

Interlaminar Shear Degradation and Cohesive Zone Modelling of Thermally Aged CFRP Laminates

Zhizhong Tan

Nanjing University of Science and Technology

Jinsheng Xu

xujinsheng@njjust.edu.cn

Nanjing University of Science and Technology

Qixuan Song

Nanjing University of Science and Technology

Yixue Yin

Nanjing University of Science and Technology

Shuwen Wang

Nanjing University of Science and Technology

Zongtao Guo

Beijing Machinery Equipment Research Institute

Research Article

Keywords: CFRP laminate, Thermal aging, Interlaminar shear strength, Cohesive zone model, Bayesian optimization

Posted Date: September 24th, 2026

DOI: <https://doi.org/10.21203/rs.3.rs-10886775/v1>

License: © ⓘ This work is licensed under a Creative Commons Attribution 4.0 International License.

[Read Full License](#)

Additional Declarations: No competing interests reported.

Interlaminar Shear Degradation and Cohesive Zone Modelling of Thermally Aged CFRP Laminates

Abstract

This study investigates interlaminar shear degradation induced by thermal aging in T700/epoxy CFRP laminates and links the measured response to equivalent cohesive zone parameters. Short beam shear tests, fracture surface characterization, Bayesian parameter inversion and evolution modelling with temperature and time were combined. Laminates were aged at 150, 180 and 200 °C for 24~1000 h. The interface penalty stiffness, tangential nominal strength and mode II critical fracture energy were identified using tree structured Parzen estimator optimization. The interlaminar shear strength increased slightly after short and intermediate exposure at 150 °C, but generally decreased at 180 and 200 °C; the strength retention after aging at 200 °C for 1000 h was 91.4%. Fracture morphology evolved from mixed matrix and interface failure toward delamination dominated by the interface. Sensitivity analysis showed that tangential nominal strength mainly controlled peak load, interface penalty stiffness governed the response before the peak, and mode II fracture energy regulated damage growth after the peak and energy dissipation. Temperature dependent evolution models calibrated with 24~500 h data predicted the 1000 h conditions with a mean absolute percentage error of 1.45% for the equivalent interface parameters and an average normalized root mean square error of 7.01% for the load displacement responses. The framework provides a quantitative route for mapping thermal aging history to interlaminar shear response and equivalent interface degradation in CFRP laminates.

Keywords: CFRP laminate; Thermal aging; Interlaminar shear strength; Cohesive zone model; Bayesian optimization

1 Introduction

Carbon fibre reinforced polymer (CFRP) laminates are widely used in load bearing aerospace, unmanned aircraft, wind energy and automotive structures because of their high specific strength and stiffness, favourable fatigue resistance and substantial lightweighting potential (Soutis 2005; Meng et al. 2015). For airborne equipment and composite load bearing components, mechanical loads such as impact, vibration, bending and shear during transport, carriage, landing and launch can act together with thermal exposure during long term storage and service. Under high temperature or thermo oxidative environments, epoxy matrices may undergo postcuring, oxidation, embrittlement and microcrack accumulation, while the fibre and matrix interface may debond or lose bond strength (Zavatta et al. 2021; Fan et al. 2024; Miao et al. 2023). Such aging damage is often difficult to detect visually, yet it can reduce residual load carrying capacity and promote interlaminar cracking, delamination growth or structural failure when critical loads are applied. Clarifying the degradation of interlaminar properties after thermal aging is therefore important for storage reliability assessment and service life prediction of airborne composite structures (Fan et al. 2024; Miao et al. 2023).

Previous studies have shown that thermal and hygrothermal aging can substantially reduce the interlaminar mechanical properties of CFRP laminates (Zavatta et al. 2021; Fan et al. 2024; Miao et al. 2023; Tan et al. 2026). Zavatta et al. (2021) reported rapid losses in flexural strength and interlaminar shear strength near or above the glass transition temperature, mainly due to epoxy degradation and fibre and matrix interface damage. Fan et al. (2024) developed an ILSS degradation and storage life model for T700/epoxy composites under hygrothermal aging and identified resin plasticization and interface weakening as major causes of degradation. Miao et al. (2023) further showed that interfacial cracks induced by thermo oxidative aging can extend under short beam shear loading and shift the failure mode toward delamination. From a modelling perspective, Yuan et al. (2023) and Shi et al. (2024) demonstrated that cohesive zone parameters strongly affect strength prediction and failure mode simulation. These studies reveal the adverse influence of thermal aging, but the quantitative relationship between ILSS degradation and the evolution of equivalent cohesive zone model (CZM) interface parameters remains insufficiently established.

Short beam shear testing is commonly used to characterize the ILSS of CFRP laminates and to evaluate residual strength after thermal or hygrothermal aging (Fan et al. 2024; Tan et al. 2026; ASTM International 2022; Barile et al. 2022). Conventional experimental studies, however, usually focus on strength retention, peak load changes or fracture morphology comparisons. Although these quantities indicate whether the material has degraded, they do not directly reveal how key interface quantities, including interface stiffness, characteristic displacement and fracture energy, evolve under the coupled effects of aging temperature and exposure time (Yuan et al. 2023; Shi et al. 2024; Camanho et al. 2003; Benzeggagh and Kenane 1996). Therefore, the macroscopic load displacement response obtained from short beam shear testing alone is insufficient for establishing a quantitative connection between ILSS degradation and interface damage mechanisms.

Inverse identification and optimization methods have increasingly been used to calibrate composite interface models (Shi et al. 2024; Wang et al. 2025). By incorporating experimental load displacement curves, peak responses, damage variables or crack growth information into the objective function, optimization algorithms can identify physically reasonable interface parameters from limited test data and reduce the nonuniqueness associated with simple curve fitting (Wang et al. 2025; Bergstra et al. 2011; Akiba et al. 2019; Rasheeda and Sikarwar 2025). Such methods provide a route for mapping macroscopic mechanical response to interface degradation parameters.

In this study, the interlaminar shear behaviour of CFRP laminates aged at different temperatures and exposure times is investigated by short beam shear testing and CZM based finite element parameter inversion. Three optimization strategies are compared to select an efficient approach for the present inverse problem, and sensitivity analysis is used to clarify the influence of the identified parameters on interlaminar load bearing and damage evolution. Temperature dependent evolution models are then established for the interface parameters, and the 1000 h conditions are used to evaluate the retrospective extrapolation capability of the models. This workflow enables a quantitative link between macroscopic shear response and interface property

degradation.

2 Experimental

2.1 Materials and specimen preparation

The CFRP material used in this work was a T700 carbon fibre reinforced epoxy prepreg supplied by Toray, Japan. The prepreg used T700-12K high strength carbon fibres as reinforcement and an epoxy resin system as the matrix. Symmetric $[0^\circ]_{20}$ laminates with 20 plies were prepared, with a fibre volume fraction of approximately $V_f = 60\%$ provided by the material supplier. Specimens and the test environment complied with GB/T 1446-2005 (2005). After layup, the laminates were cured according to the manufacturer recommended cycle: holding at 80°C for 30 min, heating to 150°C at approximately 3°C min^{-1} , and holding at 150°C for 1 h. The cured laminates were cut into standard short beam shear specimens following ASTM D2344/D2344M-22 (ASTM International 2022). The specimen length was about six times the thickness and the width about two times the thickness. The dimensions used here were $17.4\text{ mm} \times 5.8\text{ mm} \times 2.9\text{ mm}$ (length $\times$ width $\times$ thickness), and the loading span was $S = 4h$ approximately equal to 11.6 mm , as shown in Fig. 1.

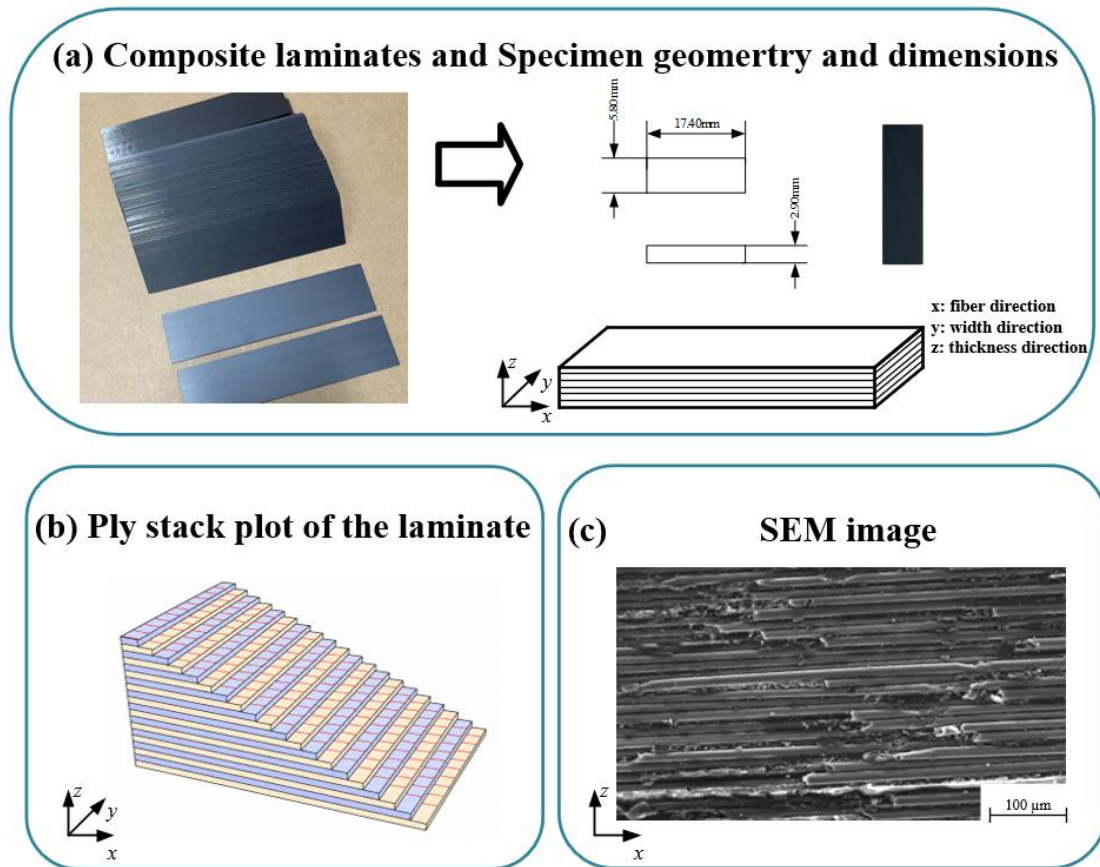


Fig. 1 Specimen information: (a) laminate photograph, specimen dimensions and cutting layout; (b) stacking schematic of the short beam shear specimen; (c) SEM image.

2.2 Accelerated aging and characterization

2.2.1 Aging conditions

The evolution of interlaminar shear properties was examined at three aging temperatures and five exposure times. The aging temperatures were 150, 180 and 200 °C, and the exposure times were 24, 100, 250, 500 and 1000 h, with unaged specimens used as the room temperature (RT) reference. This matrix was selected to capture both possible property retention or slight enhancement under short low temperature exposure (Ma et al. 2015; Wan et al. 2021) and interface degradation under prolonged high temperature exposure. At least five valid specimens were obtained for each condition, and the results were analysed using the mean and standard deviation.

The aging conditions were selected according to ASTM D3045-18 (ASTM International 2018), which specifies isothermal aging in a constant temperature hot air environment to evaluate thermal stability and lifetime, and recommends selecting accelerated temperatures with reference to the glass transition temperature (T_g). Based on commonly used aging temperature ranges for epoxy based CFRP laminates and thermo oxidative studies of high temperature epoxy systems (Jia et al. 2018; Du et al. 2023; Im et al. 2001), three isothermal aging temperatures were adopted: 150, 180 and 200 °C. Aging was conducted in dry air, and specimens were removed at 24, 100, 250, 500 and 1000 h to characterize the time dependent evolution of shear properties. Five valid specimens were tested for each condition to support statistical reliability. The mean and standard deviation were calculated for each group before parameter evaluation.

2.2.2 Short beam shear test

Interlaminar shear properties were measured by short beam shear tests conducted in accordance with ASTM D2344/D2344M-22 (ASTM International 2022). Tests were performed on a QJ-211 electronic universal testing machine using a three point bending fixture, as shown in Fig. 2. Each specimen was placed on the short beam shear fixture, and the loading nose was positioned at mid span. Loading was applied at 1 mm min⁻¹ until the prescribed displacement was reached, and the maximum load P_{max} was recorded. The specimen width to thickness ratio was 2 and the loading span to thickness ratio was 4. The measured force and displacement data were used to calculate the interlaminar shear strength (ILSS) as follows:

$$\tau_{ILSS} = \frac{3P_{max}}{4bh} \quad (1)$$

where F is the maximum load obtained from the load displacement curve (N), b is the specimen width (mm), and h is the specimen thickness (mm). After short beam shear testing, fracture surfaces were sputter coated with gold and examined by scanning electron microscopy (SEM) to characterize damage morphology and aging behaviour.

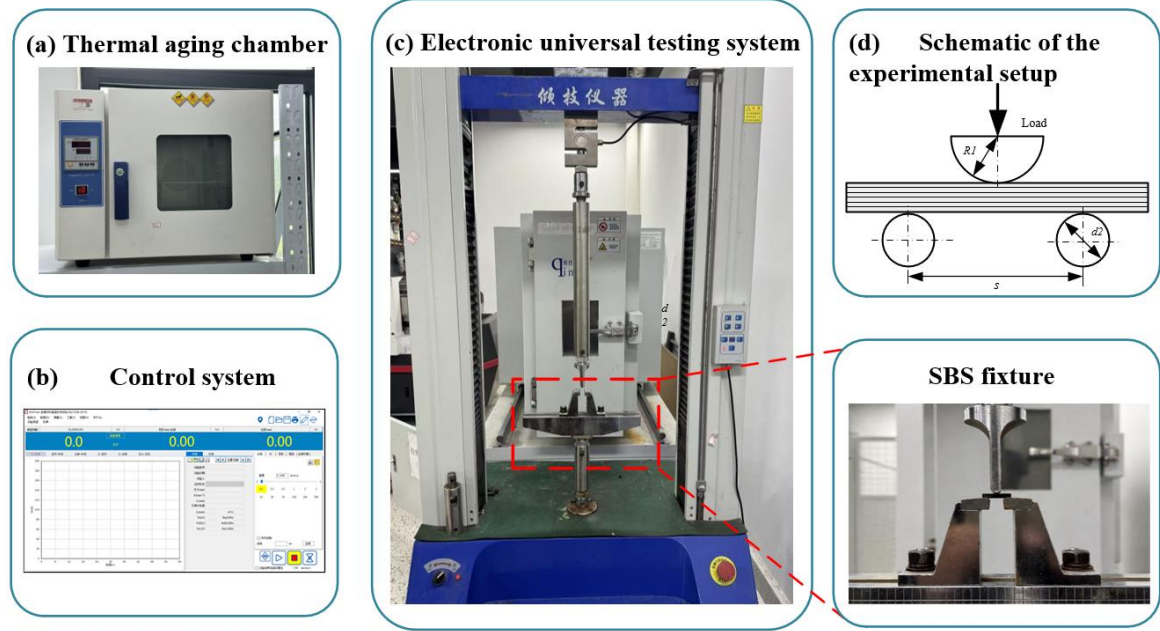


Fig. 2 Short beam shear aging and testing system: (a) aging oven; (b) testing machine control system; (c) electronic universal testing machine; (d) test fixture and short beam shear support.

2.2.3 Damage morphology characterization

SEM was used to characterize failure features induced by thermal aging, including fibre pullout, matrix cracking, matrix degradation and interface debonding (Huang et al. 2025; Jumahat et al. 2010; Paris et al. 2003; Radel et al. 2019; Zhuang et al. 2018). Fracture surfaces were observed at the Experimental Technology Center of the Nanjing Institute of Geology and Palaeontology, Chinese Academy of Sciences, using a Hitachi SU3500 tungsten filament scanning electron microscope. The instrument supports high vacuum and low vacuum imaging, operates over an accelerating voltage range of 0.3 to 30 kV, and is equipped with secondary electron and backscattered electron imaging modes. Fracture morphologies before thermal aging, after thermal aging and after short beam shear loading were compared to identify the thermal damage mechanisms of the CFRP laminates.

3 Finite element modelling and parameter inversion

To investigate the short beam shear response and interlaminar delamination damage evolution of carbon fibre reinforced epoxy laminates under thermal aging, a three dimensional finite element model of the CFRP short beam shear specimen was established. The numerical framework comprised an intralaminar three dimensional orthotropic elastic constitutive model, an interlaminar cohesive zone damage model, and an interface parameter inversion module based on Optuna TPE Bayesian optimization. The intralaminar material model describes anisotropic deformation of each ply during short beam loading, while the interlaminar interface model simulates delamination initiation and growth. Material states under different thermal aging conditions were represented by changes in interface strength, interface fracture energy and selected matrix dominated elastic properties.

3.1 Interface parameter inversion and sensitivity analysis

3.1.1 Bayesian optimization algorithm

Interlaminar cohesive parameters are difficult to measure directly from short beam shear tests. Moreover, interface penalty stiffness, nominal strength and fracture energy are coupled, and different parameter combinations may produce similar macroscopic load displacement responses (Orifici et al. 2008). CZM parameter identification is therefore a nonlinear, nonconvex and potentially nonunique finite element inverse problem (Ganjdoost et al. 2023). Here, interface parameter identification was formulated as a bounded black box optimization problem and solved using the tree structured Parzen estimator (TPE) in the Optuna framework. The method does not require objective function gradients and is suitable for Abaqus/Explicit simulations with high computational cost, nonsmooth responses and occasional solver failures. The inversion workflow is shown in Fig. 3.

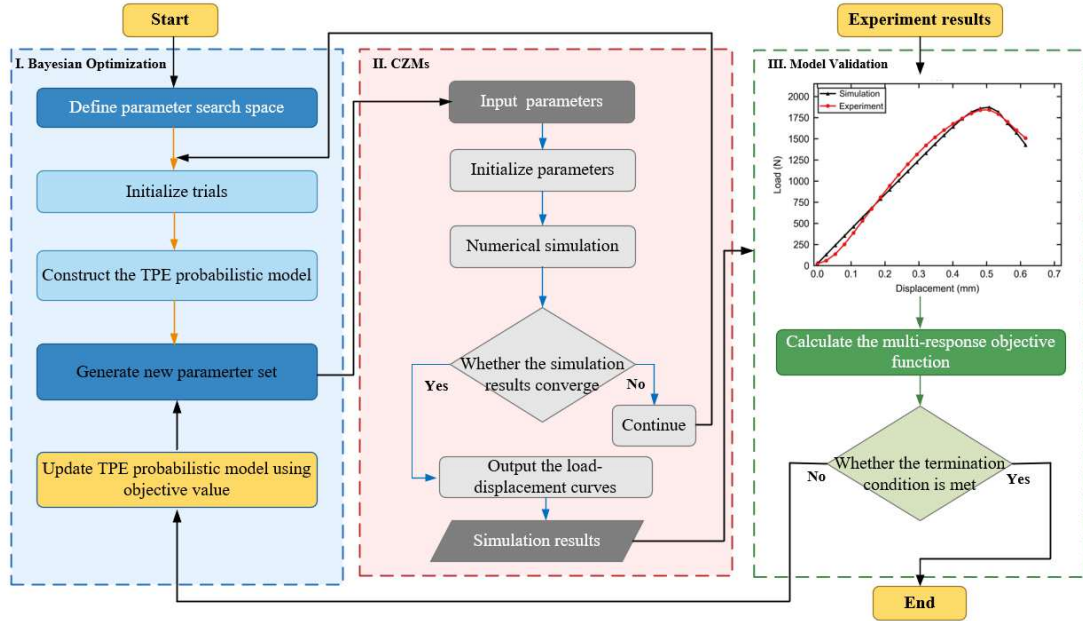


Fig. 3 Bayesian optimization framework for cohesive parameter inversion.

The interface constitutive parameter identification problem was defined as a bounded black box optimization problem. The aim was to find, within the prescribed parameter ranges, an optimal parameter set that minimizes the discrepancy between numerical and experimental responses. The optimization problem can be written as

$$\theta^* = \arg \min_{\theta \in \Omega} J(\theta) \quad (2)$$

where θ is the vector of interface constitutive parameters to be identified, Ω is the parameter search space, and $J(\theta)$ is the objective function constructed from the discrepancy between experimental and numerical responses. Based on the bilinear traction separation model, and because

the short beam shear response is mainly governed by tangential interface behaviour, the interface penalty stiffness K , tangential nominal strength t_s^0 and mode II critical fracture energy G_{IIc} were selected as the unknown parameters to reduce dimensionality and parameter correlation. The parameter vector is defined as

$$\theta = [K, t_s^0, G_{IIc}]^T \quad (3)$$

The corresponding parameter search space is expressed as

$$\Omega = \{\theta \mid \theta_{min} \leq \theta \leq \theta_{max}\} \quad (4)$$

Because the short beam shear response is dominated by tangential interface behaviour, the normal nominal strength t_n^0 , mode I critical fracture energy G_{Ic} and BK exponent η were kept constant during optimization to reduce dimensionality and parameter correlation. For each thermal aging condition, the 19 interlaminar interfaces in the 20-ply laminate were assigned the same set of equivalent cohesive parameters.

The tree structured Parzen estimator in Optuna was used for parameter search. TPE is gradient free and is suitable for highly nonlinear finite element black box problems in which each run is expensive and some parameter combinations may fail to converge. Rather than modelling $p(J|\theta)$ directly, TPE models $p(\theta|J)$ according to Bayesian reasoning. After several initial trials, the evaluated samples are divided into good and poor groups according to their objective function values. If the parameter set and objective value in the i th trial are denoted by (θ_i, J_i) , the split threshold can be written as

$$J^* = Q_\gamma(J_i, i = 1, \dots, n) \quad (5)$$

where Q_γ is the γ quantile of the historical objective function values and n is the number of completed trials. For the present minimization problem, samples with objective values smaller than J^* are treated as good samples, and those with objective values greater than or equal to J^* are treated as poor samples. TPE constructs separate conditional probability density models for these two groups:

$$p(\theta|J) = \begin{cases} l(\theta), & J < J^* \\ g(\theta), & J \geq J^* \end{cases} \quad (6)$$

where $l(\theta)$ denotes the probability density of parameter regions with smaller objective values, and $g(\theta)$ denotes the probability density of regions with larger objective values. New candidate parameters are preferentially sampled from regions with a large $l(\theta)/g(\theta)$ ratio, so that they are more likely to lie in historically favourable regions.

In the TPE framework, candidate selection can be expressed using the expected improvement criterion. For minimization, expected improvement is defined as

$$EI(\theta) = \int_{-\infty}^{J^*} (J^* - J) p(J|\theta) dJ \quad (7)$$

Furthermore, the expected improvement in TPE is related to the ratio of the good and poor sample densities as

$$EI(\theta) \propto \left[\gamma + (1 - \gamma) \frac{g(\theta)}{l(\theta)} \right]^{-1} \quad (8)$$

Accordingly, the candidate parameters for the next finite element calculation can be written as

$$\theta_{n+1} = \arg \max_{\theta \in \Omega} \frac{l(\theta)}{g(\theta)} \quad (9)$$

Equations (5) to (9) describe the candidate selection process used in the TPE search. In this implementation, candidate cohesive parameters were generated within the prescribed ranges, written into the Abaqus input file, and evaluated through the short beam shear finite element model. The resulting force displacement response, energy variables and interface damage variables were extracted to update the objective function and guide the next parameter set. Failed finite element runs were assigned a large penalty value so that numerically unstable regions were avoided.

After the maximum number of finite element evaluations was reached, the parameter set with the minimum objective value was taken as the candidate optimum. To reduce the influence of inverse solution nonuniqueness, the lowest curve error was not used as the only criterion. Instead, the optimization history, experimental and numerical curve agreement, explicit energy ratio, interface damage variable and parameter sensitivity were examined together. Thus, Bayesian TPE was used primarily to improve search efficiency under a limited computational budget, while physical plausibility was assessed through damage patterns and response sensitivity.

3.1.2 Multiple response objective function

The objective function quantifies the discrepancy between experimental and numerical responses and directly affects the reliability of the inverted parameters. Using only peak load or a single load displacement curve error may produce parameter sets with similar macroscopic responses but unrealistic initial stiffness, peak displacement or interface damage evolution. Therefore, a multiple response objective function was constructed by considering the overall curve shape, peak load, peak displacement, initial stiffness and the quasi static nature of the explicit solution.

To compare experimental and numerical curves on a consistent basis, both curves were first corrected for load baseline and displacement zero point. The displacement at which the load first reached 20 N was used as the new zero displacement, and the experimental and numerical curves were mapped onto a common displacement grid by linear interpolation. The curves were truncated at the end of the main load drop stage after the first effective peak to avoid excessive influence from contact oscillations after complete failure.

The overall curve error was quantified by the normalized root mean square error:

$$J_F = \frac{1}{P_{max}^{exp}} \sqrt{\frac{1}{N} \sum_{i=1}^N [F_i^{num} - F_i^{exp}]^2} \quad (10)$$

The peak load error was defined as

$$J_P = \left| \frac{P_{max}^{num} - P_{max}^{exp}}{P_{max}^{exp}} \right| \quad (11)$$

The peak displacement error was defined as

$$J_U = \left| \frac{U_{peak}^{num} - U_{peak}^{exp}}{U_{peak}^{exp}} \right| \quad (12)$$

The initial stiffness was determined by linear fitting between $0.2P_{max}$ and $0.5P_{max}$, and the corresponding error was defined as

$$J_K = \left| \frac{K_0^{num} - K_0^{exp}}{K_0^{exp}} \right| \quad (13)$$

For Abaqus/Explicit analysis, the maximum ratio of kinetic energy to internal energy during loading was used to assess quasi static behaviour:

$$r_E = \max_t \left(\frac{E_{kin}}{E_{int}} \right) \quad (14)$$

When this ratio exceeded the prescribed upper limit, an energy penalty term was introduced:

$$J_E = [\max(0, r_E - r_{lim})]^2 \quad (15)$$

The final objective function was written as

$$J(\theta) = w_F J_F + w_P J_P + w_U J_U + w_K J_K + w_E J_E + \Pi_{fail} \quad (16)$$

where w_F , w_P , w_U , w_K and w_E are the weights of the individual error terms, and Π_{fail} is the penalty term for failed analyses. The weights were determined according to the relative importance of the dimensionless error terms and were kept constant for all thermal aging conditions.

3.1.3 Parameter sensitivity analysis

Parameter sensitivity analysis was used to evaluate the influence of individual cohesive parameters on the short beam shear response and to determine whether the identified parameters had clear response control roles (Fascetti et al. 2021). Taking the identified optimal parameter set θ^* as the baseline, relative perturbations of -20%, -10%, 0, 10% and 20% were applied separately to K , t_s^0 and G_{IIc} , with all other parameters kept constant:

$$\theta_j^{(\alpha)} = \theta_j^* (1 + \alpha), \alpha \in \{-0.2, -0.1, 0, 0.1, 0.2\} \quad (17)$$

The response metrics were the peak load P_{max} , peak displacement U_{peak} , initial stiffness K_0 and curve energy absorption W , where

$$W = \int_0^{U_c} F(U) dU \quad (18)$$

where U_c is the common curve integration endpoint used for all calculated conditions. The normalized sensitivity coefficient of parameter θ_j to response Y was defined as

$$S_j^Y = \frac{\frac{[Y(\theta_j^{(\alpha)}) - Y(\theta_j^*)]}{Y(\theta_j^*)}}{\frac{[\theta_j^{(\alpha)} - \theta_j^*]}{\theta_j^*}} \quad (19)$$

A larger absolute value of S_j^Y indicates greater sensitivity of the response to parameter θ_j , and the sign indicates the direction of the response change when the parameter increases. Comparing the sensitivities of different parameters clarifies the roles of interface penalty stiffness, tangential nominal strength and mode II fracture energy in the elastic stage, peak load carrying capacity and softening after the peak behaviour.

3.2 Thermal aging driven cohesive zone model

3.2.1 Interface model

Before establishing the cohesive zone model driven by thermal aging, the basic mechanical parameters of the CFRP constituents and equivalent laminate were defined. Intralaminar material parameters determine the global stiffness response before damage initiation, whereas interfacial cohesive parameters control interlaminar damage initiation and propagation. To define the intralaminar and interfacial constitutive parameters clearly in the finite element model, the engineering elastic constants, strength parameters and Poisson's ratios of the CFRP laminate are listed in Table 1. The interface parameter evolution dependent on thermal aging relationships were then introduced to construct the aging driven cohesive zone model.

Table 1 Constitutive parameters of the CFRP laminate.

CFRP composite	E1 (GPa)	E2=E3 (GPa)	G12=G13 (GPa)	G23 (GPa)
	134	9.4	5.2	3.24
	ν_{23}	$\nu_{13}=\nu_{12}$	$\rho(\text{g/cm}^3)$	
	0.45	0.3	1.8	

The interlaminar bonding, damage initiation and delamination growth between adjacent CFRP plies were described using surface based cohesive contact (ASTM International 2022; Barile et al. 2022; Orifici et al. 2008). Compared with inserting zero thickness cohesive elements between adjacent plies, surface based cohesive contact does not require an additional interface geometry layer and avoids mesh incompatibility errors between cohesive elements and neighbouring solid elements (Orifici et al. 2008; Fascetti et al. 2021).

The interfacial constitutive response was described by a bilinear traction separation law, as shown in Fig. 4a. The response includes a before damage linear elastic stage, a damage initiation stage and a after initiation linear softening stage. Before damage initiation, the normal and two tangential tractions are related to the corresponding separation displacements by

$$\begin{bmatrix} t_n \\ t_s \\ t_t \end{bmatrix} = \begin{bmatrix} K_n & 0 & 0 \\ 0 & K_s & 0 \\ 0 & 0 & K_t \end{bmatrix} \begin{bmatrix} \delta_n \\ \delta_s \\ \delta_t \end{bmatrix} \quad (20)$$

where t_n , t_s and t_t are the normal and two tangential interface tractions; δ_n , δ_s and δ_t are the corresponding separation displacements; and K_n , K_s and K_t are the normal and tangential penalty stiffnesses. A uniform equivalent interface stiffness assumption was adopted, treating the interlaminar interface as an isotropic penalty stiffness interface:

$$K_n = K_s = K_t = K \quad (21)$$

Damage initiation was evaluated using the quadratic nominal stress criterion when the interface tractions reached the critical state:

$$\left(\frac{\langle t_n \rangle}{t_n^0} \right)^2 + \left(\frac{t_s}{t_s^0} \right)^2 + \left(\frac{t_t}{t_t^0} \right)^2 = 1 \quad (22)$$

where t_n^0 , t_s^0 and t_t^0 are the normal and two tangential nominal strengths, respectively, and $\langle \cdot \rangle$ denotes the Macaulay bracket, defined as

$$\langle t_n \rangle = \frac{t_n + |t_n|}{2} \quad (23)$$

After the quadratic nominal stress damage initiation criterion was satisfied, a scalar damage variable D was introduced to describe progressive interface stiffness degradation. For linear softening, the damage variable is expressed as

$$D = \frac{\delta_m^f (\delta_m^{\max} - \delta_m^0)}{\delta_m^{\max} (\delta_m^f - \delta_m^0)} \quad (24)$$

where δ_m^0 and δ_m^f are the effective separation displacements at damage initiation and complete failure, respectively, and $\delta_m^{\max}$ is the maximum effective separation reached during loading. When Eq. (24) reaches unity, interface damage initiates. After damage initiation, the tractions are given by

$$t = (1 - D)K\delta \quad (25)$$

where $D = 0$ indicates an undamaged interface, $0 < D < 1$ indicates the damage evolution stage, and $D = 1$ denotes complete interface failure. A fracture energy based linear damage evolution law was used; thus, after the nominal strength was reached, the interface traction decreased linearly with increasing separation until the critical separation displacement and complete failure were reached.

Because interlaminar interfaces in short beam shear tests generally experience both normal opening and tangential sliding, the Benzeggagh-Kenane (BK) criterion was used to describe mixed mode fracture behaviour, as shown in Fig. 4b. The mixed mode critical fracture energy is expressed as

$$G^c = G_I^c + (G_{II}^c - G_I^c) \left(\frac{G_s}{G_T} \right)^\eta \quad (26)$$

where G_{Ic} and G_{IIc} are the mode I and mode II critical fracture energies, respectively, and η is the BK mixed mode exponent, taken as $\eta = 1.45$. The two tangential directions were assigned the

same critical fracture energy. For each aging condition, all 19 interlaminar interfaces in the 20-ply CFRP laminate were assigned the same set of equivalent cohesive parameters, without artificially defining strong and weak interfaces. The initial interface parameters are listed in Table 2. Parameter changes between aging conditions were used to describe the effects of temperature and exposure time on equivalent interlaminar properties.

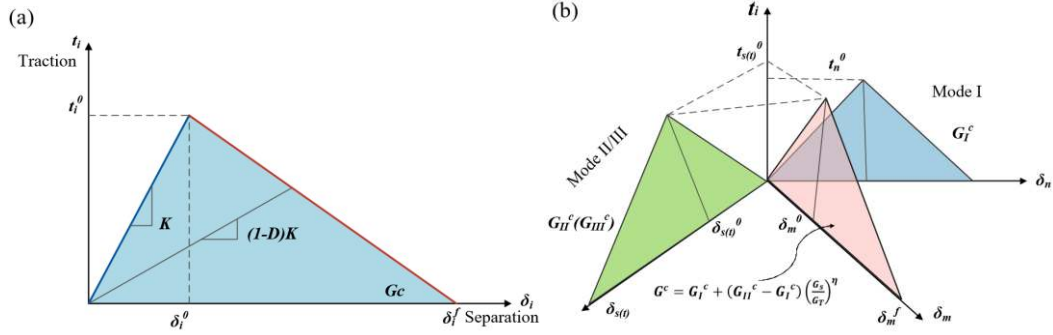


Fig. 4 Interface constitutive model: (a) bilinear traction separation law; (b) schematic of the bilinear mixed mode cohesive zone model.

Table 2 Initial interface model parameters.

Interface	K (N/mm ³)	$t_s^0 = t_i^0$ (MPa)	t_n^0 (MPa)	$G_{IIc} = G_{IIIc}$ (N/mm)
	30000	65	35	2.4
	G_{Ic} (N/mm)			
	0.6			

3.2.2 Interface parameter evolution

The bilinear cohesive zone model described above defines the basic interlaminar response from elastic load transfer to damage initiation and propagation. The interface penalty stiffness K , tangential nominal strength t_s^0 and mode II critical fracture energy G_{IIc} characterize the before damage load transfer capability, initiation of shear damage level and energy dissipation capacity during delamination growth, respectively. In conventional CZM analyses, these parameters are usually treated as constants independent of service history. During thermal aging, however, the resin matrix may undergo postcuring, thermo oxidation and embrittlement, and the load transfer capability and delamination resistance of the fibre and matrix interface may vary with aging temperature and exposure time (Lu et al. 2019). Fixed parameters are therefore insufficient for describing the continuing evolution of interface damage under different thermal aging states.

The key equivalent interface parameters were therefore extended as functions of thermal aging history while retaining the bilinear traction separation law, damage initiation criterion and mixed mode damage evolution form. The Bayesian Optuna inversion method described in Sect. 3.1 first provided discrete equivalent interface parameters for each aging condition:

$$\theta_i = [K_i, t_{s,i}^0, G_{IIc,i}] \quad (27)$$

where subscript i denotes the material state associated with a given aging temperature and exposure time combination. The three key equivalent interface parameters were then expressed as functions of aging temperature T and aging time t . The interface penalty stiffness K mainly characterizes the elastic load transfer capability before damage initiation and affects the stiffness before the peak of the short beam shear load displacement curve. The inverted results show that K at 150 °C first increases and then decreases with aging time, whereas K generally decreases at 180 and 200 °C. Different exponential models were therefore used for different temperatures. For 150 °C, a continuous two stage exponential model was used to describe early enhancement and subsequent degradation:

$$K_{150}(t) = \begin{cases} K_c \exp[k_{K1}(t - t_{c,K})], & 24 \leq t \leq t_{c,K}, \\ K_c \exp[k_{K2}(t - t_{c,K})], & t > t_{c,K}. \end{cases} \quad (28)$$

where K_c is the interface penalty stiffness at the stage transition time t_c , and k_1 and k_2 are the evolution rate coefficients for the early and later stages, respectively. Continuity at $t = t_c$ is imposed, so the two branches give the same K_c at the transition. Based on the parameter evolution within the 24~500 h calibration range, t_c was set to 250 h.

For 180 and 200 °C, the interface penalty stiffness was described by a standard exponential model:

$$K_T(t) = K_{24,T} \exp[-\beta_{K,T}(t - 24)], \quad T = 180, 200 \text{ °C}. \quad (29)$$

where $K_{24}(T)$ is the equivalent interface penalty stiffness at 24 h for temperature T , and β_K is the degradation rate coefficient with units of h^{-1} . The inverted K values can be affected by global structural stiffness, contact conditions, machine compliance and parameter coupling. Therefore, the K evolution model is used mainly to describe the equivalent elastic load transfer trend within the present test range.

The tangential nominal strength t_s^0 mainly controls initiation of shear damage and strongly affects the peak load and near peak response of the short beam shear curve. At 150 °C, t_s^0 increases slightly at the early aging stage and then decreases gradually; at 180 and 200 °C, it shows faster early degradation followed by a gradually decreasing degradation rate. For 150 °C, a continuous two stage exponential model was used:

$$t_{s,150}^0(t) = \begin{cases} t_{s,c}^0 \exp[k_{t1}(t - t_{c,t})], & 24 \leq t \leq t_{c,t} \\ t_{s,c}^0 \exp[k_{t2}(t - t_{c,t})], & t > t_{c,t} \end{cases} \quad (30)$$

where t_c^0 is the tangential nominal strength at the stage transition, t_c is the transition time, and k_1 and k_2 are the evolution rates for the early and later stages, respectively. Based on the calibrated trend, t_c was set to 100 h. For 180 and 200 °C, a stretched exponential model was used:

$$t_{s,T}^0(t) = t_{s,24,T}^0 \exp[-\beta_{t,T}(t - 24)^n], \quad T = 180, 200 \text{ °C} \quad (31)$$

where $t_{24}^0(T)$ is the tangential nominal strength at 24 h for temperature T , β_t is the degradation rate coefficient, and n is the stretched exponent controlling curve shape. When $0 < n < 1$, this model represents rapid early degradation followed by a slower but continuing decrease. Because the

number of calibration time points was limited, n was fixed at 0.2 to reduce coupling between n and β_t and to avoid overparameterization. Under this assumption, β_t has units of h^{-n} .

The mode II critical fracture energy G_{IIc} mainly controls after initiation softening, crack propagation and energy dissipation. For all three temperatures, G_{IIc} decreases with aging time and gradually approaches a long term residual level. Therefore, an exponential model with a residual value constraint was used:

$$G_{IIc,T}(t) = G_{IIc,\infty,T} + (G_{IIc,24,T} - G_{IIc,\infty,T})\exp[-\beta_{G,T}(t - 24)] \quad (32)$$

where $G_{24}(T)$ is the mode II critical fracture energy at 24 h for temperature T , G_{inf} is the residual fracture energy after long term thermal aging, and β_G is the fracture energy degradation rate coefficient with units of h^{-1} . This form ensures that G_{IIc} approaches a finite residual value as aging time increases and avoids nonphysical predictions such as unlimited decay or negative fracture energy.

All temperature dependent parameter evolution models were calibrated using the discrete equivalent interface parameters obtained by Bayesian Optuna inversion. The parameters identified for the 24, 100, 250 and 500 h conditions were used to determine the model coefficients. The 1000 h conditions were not included in the fit and were instead used as retrospective references for extrapolation consistency. Model coefficients were identified by nonlinear least squares, with the objective function

$$\min_{\boldsymbol{\phi}} \sum_{i=1}^N [P_i^{inv} - P_T(t_i; \boldsymbol{\phi})]^2 \quad (33)$$

where p_i is the equivalent interface parameter inverted at the i th time point, p_{model} is the model prediction, $\mathbf{a}$ is the vector of model coefficients to be identified, and N is the number of calibration time points.

The goodness of fit was evaluated using the coefficient of determination R^2 , the root mean square error (RMSE) and the mean absolute percentage error (MAPE). The relative prediction error for the 1000 h condition was defined as

$$e_{1000} = \frac{|P_{T,pred}(1000) - P_{T,inv}(1000)|}{P_{T,inv}(1000)} \times 100\% \quad (34)$$

The parameter evolution relationships proposed here are temperature dependent empirical models limited to the present experimental range of 150~200 °C and 24~1000 h. They are intended to describe the continuous variation of equivalent CZM parameters within the current temperature and time range.

The Bayesian Optuna inversion of equivalent interface parameters under different thermal aging conditions, combined with parameter evolution modelling with temperature and time, provides a continuous representation of cohesive parameters and forms the basis for predicting CFRP interlaminar damage response after thermal aging.

3.2.3 Coupling between aging dependent parameters and the interface model

For a prescribed aging temperature T and aging time t , the evolution models in Sect. 3.2.2 provide $K(T,t)$, $t_s^0(T,t)$ and $G_{IIc}(T,t)$, which are then introduced into the bilinear traction separation law. Under conditions dominated by shear, the interface damage initiation displacement and complete failure displacement can be written as

$$\delta_s^0(T,t) = \frac{t_{s,T}^0(t)}{K_T(t)}, \quad (35)$$

$$\delta_s^f(T,t) = \frac{2G_{IIc,T}(t)}{t_{s,T}^0(t)} \quad (36)$$

Here, K mainly controls the before damage elastic load transfer behaviour, t_s^0 controls initiation of shear damage and near peak load bearing capacity, and G_{IIc} controls after initiation softening, crack propagation and energy dissipation. In the calculations, the basic bilinear CZM form, damage initiation criterion and BK mixed mode criterion were unchanged; only the fixed K , t_s^0 and G_{IIc} were replaced by their values at the corresponding aging temperature and exposure time. The normal nominal strength, mode I critical fracture energy and BK exponent were kept constant for all aging conditions.

3.3 Finite element model and loading conditions

A three point bending finite element model was developed in Abaqus 2024/Explicit to simulate the load response and interlaminar damage evolution of the T700/epoxy CFRP laminates in short beam shear tests (Fig. 5). The specimen was modelled as a 20-ply unidirectional $[0^\circ]_{20}$ laminate, with the fibre direction along the specimen length (X direction). The specimen length, width and total thickness were approximately 17.4, 5.8 and 2.90 mm, respectively, and the ply thickness was 0.1449 mm. Adjacent plies were modelled as independent solid layers, giving 19 interlaminar interfaces. Each CFRP ply was discretized using eight node linear reduced integration solid elements (C3D8R) to describe orthotropic elastic intralaminar response. Unlike approaches that insert zero thickness COH3D8 cohesive elements between plies, this study used surface based cohesive contact to represent interlaminar damage. Short beam loading was applied by an upper loading nose and two lower supports. The upper nose radius was 3.0 mm, the support radius was 1.5 mm, and the support span was 11.6 mm. The loading nose and supports were discretized using C3D8R elements and tied to their reference points by rigid body constraints. The two support reference points were fully fixed, while the loading nose reference point was constrained in lateral translations and rotations and allowed to move only in the vertical loading direction. Hard normal contact was used between the loading/support tools and the laminate outer surfaces, and tangential behaviour was described by penalty friction with a coefficient of 0.3. Hard contact prevents nonphysical penetration between the rigid tools and laminate surface while allowing separation, and it avoids introducing artificial bonding at the tool specimen interface. Penalty friction provides a stable and efficient description of finite sliding resistance during loading (Rasheeda and Sikarwar 2025).

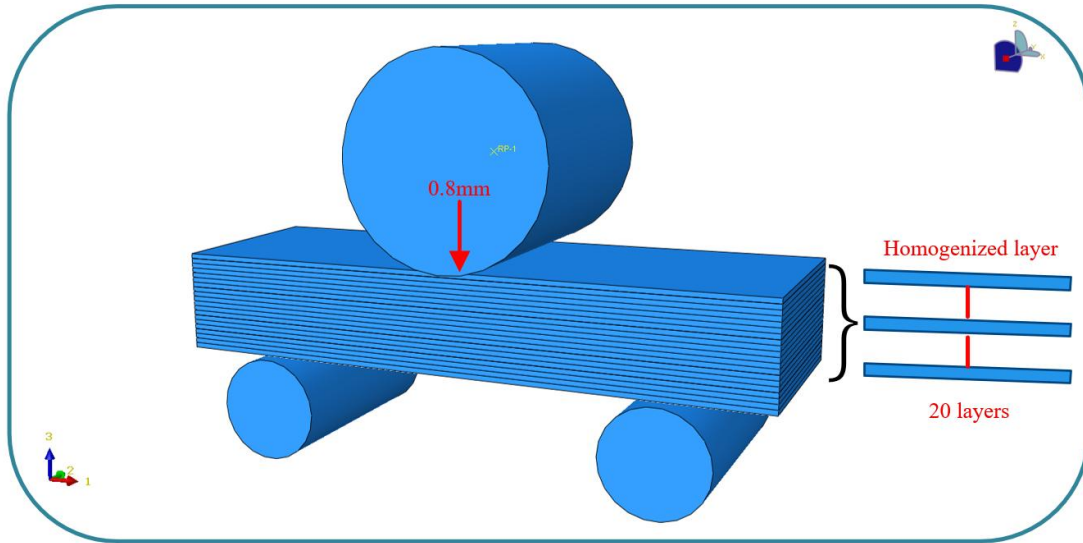


Fig. 5 Finite element model of the short beam shear test.

4 Results and discussion

4.1 Microscopic morphology after thermal aging

SEM was used to examine the fracture surfaces of unaged and representative thermally aged short beam shear specimens to clarify the influence of thermal aging on interlaminar shear performance and interface failure, as shown in Fig. 6. The analysis focused on resin residue on fibre surfaces, fibre and matrix debonding, matrix cracks, fibre pullout and local pores. These features were interpreted together with the load displacement response and ILSS evolution to identify possible degradation mechanisms. All images were obtained using the same secondary electron imaging mode, magnification and scale bar to reduce the influence of imaging conditions. The red dashed boxes indicate resin residue, matrix debris, exposed fibres and interface separation regions extending along the fibre direction.

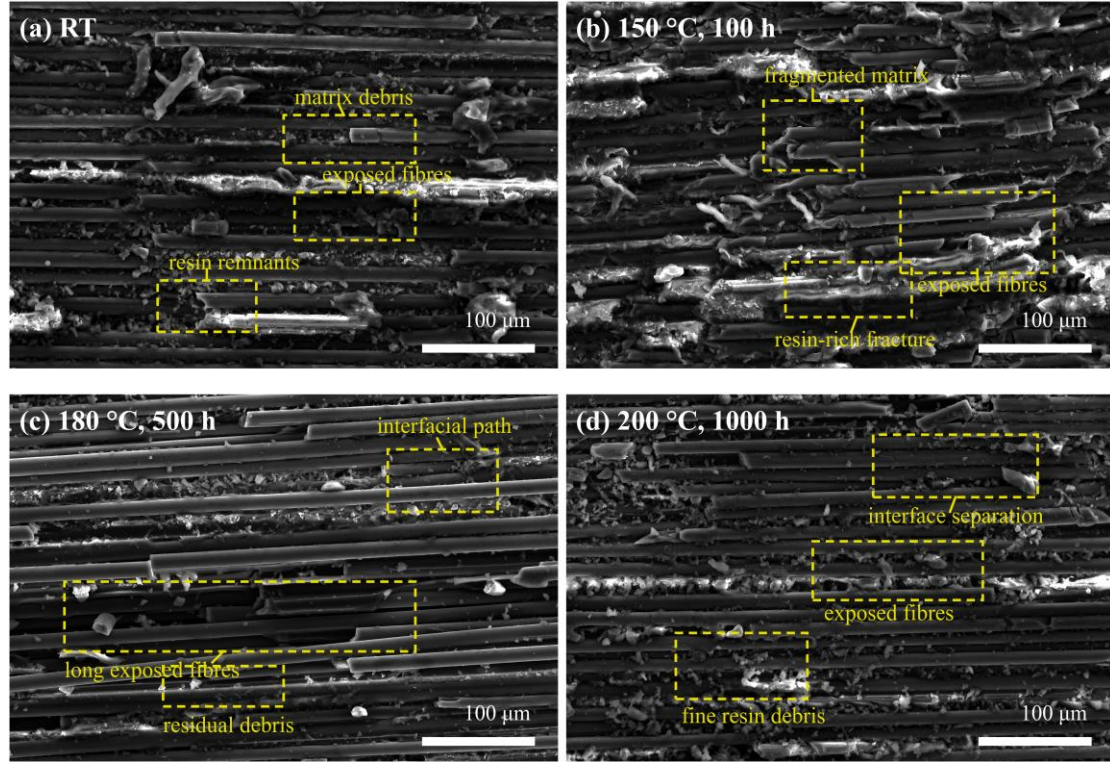


Fig. 6 SEM morphology of delaminated fracture surfaces of short beam shear specimens under four representative aging conditions.

The RT specimen retained considerable resin residue, matrix debris and locally exposed fibres, indicating that cracks did not propagate entirely along the fibre/resin interface. Instead, crack growth alternated between resin rich regions and local interfaces, corresponding to mixed failure controlled by matrix fracture and interface separation. After initial aging (Fig. 6b), resin rich fracture regions and fragmented matrix remained evident, but the amount of locally exposed fibre increased. This indicates that short thermal exposure had not fully transformed the failure mode into failure dominated by the interface, although cracks had begun to deflect more readily near the fibre/resin interface. With increasing aging severity (Fig. 6c,d), continuous exposed fibres, interface separation paths and fine resin debris became more prominent, whereas continuous resin coverage and rough matrix fracture regions decreased. This trend suggests a gradual shift of the crack path from mixed matrix interface propagation toward propagation near the interface.

The fracture surface evolution indicates that thermal aging weakens matrix constraint and fibre/resin load transfer in the interlaminar region. At low aging severity, residual resin and fractured matrix show that the matrix still contributes to crack growth and energy dissipation. After long term high temperature aging, interface debonding and separation along the fibre direction become more pronounced, corresponding to lower interlaminar shear load carrying capacity and reduced energy dissipation after the peak. This morphological evidence is consistent with the ILSS decrease, enhanced softening after the peak in the load displacement curves, and degradation of the inverted equivalent interface strength and fracture energy. Thermal aging therefore changes not only the peak load but also the shear load transfer and damage growth energy dissipation, shifting short beam

shear failure from matrix involved mixed fracture toward delamination dominated by the interface.

4.2 Interlaminar shear behaviour

4.2.1 Load displacement response

Because aging temperature and exposure time strongly influence the degradation of CFRP interlaminar shear performance, short beam shear tests were conducted at three temperatures and five aging times. The load displacement curves provide direct mechanical evidence of interlaminar load bearing, damage initiation and growth after the peak. Figure 7 shows the short beam shear load displacement curves under different temperatures and exposure times. All specimens exhibited three typical stages: initial linear loading, near peak nonlinear damage evolution and softening after the peak. With increasing thermal exposure, the curves changed mainly in peak load capacity, damage initiation position and after the peak residual load capacity, reflecting the coupled influence of thermal aging on damage initiation and propagation.

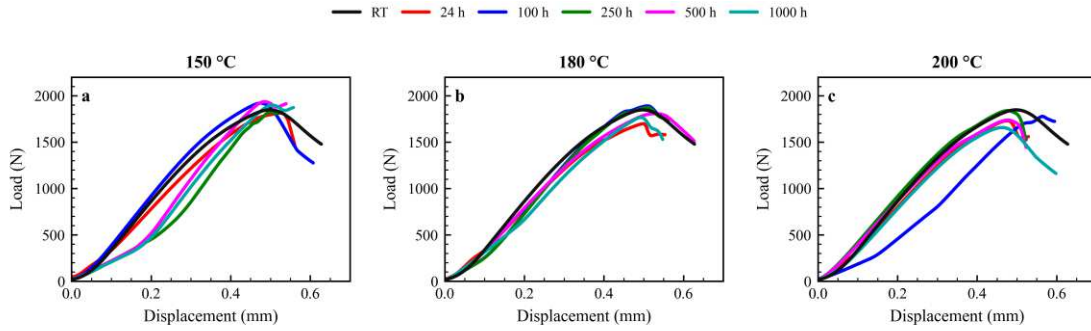


Fig. 7 Load displacement responses from short beam shear tests.

At 150 °C, the aged curves remained close to the RT response, and some short and intermediate time specimens showed slightly higher peak loads, indicating that limited thermal exposure may induce further curing or structural stabilization of the epoxy matrix. However, as exposure time increased, load capacity after the peak gradually decreased, suggesting that long term thermal effects began to dominate interface degradation. When the temperature increased to 180 and 200 °C, the load displacement curves showed more pronounced degradation. The peak load decreased progressively and softening after the peak occurred earlier, indicating weakened fibre/resin load transfer and promoted interlaminar damage growth. The long term 200 °C condition showed the most pronounced after the peak drop and reduction in residual load capacity, confirming accelerated matrix damage and interface degradation under prolonged high temperature exposure.

Overall, thermal aging does not cause a simple linear decrease in interlaminar shear behaviour. The response is controlled by a competition between early structural stabilization and long term thermal damage accumulation. Short term exposure at lower temperature may temporarily maintain or slightly improve interlaminar load bearing capacity, whereas long term high temperature exposure causes interface weakening and enhanced damage growth. These observations provide the mechanical basis for subsequent ILSS degradation analysis and CZM interface parameter evolution

modelling.

4.2.2 Interlaminar shear strength

Different characteristic quantities from the load displacement curves represent different stages of the interlaminar shear failure process. ILSS reflects the peak load carrying capacity, the initial stiffness K_0 represents the structural response before the peak, and the energy absorption before the peak W describes the mechanical energy absorbed before pronounced damage growth. To quantify the nonmonotonic curve evolution observed in Sect. 4.2.1, these metrics were extracted and their retention ratios were calculated using the RT specimens as the reference.

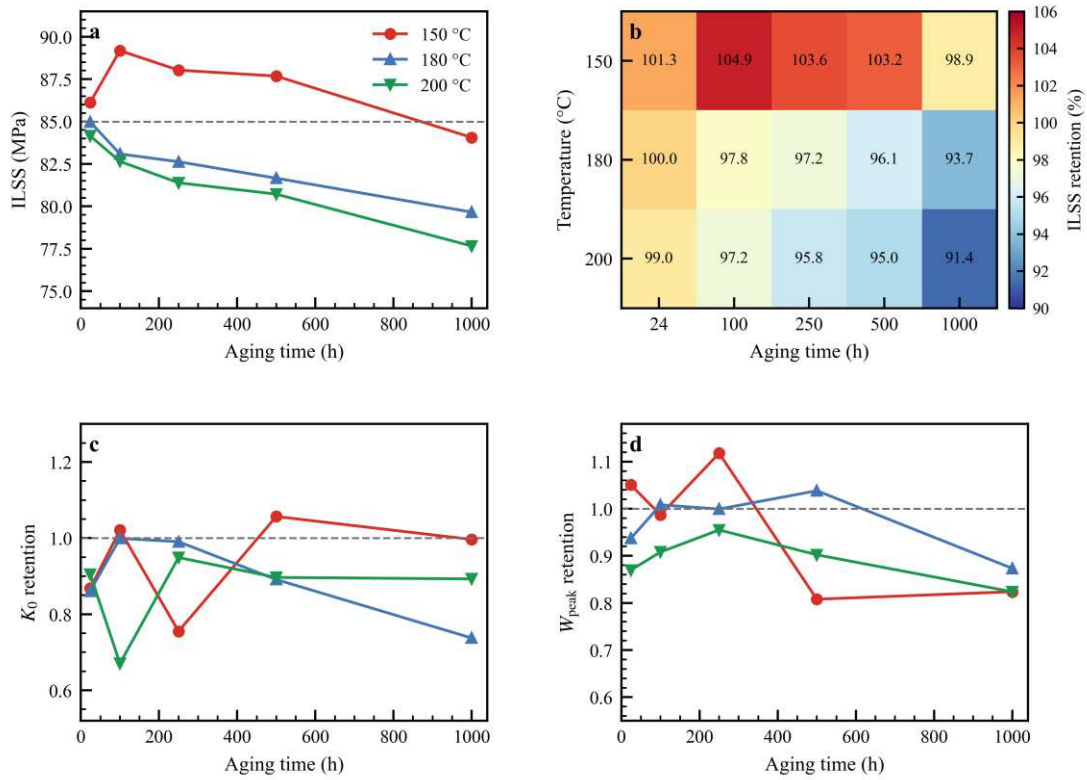


Fig. 8 Evolution of interlaminar shear response metrics of CFRP laminates under thermal aging: (a) ILSS as a function of aging time at different temperatures; (b) temperature and time distribution of ILSS retention; (c) initial stiffness retention $K_0/K_{0,RT}$; (d) before the peak energy absorption retention W/W_{RT} . The grey dashed line denotes the unaged RT reference. $K_0/K_{0,RT}$ and W/W_{RT} were normalized by the corresponding RT values.

To distinguish the influence of thermal aging on peak load carrying capacity, structural response before the peak and energy absorption, ILSS, initial structural stiffness K_0 and energy absorption before the peak W were extracted from the load displacement curves and normalized by their RT values. These three metrics characterize peak strength, approximately linear response before the peak, and combined deformation and damage energy before the peak load, respectively.

ILSS showed clear temperature and time coupling. After short and intermediate aging at 150 °C, the strength retention remained above the RT level and reached 104.9% at 100 h; after 1000 h, it decreased to 98.9%. By contrast, ILSS generally decreased with aging time at 180 and 200 °C, with

1000 h retention values of 93.7% and 91.4%, respectively. These results indicate that limited structural stabilization may occur at lower aging temperature, temporarily maintaining interlaminar load bearing capacity. As temperature and exposure time increase, matrix and interface degradation become dominant and lead to continuing ILSS loss.

The initial stiffness retention $K_0/K_{0,RT}$ did not vary monotonically with ILSS, indicating that the initial response before the peak is affected not only by material stiffness but also by loading nose contact, local indentation and machine compliance. It should therefore not be interpreted directly as interface stiffness degradation. In contrast, the before the peak energy absorption retention W/W_{RT} was more sensitive to long term thermal exposure. For example, at 150 °C for 1000 h, ILSS remained close to the RT level whereas W/W_{RT} decreased to about 0.82, showing that strength retention does not imply retention of before the peak energy absorption capacity. Thermal aging may first appear as a reduction in energy absorption and then develop into clear strength degradation under long term high temperature conditions. Accordingly, evaluating ILSS together with W gives a more complete picture of interlaminar shear degradation than using peak strength or initial stiffness alone.

4.3 CZM parameter inversion, model validation and sensitivity analysis

4.3.1 Interface parameter evolution

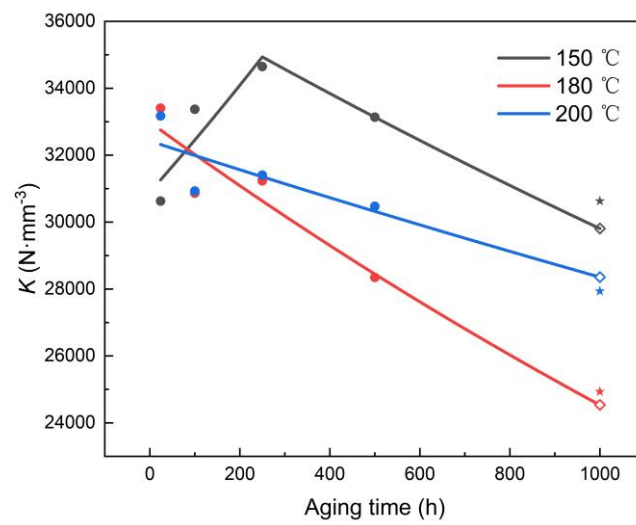
The discrete equivalent interface parameters obtained by Bayesian Optuna inversion were fitted using the temperature dependent evolution models defined in Sect. 3.2.2. The 24, 100, 250 and 500 h data were used to identify model coefficients, while the 1000 h parameters were used as references for retrospective extrapolation consistency. The fitted model parameters are listed in Table 3.

Table 3 Fitted temperature dependent equivalent interface parameter evolution models and 1000 h extrapolation results.

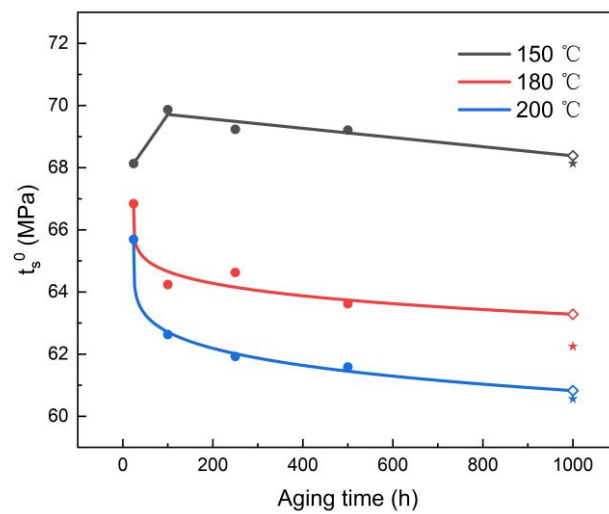
Parameter and temperature	Model form	R ²	RMSE	1000 h prediction	1000 h inversion	Relative error (%)
K at 150 °C	Two stage exponential	0.843	576.92	29808.48	30629.17	2.68
K at 180 °C	Standard exponential:	0.834	731.89	24535.99	24936.63	1.61
K at 200 °C	Standard exponential	0.550	686.82	28357.32	27936.34	1.51
ts0 at 150 °C	Two stage exponential	0.937	0.157	68.383	68.136	0.36
ts0 at 180 °C	Stretched exponential	0.929	0.323	63.284	62.248	1.66

ts0 at 200 °C	Stretched exponential	0.997	0.092	60.826	60.559	0.44
GIIc at 150 °C	Residual exponential	0.877	0.0297	1.8264	1.8052	1.18
GIIc at 180 °C	Residual exponential	0.990	0.0076	1.9098	1.9025	0.38
GIIc at 200 °C	Residual exponential	0.989	0.0206	1.7713	1.7157	3.24

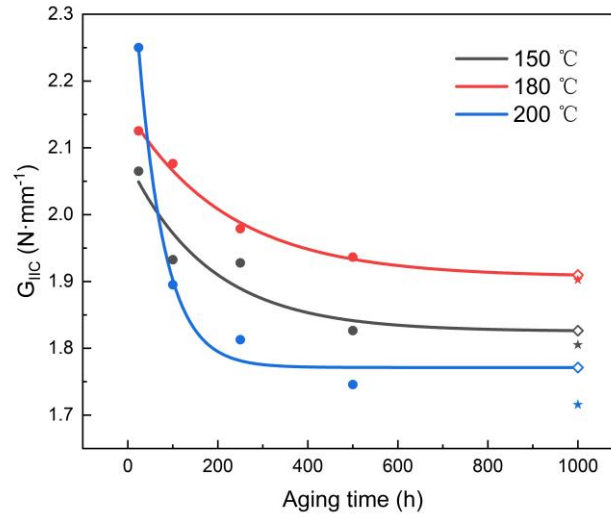
Note: K is in N mm^{-3} , t_s^0 is in MPa, and G_{IIc} is in N mm^{-1} ; RMSE has the same unit as the corresponding parameter.



(a)



(b)



(c)

Fig. 9 Evolution models and 1000 h extrapolation results for cohesive parameters at different aging temperatures: (a) K evolution model; (b) t_s^0 evolution model; (c) G_{IIc} evolution model. Solid circles denote parameters calibrated from 24~500 h data, solid lines denote fitted evolution models, open diamonds denote 1000 h predictions, and stars denote 1000 h inversion references.

Table 3 and Fig. 9 show that the temperature dependent interface models capture the main evolution trends of the three equivalent interface parameters. At 150 °C, K and t_s^0 exhibit an early increase followed by later decrease, reflecting possible residual curing or interface densification during initial low temperature exposure and subsequent thermo oxidative degradation. At 180 and 200 °C, K generally decreases exponentially, while t_s^0 shows faster early degradation followed by slower degradation. At all three temperatures, G_{IIc} decreases with aging time and gradually approaches a residual level, indicating reduced resistance to interlaminar crack growth and lower energy dissipation after the peak.

In terms of fitting stability, the evolution trends of t_s^0 and G_{IIc} are clearer than that of K . The coefficient of determination for K at 200 °C is only 0.550, indicating relatively scattered inverted stiffness values; this model should therefore be used to describe the overall trend rather than local fluctuations. For the 1000 h extrapolation, the average relative errors of K , t_s^0 and G_{IIc} were 1.93%, 0.82% and 1.60%, respectively, and the overall mean absolute percentage error for the nine parameters was about 1.45%. The model can therefore continuously describe the changes in equivalent elastic load transfer, damage initiation level and crack growth resistance caused by thermal aging within 150~200 °C and 24~1000 h, providing a basis for finite element validation using predicted parameters.

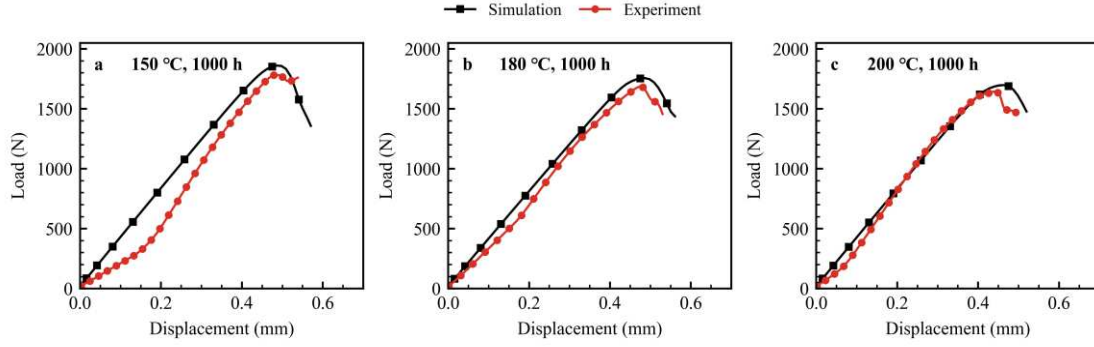


Fig. 10 Validation of extrapolated finite element responses.

The extrapolated finite element responses in Fig. 10 show that the interface parameters predicted by the aging models for 1000 h can reproduce the short beam shear load displacement responses at different temperatures. The average errors in peak load and peak displacement for the three temperatures were 3.94% and 2.57%, respectively, and the average curve NRMSE was 7.01%. The overall curve agreement was higher for the 180 and 200 °C conditions, whereas the deviation for 150 °C was mainly concentrated in the initial before the peak segment and after the peak drop. The aging dependent interface parameter model therefore has reasonable macroscopic response extrapolation capability, but its prediction of local damage growth after the peak and delamination path should be interpreted with caution.

4.3.2 Comparison of inversion results

To compare the computational efficiency of different search strategies for finite element driven interface parameter inversion, Bayesian TPE, random search and Hooke Jeeves were evaluated. The three derivative free methods used the same cohesive parameter search ranges, multiple response objective function, finite element model and maximum computational budget. The horizontal axis was the actual number of finite element model calls. Four representative conditions, RT, 150 °C-100 h, 180 °C-500 h and 200 °C-1000 h, were selected to cover the unaged reference state, low temperature early strengthening, medium/high temperature degradation and long term high temperature degradation. The objective function evolution during inversion is shown in Fig. 11.

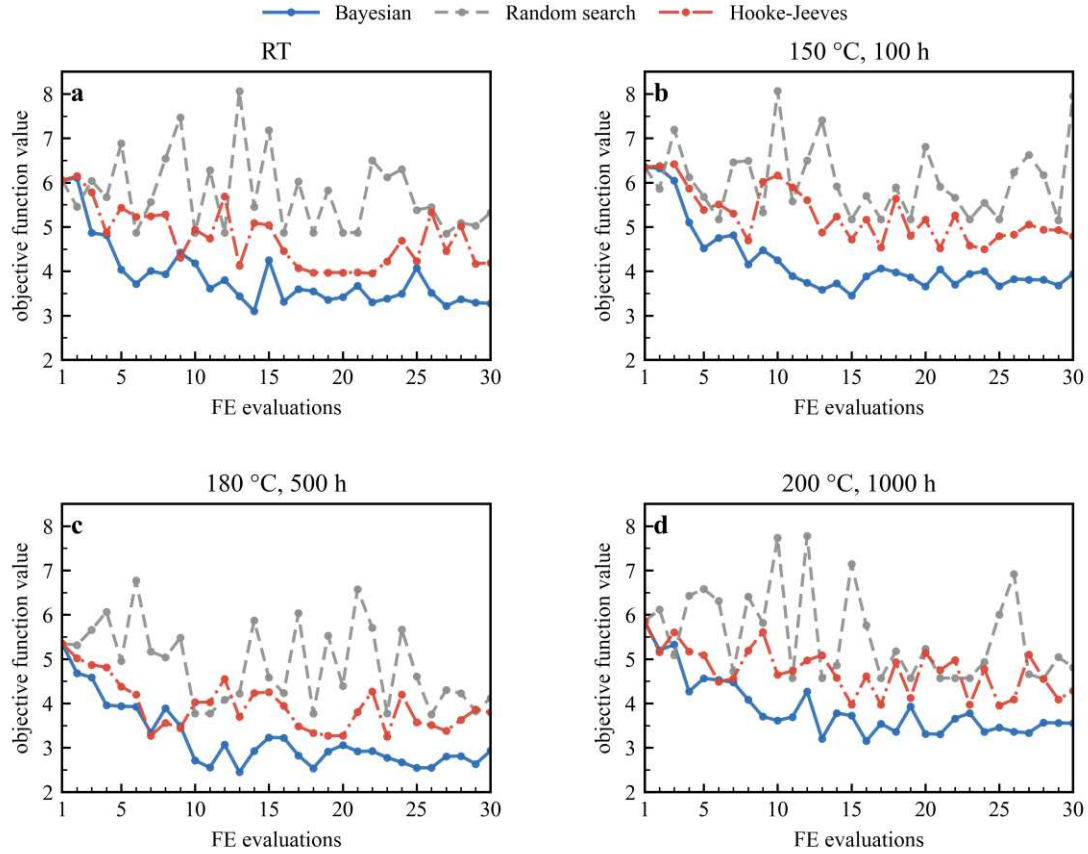


Fig. 11 Objective function evolution of the three optimization algorithms.

Overall, the objective function values obtained by random search fluctuated substantially during the calculation, indicating strong dependence on the random sampling positions. The Hooke Jeeves method gradually reached lower objective function regions, but because it relies mainly on pattern moves and local exploration around the current point, it remained sensitive to the initial parameter position and local objective function landscape. By contrast, Bayesian TPE reached low objective function regions earlier in all four representative conditions and continued to identify low objective candidate parameter sets after approximately 10 to 15 finite element evaluations. This indicates that it can update the parameter sampling distribution using previous trial results and improve the probability of effective search under a limited computational budget.

The relative performance of the three algorithms was consistent across aging conditions. For RT and 150 °C-100 h, Bayesian TPE showed a marked decrease in objective function during the early evaluation stage. For the 180 °C-500 h and 200 °C-1000 h conditions, where interface properties had degraded substantially, Bayesian TPE still identified low objective parameter regions rapidly. Although Hooke Jeeves occasionally obtained values close to those of Bayesian TPE, its overall objective function level and subsequent search stability were weaker. Random search depended on occasional sampling in favourable regions and exhibited the largest fluctuations between evaluations.

Under the parameter ranges, objective function form and finite element evaluation budget used

here, Bayesian TPE showed the best overall search efficiency. Its advantage lies in using historical calculation information to guide subsequent sampling and to obtain parameter combinations with low objective values with fewer finite element calls, rather than relying on uninformative random sampling or local search near a single starting point. Based on this comparison, the equivalent cohesive parameters for all thermal aging conditions were analysed using Bayesian TPE inversion results.

After selecting Bayesian TPE as the main interface parameter inversion method, the identified K , ts_0 and GII_c values were introduced into the short beam shear finite element model and the numerical load displacement responses were compared with the experimental curves. Four representative conditions, RT, 150 °C-100 h, 180 °C-500 h and 200 °C-1000 h, were selected to evaluate the applicability of the inversion framework across different aging stages. These conditions represent the unaged reference, low temperature short and intermediate enhancement, medium/high temperature degradation and long term high temperature degradation observed in the experiments.

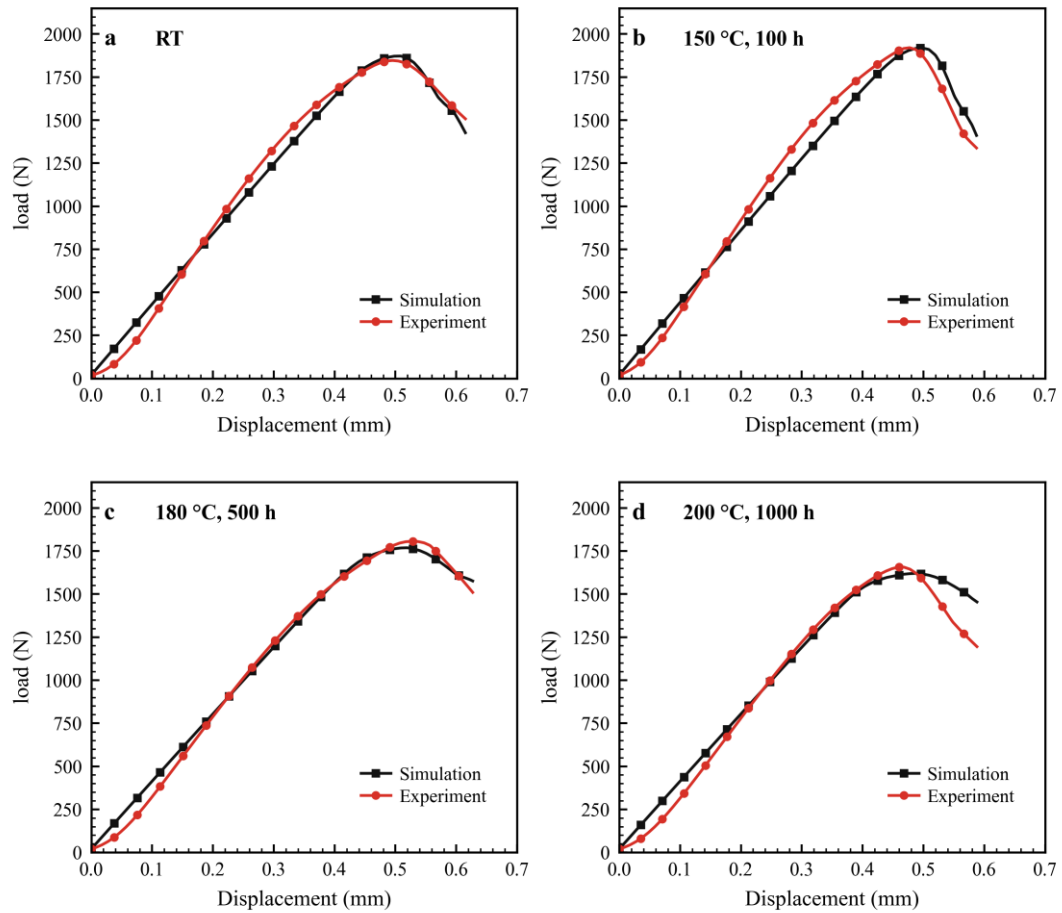


Fig. 12 Experimental and numerical comparison of short beam shear load displacement curves of CFRP laminates under representative aging conditions: (a) RT; (b) 150 °C-100 h; (c) 180 °C-500 h; (d) 200 °C-1000 h. Black curves denote numerical simulations using inverted CZM parameters, and red curves denote short beam shear test results.

Figure 12 shows that the CZM inversion model reproduces the main features of the

experimental load displacement curves, including the initial rising segment, peak load, peak displacement and softening after the peak. For the four representative conditions, peak load errors were within approximately $\pm 2\%$, peak displacement errors were below 5%, and curve NRMSE values were 2.53~4.77%. These results indicate that the inversion framework can match not only the peak load carrying capacity but also the damage induced by aging changes in peak position and overall curve shape. The curve agreement was assessed together with the interface damage variable, explicit energy ratio and parameter sensitivity to avoid nonunique interpretations based only on load displacement curve fitting. Thermal aging states can therefore be converted into equivalent interface parameters for finite element response prediction, rather than being described solely by empirical ILSS retention (Ciampaglia et al. 2024; Nah and Lee 2020).

4.3.3 Interlaminar damage distribution

To further verify whether the inverted equivalent cohesive parameters can describe the spatial distribution of interlaminar damage, the interface cohesive damage variable CSDMG was extracted for the four representative aging conditions, as shown in Fig. 13. All four conditions were plotted using the same viewing angle and a common 0–1 colour scale to enable direct comparison of damage distribution and extension under different thermal aging states.

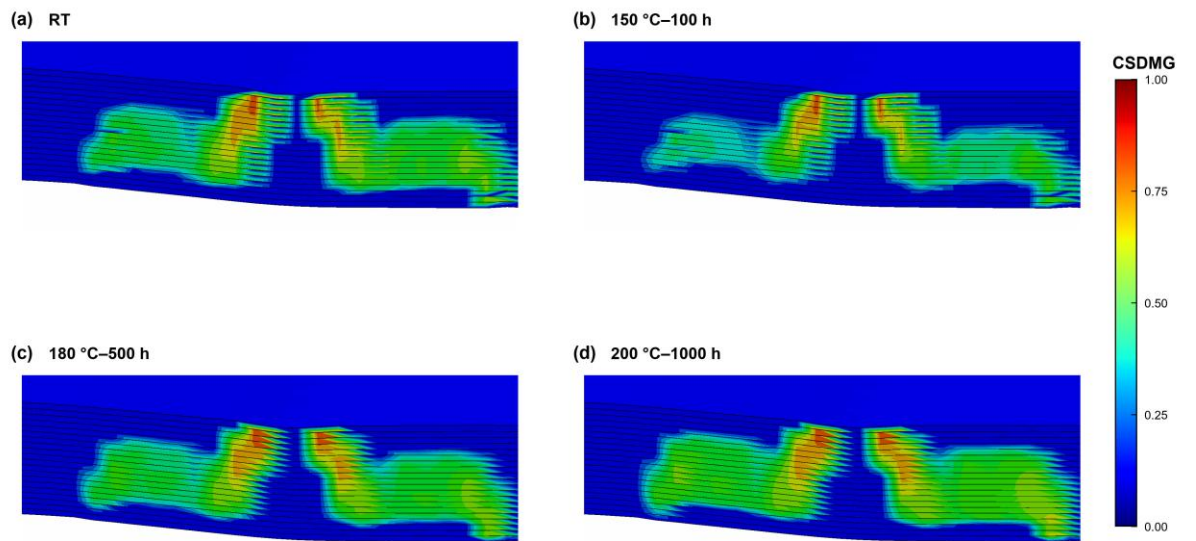


Fig. 13 Interlaminar cohesive damage distribution (CSDMG) under four representative aging conditions: (a) RT; (b) 150 °C-100 h; (c) 180 °C-500 h; (d) 200 °C-1000 h. A common 0–1 CSDMG scale was used for all conditions.

As shown in Fig. 13, high damage regions are mainly concentrated in the mid span region dominated by shear and extend along adjacent interlaminar interfaces. Compared with the RT and 150 °C-100 h conditions, the 180 °C-500 h and 200 °C-1000 h conditions show broader and more continuous CSDMG regions with medium to high damage. This trend is consistent with the

increased continuous exposed fibres and delamination dominated by the interface observed in the fracture surfaces after long term high temperature aging. The inverted equivalent interface parameters therefore reconstruct both the global load displacement response and a spatial damage pattern consistent with the observed failure features. CSDMG is an equivalent interface damage variable obtained from the present short beam shear model and inverted parameters; it should be used as consistency evidence for damage distribution rather than being interpreted as an independently measured delamination length or intrinsic material fracture parameter.

4.3.4 Parameter sensitivity analysis

To evaluate whether the inverted interlaminar cohesive parameters have clear response control roles, single factor perturbation analysis was carried out using the optimal parameter set for each condition as the baseline. The interface penalty stiffness K , tangential nominal strength t_s^0 and mode II critical fracture energy G_{IIc} were perturbed one at a time while the other parameters were kept unchanged. Perturbation levels of $\pm 10\%$ and $\pm 20\%$ were used. The response metrics were peak load P_{max} , peak displacement U_{peak} , initial stiffness K_0 and curve energy absorption W . The results are shown in Fig. 14.

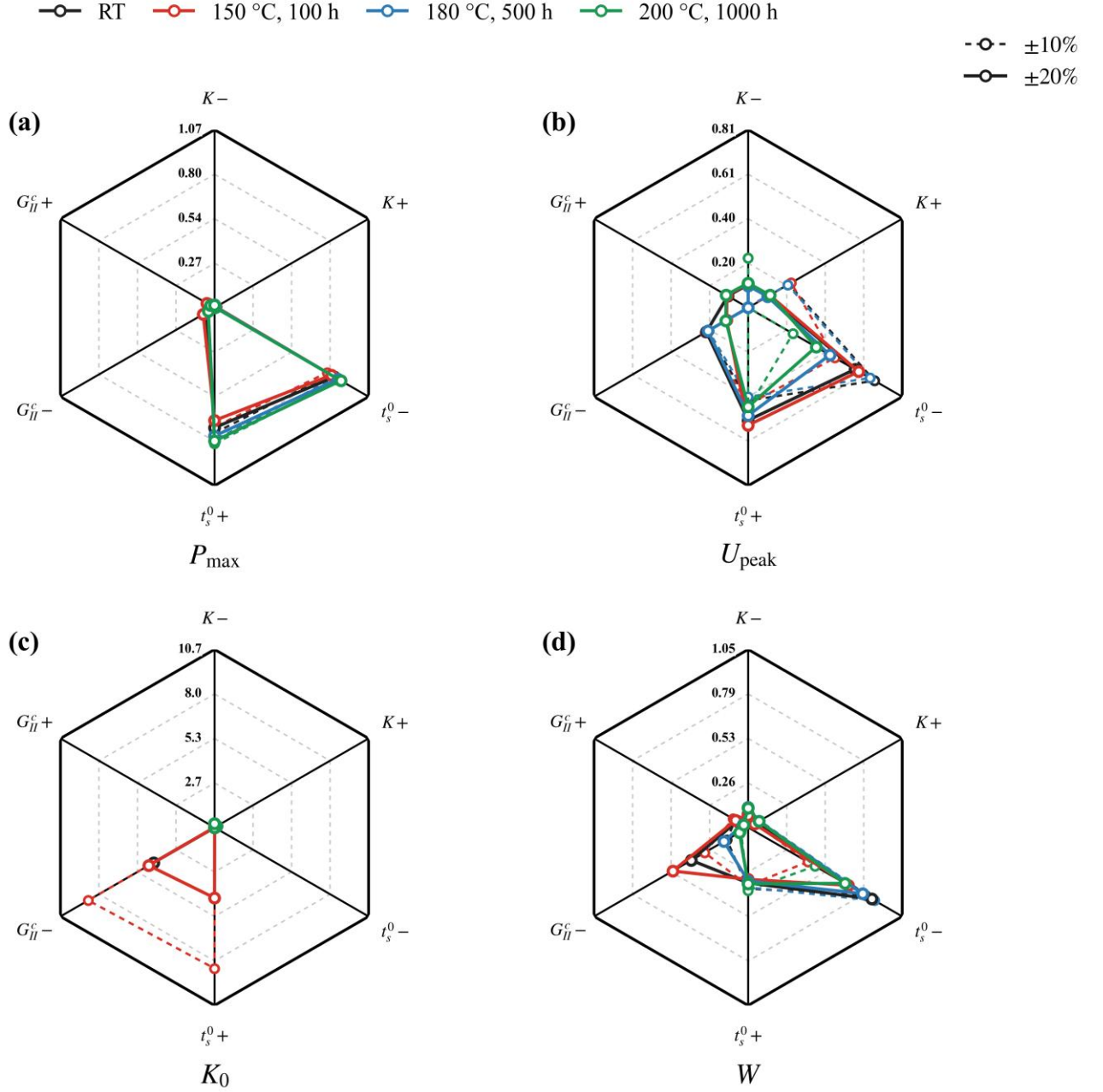


Fig. 14 Sensitivity analysis of cohesive parameters under four representative aging conditions.

The single factor perturbation results for the four representative conditions show a consistent parameter control pattern. Under all conditions, t_s^0 is the dominant parameter controlling $P_{\max}$, U_{peak} and W . Reducing t_s^0 markedly decreases peak load and energy absorption, indicating that the ILSS response is mainly governed by interlaminar initiation of shear damage. In contrast, K has a weaker influence on $P_{\max}$, suggesting that the current interface penalty stiffness is sufficient for interlaminar load transfer and that the peak load is not governed by numerical interface compliance.

The 180 °C-500 h condition illustrates the division of parameter roles most clearly: perturbing t_s^0 changes $P_{\max}$, U_{peak} and W , whereas the influence of K is mainly concentrated in the initial

stiffness K_0 . Thus, ts_0 determines damage initiation and load bearing capacity near the peak, while K mainly reflects before damage elastic load transfer. G_{IIc} has a limited direct influence on peak load but regulates energy dissipation after the peak and absorbed energy, consistent with the physical role of mode II fracture energy in controlling damage propagation rather than initiation. The apparent sensitivity of K_0 to G_{IIc} or ts_0 in some conditions may arise from the initial stiffness fitting window, nonlinear damage before the peak and local contact coupling; it should not be interpreted as fracture energy directly controlling linear elastic stiffness. Overall, the sensitivity analysis supports the physical interpretability of the inverted parameters: thermal damage induced by aging degradation of peak load and energy absorption is mainly described by the evolution of ts_0 and G_{IIc} .

5 Conclusions

This study established an integrated framework combining thermal aging experiments, interlaminar shear characterization, equivalent cohesive parameter inversion and evolution modelling with temperature and time. The framework provides a quantitative connection between thermal aging conditions, macroscopic shear response and interface damage parameters of CFRP laminates. The main conclusions are as follows.

(1) The interlaminar shear properties of CFRP laminates under thermal aging exhibit clear temperature and time coupling rather than a simple monotonic decrease. At lower temperature and short and intermediate exposure times, postcuring or structural stabilization may temporarily maintain or slightly increase ILSS. As temperature and exposure time increase, thermo oxidative damage becomes dominant and degrades both interlaminar load carrying capacity and energy absorption before the peak. Energy before the peak absorption decreases earlier than ILSS under some conditions, indicating that peak strength alone may underestimate early thermal aging damage.

(2) The macroscopic mechanical response and fracture morphology reveal mutually consistent damage evolution. In unaged and mildly aged specimens, resin residue, matrix fragmentation and local interface debonding coexist. After long term high temperature aging, exposed fibres and interface separation along the fibre direction increase, whereas continuous resin coverage and matrix fracture regions decrease. Thermal aging therefore reduces peak load and shifts crack propagation from mixed failure involving the matrix toward delamination dominated by the interface.

(3) Multiple response inversion, CSDMG damage field comparison and sensitivity analysis jointly verify the mechanical roles of the three equivalent cohesive parameters. The tangential nominal strength mainly controls initiation of shear damage and peak load, the interface penalty stiffness characterizes before damage elastic load transfer, and the mode II critical fracture energy controls softening after the peak, delamination growth and energy dissipation. CSDMG contours for the four representative conditions show broader and more continuous regions with medium to high damage after long term high temperature aging, consistent with the delamination dominated by the interface observed from fracture morphology. Under the unified parameter ranges, objective function and finite element budget used in this study, Bayesian TPE entered regions with low

objective values early in representative runs and produced parameter sets consistent with the experimental load displacement responses. This supports its suitability as the inversion tool for the present study, but does not constitute proof of general statistical superiority over other algorithms.

(4) The temperature dependent parameter evolution models continuously describe changes induced by thermal aging in equivalent interface load transfer capability, damage initiation level and crack growth resistance. In retrospective extrapolation using the 1000 h conditions, which were not included in model fitting, the overall mean absolute percentage error of the nine equivalent interface parameters was 1.45%, and the average normalized root mean square error of the corresponding load displacement curves was 7.01%. These results indicate that, within the present experimental range, the proposed model can map thermal aging conditions to equivalent interface parameters and then to macroscopic interlaminar shear response.

References

- Akiba T, Sano S, Yanase T, Ohta T, Koyama M (2019) Optuna: a next-generation hyperparameter optimization framework. In: Proceedings of the 25th ACM SIGKDD International Conference on Knowledge Discovery & Data Mining, pp 2623-2631. <https://doi.org/10.1145/3292500.3330701>
- ASTM International (2018) ASTM D3045-18: Standard practice for heat aging of plastics without load. ASTM International, West Conshohocken
- ASTM International (2022) ASTM D2344/D2344M-22: Standard test method for short-beam strength of polymer matrix composite materials and their laminates. ASTM International, West Conshohocken. https://doi.org/10.1520/D2344_D2344M-22
- Barile C, Casavola C, Pappaletta G, Kannan VP, Renna G (2022) Investigation of interlaminar shear properties of CFRP composites at elevated temperatures using the Lempel-Ziv complexity of acoustic emission signals. *Materials* 15:4252. <https://doi.org/10.3390/ma15124252>
- Benzeggagh ML, Kenane M (1996) Measurement of mixed mode delamination fracture toughness of unidirectional glass/epoxy composites with mixed mode bending apparatus. *Compos Sci Technol* 56:439-449. [https://doi.org/10.1016/0266-3538\(96\)00005-X](https://doi.org/10.1016/0266-3538(96)00005-X)
- Bergstra J, Bardenet R, Bengio Y, Kegl B (2011) Algorithms for hyper-parameter optimization. *Adv Neural Inf Process Syst* 24:2546-2554
- Camanho PP, Davila CG, de Moura MF (2003) Numerical simulation of mixed mode progressive delamination in composite materials. *J Compos Mater* 37:1415-1438. <https://doi.org/10.1177/0021998303034505>
- Ciampaglia A, Fiumarella D, Boursier Niutta C, Ciardiello R, Belingardi G (2024)

Physics based data driven method for the crashworthiness design of origami composite tubes. *Int J Mech Mater Des* 20:445-462. <https://doi.org/10.1007/s10999-023-09685-2>

Dai C, Yu P, Long J (2025) Study on multilevel optimization strategy of carbon fiber-reinforced polymer seatback and seat pan. *Int J Mech Mater Des* 21:209-228. <https://doi.org/10.1007/s10999-024-09734-4>

Du Y, Xue Y, Liu F, Zhang J, Cheng X, Chen S (2023) Effect of hygrothermal aging on moisture diffusion and tensile behavior of CFRP composite laminates. *Chin J Aeronaut* 36:382-392. <https://doi.org/10.1016/j.cja.2022.11.022>

Fan J, Zhang Q, Chen X, He Y (2024) Interlaminar shear strength change and storage life prediction of carbon fiber/epoxy composites with hygrothermal accelerated aging. *Polymers* 16:1109. <https://doi.org/10.3390/polym16081109>

Fascetti A, Feo L, Abbaszadeh H (2021) A critical review of numerical methods for the simulation of pultruded fiber-reinforced structural elements. *Compos Struct* 273:114284. <https://doi.org/10.1016/j.compstruct.2021.114284>

Ganjdoust F, Kefal A, Tessler A (2023) A novel delamination damage detection strategy based on inverse finite element method for structural health monitoring of composite structures. *Mech Syst Signal Process* 192:110202. <https://doi.org/10.1016/j.ymssp.2023.110202>

GB/T 1446-2005 (2005) Fiber-reinforced plastics composites - The generals for determination of properties. Standards Press of China, Beijing

Huang WJ, Li YT, Zhang YM, Xiao ZM, Li WG (2025) Experimental and numerical investigations of interlaminar shear behaviors of CFRP composites at cryogenic and high temperatures. *Compos Struct* 352:118681. <https://doi.org/10.1016/j.compstruct.2024.118681>

Im KH, Cha CS, Kim SK, Yang IY (2001) Effects of temperature on impact damages in CFRP composite laminates. *Compos Part B Eng* 32:669-682. [https://doi.org/10.1016/S1359-8368\(01\)00046-4](https://doi.org/10.1016/S1359-8368(01)00046-4)

Jia Z, Li T, Chiang FP, Wang L (2018) An experimental investigation of the temperature effect on the mechanics of carbon fiber reinforced polymer composites. *Compos Sci Technol* 154:53-63. <https://doi.org/10.1016/j.compscitech.2017.11.015>

Jumahat A, Soutis C, Jones FR, Hodzic A (2010) Fracture mechanisms and failure analysis of carbon fibre/toughened epoxy composites subjected to compressive loading. *Compos Struct* 92:295-305. <https://doi.org/10.1016/j.compstruct.2009.08.010>

Lu X, Ridha M, Chen BY, Tan VBC, Tay TE (2019) On cohesive element parameters and delamination modelling. *Eng Fract Mech* 206:278-296. <https://doi.org/10.1016/j.engfracmech.2018.12.009>

Ma R, Chang X, Zhang X, Fang P, Long B, Liu W (2015) Effect of curing method on mechanical and morphological properties of carbon fiber epoxy composites for solid rocket motor. *Polym Compos* 36:1703-1711. <https://doi.org/10.1002/pc.23081>

Meng M, Rizvi MJ, Grove SM, Le HR (2015) Effects of hygrothermal stress on the failure of CFRP composites. *Compos Struct* 133:1024-1035. <https://doi.org/10.1016/j.compstruct.2015.08.016>

Miao C, Gao X, Tang B, Dai Z, Li H, Yi J (2023) Multiscale thermal oxidative ageing mechanisms of carbon fiber/epoxy plain woven composites under short beam shear loading. *Thin-Walled Struct* 186:110566. <https://doi.org/10.1016/j.tws.2023.110566>

Nah JS, Lee J (2020) Reliability assessment of display delamination considering adhesive properties based on statistical model calibration and validation. *Int J Mech Mater Des* 16:191-206. <https://doi.org/10.1007/s10999-019-09454-0>

Orifici AC, Herszberg I, Thomson RS (2008) Review of methodologies for composite material modelling incorporating failure. *Compos Struct* 86:194-210. <https://doi.org/10.1016/j.compstruct.2008.03.007>

Paris F, Correa E, Canas J (2003) Micromechanical view of failure of the matrix in fibrous composite materials. *Compos Sci Technol* 63:1041-1052. [https://doi.org/10.1016/S0266-3538\(03\)00017-4](https://doi.org/10.1016/S0266-3538(03)00017-4)

Radel M, Willberg C, Krause D (2019) Peridynamic analysis of fibre-matrix debond and matrix failure mechanisms in composites under transverse tensile load by an energy-based damage criterion. *Compos Part B Eng* 158:18-27. <https://doi.org/10.1016/j.compositesb.2018.08.124>

Rasheeda P, Sikarwar RS (2025) Modelling and damage evaluation of glass fiber-reinforced aluminium laminates subjected to quasi static indentation. *Int J Mech Mater Des* 21:823-847. <https://doi.org/10.1007/s10999-025-09753-9>

Shi T, Pang M, Wang Y, Zhang Y (2024) Inverse parameter identification framework for cohesive zone models based on multi-island genetic algorithm. *Eng Fract Mech* 300:110005. <https://doi.org/10.1016/j.engfracmech.2024.110005>

Soutis C (2005) Carbon fiber reinforced plastics in aircraft construction. *Mater Sci Eng A* 412:171-176. <https://doi.org/10.1016/j.msea.2005.08.064>

Tan W, Jiang Y, Wang G, Na J, Jin Q (2026) The influence of thermal aging on the interlaminar mechanical properties and failure mechanisms of CFRP under complex stress states. *Mech Adv Mater Struct* 33:2445217. <https://doi.org/10.1080/15376494.2024.2445217>

Wang Y, Shi T, Zhang Y, Pang M (2025) Identification of effective cohesive parameters for FRP-concrete interface using multi-island genetic algorithm. *Structures* 71:108016.

<https://doi.org/10.1016/j.istruc.2024.108016>

Wan X, Demir B, An M, Walsh TR, Yang N (2021) Thermal conductivities and mechanical properties of epoxy resin as a function of the degree of cross-linking. *Int J Heat Mass Transf* 180:121821. <https://doi.org/10.1016/j.ijheatmasstransfer.2021.121821>

Yuan XW, Li WG, Xiao ZM, Zhang YM (2023) Prediction of temperature dependent transverse strength of carbon fiber reinforced polymer composites by a modified cohesive zone model. *Compos Struct* 304:116310. <https://doi.org/10.1016/j.compstruct.2022.116310>

Zavatta N, Rondina F, Falaschetti MP, Donati L (2021) Effect of thermal ageing on the mechanical strength of carbon fibre reinforced epoxy composites. *Polymers* 13:2006. <https://doi.org/10.3390/polym13122006>

Zhuang L, Pupurs A, Varna J, Talreja R, Ayadi Z (2018) Effects of inter-fiber spacing on fiber-matrix debond crack growth in unidirectional composites under transverse loading. *Compos Part A Appl Sci Manuf* 109:463-471. <https://doi.org/10.1016/j.compositesa.2018.03.031>

Supplementary Files

This is a list of supplementary files associated with this preprint. Click to download.

- [SupplementaryMaterial.docx](#)