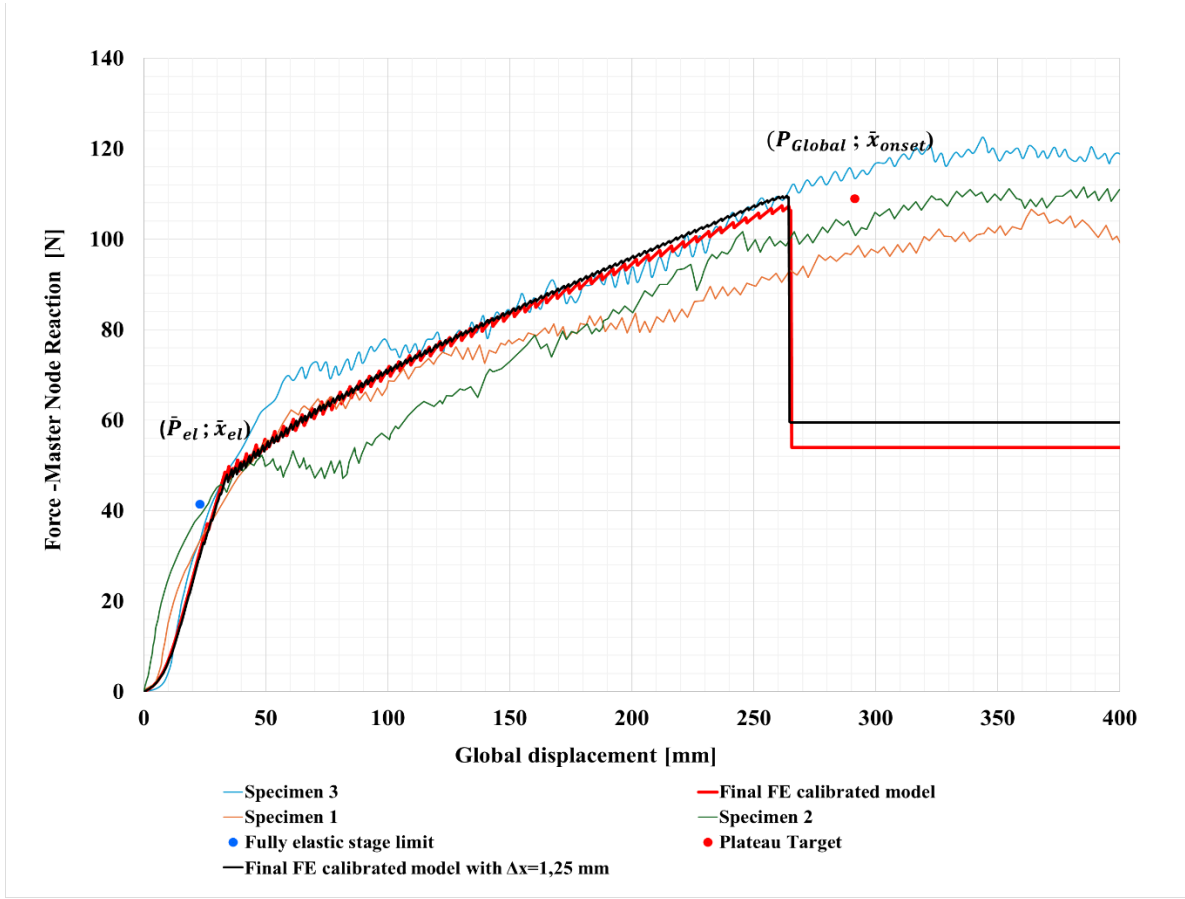


Figure 6.11 Force vs global displacement comparison: FE final calibrated model ($\Delta x = 2.5\text{mm}$) vs FE final calibrated model with $\Delta x = 1.25\text{ mm}$, including peel-test experimental curves and trilinear targets

Figure 6.11 shows that, after re-scaling, the global force–displacement curve remains essentially unchanged in terms of the main calibration targets (global stiffness trend and plateau force level). The most noticeable effect of the refined discretization is the increased density of the serrations: because more interface stations are available to fail progressively, the force drops associated with individual station failures become more frequent and smaller in magnitude, producing a smoother apparent response while preserving the same macroscopic envelope.

The refinement serves as a robustness check supporting that the adopted discretization is sufficiently fine for the purposes of global calibration, while mainly affecting the smoothness of the serrated response, which is relevant when interpreting local indicators (e.g., active zone length) extracted from a discrete station-based representation. Therefore, it can be concluded that the macroscopic response can be made nearly discretization-independent through consistent re-scaling.

6.5.3. Software limitations in interface constitutive modelling

Once the influence of the interface station size Δx is shown to be minor at the macroscopic level when the cut-off-bar parameters are scaled consistently, the discussion can focus on the dominant limitation of the present implementation, which is imposed by the constitutive options available for cut-off bars in the employed software.

In Straus7 software, each cut-off bar can only be defined as either brittle (instant loss of load-carrying capacity after the tensile threshold F_u) or ductile (perfectly plastic response with the axial force capped at F_u). In both cases, the only failure parameter is the maximum tensile force F_u ; no post-peak softening and no energy-based damage evolution (e.g., energy fracture G_c) or a degrading traction–separation law can be prescribed. As a consequence, the model cannot enforce a physically controlled, stable process zone governed by a prescribed dissipation. With the ductile option, once a bar reaches F_u it remains capped at that force, so the global reaction can keep evolving through the progressive engagement of additional interface stations rather than stabilizing. Conversely, with the brittle option, the force drops to zero immediately after F_u , which tends to produce an overly abrupt local unloading and a short transition. This behavior is illustrated by the sensitivity runs reported in Figure 6.12, where $F_{u,1}$ and $F_{u,3}$ are kept constant while $F_{u,2}$ is varied.

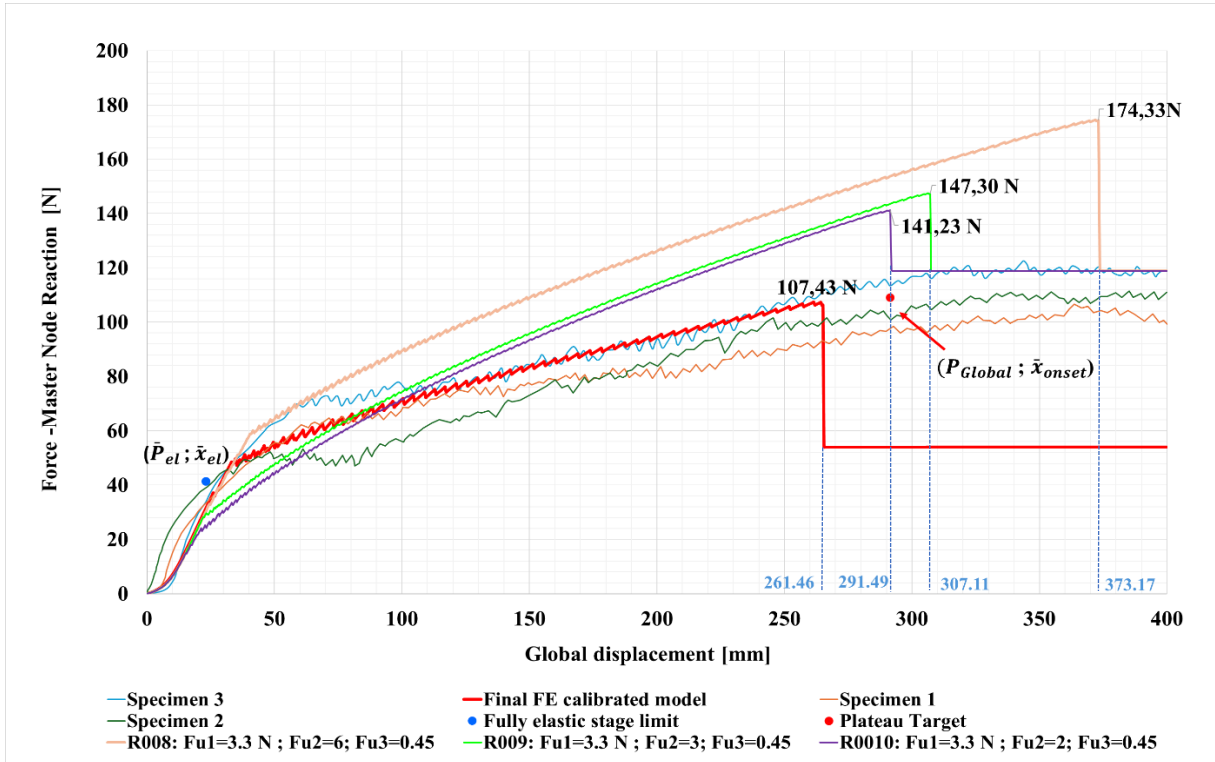


Figure 6.12 Force vs global displacement curves: Comparison of Sensitivity runs (RUN008–RUN010) of the global peel response to the interface cut-off-bar tensile thresholds and on the apparent plateau onset

The curves show that increasing $F_{u,2}$ delays the occurrence of the global peak (i.e., shifts the displacement coordinate at which complete separation is reached), but it may also increase the global force level reached during propagation. This trend is mechanically consistent with the discrete nature of the interface and the ductile cap: a higher $F_{u,2}$ modifies the sequence of local failures within the active zone and redistributes the load between the parallel bars at each station and among neighboring stations, while the ductile elements that have already reached their cap continue to contribute a residual force. Therefore, even when $F_{u,3}$ is unchanged, changes in $F_{u,2}$ can affect the macroscopic envelope and shift the apparent plateau/peak level and its onset.

These results support that within the present direct implementation—where the post-peak response is either a sudden drop (brittle) or a perfectly plastic cap (ductile)—matching the experimental plateau level can be accomplished but reproducing an accurate plateau onset displacement, and a sustained steady-state regime remains intrinsically challenging. A closer reproduction of the experimental quasi-stable propagation would likely require an interface formulation with prescribed post-peak softening and an associated dissipation, which is not directly implementable with the available cut-off-bar constitutive options in Straus 7.

6.5.4. Final discussion

The calibration and sensitivity checks presented in this chapter indicate that the Final FE calibrated model (Figure 6.8) is a robust, global-response-equivalent representation of the peel test within the adopted cut-off-bar framework. In particular, the interface-discretization verification shows that the macroscopic response can be preserved under refinement when the cut-off-bar parameters are re-scaled adequately, while grip idealization primarily affects displacement markers through early-stage accommodation rather than the calibrated propagation resistance.

It should also be noted that the adopted interface idealization transfers load only through normal (Mode I) opening of the station-wise cut-off bars. In a real peel test, the interfacial state can involve a mixed-mode contribution (normal opening combined with shear), and neglecting explicit Mode II transfer may introduce secondary differences in the global response. Nevertheless, the residual mismatch, especially in the plateau-onset displacement, is interpreted primarily as the combined effect of experimental scatter and marker identification from trilinear idealization, geometric nonlinearity and setup compliance, and the lack of a controllable post-peak softening/energy-based evolution in the implemented interface law due to the constitutive options available in the software.

Accordingly, the Final FE calibrated model is retained as the reference configuration for the final comparison and reporting in Chapter 7.

7 Results, conclusions and future developments

7.1. Results

For completeness and consistency with the experimental results presentation, the final calibrated FE response is here compared with Pianese et al. experimental curves in the original reporting format, namely width-normalized peel force F/b versus global displacement, as illustrated in Figure 7.1. Since F/b is obtained by dividing the force by the specimen width $b = 25 \text{ [mm]}$, the relative errors reported for F/b -based metrics, are identical to those obtained in the force-based comparison as is shown in Table 7.1

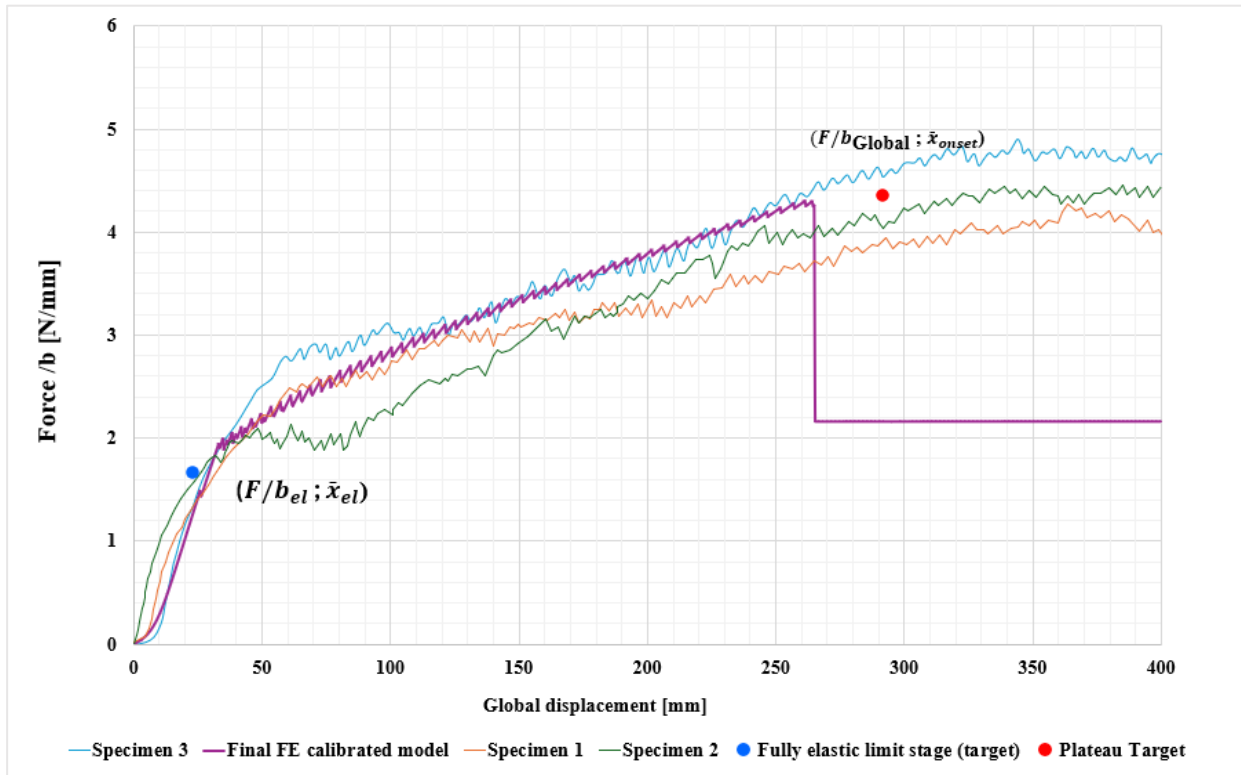


Figure 7.1 F/b vs global displacement : Peel-test experimental curves vs FE final calibrated model, showing trilinear targets.

Table 7.1 FE final calibrated model versus trilinear targets -F/b vs Global Displacement metrics.

Curve	Fully elastic limit			Steady-state plateau stage	
	Global Displacement x [mm]	F/b [N/mm]	kn avg [N/mm ²]	Global Displacement x [mm]	F/b [N/mm]
Trilinear Target	23,11	1,65	0,072	291,55	4,35
Final Calibrated - FE Model	26,64	1,48	0,056	261,46	4,29
Relative Error [%]	15,27	10,20	22,10	10,32	1,36

For the fully elastic-stage limit, the target values are $x_{el} = 23.11$ [mm] and $(F/b)_{el} = 1.65$ [N/mm], whereas the FE model predicts $x_{el}^{FE} = 26.64$ [mm] and $(F/b)_{el}^{FE} = 1.48$ [N/mm]. This corresponds to relative errors of 15.27% in the displacement marker and 10.20% in the force-per-width marker. The initial average slope is therefore underestimated, with $k_{n,avg} = 0.072$ [N/mm²] (target) versus $k_{n,avg}^{FE} = 0.056$ [N/mm²] giving a relative error of 22.10%.

In the propagation regime, the model reproduces the plateau level with high accuracy. The target plateau is $(F/b)_{plat} = 4.35$ [N/mm], while the FE model gives $(F/b)_{plat}^{FE} = 4.29$ [N/mm], corresponding to a relative error of 1.36%. The plateau onset occurs at $x_p = 291.55$ [mm] in the trilinear target and at $x_p^{FE} = 261.46$ [mm] in the FE model (relative error 10.32%). Overall, the results confirm that the final calibrated configuration matches the global propagation resistance level reliably, while larger discrepancies remain in the displacement-based markers associated with the early-stage response and the transition toward steady-state peeling.

7.2. Limitations

Based on the results, the final calibrated configuration should be interpreted as a global-response-equivalent interface model: it provides a set of cut-off-bar parameters that reproduce the observed peel curve at the structural level, but it is not claimed to uniquely identify the true local traction–separation relationship of the adhesive system. The finite element model presented; therefore, provides the closest global-level reproduction of the experimental peel response of the rubber–textile assembly within the adopted modelling framework. The remaining discrepancies and the

interpretation of the identified parameters are primarily governed by the following limitations:

- **Non-uniqueness due to lack of local interface data:** In the absence of an experimentally measured local traction–separation law for the rubber–textile interface, the calibration is performed against global quantities only. Consequently, the inverse identification is intrinsically non-unique: different combinations of $(E_1, E_2, E_3, F_{u,1}, F_{u,2}, F_{u,3})$ may yield similar global curves.
- **Sensitivity to nonlinear peel kinematics and boundary conditions:** Hyperelasticity, large strains, and large rotations of the rubber strip increase sensitivity to boundary conditions, initial peeled-arm geometry, and grip/test compliance. Even when the model remains stable and convergent, these effects can influence the early curvature of the response and the displacement coordinate of transition markers extracted from the global curve.
- **Software constraint on post-peak evolution:** The interface response is limited by the software cut-off-bar formulation (brittle drop or ductile force capping), which prevents prescribing post-peak softening and energy-based damage evolution and can therefore bias displacement-based markers such as the plateau onset.
- **Opening-dominated interface idealization (Mode I):** The adopted discrete interface transfers load primarily through normal opening (Mode I) of station-wise elements; potential mixed-mode coupling (opening + sliding) may therefore be only partially represented through the global kinematics, which can affect the transition stage and displacement-based markers.

7.3. Conclusions

- The final calibrated FE model proposed provides the closest agreement achievable within the adopted modelling framework with the experimental peel dataset and with the calibration objectives derived from the trilinear idealization of the global curves. In particular, it captures the overall transition from the initial rising branch to a quasi-steady propagation regime and reproduces the plateau force with very good accuracy (relative error = 1.36%), confirming that the proposed cutoff-bars (CoB) interface discretization is effective for matching the dominant metric of sustained peeling resistance.

- Residual discrepancies in displacement-based markers (e.g., the end of the fully elastic stage x_e and the plateau onset x_p) between experimental curves and FE model, ranging between 10.20 to 15.27% are expected and consistent with: the scatter of experimental peel curves, the fact that the target points are extracted from a trilinear idealization of noisy signals, the additional contribution of geometric nonlinearity; the test compliance to the early-stage response and the absence of a controllable post-peak softening/energy-based evolution in the interface implementation, due to the limitations of the constitutive options of the used software.
- The implementation of a discrete sawtooth-like degradation, via sequential cut-off bars (CoB) failure, provides a numerically stable way to trigger and propagate debonding under large deformations, while preserving a clear physical interpretation of stiffness drops and force serrations.
- In the proposed interface representation, the macroscopic envelope of the overall curve can be effectively made discretized, provided that the CoB parameters are consistently rescaled to preserve the effective stiffness and strength per unit length of interface.
- Peel test is generally a mixed-mode I +II fracture process, while the present interface formulation intentionally focuses on an opening-dominated representation by using CoBs oriented to transmit traction only in tension and aligned with the opening direction. This simplification was adopted to enable a robust and transparent implementation in Straus7 and to keep the identification problem tractable. Nevertheless, neglecting the Mode II contribution can indirectly affect the timing of damage initiation and plateau onset, and therefore it is a plausible source of residual mismatch in the displacement-based metrics x_e and x_p , even when the plateau level is matched closely.
- Within the constraints of having only global experimental information and a direct FE implementation without local cohesive subroutines, the calibrated model demonstrates that a discrete cut-off bars (CoB) interface can reproduce the global peel response of rubber-textile/fabric assemblies with good fidelity, particularly in terms of the propagation resistance level, while maintaining a clear physical interpretation of the role of stiffness and tensile thresholds in controlling the global curve.

7.4. Future developments

Despite the sensitivity calibration strategy and the implemented finite element model have provided remarkable results, there are still many improvements that can be made.

- Further Finite Element models can be developed to study the interface representation to an explicit mixed-mode formulation, introducing tangential (Mode II) components in addition to the opening (Mode I) response. This may be achieved by coupling normal and tangential connectors (or an equivalent mixed-mode law) so that the model can represent the combined opening/sliding kinematics at the peel front. Such an extension is expected to improve the prediction of displacement-based markers x_e and x_p , which are the quantities where the current Mode I-only idealization shows the largest discrepancies.
- Further experimental tests can be made regarding identification of the local behavior of an interface in peel test for rubber-textile assemblies. High-resolution digital image correlation near the peel front could provide local opening and slip fields, enabling the identification of an effective traction–separation relation and the validation of the numerically implied process-zone evolution. This would transform the present global calibration into a two-level validation framework: local law identification and global response prediction.
- From the numerical standpoint, considering that the finite element model developed in this work can provide axial forces and elements deformations, future analysis can be performed to reconstruct an approximate local interface response by combining the bar forces and the local separation at each interface portion. This reconstruction would be meaningful only if supported by independent local experimental data for comparison, which could provide an important bridge between the current global calibration approach and a more physically grounded local characterization.
- Finally, the calibration procedure could be strengthened by systematic parameter studies addressing identifiability and robustness, e.g., exploring multiple initial guesses and constrained optimization to quantify ranges of $(E_t, F_{u,i})$ that reproduce the target response within tolerance. This would provide confidence intervals for the discrete interface parameters and clarify which parameters are primarily responsible for each stage of the global curve, especially for highly nonlinear rubber peel configurations where geometric effects and interface degradation interact.

Bibliography

- [1] International Organization for Standardization. (2020). ISO 36:2020 Rubber, vulcanized or thermoplastic — Determination of adhesion to textile fabrics. ISO, (n.d.).
- [2] G. Pianese, N. Van Engelen, H. Toopchi-Nezhad, G. Milani, High-damping fiber-reinforced elastomeric seismic isolator in different boundary conditions: An experimental insight, *Eng. Struct.* 300 (2024) 117199. <https://doi.org/10.1016/J.ENGSTRUCT.2023.117199>.
- [3] International Organization for Standardization. (2022). ISO 11339:2022 Adhesives — T-peel test for flexible-to-flexible bonded assemblies. ISO., (n.d.).
- [4] D.A. Dillard, Advances in structural adhesive bonding, *Adv. Struct. Adhes. Bond.* (2010) 1–637. <https://doi.org/10.1533/9781845698058>.
- [5] International Organization for Standardization. (2022). ISO 10365:2022 Adhesives — Designation of main failure patterns. ISO., (n.d.).
- [6] R. Dimitri, P. Cornetti, V. Mantič, M. Trullo, L. De Lorenzis, Mode-I debonding of a double cantilever beam: A comparison between cohesive crack modeling and Finite Fracture Mechanics, *Int. J. Solids Struct.* 124 (2017) 57–72. <https://doi.org/10.1016/j.ijsolstr.2017.06.007>.
- [7] (PDF) MODELLING ADHESION IN PACKAGING MATERIALS Physical Tests and Virtual Tests in Abaqus, (n.d.). https://www.researchgate.net/publication/338479968_MODELLING_ADHESION_IN_PACKAGING_MATERIALS_Physical_Tests_and_Virtual_Tests_in_Abaqus (accessed February 18, 2026).
- [8] T.L. Anderson, FRACTURE MECHANICS: Fundamentals and Applications, Fourth Edition, *Fract. Mech. Fundam. Appl.* Fourth Ed. (2017) 1–661. <https://doi.org/10.1201/9781315370293>.
- [9] N. Pingaro, A.S. Calabrese, G. Milani, C. Poggi, Debonding sawtooth analytical model and FE implementation with in-house experimental validation for SRG-strengthened joints subjected to direct shear, *Compos. Struct.* 319 (2023) 117113. <https://doi.org/10.1016/j.compstruct.2023.117113>.
- [10] Straus7 Pty Ltd, Year. Strand7, Version R3.1.3. Sydney: Straus7 Pty Ltd., (n.d.).
- [11] Dassault Systèmes. (2026). Abaqus CAE (Version 2026) [Computer software].

- Dassault Systèmes SIMULIA., (n.d.).
- [12] D. Hull, T.W. Clyne, *An Introduction to Composite Materials*, An Introd. to Compos. Mater. (1996). <https://doi.org/10.1017/cbo9781139170130>.
 - [13] I. Daniel, O. Ishai, *Basic Concepts, Materials, Processes and Characteristics*, Eng. Mech. Compos. Mater. (2006) 18–42. <https://global.oup.com/academic/product/engineering-mechanics-of-composite-materials-9780195150971> (accessed February 19, 2026).
 - [14] J. Cui, Finite element modeling of adhesive failure with adherend yielding, (2001). <http://www.collectionscanada.ca/obj/s4/f2/dsk3/ftp05/MQ63117.pdf> (accessed February 20, 2026).
 - [15] The MathWorks Inc., “MATLAB version 9.9 (R2021a),” [Online]. Available: <https://www.mathworks.com/>. [Accessed: Jan. 11, 2025]., (n.d.).
 - [16] J.G. Williams, End corrections for orthotropic DCB specimens, *Compos. Sci. Technol.* 35 (1989) 367–376. [https://doi.org/10.1016/0266-3538\(89\)90058-4](https://doi.org/10.1016/0266-3538(89)90058-4).
 - [17] A.J. Kinloch, Y. Wang, J.G. Williams, P. Yayla, The mixed-mode delamination of fibre composite materials, *Compos. Sci. Technol.* 47 (1993) 225–237. [https://doi.org/10.1016/0266-3538\(93\)90031-B](https://doi.org/10.1016/0266-3538(93)90031-B).

A Appendix A - Code

A.1. MATLAB

Trilinear Idealization Algorithm

```
%-----
%                               POLITECNICO DI MILANO
%                               BUILDING AND ARCHITECTURAL ENGINEERING
%-----
% Code developed by: Jara Obregón Lucía Valeria
% E-mail: lucyvaleri_jara@hotmail.es
%-----

clear; clc;
e = 25; % Width of specimen [mm]
% >>> READING INPUT DATA <<<
fname = 'Peel_test_chemosil231.xlsx'; % Dataset .xlsx file
sheet = 1; %
M = readmatrix(fname, 'Sheet', sheet);
getpair = @(A,cols) rmmissing(A(:,cols));
S1 = getpair(M, [1 2]);
S2 = getpair(M, [4 5]);
S4 = getpair(M, [10 11]);
% Ensure x-coordinates are in ascending order
S1 = sortrows(S1,1);
S2 = sortrows(S2,1);
S4 = sortrows(S4,1);
% (Global Displacement [mm]) and (Force per width [N/mm])
x1 = S1(:,1); y1 = S1(:,2);
x2 = S2(:,1); y2 = S2(:,2);
x4 = S4(:,1); y4 = S4(:,2);
% === Prepare data for the loop and average calculation
X_data = {x1, x2, x4};
Y_data = {y1, y2, y4};
Specimen_Labels = {'Specimen 2', 'Specimen 1', 'Specimen 3'};
Max_X_all = max([x1; x2; x4]);
Max_Y_all = max([y1; y2; y4]);
% FIGURE 1
%% === Peel tests experimental results chart (F/b vs Displacement) ===
figure(1);
clf;
set(gcf, 'Color', 'w'); hold on; grid on; box on;
plot(x1, y1, 'Color', [0 0.6 0], 'LineWidth', 2.0, 'DisplayName', 'Specimen 2');
plot(x2, y2, 'Color', [1 1 0], 'LineWidth', 1.0, 'DisplayName', 'Specimen 1');
plot(x4, y4, 'Color', [1 0.5 0], 'LineWidth', 1.0, 'DisplayName', 'Specimen 3');
xlabel('Displacement (x) [mm]', 'FontWeight', 'bold');
ylabel('Force per length [N/mm]', 'FontWeight', 'bold');
title('Experimental Peel Test Results - F/b vs Displacement (x)- Chemosil 231G', 'FontWeight', 'bold');
legend('Location', 'southoutside', 'Orientation', 'horizontal');
```

```

xlim([0 Max_X_all]);
ylim([0 ceil(Max_Y_all*1.05)]);
ax1 = gca;
%% === Figure 2: Total Force vs Displacement ===
Yfuerza1 = y1*e;
Yfuerza2 = y2*e;
Yfuerza4 = y4*e;
figure(2);
clf;
set(gcf, 'Color', 'w'); hold on; grid on; box on;
xlabel('Displacement (x) [mm]', 'FontWeight', 'bold');
ylabel('Force [N]', 'FontWeight', 'bold');
title('Experimental Peel Test Results - Force (F) vs Displacement (x) - Chemosil 231G', 'FontWeight', 'bold');
legend('Location', 'southoutside', 'Orientation', 'horizontal');
plot(x1, Yfuerza1, 'Color', [0.6 0 0.8], 'LineWidth', 1.0, 'DisplayName', 'Specimen 2');
plot(x2, Yfuerza2, 'Color', [1 1 0], 'LineWidth', 1.0, 'DisplayName', 'Specimen 1');
plot(x4, Yfuerza4, 'Color', [1 0.5 0], 'LineWidth', 1.0, 'DisplayName', 'Specimen 3');
Max_F_all = max([Yfuerza1; Yfuerza2; Yfuerza4]);
xlim([0 Max_X_all]);
ylim([0 ceil(Max_F_all*1.05)]);
ax2 = gca;
maxlen = max([numel(x1), numel(x2), numel(x4)]);
pad = @ (v) [v; nan(maxlen-numel(v),1)];
T = table(pad(x1), pad(y1), pad(x2), pad(y2), pad(x4), pad(y4), ...
    'VariableNames', {'x_S1', 'y_S1', 'x_S2', 'y_S2', 'x_S4', 'y_S4'});
%-----
%----- SLOPE CALCULATION IN THE ELASTIC RANGE (LOOP)
%-----
% k_n = Initial slope of the F-Displacement curve (in the linear elastic segment) in [N/mm^2] - represents the initial stiffness of the interface
% minPts = Minimum number of points that any segment must have to be considered in the fitting.
% R2target = Target coefficient of determination (if R2 target is very high, close to 1) it indicates excellent linearity.
% R2fallback = A less strict (R^2) value than R2target, set to 0.98 if no segment satisfies R2target
% ys = Moving mean - computes the smoothed value at each point "i" of ys,
% istar (i*) = The last point that defines the linear elastic zone
% It is the last point of the longest linear segment that meets the selected {R^2} criterion
%-----

%% Estimation of the initial slope k_n (elastic zone) on the F/b vs Displacement curve
minPts = 20; % minimum number of points for fitting
R2target = 0.995; % target R^2 for "elastic" linearity
R2fallback = 0.98; % looser (more permissive) R^2 if the target is not achieved
% Initialize result containers
kn_results = zeros(1, numel(X_data));
R2_results = zeros(1, numel(X_data));
istar_results = zeros(1, numel(X_data));
x_endpoints = zeros(1, numel(X_data));

```

```

% Loop to compute kn, c0, and R2 for each replicate (S1, S2, S4)
for k = 1:numel(X_data)
    x = X_data{k};
    y = Y_data{k};
    % Moving mean - light smoothing (only for linearity detection)
    ys = movmean(y, 5);
    % Incremental fits from the origin
    n = numel(x); % n = total number of points
    R2 = nan(n,1); % Create a vector to store the {R^2} value of each fit
    for i = minPts:n % Create a loop; at each iteration i, a segment from
point 1 to point i is fitted
        xi = x(1:i);
        yi = ys(1:i);
        p = polyfit(xi, yi, 1); % Perform linear fit (degree-1
polynomial)
        yhat = polyval(p, xi); % Fitted line values
        SSres = sum((yi - yhat).^2); % Residual Sum of Squares
        SStot = sum((yi - mean(yi)).^2); % Total Sum of Squares
        R2(i) = 1 - SSres/SStot; % Coefficient of
Determination (R^2)
    end
    % Select the longest prefix with good linearity (iStar)
    idx = find(R2 >= R2target);
    if isempty(idx)
        idx = find(R2 >= R2fallback);
    end
    if isempty(idx)
        iStar = minPts; % no clear segment, use minimum
    else
        iStar = idx(end); % longest segment that satisfies R2
    end
    x_seg = x(1:iStar);
    y_seg = y(1:iStar);
    kn_no_intercept = sum(x_seg .* y_seg) / sum(x_seg.^2);
    % Store results
    kn_results(k) = kn_no_intercept; % Slope kn [N/mm^2]
    R2_results(k) = R2(iStar); % Final R^2
    iStar_results(k) = iStar;
    x_endpoints(k) = x(iStar);
end

% === COMPUTE AND PLOT THE AVERAGE STIFFNESS
kn_mean = mean(kn_results);
x_plot_limit = mean(x_endpoints);
x_fit_mean = linspace(0, x_plot_limit, 50)';
y_fit_mean = kn_mean * x_fit_mean;
% --- Draw the AVERAGE slope on Figure 1 (F/b vs delta) ---
axes(ax1);
plot(x_fit_mean, y_fit_mean, '--', 'LineWidth', 2, 'Color', [0 0 1], ...
'DisplayName', 'Average Elastic Stiffness (k_{n,avg})');
plot(x_plot_limit, y_fit_mean(end), 'o', 'MarkerSize', 5,
'MarkerFaceColor', [0 0 1], ...
'MarkerEdgeColor', 'k', 'DisplayName', sprintf('Average End elastic
zone (x = %.4f mm)', x_plot_limit));
legend('Location','southoutside','Orientation','horizontal');
% --- Draw the AVERAGE slope converted to total force (N) in Figure 2:
Force vs Displacement
axes(ax2);
y_fit_force_mean = y_fit_mean * e;
plot(x_fit_mean, y_fit_force_mean, '--', 'LineWidth', 2, 'Color', [0 0 1],

```

```

...
    'DisplayName', 'Average Elastic Stiffness (k_{n,avg})');
plot(x_plot_limit, y_fit_force_mean(end), 'o', 'MarkerSize', 5,
'MarkerFaceColor', [0 0 1], ...
'MarkerEdgeColor', 'k', 'DisplayName', sprintf('Average End elastic
zone (x = %.4f mm)', x_plot_limit));
legend('Location','southoutside','Orientation','horizontal');
% --- Display individual and average results in the command window ---
fprintf('\n=====\\n');
fprintf('  RESULTADOS DETALLADOS Y PROMEDIO DE RIGIDEZ (kn)\\n');
fprintf('  (Regresión Lineal Forzada a Origen (0,0))\\n');
fprintf('=====\\n');
for k = 1:numel(kn_results)
    fprintf('Réplica %d (%s) - Tramo: 1..%d\\n', k, Specimen_Labels{k},
iStar_results(k));
    fprintf('  kn (R^2-referencia=%.4f) = %.5f N/mm^2\\n', R2_results(k),
kn_results(k));
end
fprintf('-----\\n');
fprintf('kn Promedio (Rigidez de Interfaz)           = %.4f N/mm^2\\n',
kn_mean);
fprintf('Límite Elástico Promedio (Desplazamiento) = %.4f mm\\n',
x_plot_limit);
fprintf('=====\\n');
%-----
%----- CALCULATION OF THE "STABLE" HORIZONTAL ZONE - PLATEAU
wlen      = 11;      % window (number of points) for moving averages (odd)
minDisp0  = 20;      % ignore the first mm (startup transient)
slopeTol  = 0.005;   % slope tolerance [ (N/mm) per mm ]
cvTol     = 0.10;    % coefficient of variation tolerance (10%)
minRun    = 40;      % minimum number of contiguous points to consider a
plateau
nSeries = numel(X_data);
idxPlateau_all = cell(1,nSeries);
xP1_all = nan(1,nSeries);
xP2_all = nan(1,nSeries);
nPts_all = zeros(1,nSeries);
Planteu_i      = nan(1,nSeries); % [N/mm]
PlanteuStd_i   = nan(1,nSeries); % [N/mm]
fprintf('\\n=== Detección de PLATEAU (peel 180°) - POR CURVA ===\\n');
for k = 1:nSeries
    x = X_data{k};
    y = Y_data{k};
    ys = movmean(y, wlen, 'omitnan');
    dy = gradient(ys, x);
    locMean = movmean(y, wlen, 'omitnan');
    locStd  = movstd(y, wlen, 'omitnan');
    locCV   = locStd ./ max(locMean, eps);
    mask = (abs(dy) <= slopeTol) & (locCV <= cvTol) & (x >= minDisp0);
    dmask = diff([false; mask; false]);
    runStarts = find(dmask == 1);
    runEndsIdx = find(dmask == -1) - 1;
    runLengths = runEndsIdx - runStarts + 1;
    ok = find(runLengths >= minRun);
    if isempty(ok)
        slopeTol2 = slopeTol*2;
        cvTol2     = cvTol*1.5;
        mask = (abs(dy) <= slopeTol2) & (locCV <= cvTol2) & (x >=
minDisp0);

```

```

        dmask = diff([false; mask; false]);
        runStarts = find(dmask == 1);
        runEndsIdx = find(dmask == -1) - 1;
        runLengths = runEndsIdx - runStarts + 1;
        ok = find(runLengths >= max(ceil(minRun/2),15));
    end
    if isempty(ok)
        warning('No se detectó un plateau claro en %s. Revisa
parámetros/datos.', Specimen_Labels{k});
        idxPlateau = false(size(x));
        idxPlateau_all{k} = idxPlateau;
        fprintf('%s: [SIN PLATEAU DETECTABLE]\n', Specimen_Labels{k});
        continue;
    end
    [~, imax] = max(runLengths(ok));
    s = runStarts(ok(imax));
    eIdx = runEndsIdx(ok(imax));
    idxPlateau = false(size(x));
    idxPlateau(s:eIdx) = true;
    idxPlateau_all{k} = idxPlateau;
    xP1 = x(find(idxPlateau,1,'first'));
    xP2 = x(find(idxPlateau,1,'last'));
    xP1_all(k) = xP1;
    xP2_all(k) = xP2;
    nPts_all(k) = sum(idxPlateau);
    Planteu_vals = y(idxPlateau);
    Planteu_i(k) = mean(Planteu_vals,'omitnan');
    PlanteuStd_i(k) = std(Planteu_vals,'omitnan');
    fprintf('%s: Planteu Range x = %.3f .. %.3f mm (%d points) | Planteu =
%.4f ± %.4f N/mm\n', ...
        Specimen_Labels{k}, xP1, xP2, nPts_all(k), Planteu_i(k),
        PlanteuStd_i(k));
    end
    % === GLOBAL PLATEAU (SIMPLE AVERAGE OF Planteu_i) ===
    valid = ~isnan(Planteu_i) & ~isnan(xP1_all) & ~isnan(xP2_all);
    Planteu_Global = nan;
    PlanteuStd_Global = nan;
    if ~any(valid)
        fprintf('\n[INFO] No se pudo determinar plateau en ninguna curva con
los criterios actuales.\n');
    else
        Planteu_Global = mean(Planteu_i(valid), 'omitnan'); % N/mm
        PlanteuStd_Global = std(Planteu_i(valid), 'omitnan'); % N/mm
        xPlot1 = mean(xP1_all(valid), 'omitnan'); % average plateau start
        xPlot2 = mean(xP2_all(valid), 'omitnan'); % average plateau end
        xEndDraw = Max_X_all; % end of test (e.g., ~400
mm)
        fprintf('\n=== PLATEU GLOBAL (Average of Planteu_i) ===\n');
        fprintf('Planteu_i válidos: %s\n', mat2str(Planteu_i(valid),4));
        fprintf('Planteu_Global = %.4f N/mm (std entre curvas = %.4f N/mm)\n',
Planteu_Global, PlanteuStd_Global);
        fprintf('Average detected range (solid line): x = %.3f .. %.3f mm\n',
xPlot1, xPlot2);
        fprintf('Nota: Se dibuja una EXTENSIÓN PUNTEADA desde x = %.3f hasta x
= %.1f mm como idealización del modelo.\n', ...
            xPlot2, xEndDraw);
        yLow = max(Planteu_Global - PlanteuStd_Global, 0);
        yHigh = Planteu_Global + PlanteuStd_Global;
    end

```

```

    % ===== ELASTIC END POINTS =====
    xElasticEnd = x_plot_limit;          % average elastic zone end (x)
    yElasticEnd = y_fit_mean(end);      % F/b at the average elastic end (y)
    % --- FIGURE 1 (F/b vs delta) ---
    axes(ax1);
    patch([xPlot1 xPlot2 xPlot2 xPlot1], [yLow yLow yHigh yHigh], [0.85
0.85 1.0], ...
        'FaceAlpha', 0.35, 'EdgeColor', 'none', ...
        'DisplayName', 'Planteu Global ( $\pm 1$  sigma entre curvas)');
    plot([xPlot1 xPlot2], [Planteu_Global Planteu_Global], '-',
'LineWidth', 1.8, 'Color', [0 0 0.8], ...
        'DisplayName', sprintf('Planteu_{Global} = %.3f N/mm (inicio x=%.1f
mm)', Planteu_Global, xPlot1));
    % Red point: plateau start
    plot(xPlot1, Planteu_Global, 'o', 'MarkerSize', 6, ...
        'MarkerFaceColor', [1 0 0], 'MarkerEdgeColor', 'k', ...
        'DisplayName', sprintf('Inicio del Planteu (x = %.1f mm)',
xPlot1));
    % >>> Solid blue line: elastic end -> plateau start
    plot([xElasticEnd xPlot1], [yElasticEnd Planteu_Global], '-', ...
        'Color', [0 0 1], 'LineWidth', 2.0, ...
        'DisplayName', 'Trilineal Model');
    % Dotted extension (idealization) of the plateau up to the end of the
test
    if xEndDraw > xPlot2
        plot([xPlot2 xEndDraw], [Planteu_Global Planteu_Global], ':',
'LineWidth', 2.2, 'Color', [0 0 0.8], ...
            'DisplayName', 'Idealization of stable Planteu');
    end
    legend('Location', 'southoutside', 'Orientation', 'horizontal');
    % --- FIGURE 2 (Total Force vs delta) ---
    axes(ax2);
    PlanteuForce_Global = Planteu_Global * e; % N
    yLow2 = yLow * e;
    yHigh2 = yHigh * e;
    patch([xPlot1 xPlot2 xPlot2 xPlot1], [yLow2 yLow2 yHigh2 yHigh2], [0.85
0.85 1.0], ...
        'FaceAlpha', 0.35, 'EdgeColor', 'none', ...
        'DisplayName', 'Planteu Global ( $\pm 1$  sigma entre curvas)');
    plot([xPlot1 xPlot2], [PlanteuForce_Global PlanteuForce_Global], '-',
'LineWidth', 1.8, 'Color', [0 0 0.8], ...
        'DisplayName', sprintf('Planteu_{Global} = %.1f N (inicio x=%.1f
mm)', PlanteuForce_Global, xPlot1));
    % Red point: plateau start
    plot(xPlot1, PlanteuForce_Global, 'o', 'MarkerSize', 6, ...
        'MarkerFaceColor', [1 0 0], 'MarkerEdgeColor', 'k', ...
        'DisplayName', sprintf('Inicio del Planteu (x = %.1f mm)',
xPlot1));
    yElasticEnd_F = y_fit_force_mean(end);
    plot([xElasticEnd xPlot1], [yElasticEnd_F PlanteuForce_Global], '-',
...
        'Color', [0 0 1], 'LineWidth', 2.0, ...
        'DisplayName', 'Trilineal Model');
    % Dotted extension (idealization) of the plateau up to the end of the
test
    if xEndDraw > xPlot2
        plot([xPlot2 xEndDraw], [PlanteuForce_Global PlanteuForce_Global],
':', 'LineWidth', 2.2, 'Color', [0 0 0.8], ...
            'DisplayName', 'Idealization of stable Planteu');
    end
    legend('Location', 'southoutside', 'Orientation', 'horizontal');
end

```

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