



Thermographic studies on the capability of artificial defects to initiate realistic fracture in CFRP components[☆]

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ABSTRACT

For accurate fracture toughness assessment of Carbon Fibre-Reinforced Polymer (CFRP) composites, it is critical that when artificial defects are used, in the form of inserts, they are able to realistically initiate fracture. The behaviour of artificial defects created by 5 μm steel film inserts subjected to mode II loading is studied using IM7/8552 Centre Crack Ply (CCP) specimens. The effect of using a release agent with the inserts is assessed by employing full-field infrared (IR) imaging and Digital Image Correlation (DIC) under both static and cyclic loading. The rapid transient temperature increase at failure is captured using IR imaging. Measurement of the temperature evolution forms the basis of a novel approach that is proposed for the evaluation of the mode II fracture toughness, G_{IIc} , which is validated against the frequently used method of monitoring the rapid reduction in load caused by fracture initiation. The capability of the artificial defects to behave as actual delamination damage is studied using the IR image processing technique of Thermoelastic Stress Analysis (TSA). It is shown that the non-adiabatic thermoelastic response reveals subsurface behaviour relating to the bond characteristics. A validated *meso*-scale finite element model that includes the thermoelastic heat sources is used to support the experimental results, confirming that the insert without release agent does not adequately replicate a defect that causes delamination damage. Finally, to demonstrate the utility of the proposed IR thermography-based mode II fracture toughness methodology in complex structural testing, it is applied to a C-spar substructure containing artificial defects.

1. Introduction

Delamination constitutes one of the principal failure modes in composite materials, and interlaminar fracture toughness represents a key input parameter in composites design and failure analysis. The experimental evaluation of fracture parameters often relies upon experimental setups where cracks and delaminations initiate from interlaminar inserts that act as artificial defects. Understanding the behaviour of such inserts within the laminate is therefore essential, as the reliability and validity of fracture toughness evaluations depend on adequate replication of the real failure mechanisms.

Standardised tests to evaluate the interlaminar fracture toughness of composite materials, e.g. mode I [1], mode II [2], and mixed mode [3], typically employ inserts made from thin films of polytetrafluoroethylene (PTFE commonly known as *Teflon*) to act as artificial defects that will

initiate and propagate delamination. Often, it is necessary to generate a starter crack from the artificial defect prior to final testing to adequately replicate delamination damage [4]. Furthermore, the standard tests require additional, and in some cases costly and complex, fixtures to be fitted to the test machine [5,6]. Hence, a widely adopted configuration for investigating mode II fracture of laminated composites is the non-standardised Centre Crack Ply (CCP) specimen test, also known as the Transverse Cut Tension (TCT) test, [7–15]. Fig. 1 shows a sketch of the CCP specimen, which consists of a unidirectional composite laminate that incorporates a number of transversely cut plies to create an artificial crack. This cut induces a localised stress concentration that initiates four interlaminar matrix cracks, which then propagate in an unstable manner under tensile loading.

Although the CCP test is not standardised, it offers distinct advantages, as the specimens are straightforward to manufacture and can be

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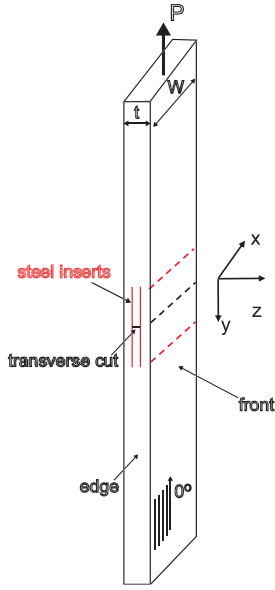


Fig. 1. Sketch of the CCP specimen. $L = 200$ mm, $W = 10$ mm, $t = 1.22$ mm. y -axis is aligned with principal material direction 1 (longitudinal fibre direction) and x -axis is aligned with principal material direction 2 (transverse direction).

fitted directly to the test machine grips. More importantly, the CCP test enables the evaluation of G_{IIc} by identifying the failure load directly from the test machine load cell read out, alongside a knowledge of the test specimen material properties and geometry. The mode II fracture toughness, G_{IIc} , is calculated via an energy-based formulation, as proposed by Wisnom [7], which depends on specimen width, W , and thickness, t , Young's modulus, E , the ratio of cut plies, x , and the critical load, P , at which unstable crack propagation is triggered:

$$G_{IIc} = \frac{P^2}{4W^2Et} \left(\frac{x}{1-x} \right) \quad (1)$$

where P is identified at the first reduction in load, or “load drop,” observed in the load–displacement curve.

Early implementations of the CCP test relied solely on the cut plies to initiate damage [7,15]. Experimental and numerical studies by Scalici *et al.* [12] showed that using the cut plies alone for initiation often led to asymmetric and irregular crack propagation. It was shown that artificial delaminations, created by using interlaminar inserts at the pre-crack location, helped to improve the test reliability. Essentially, the inserts served two principal functions: (i) stabilising crack propagation by promoting symmetry with respect to the laminate mid-plane, and (ii) minimising the opportunity for the crack to propagate in a mixed-mode manner. In [12] it was observed that if the specimens contain an insert that is larger than the fracture process zone, the mode I/II mixity is eliminated. However, the extent to which different insert materials and surface treatments truly replicate delamination behaviour remained an open question with significant implications for test validity. Le Cahain *et al.* [10] conducted a comparative study on the effects of various insert materials, demonstrating that steel inserts offered closer agreement to the behaviour of the CCP specimens without the insert compared to the more commonly used PTFE inserts. Despite these findings, PTFE remains the prevalent method for introducing artificial delamination, potentially due to the clear disadvantage of steel in that it requires pretreatment with release agent to prevent it from bonding to the laminate. Therefore, the use of steel inserts as a means of producing artificial delaminations warrants further investigation.

In the present paper the bond between the steel and the laminate resin is characterised using Carbon Fibre Reinforced Polymer (CFRP) CCP specimens manufactured from the well characterised IM7/8552 [16] prepreg. The stainless-steel shim inserts were introduced into the

laminates either as bare metal or with a coating with Frekote® release agent. In contrast to previous investigations [7–11,14,15] full-field imaging techniques are used. The focus is on the development of both passive and active infrared (IR) thermography to characterise the role of the bond in the fracture event, supported by Digital Image Correlation (DIC) [17] to obtain strains in a non-contact manner. It is acknowledged that several approaches [18,19,20,21] have employed passive IR thermography to monitor progressive intralaminar fibre failure in composites initiated from a macroscale notch. However, the approaches reported often rely on complex solutions to the 3D heat diffusion equation and require accurate estimation of the Taylor-Quinney coefficient, β , to quantify the fraction of mechanical work converted to heat during the fracture process [22]. For intralaminar cracks in composite materials β usually ranges from 0.5 to 0.9 [20,21] constituting a significant source of uncertainty that has hindered the development of reliable thermal methods for fracture toughness evaluation. However, studies have shown that $\beta \rightarrow 1$ for fast-propagating cracks in brittle materials [20,21]. In the CCP test the primary failure mechanism is unstable, rapid, resin-dominated, brittle, interlaminar fracture, thus providing a suitable benchmark to assess the use of IR thermography in quantitative fracture studies.

An additional consideration for cracks propagating from a macro scale notch is the motion of the test object relative to the thermal camera sensor as the crack progresses. *Motion compensation* techniques are required to account for the motion. These are usually achieved by cross correlating the captured IR images or by implementing DIC to track displacements between the images, e.g. [20]. The rapid, unstable energy release characteristic of the CCP test means that motion compensation is not required. Therefore, the novel methodology proposed in the present paper evaluates G_{IIc} directly from the local transient temperature increase measured during the failure event. This approach exploits the instantaneous and uniform nature of the failure, which is validated against the load-drop method given in Eq. (1) [7].

It is noteworthy that the CCP test exhibits a size effect not predicted by traditional linear elastic fracture mechanics, as reported in [7,14,15]. The measured G_{IIc} increases with specimen thickness, indicating that the apparent fracture toughness is influenced by specimen geometry rather than being solely a material property. Van der Meer and Sluys [14] and Cui *et al.* [15] investigated the causes of such size effects, identifying much better agreement with experimental observations when fracture toughness is considered a function of specimen thickness and when through-thickness compressive strains and frictional effects are accounted for. This geometry dependence has resulted in recommendations to evaluate interlaminar fracture toughness under conditions as close as possible to real application scenarios [7]. Therefore, a methodology, such as the IR-based approach proposed in the present paper, which is capable of evaluating energy release locally at the crack tip in structural components would be a valuable tool for designers.

Beyond static fracture characterisation, the CCP specimens have also been employed for fatigue testing. Allegri *et al.* [9] employed clip-on extensometers to monitor delamination growth rates and formulated semi-empirical crack-growth laws. More recently, a full-field active thermography technique known as Thermoelastic Stress Analysis (TSA) [23], has been used to monitor the mechanical performance of CCP specimens. TSA utilises IR thermography to obtain the small, reversible temperature variations, ΔT , that occur in materials under cyclic stress due to the thermoelastic effect. For an anisotropic material, the thermoelastic coupling under adiabatic conditions is expressed as:

$$\Delta T = -\frac{T_0}{\rho C_p} (\alpha_1 \Delta \sigma_1 + \alpha_2 \Delta \sigma_2) (2)$$

where T_0 is the sample temperature, ρ is density, C_p is specific heat capacity at constant pressure, α_1 and α_2 are the coefficients of thermal expansion, and $\Delta \sigma_1$ and $\Delta \sigma_2$ are the stress variations, with 1 and 2 denoting the principal material directions.

TSA utilises a thermal image series captured from a specimen undergoing cyclic loading, which is processed using a lock-in approach to ΔT . Most relevant to the present paper is the work of Pitarresi *et al.* [13], where TSA was used to monitor crack propagation during cyclic loading. The IR camera was focused on the CCP sample so that the through-thickness laminate stack was viewed (edge view, in Fig. 1) enabling the propagating crack tip to be observed directly. The thermal response modulated at twice the loading frequency was correlated to frictional dissipative effects occurring at load reversal. In contrast, the work described in the present paper utilises the response modulated at the loading frequency and thermal conduction through the laminate thickness to characterise the bonding of the artificial insert. Hence, the IR images are captured from the surface view in the plane (front view, in Fig. 1) of the laminate stack to demonstrate the feasibility of the proposed technique when a direct view of the crack tip is not available, i.e. when the artificial defect is enclosed in a structure.

In laminated CFRP materials with thin plies the necessary adiabatic conditions for TSA only prevail at high loading frequencies [24–27] that cannot be achieved in a typical test machine for realistic composite components. Recently, focus has turned to utilising the non-adiabatic response for detecting subsurface damage [27] and extracting material properties [28]. The non-adiabatic behaviour is caused mainly by heat transfer through the laminate thickness due to the different thermo-mechanical responses of each ply [24–27]. In the present paper, heat transfer is used to characterise the bonding between the artificial delaminations and the laminate by monitoring the surface response of the specimen. A validated *meso*-scale finite element (FE) model with a custom user subroutine for the evaluation of the thermomechanical heat source [26,29] is employed to support the experimental observations.

In the following sections of the paper, first it is demonstrated that the novel IR-based mode II fracture methodology can reproduce the results obtained by the load-drop method when conducting quasi-static tensile tests until failure. The work then moves on to the application of the TSA technique to CCP specimens, with coated and uncoated steel inserts, subjected to cyclic tensile loading. It is shown that TSA is able to characterise the bond between the steel and the laminate resin. Finally, to demonstrate the practical utility of the method beyond standard coupon geometries, the IR-based fracture toughness evaluation approach is applied to a realistic composite substructure. IR images are captured from two IM7/8552C-spar components [30,31] during combined compression and bending loading to failure. The ability of the new methodology to provide local estimates of energy release in non-standard configurations, where traditional fracture mechanics tests might not be applicable, is established.

2. Arrangements for CCP experiments

2.1. Test specimens and materials

Two panels of 10-ply IM7/8552 CFRP material [16] were produced, with the 5th and 6th plies cut transversely to fibre direction. Stainless steel film inserts, 20 mm in length and 5 μm in thickness, were introduced as an artificial defect between the 4th and 5th plies and between the 6th and 7th plies. To investigate the effect of the release agent on the behaviour of the artificial defect, two laminated panels were produced where the steel inserts of the first panel were left uncoated while the steel inserts of the second panel were coated with Frekote® release agent. The laminate stack was consolidated on an aluminium mould plate, using a vacuum bag and associated consumables. This ensured that both plates had a face that was smooth (mould side), compared to the bag side face containing a peel ply imprint resulting in a rough surface. The panels were cured in the autoclave according to the manufacturer's recommendations. The finished panels had a stacking sequence denoted as $[0_4, 0]_s$, where the underscore indicates the transversely cut plies.

Test specimens were cut from both panels as rectangular strips with

dimensions of 200 mm $\times$ 10 mm $\times$ 1.22 mm ($L \times W \times t$; see Fig. 1). The test specimens with the coated inserts are identified with the notation “IC”, while the test specimens with inserts that were left uncoated are identified as “IU”. Two Tufnol end tabs with a length of 50 mm and thickness of 1 mm were applied to each end of the specimen to facilitate clamping in the grips of a test machine. The length between the grips was 100 mm. The CCP specimen geometry is consistent with that employed in [10], ensuring that the fracture toughness results are comparable to literature values; as described above, fracture toughness derived from CCP specimens is known to be affected by specimen thickness [7,14,15]. The material properties used in the analysis and numerical modelling are detailed in Table 1 for IM7/8552 [28] and Table 2 for the steel inserts [32].

Finally, the specimens were prepared for the application of the imaging techniques by applying a thin layer of matt black paint to the smooth surface of each specimen to ensure uniform emissivity for the infrared imaging. Then a speckle pattern was applied on the matt black paint for the DIC analysis using a fine matt white spray. It has been demonstrated, e.g. [28,33], that the fine white speckle pattern has a negligible effect on the IR images. Hence, the white light and infrared images can be captured together during a test. Although the lighting for the DIC was provided by low thermal emission LEDs, prior to the test the specimen was illuminated for sufficient time to ensure thermal equilibrium and minimise the effect of transient heating. Since the analysis is based on temperature change, rather than absolute temperature, the small amount uniform heating introduced by the lights could not affect the measurement.

Utilising the smooth side of the specimens avoids aliasing effects in the DIC, as the high spatial frequency regular pattern created by the peel ply imprint causes aliasing effect in DIC. The peel ply imprint is also known to cause deleterious effects in IR imaging amplifying reflections, and the inhomogeneous surface resin thickness introduced by the peel ply causes variations in the thermoelastic response.

2.2. Imaging capture and loading

The mechanical testing was performed using an Instron 1342 servo-hydraulic test machine equipped with a 100 kN load cell. Fig. 2 (a) shows a photograph of the test arrangement with the CCP specimen mounted in the test machine and the relative position of the cameras. Fig. 2 (b) shows a schematic diagram of the test set-up showing the position of the cameras and the orientation of the test machine grips to enable image capture on three sides of the specimen. Infrared images were captured using a photon detector Telops FAST M3K thermal camera (320 $\times$ 256 pixels, NETD < 25 mK) with a 50 mm lens. White light images for the DIC were acquired using two FLIR Blackfly cameras in a stereo configuration, with 12 Mpx sensors and 25 mm lenses (all the lenses are shown in black in Fig. 2 (b)). In both the IR and white light imaging for DIC, the cameras were set-up to capture an in-plane view of the smooth side of the specimens. To verify symmetric crack propagation about both the mid-plane of the laminate and the plane of the cut

Table 1
Material properties for IM7/8552 carbon fibre/epoxy prepreg [28].

Property	Value
Young's modulus E_1 (GPa)	161.79
Young's modulus E_2 (GPa)	9.5
Poisson's ratio ν_{12}	0.32
Shear modulus G_{12} (GPa)	5.15
Shear modulus G_{23} (GPa)	4
Coefficient of thermal expansion α_1 (10^{-6} K^{-1})	−0.085
Coefficient of thermal expansion α_2 (10^{-6} K^{-1})	29.05
Thermal conductivity k_1 (W m K^{-1})	4.89
Thermal conductivity $k_2 = k_3$ (W m K^{-1})	0.615
Density ρ (kg m^{-3})	1569
Specific heat capacity C_p ($\text{J kg}^{-1} \text{ K}^{-1}$)	884.53

Table 2
Material properties for the stainless-steel insert [32].

Property	Value
Young's modulus E (GPa)	200
Poisson's ratio ν (—)	0.3
Coefficient of thermal expansion α (10^{-6} K^{-1})	12
Thermal conductivity k (W m K^{-1})	16
Density ρ (kg m^{-3})	8000
Specific heat capacity C_p ($\text{J kg}^{-1} \text{ K}^{-1}$)	500

plies of the CCP specimens, an Infratec Variocam HD (NETD < 50 mK) microbolometer camera fitted with a 30 mm lens captured a through thickness view of the specimens, whilst a white light CMOS XCAM-TOP4K ToupTek camera equipped with a monocular zoom objective captured the other through thickness view.

To obtain the fracture toughness values three of both types of CCP

specimens (i.e. with (IC) and without (IU) release agent on the insert) were subjected to a quasi-static tensile load to failure using a displacement rate of 0.5 mm/min. The frame rates were 51 Hz for the Telops MK3 IR camera and 2 Hz for the white light images for the FLIR Blackfly cameras, which provided temporal resolution to capture more than 30 thermal images across the fracture event. The frame rate for the bolometer was 30 Hz, while the microscope camera, mounted on a linear-stage actuator, scanned the edge of the specimen, whilst the load was held, following the first load drop. The MatchID [34] DIC system was used to synchronise the stereo image capture with the load signal from the test machine load cell and to process the image data into strain using the parameters given in Table 3.

The cyclic loading for the TSA was provided by an electrodynamic Instron Electropuls E10000 fitted with a 10 kN load cell. One of each type of CCP specimen was subject to a cyclic load of $5 \pm 4.5 \text{ kN}$ with a loading frequency of 1.1 Hz. Previous work [25,26,28] informed the

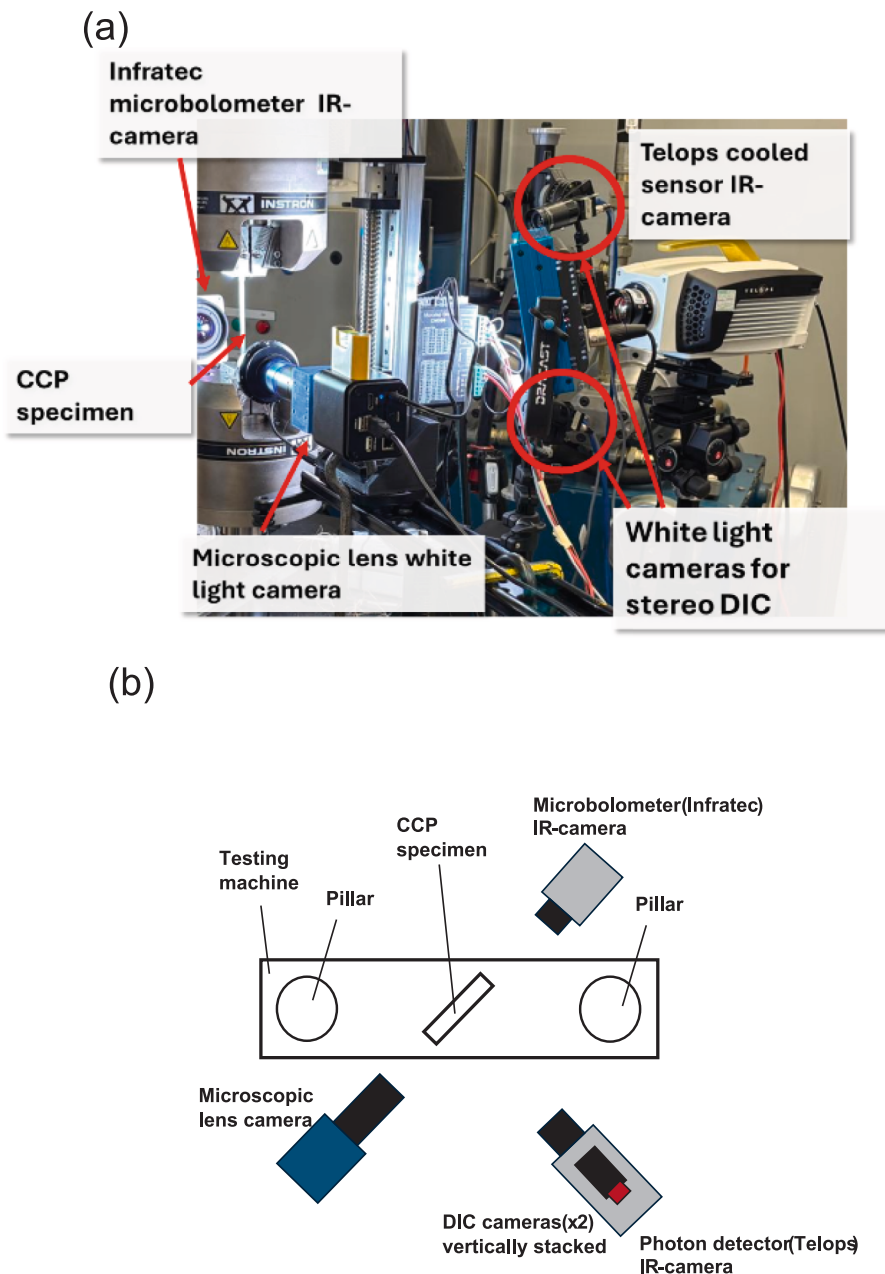


Fig. 2. (a) Photograph of the experimental setup showing the combined white light and infrared thermography imaging configuration; (b) schematic representation of the experimental setup. All the lenses are represented in black, while the camera bodies are shown using different colours.

Table 3

Parameters employed in stereo DIC data processing.

Technique used	3D/Stereo digital image correlation
	Cameras (Blackfly-12.3 Mpx)
Sensor, digitalization	Sony IMX226 (CMOS), 4096 x 3000
Stereo angle	26.92°
Frame rate	2 Hz
Focal length	25 mm
Total number of images	184
DIC software package	MatchID
Pixel to mm conversion	1 px = 0.03597 mm
ROI (mm)	91.75 × 8.48
Coordinate transformation	Best-plane-Fit
Subset, step	21, 10
Interpolation, shape function, correlation	Local Bicubic Splines
Pre-smoothing	Gaussian Smoothing
Displacement resolution (y axis)	3.037 · 10 ⁻⁴ mm
Strain conversion	Log. Euler-Almansi
Smoothing technique	Local polynomial – affine least-squares fit
Strain window (pixels)	5 data points
VSG	61 pixels
Strain resolution (y axis)	0.00192%

selection of the low loading frequencies, as it was shown to enhance subsurface damage/defect visualisation by allowing longer times for the through-thickness heat diffusion [24,25,27]. Only infrared imaging was used in this case, with the Telops camera recording 6000 thermal images with a frame rate of 383 Hz. The thermal image series was processed as described in [33] using an in-house least squares fitting algorithm applied pixel by pixel to provide the notch filtering to extract the thermoelastic signal ($\frac{\Delta T}{T_0}$) and phase of the response relative to the load signal [33]. The least squares fitting algorithm was used because it removes the need to sample an integer number of loading cycles to avoid spectral leakage, thereby requiring less processing than other notch filtering approaches [33,35]. Eq. (2) shows that under adiabatic conditions the load signal is 180° out of phase with ΔT , when $\alpha_1, \alpha_2 > 0$ [29]. This provides a means of identifying whether the stress in the specimen is tensile or compressive. More importantly it provides an indication of adiabatic behaviour; heat transfer is occurring if the phase value is anything other than 0 or 180 degrees.

3. Quasi-static loading: Fracture toughness evaluation using thermal imaging

3.1. Hypotheses underpinning the derivation of G_{IIc}

As described in the introduction of the paper, under quasi-static loading the CCP specimen design promotes a significant energy release at failure. Hence, the CCP test has the potential to enable mode II fracture toughness evaluation from transient temperature evolutions that can be captured by thermal imaging. The fracture initiated by the artificial defect results in an unstable crack propagation front that is assumed to advance effectively instantaneously providing uniform energy release along the length and across the width of the specimen. It is also assumed that the through-thickness symmetry of the CCP specimen facilitates the homogenisation of temperature through the thickness, therefore providing a temperature increase, θ , that is uniform throughout the volume of material associated with the newly created fracture surfaces. Furthermore, as the matrix material in the CCP is brittle it can be assumed that all fracture energy is dissipated as heat, Q , so the Taylor-Quinney coefficient β can be taken as unity [36,37]. Finally, the thermoelastic coupling is not considered as the thermoelastic temperature change (Eq. (2)) is estimated to be approximately 1% of the temperature evolution at fracture.

Based on the assumptions outlined above, it is hypothesised that the

variation in thermal energy, δQ , generated in a material volume, dV , can be expressed as:

$$\delta Q = \rho C_p \theta dV \quad (3)$$

As it is assumed $\beta \approx 1$, the energy release rate (ERR) can be defined as the variation of released thermal energy per unit of newly created crack area, dA :

$$ERR = \frac{\delta Q}{dA} \quad (4)$$

At the onset of unstable, rapid crack propagation in the CCP specimen, ERR reaches the mode II critical energy release rate, or fracture toughness, G_{IIc} . Inserting Eq. (3) into Eq. (4) and using $dV = dA \times t$, G_{IIc} is obtained as:

$$G_{IIc} = \frac{\rho C_p \theta dA t}{dA} \quad (5)$$

Hence, G_{IIc} can be evaluated based solely on the measurable temperature rise, as follows:

$$G_{IIc} = \rho C_p \theta t \quad (6)$$

Eq. (6) provides a simple means to evaluate fracture toughness requiring only material properties, the specimen thickness, and the measured θ from the thermal images at failure. Importantly, it does not require a knowledge of the load applied to the specimen, or assume a stress state, as is the case for Eq. (1). It should also be noted that an approach using the same assumptions proved effective for comparing the different mode-mixities of interfacial failure between the face sheet and core material of sandwich structures [38]. However, it was not possible to derive quantitative values of fracture toughness to fully validate the results. Hence, the following section provides confirmation that the hypothesis discussed above is valid for the CCP specimen through a quantitative validation of the results.

3.2. Experimental validation of proposed methodology to evaluate G_{IIc}

To verify the hypotheses described in Section 3.1, the displacements and strains in the CCP specimens were obtained using the MatchID DIC software using the parameters given in Table 3. Fig. 3 (a) shows a typical map of the longitudinal strain, ϵ_y , in a IU CCP test specimen. The local increase in ϵ_y in the centre of the specimen is consistent with the effective reduction of load-bearing cross-section area due to the two central cut plies. The load–displacement plot shown in Fig. 3 (b) was obtained by taking displacements at known loads utilising a “digital extensometer” over the DIC gauge length shown in black in Fig. 3 (a). The digital extensometer utilised the average displacement values taken from 50 measurement points from the DIC displacement images over the area marked in black in Fig. 3(a), i.e. the difference between the displacement given at the top and bottom of the DIC gauge length (≈ 75 mm). The load–strain curves from the quasi-static tensile tests to failure on the CCP specimens, clearly show the load drops, which indicates that cracks are propagating in an unstable manner.

Passive IR thermography was also conducted during the quasi-static loading of the CCP specimens. Fig. 4 (a) shows a full field image of temperature captured from an IU CCP specimen at the first load-drop. The localised temperature increase corresponds to the energy release. The average temperature rise for each specimen was obtained over the region of interest (ROI) of 9 x 21 mm, indicated by the black rectangle, shown in Fig. 4 (a). A representative plot of temperature against time is given in Fig. 4 (b), showing a clear temperature increase of around 0.7 K over a short time period (~ 0.43 s).

During the tests, the delamination at the top and bottom interfaces (with respect to the length of the sample) initiated at slightly different times and correspond to different applied load values, leading to a two-

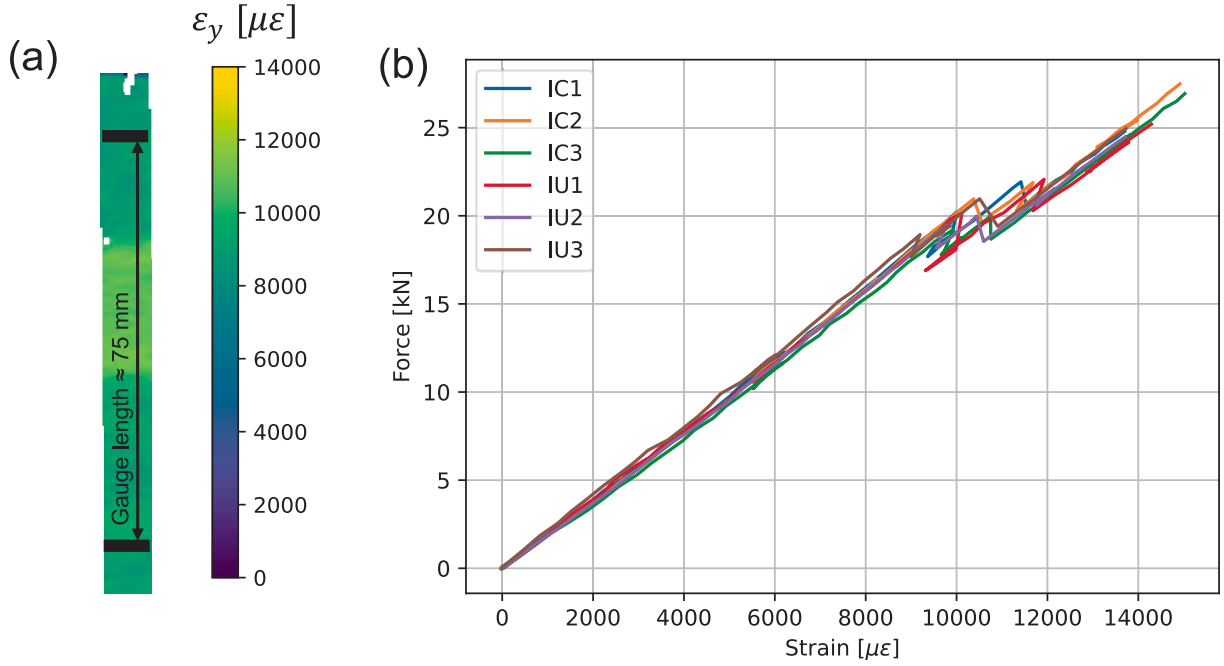


Fig. 3. Results from the quasi-static tensile tests: (a) DIC full-field strain map in the fibre direction (ε_y) from the IU1 CCP specimen ($P \approx 20$ kN), and the digital extensometer gauge length for displacement evaluation, and (b) load-displacement curves.

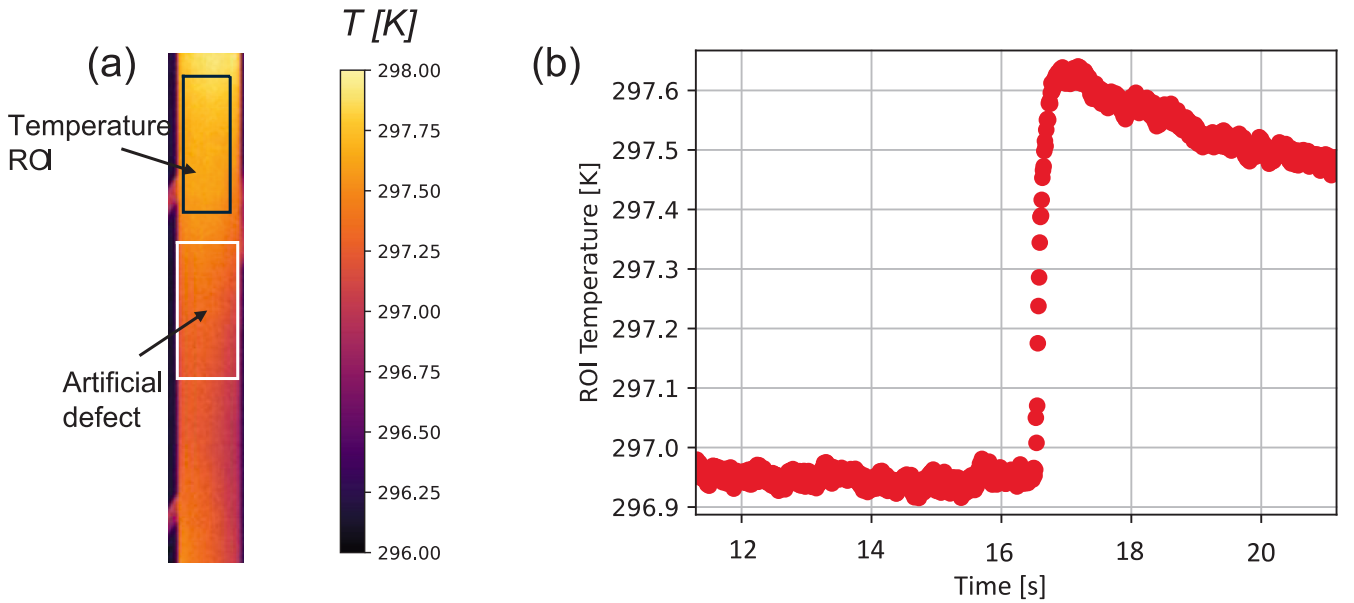


Fig. 4. Representative result from a quasi-static tensile test: (a) Thermal image from a CCP specimen with an artificial defect without Frekote® showing the temperature increase during the failure event. The white rectangle indicates the artificial defect location, while the black rectangle indicates the ROI for the temperature evaluation. (b) Average temperature evolution over time.

stage load drop as seen in Fig. 3 (b). This phenomenon, attributed to small differences in the interface bond, is a common occurrence in CCP testing and does not compromise the validity of the fracture toughness evaluation [8,13]. The through-thickness views of the sides of the specimen obtained by the microbolometer on one side and the single white light camera on the other confirmed that, in all cases, the crack propagation was symmetric through the thickness of the material. Typical images of through-thickness views of a CCP specimen are shown in Fig. 5 before (a) and after (b) failure. Prior to failure, no particular trend is observed in either the white light or infrared images, while the steel inserts are clearly visible in the white light images, with a slight

misalignment and marginal difference in length between the two. After failure, the white light images (shown both in full view and zoomed for clarity) confirm that the crack propagated symmetrically and along the entire length of the specimen. The infrared image after failure reveals a localised thermal increase in the left region of the field of view (top half of the specimen), which is uniform through the thickness of the specimen, and consistent with the symmetric crack propagation observed optically.

To determine the temperature increase, θ , the temporal average of 10 thermal images taken before the failure event was subtracted from the temporal average of 10 thermal images taken after the failure event. A

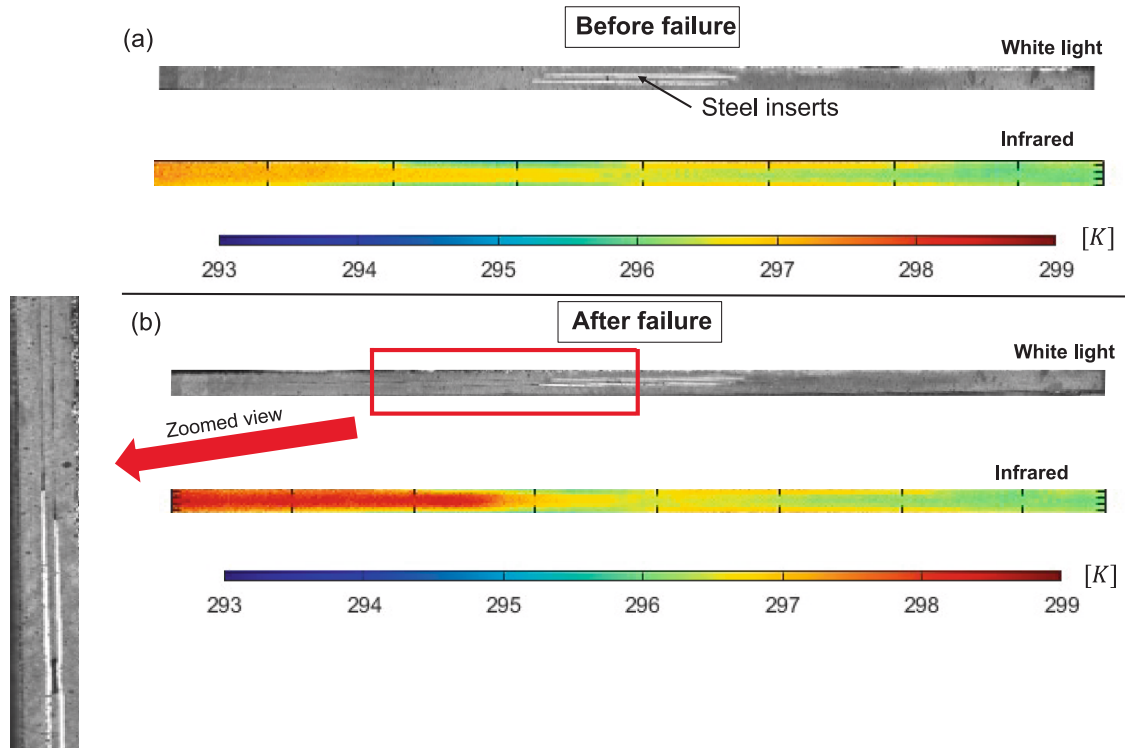


Fig. 5. White light and Infrared images of the edges of the specimen (a) before failure (b) after failure. A zoomed view after crack propagation confirms that the specimen failed symmetrically with respect to the thickness.

series of 10 thermal images corresponds to a time of 0.2 s, which was deemed short enough to avoid significant thermal diffusion into the surroundings, whilst being sufficient to reduce noise in the measurement. A full-field image of θ in Fig. 6 (a) shows a clear region of increased temperature difference. To verify the hypothesis of uniform thermal distribution on the external surface due to crack propagation, a statistical analysis was performed. The θ values were obtained from ten

small (7×7 pixel) sub-regions randomly sampled across the delaminated region. The results for all the CCP specimens are plotted in Fig. 6 (b), showing that θ is consistent regardless of the specific sample area chosen, with a slight variation observed for each CCP specimen. Table 4 reports the spatial averages and standard deviations (SDs) of θ for each individual specimen. As mentioned earlier, delamination did not initiate simultaneously at the top and bottom edges of the insert. The

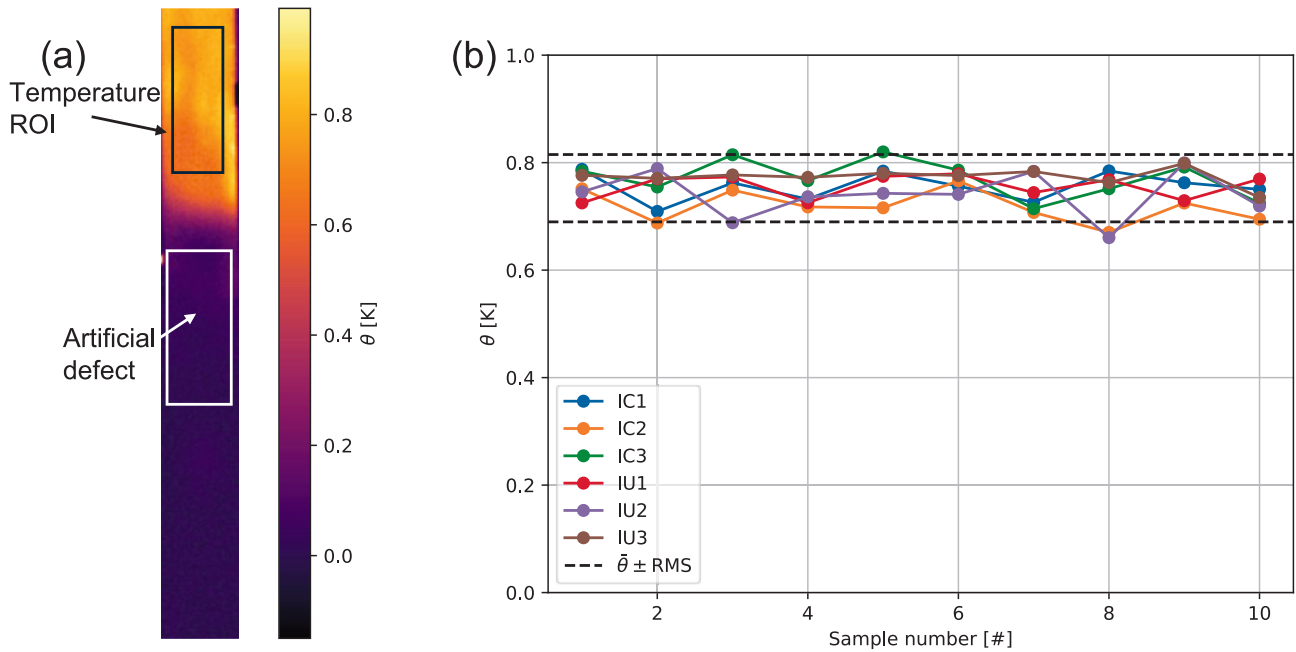


Fig. 6. (a) Full-field θ map for a CCP specimen without Frekote. The white rectangle indicates the artificial defect location, while the black rectangle indicates the ROI for the temperature evaluation. (b) Average θ measured in ten randomly selected 7×7 pixel sub-regions across the delaminated area, demonstrating uniform temperature distribution. Dashed lines represent the RMS of the SD across all tested samples.

Table 4

Average and standard deviation (SD) of θ over the crack propagation area for all the tested samples.

Sample	Average θ [K]	SD θ [K]	Failure location relative to defect
IC1	0.786	0.041	Below
IC2	0.713	0.086	Above
IC3	0.751	0.046	Below
IU1	0.723	0.088	Above
IU2	0.707	0.047	Below
IU3	0.719	0.049	Below

propagation of failure either from the top or the bottom of the defect may be attributed to small asymmetries in the manufacturing of the insert. Therefore, Table 4 includes the location of the failure initiation, i. e. either above or below the defect. Notably, θ remains consistent across all specimens, indicating the robustness of the thermal approach. A representative variability across specimens was subsequently obtained by compounding the standard deviation (SD) values using a root-mean-square (RMS) approach ($RMS = 0.0626$ K), which was used to bound the average θ values reported in Fig. 6.

The average G_{IIC} values, SD and coefficients of variation (CoV) calculated using the load drop approach in Eq. (1) and the new IR imaging based approach in Eq. (6) are compared alongside values from the literature [10] in Table 5. The average G_{IIC} obtained from Eq. (1) was 1271 J/m^2 for Frekote®-coated IC specimens and 1224 J/m^2 for IU specimens. The proposed thermal method yielded average values of 1282 J/m^2 and 1222 J/m^2 , respectively. Although the IR imaging can only capture the heat generated during fracture, the excellent agreement in G_{IIC} with the load-drop method confirms the applicability of the thermal approach and supports the validity of the assumption that $\beta \approx 1$.

Table 5 shows that the IC specimens yielded an average G_{IIC} of 47 and 60 J/m^2 greater than the IU specimens from the load drop and IR imaging method respectively. Both these differences are within standard deviations reported in the Table 5 and with the small sample size (3 specimens of each) it is not possible to assess if the results from the two types of defect are statistically different. Importantly it is clear that the level of variance in the load-drop method is very close to that given by the IR imaging method. Interestingly, a similar difference was reported in [10], where coated steel inserts gave a G_{IIC} of 63 J/m^2 greater than specimens without artificial defects; see Table 5. The quantitative agreement in the differences in G_{IIC} suggests that the uncoated steel insert (IU specimen) behaves in a mechanically similar manner to the CCP specimens without inserts described in [10]. The conclusion is that in the absence of a release agent, instead of acting as an artificial defect, an uncoated steel insert (as in the IU CCP specimens) acts as a bonded inclusion. In this case, as with the specimen without an insert [10], fracture is primarily governed by the cut plies.

The consistently smaller G_{IIC} values obtained from the IU specimen compared with the IC specimen, echoed in [10], warrants further discussion, despite the differences being within the standard deviations reported in Table 5. The assumption is that for the IC CCP specimen the coated insert acts as an unbonded artificial defect, i.e. a delamination,

Table 5

Fracture toughness from CCP specimens.

G_{IIC} [J/m ²]	No insert	Insert Coated (IC)			Insert Uncoated (IU)	
	Load drop method Eq. (1) [10]	Load drop method Eq. (1) [10]	Load-drop method Eq. (1)	IR Imaging method Eq. (6)	Load drop method Eq. (1)	IR Imaging method Eq. (6)
Average	1140	1203	1271	1282	1224	1222
Std. dev.	14.8	63.7	97.6	51.1	52.6	47.6
CoV	1.30%	5.36%	7.68%	3.95%	4.3%	3.9%

which produces a path for the cracks to propagate without resistance. Hence, the expectation would be a lower G_{IIC} for the IC specimens. As this is not the case, it should be considered that the coating the insert with release agent generates a greater resistance to crack growth. To draw any definitive conclusions about the condition of the interface in the IU and IC specimens further investigations are required. Therefore section 4 discusses using TSA as potential means of characterising the condition of interfacial bond. The overall conclusion is that coating the steel insert makes little difference to the fracture behaviour.

It is noteworthy that the validity of the proposed local evaluation of ERR using temperature evolutions is not limited to the CCP configuration. In principle, the methodology is applicable to any configuration where crack propagation occurs in an unstable manner, and the primary failure mechanism is delamination. It is worth noting that the entire energy budget, including elastic wave radiation and crack surface energy, should be considered for other materials and test configurations. However, if $\beta \approx 1$, the assumption of instantaneous energy release holds. Therefore a small area of interest close to the crack tip can be selected over which the temperature evolution is tracked to provide a direct local measurement of energy release. This capability is particularly significant for complex load cases or geometries, as it allows evaluation of the energy release locally at the crack tip without any prior calibration procedure. A demonstration of the application of the local methodology to a C-spar substructure is presented in Section 5.

4. Cyclic loading: Characterisation of artificial delamination using TSA

The aim of the present section is to demonstrate a new approach for characterising the bond between the artificial defects and the CCP specimen using TSA, to identify the reason for G_{IIC} being greater in the IC specimens than the IU specimens. TSA signal processing was applied to thermal images acquired during cyclic loading of the IU and IC CCP specimens at a level $5 \pm 4.5 \text{ kN}$. The load was applied in the fibre direction (i.e. along the y-axis in Fig. 1), where it generates a unidirectional stress state ($\sigma_2 = 0$) in regions away from the crack tips. To avoid crack propagation a sufficiently small maximum load was selected of 9.5 kN compared to the load required to propagate the crack (approximately 20 kN).

A loading frequency of 1.1 Hz was used for the cyclic loading necessary for TSA to generate significant through-thickness heat transfer in the vicinity of the artificial defects. To confirm if the loading frequency would generate heat transfer through the thickness of the specimens, the thermal diffusion length, l , was calculated as follows:

$$l = \sqrt{\frac{\gamma}{\pi f}} \quad (7)$$

where $\gamma = k/\rho C_p$, i.e. the thermal diffusivity through the thickness of the material and f is the loading frequency.

Using the material properties in Table 1, l was calculated to be approximately 0.35 mm . Therefore, the subsurface response at the crack-tips (0.5 mm from the surface) and in the vicinity of the defects would be captured in the surface response obtained from the IR images. Moreover, using a characteristic length of half the specimen thickness, the Fourier number, F_o , [39] was determined to be 1.09 , indicating that thermal diffusion through the thickness of the specimen over one loading cycle is significant. Nevertheless, it should be noted that the $\frac{\Delta T}{T_o}$ values obtained under these conditions remain qualitatively representative of the stress distribution within the specimens and that localised stress concentrations (i.e., at the artificial defect tip) can be visualised.

Fig. 7 presents the experimental results ($\frac{\Delta T}{T_o}$ and phase) for both the coated (a, c) and uncoated (b, d) artificial defects. The $\frac{\Delta T}{T_o}$ images showed a considerable inhomogeneity as result of the fibre/resin distribution, so a median filter (having a kernel of $3 \text{ rows} \times 21 \text{ columns}$, with a pixel size

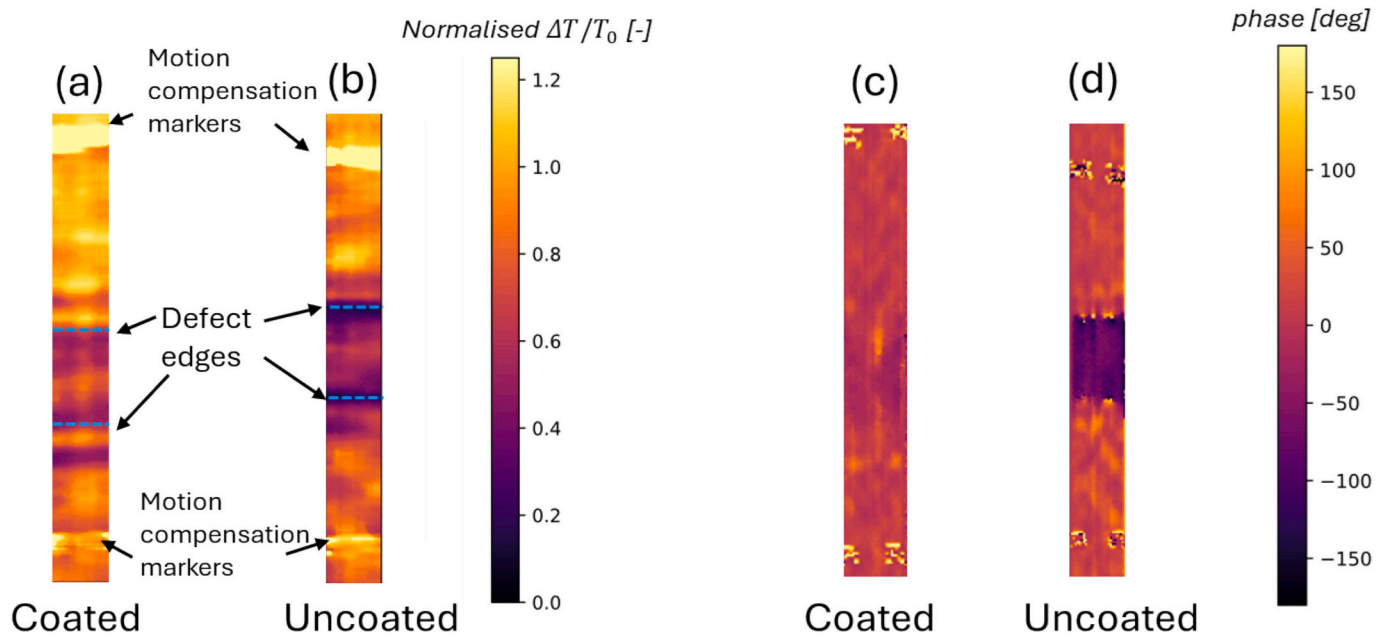


Fig. 7. Experimental TSA results at 1.1 Hz. Normalised $\Delta T/T_0$ for (a) Frekote®-coated and (b) uncoated steel artificial defects. The light blue lines indicate the approximate extent of the defect. Phase maps for (c) Frekote®-coated and (d) uncoated artificial defects.

of 0.37 mm) was applied to smooth the data across the width of the specimen to provide a more homogeneous response. To aid interpretation, some dashed lines are added to indicate the approximate location and extent of the defect in both the experimental and numerical results. In contrast, the phase data was less affected by the variations in the fibre and resin distribution, so it was not necessary to filter the data. For ease of comparison with the FEA results provided later in the section the $\frac{\Delta T}{T_0}$ values are normalised with respect to the average response in the far field, where the crack has no influence on $\frac{\Delta T}{T_0}$.

The filtered $\frac{\Delta T}{T_0}$ image for both specimens show regions of greater response at the top and bottom of the specimen. These come from the fiducial markers used for compensating for the motion in the specimen, which can also be seen clearly in the phase images. It is noteworthy that $\frac{\Delta T}{T_0}$ is generally larger in the upper part of the specimen, suggesting that motion might be affecting the data. However, when a motion compensation routine was applied to the raw IR data it did not change the $\frac{\Delta T}{T_0}$ image. This is because, the maximum displacement, i.e. motion caused by the cyclic loading (obtained from the DIC at a load of 9 kN) was 0.38 mm is very close to the pixel size of 0.37 mm. In cases where the motion is the same order as the pixel size, attempting to remove the motion risks introducing noise, which affects the image quality. Hence, in the following analysis the IR images were not corrected for motion as it was considered counterproductive.

For the specimen with the coated insert, the filtered $\frac{\Delta T}{T_0}$ image shows horizontal features that correspond to the boundaries of the defect regions that are labelled in Fig. 7 (a), while the phase map in Fig. 7 (c) shows a relatively uniform distribution. As the coefficient of thermal expansion (α_1 see Eq. (2)) of the IM7/8552 CFRP is negative [28], it is expected that for adiabatic conditions φ would be close to zero, which is the case in Fig. 7(c). However, the average phase value away from the defect is approximately 20°. This is caused by heat transfer between the 0° fibres and the resin rich regions of the ply [26,28].

Fig. 7 (b) shows the filtered $\frac{\Delta T}{T_0}$ image for the specimen with the uncoated artificial defect. The regions of elevated response are apparent at the top and bottom of the specimen due to the markers for motion compensation. The important observation is that there is a more pronounced reduction in $\frac{\Delta T}{T_0}$ in the defect area when compared to the

uncoated specimen in Fig. 7 (a). The phase plot in Fig. 7 (d) shows a distinct phase shift from positive to negative values across the region of the insert. The observed phase difference clearly indicates different heat transfer characteristics between the coated and uncoated inserts. Without the release agent, the artificial defect is fully bonded to the laminate and acts as an inclusion. Therefore, it carries stress during cyclic loading and generates a stress-induced temperature change that contributes to the thermoelastic response measured on the surface. It is necessary to recall that the carbon fibre plies in the longitudinal direction are characterised by a negative and low coefficient of thermal expansion ($\alpha_1 = -0.085 \times 10^{-6} \text{ K}^{-1}$), while the steel insert has a positive and higher coefficient of thermal expansion ($\alpha = 12 \times 10^{-6} \text{ K}^{-1}$). This mismatch causes the steel to produce a thermoelastic response that is out of phase with respect to that of the surrounding carbon fibre plies. The relatively low loading frequency (1.1 Hz) provides sufficient time for through-thickness heat diffusion, allowing the thermoelastic effect generated within the embedded insert to propagate to the surface. The heat transfer through the thickness of the specimen enables visualisation of the defect as a region of reduced $\frac{\Delta T}{T_0}$ maps with a distinct phase shift. In the specimen with the coated artificial defect there is no such phase shift, suggesting that the Frekote® has prevented the bonding of the steel to the laminate.

A meso-scale FE model was constructed in Abaqus [40] to provide further insight into the experimental observations and support the characterisation of the bonded interface between the artificial defect and the laminate. The model implements the user subroutine described in [26] to generate simulated values of ΔT , T_0 and phase from computed strain fields. To reduce computational cost, only one-eighth of the sample was simulated, with symmetry boundary conditions applied in the length, thickness, and width directions. Parametric, linear elastic solid models of the CCP specimens were constructed using Python scripting. The experimental loading conditions were replicated in the models, ($5 \pm 4.5 \text{ kN}$ at a loading frequency of 1.1 Hz), while the different treatments of the steel inserts were represented in the model space, as follows:

- The artificial defect/coated insert (IC specimen) was modelled as a delamination by using disconnected nodes between the cut plies and adjacent plies, without including the steel insert.

- The inclusion/uncoated insert (IU specimen) was modelled as a perfectly bonded 5 μm thick solid with prescribed steel material properties and permitted to generate a thermoelastic response.

In both cases, a resin-rich surface layer of 10 μm thickness was included, consistent with typical values encountered in IM7/8552 prepreg manufacturing [25,28]. The model is homogeneous at the meso-scale and therefore does not account for local variations in resin and fibre volumes that would lead to a non-homogeneous thermoelastic response, which occur in the real material. Three loading cycles (50 points per cycle) were simulated and processed through the same TSA algorithm used to obtain the experimental data. Typically, the simulated thermoelastic response converges to a stable value after the first cycle [26]. Therefore, only the second and third loading cycles were included in the lock-in procedure. Since the simulated results are not affected by experimental noise, two cycles are sufficient to produce a reliable simulation of the thermoelastic behaviour.

Fig. 8 (a) shows the simulated $\frac{\Delta T}{T_0}$ distribution for the specimen with the artificial defect, with the horizontal features that are evident in the experimental data reproduced at the upper and lower boundaries of the defect region. Although the horizontal bands of alternating high and low thermoelastic response at the artificial defect boundaries are not as prominent in the experimental data in Fig. 6. The indication is that the coated steel insert does not provide the equivalent of a fully delaminated region, as the local stress concentration at the artificial defect tip [13] is reduced.

For the specimen with the uncoated insert, the model shown in Fig. 8 (b) provides a clear reduction in $\frac{\Delta T}{T_0}$ within the central defect region. Moving vertically away from this central region, a transition zone appears where the thermoelastic signal decreases further before returning to a behaviour dominated by the 0° plies. Areas with $\frac{\Delta T}{T_0} \rightarrow 0$ correspond to an abrupt transition in the phase maps, due to the opposite signs of $\frac{\Delta T}{T_0}$ generated by the 0° plies and the steel insert.

The model with the coated artificial defect (IC specimen) shown in Fig. 8 (c) has a uniform phase distribution, apart from a small increase in phase at the defect tips. The phase distribution follows the experimental results except for the change in phase at the defect tip, which is consistent with the actual insert not adequately representing a delamination. The model of the specimen with the uncoated artificial defect (IU specimen) shown in Fig. 8 (d) exhibits a clear phase shift in the defect region, which closely corresponds to the experimental result confirming that the steel is fully bonded to the laminate.

To provide a more detailed comparison between the experimentally

obtained and numerically derived $\frac{\Delta T}{T_0}$ value and the phase shift, line plots were obtained along the length of the CCP specimens, as shown in Fig. 9. For the experimental data, the full-field maps were averaged across the specimen width, with the aim of minimising measurement noise and mesoscale inhomogeneities arising from local variations in resin and fibre content, which are not accounted for in the numerical model. As already done for the $\frac{\Delta T}{T_0}$ values, both experimental and numerical phase profiles were normalised to a common reference (far-field phase = 0°) to facilitate direct comparison of the phase shifts. The zero x-coordinate position values shown in the line plots is at the centre of the defect (see Fig. 1); negative position values are assigned to the region above the defect centre.

Line plots of the normalised $\frac{\Delta T}{T_0}$ values for the IC specimen are provided in Fig. 9 (a), where it is clear there is little agreement between the experiment and the model. However, this provides an explanation for the unexpected result of G_{IIc} for the IC specimen being larger than obtained for the IU specimen. The lack of similarity between the model and the experimental results, underlines that the coated artificial defect is not acting as a complete delamination in the IC specimen. In [13] it was shown that correlating the thermal image series to the 2nd harmonic frequency, rather than the actual loading frequency revealed that friction was occurring in the region of an artificial defect in a CCP specimen. As the frictional force peaks twice in the loading cycle a response is generated at the 2nd harmonic frequency. A similar effect was observed for both the IC and IU specimens local to the insert when the response was correlated to the 2nd harmonic indicating that there is some friction occurring at the interface. The IC specimen showed a non-uniform 2nd harmonic response over a larger region of the defect, further supporting the premise of partial debonding.

It is evident that prominent alternating high/low values occur at the defect boundaries, albeit at lower values in the experiment, with the peaks offset indicating that the defect in the specimen is affecting a larger region than anticipated by the model. It can be seen in the through thickness views provided in Fig. 5 that the edges of the defects are not aligned and of a different length. A further difference is at the centre of the defect where there is a peak in the experimental data; this could be attributed to partial connectivity provided by the resin pocket in the region of the cut plies, which was not modelled; in any future work sensitive NDE such as ultrasound should be used to provide confirmation. The phase profiles given in Fig. 9 (b) show a better correspondence between the experiment and the model. In the model results, a peak at both edges of the artificial defect is observed in the phase plots, as a result of the stress concentration at the crack tips. A similar peak is only

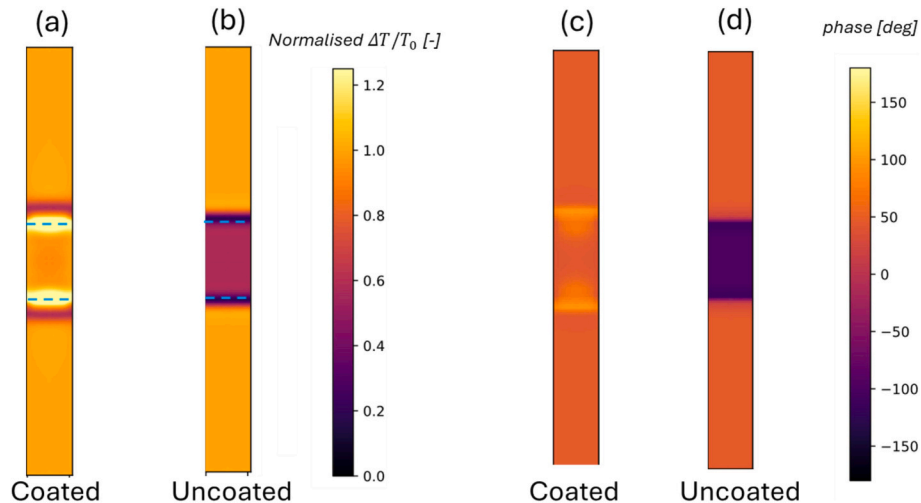


Fig. 8. Numerical TSA results at 1.1 Hz. $\Delta T/T_0$ for (a) artificial defect (coated insert) and (b) bonded inclusion (uncoated insert). The light blue lines indicate the approximate extent of the defect. Phase maps for (c) artificial defect and (d) bonded inclusion.

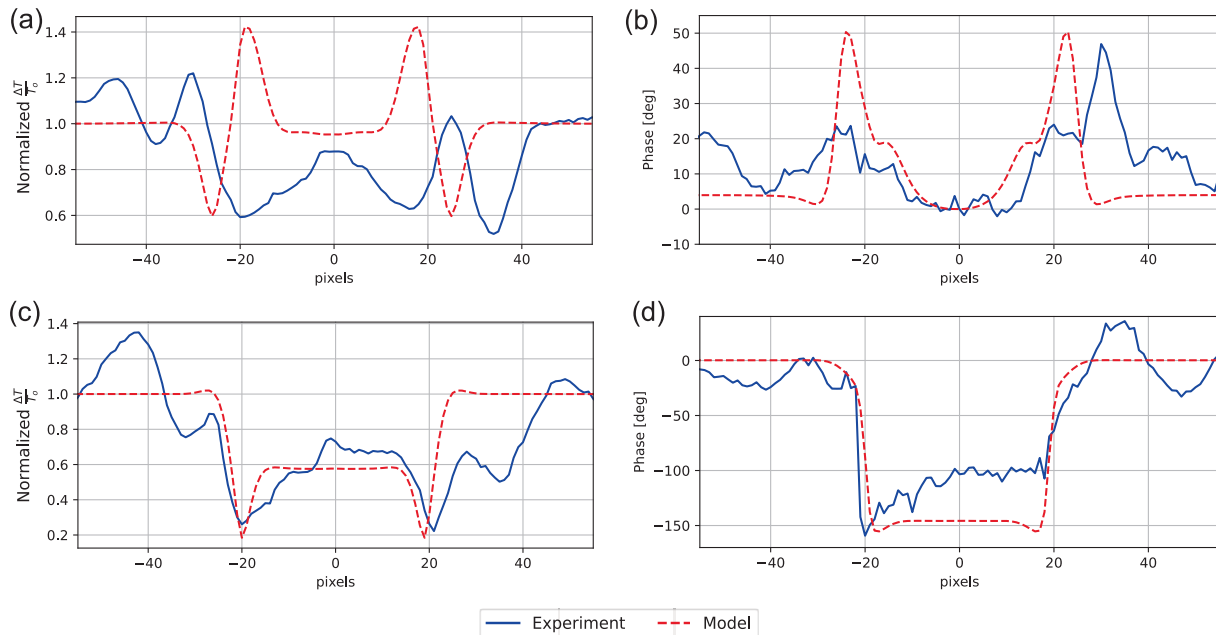


Fig. 9. $\frac{\Delta T}{T_0}$ and phase profiles extracted along the length of the specimen. Experimental data (blue solid lines) are compared with numerical predictions (red dashed lines) for (a, b) Frekote®-coated artificial defect and (c, d) uncoated steel inclusion.

evident at the lower defect tip in the experimental data. Similar observations have been made, e.g. [41], regarding the local increase in the phase values at the edges of delaminations, which were used to locate the crack tips and thereby identify the extent of the damage.

Again, the results indicate inconsistency in the artificial defect creation and manufacturing imperfections not accounted for in the idealized model. Further, the results suggest defect misalignment, variations in the actual insert length, or local material inhomogeneities (e.g., resin-rich regions). The comparison between the model and experimental data shows that, while the coating prevents the insert from fully bonding, some mechanical coupling may still occur, positioning the actual behaviour somewhere between a perfect delamination and a bonded inclusion.

For the IU specimen, both the $\frac{\Delta T}{T_0}$ plots (Fig. 9 (c)) and phase profiles (Fig. 9 (d)) show much better agreement than those given for the coated artificial defect. The model captures the pronounced reduction in the $\frac{\Delta T}{T_0}$ values in the central region of the specimen, while an exact match is inhibited by the fibre/resin heterogeneity that is not accounted for in the FE model. In contrast, the phase shift shows good agreement in both the magnitude and spatial extent of the defect. The 150° change in phase confirms the interpretation that the bonded steel insert generates an out-of-phase thermoelastic response due to the mismatch in the coefficient of thermal expansion between steel and CFRP.

The TSA results have shown that the uncoated steel insert is mechanically coupled to the surrounding laminate and experiences stress during loading, rather than acting as an independent, stress-free interface. Despite the differences between the experimental and numerical data, which can be attributed to factors such as material variability and manufacturing differences, it has been demonstrated that TSA has the potential to be used as tool for characterising the interfaces of artificial, subsurface defects.

5. Application of the IR thermography methodology to assess energy release in C-SPAR substructures

To demonstrate the practical utility of applying Eq. (6) to obtain the ERRs in a realistic complex structural component, the approach was applied on C-spar substructures developed for the CerTest project [30].

The C-spars were manufactured from IM7/8552 with a quasi-isotropic layup of $[+45_2/-45_2/90_2/0_2]_{3S}$ and tested under combined compression and bending. The dimensions of the C-spars are given in Fig. 10 (a). Prior to testing, the samples were bonded to steel end-blocks, which allow precise positioning in slots machined in a custom testing rig that was designed to enable the load to be applied eccentrically to induce a combined compression and bending load case. Full details of the specimen manufacture and the custom designed test rig are given in [31].

Two specimens with different artificial defect types were used as a case study. To generate the maximum difference in the interfacial conditions an uncoated steel inclusion was used and compared with a Teflon (PTFE) artificial delamination; the specimen with the Teflon artificial defect is termed C-SPAR-T and the specimen with the steel inclusion is termed C-SPAR-S. The artificial defect and inclusion were introduced during manufacture at a critical location in the corner of the spar, between the 9th and 10th plies. Fig. 10 (b) shows the C-spar mounted in the testing rig and the approximate defect location. Both samples were nominally identical in geometry, layup, and the same eccentricity was used. The main failure mechanism for both samples was unstable delamination initiating at the defect location.

Fig. 10 (c) presents the load–displacement curves for both samples tested at a displacement rate of 1 mm/min until failure. The load–displacement curves show that each C-spar exhibits the same initial stiffness and follows a similar nonlinear softening trend initiating at 150 kN. Despite this modest difference in peak load, C-SPAR-S also exhibited greater displacement at failure, suggesting that more energy was stored in the sample and potentially resulting in different dissipative failure mechanisms. Clearly Eq. (1) cannot be used to assess the energy release but, as Eq. (6) does not rely on the sample dimensions or a knowledge of local loading, the thermal imaging approach can be used to estimate the energy release. The C-spars were imaged using the Telops IR camera capturing images with a frame rate 150 Hz. Since, in both cases, the primary failure mechanism was unstable delamination starting from the artificial defect and the inclusion, the main condition for applying the proposed thermal evaluation method (Eq. (6)) was satisfied.

For both samples, the temperature evolution was tracked in a small ROI above the defect location, thus allowing a direct local measurement of the energy released immediately following the failure event. The

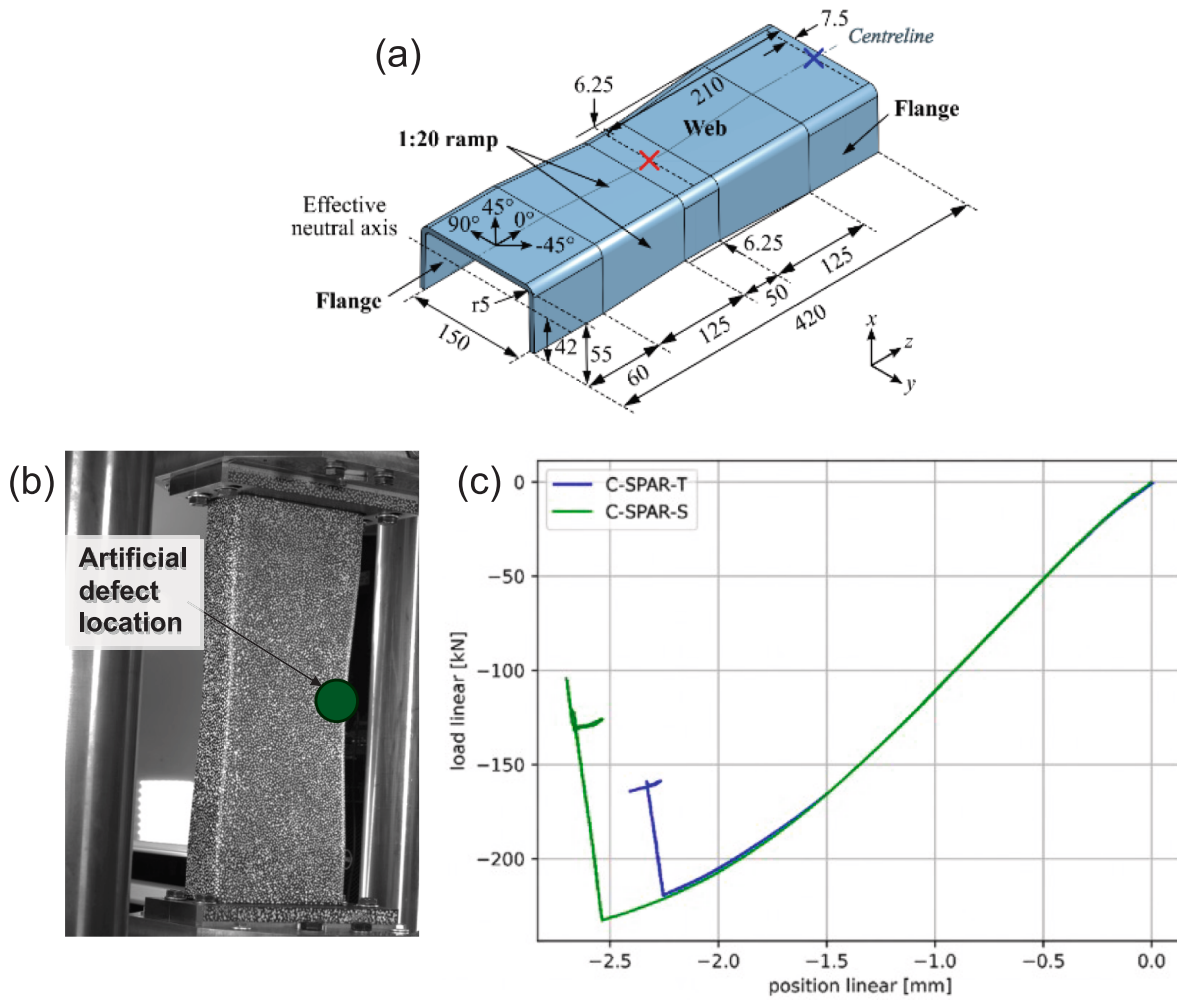


Fig. 10. (a) Schematic diagram of the C-spar specimen geometry [31]. (b) C-spar specimen mounted in the test rig with the defect location indicated, and (c) load-displacement curves for specimens C-SPAR-T and C-SPAR-S.

defect position was readily identified using IR-visible fiducial markers manufactured according to the methodology described in [42]. These

markers appear as four dots visible in both white light and infrared spectra, as seen in the centre of both Fig. 11 and Fig. 12. Fig. 11 presents

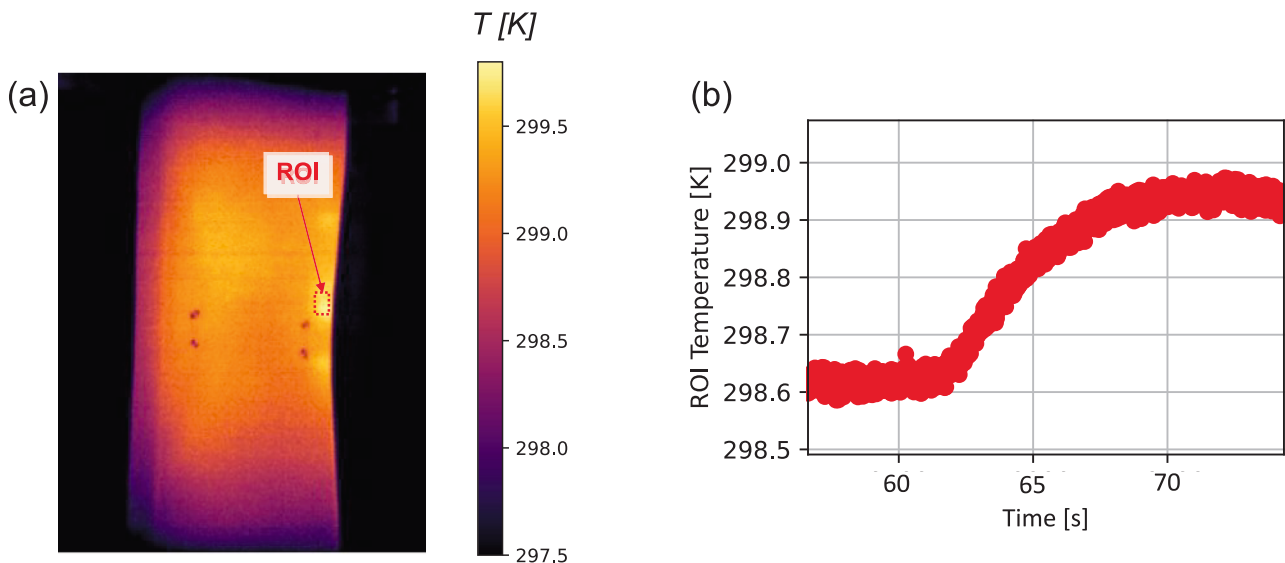


Fig. 11. C-SPAR-T (Teflon insert). (a) Post-failure thermogram showing the heat signature from damage propagation, and the selected ROI for temperature rise evaluation. (b) Temperature evolution in the selected region (temperature rise of approximately 0.32°C.).

the results for specimen C-SPAR-T. Fig. 11 (a) shows a post-failure thermal image with the heat signature from the propagating defect and the selected ROI, while Fig. 11 (b) shows the average temperature evolution in this region. The measured temperature increase was approximately 0.32°C . Fig. 12 shows corresponding results for specimen C-SPAR-S. A similar ROI was selected, and the temperature evolution is presented in Fig. 12 (b), showing a larger temperature increase of approximately 0.48°C . The fiducial markers are less visible on the righthand side of C-SPAR-S (Fig. 12 (a)) than in C-SPAR-T (Fig. 11 (a)) because during the rapid failure event shown by the step in temperature at 46 s in Fig. 12 (b) the region on the surface where the fiducial markers were located failed catastrophically.

It should be noted that the thermal image in Fig. 12 (a) also shows high-temperature regions on the left side of the sample, which likely indicate damage. However, one of the strengths of the proposed method lies in its ability to precisely obtain the local energy release. In this case, allowing an assessment of the energy required to initiate delamination propagation from the inclusion, while being insensitive to failure mechanisms occurring elsewhere in the specimen.

For both the artificial defect and the steel inclusion, the temperature approaches a steady-state condition after approximately 10 s, suggesting that thermal equilibrium through the specimen thickness has been achieved. This observation supports the applicability of the methodology to thicker structural components, which take longer to equilibrate than observed in the thin CCP coupons, where the thermal equilibrium was almost instantaneous.

There is a substantial difference in the released energy between the steel inclusion and the artificial defect. Despite the modest 5% difference in failure load, the steel insert (C-SPAR-S) caused an approximately 50% larger temperature increase θ , compared to the Teflon insert (C-SPAR-T) (0.48°C vs 0.32°C). This finding strongly aligns with the TSA results presented in Section 4, that uncoated steel inserts act as bonded inclusions rather than delaminations. In this case, the bonded steel insert evidently requires more energy to initiate fracture, as manifested by the greater local energy dissipation. As the insert has little bulk and is located in the interior of the specimen it was considered to have little effect on the temperature evolution. This substantial difference in energy dissipation contrasts with the results observed in Table 5 for the CCP test, where the bonded inclusion required a lower energy to initiate fracture. The difference is attributed to the test configuration and fracture initiation mechanisms. Unlike the CCP test, where the cut plies initiated failure, the insert provides the primary fracture initiation in the C-spar. Consequently, as the interfacial conditions of the insert have a direct effect on the failure initiation, it is of utmost importance that the insert accurately represents a delaminated interface, as with the Teflon.

Additionally, to support this assertion, C-SPAR-S failed in a more catastrophic manner, with visible damage in multiple regions, whilst C-SPAR-T failed locally to the defect region.

It is important to note that unlike the mode II evaluation validated for CCP specimens in Section 3, the C-spar configuration involves mixed-mode loading. Therefore, the measured temperature rise represents the total energy release from combined mode I and mode II contributions, rather than pure mode II fracture toughness. For the C-spar application, the θ measured at the surface serves as a comparative metric between insert types rather than an absolute quantitative measure of critical energy release rate. Nonetheless, the significant difference in thermal signatures between insert materials demonstrates the sensitivity of the technique and its value for evaluating failure behaviour in complex structural components.

6. Conclusions

The capability of artificial defects to replicate fracture behaviour in CFRP components has been assessed through experimental and numerical approaches by combining Digital Image Correlation, infrared thermography and finite element modelling. Steel film inserts ($5\text{ }\mu\text{m}$ thickness) were examined under both static and cyclic loading in Centre Crack Ply specimens, studying the effect of release agent application on the bonding of the insert in the laminate stack and the fracture toughness evaluation.

A novel methodology for evaluating interlaminar fracture toughness directly from transient temperature rise during unstable crack propagation was proposed and validated. The approach yielded fracture toughness values ($G_{IIc} = 1282\text{ J/m}^2$ for release agent-coated, 1222 J/m^2 for uncoated inserts) that are in excellent agreement with the traditional load-drop method (1271 J/m^2 and 1224 J/m^2 , respectively). This confirms that the Taylor-Quinney coefficient can be assumed to be close to unity for fast-propagating interlaminar fractures in brittle epoxy matrix composites, thus establishing confidence in thermal methods for energy release rate evaluation for fast propagating cracks.

Under monotonic loading, the presence of release agent on steel inserts produced only a modest difference in calculated fracture toughness ($\sim 60\text{ J/m}^2$), insufficient by itself to distinguish insert behaviour. However, it was shown that thermoelastic stress analysis, conducted when the specimens containing the artificial defects were subjected to low frequency cyclic loading (1.1 Hz), was able to characterise the differences in the interfacial behaviour between the laminate and the uncoated and coated inserts. A marked reduction in the thermoelastic response local to the uncoated insert, accompanied by a large phase

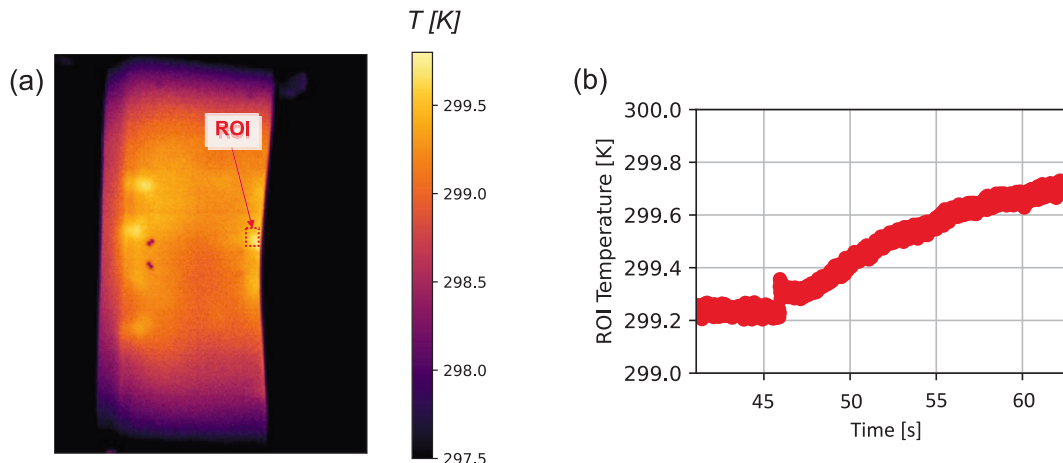


Fig. 12. C-SPAR-S (steel insert). (a) Post-failure thermogram showing the heat signature from damage propagation, and the selected ROI for temperature rise evaluation. (b) Temperature evolution in the selected region (temperature rise of approximately 0.48°C).

shift, indicated that the insert was acting as a bonded inclusion. As the insert was bonded, it was able to generate a stress-induced temperature change that was 180° out of phase with the response in the unidirectional carbon fibre. The steel has a positive coefficient of thermal expansion, whereas the CFRP has a negative value and at the relatively low loading frequency through thickness heat transfer allowed the response from the steel to be on the surface of the specimen. In contrast, the specimen with the coated insert showed a largely uniform phase distribution, indicating that the steel was unable to generate a response as it was not fully bonded in the laminate. However, comparison between experimental and numerical results strongly indicated that even with a release agent coating, some degree of mechanical coupling may occur, positioning the actual bonding state between the idealized extremes of a perfect delamination and a fully bonded inclusion. These findings highlight the potential of TSA as a tool for characterising the quality of bonded interfaces in CFRP laminates, being able to identify anomalies that occur during manufacture. Although further investigation is required, the work shows that utilising the through thickness heat transfer generated at low loading frequencies offers the possibility of probing below the surface of the laminated structure and assessing internal features, including crack propagation. Most importantly, TSA enabled a characterisation of the interface between the laminate and the artificial defect that was not evident from fracture toughness measurements alone.

It was demonstrated that an assessment of energy release rates using IR-based temperature evolution measurements can be applied to realistic structures. Studies of CFRP C-spars subjected to combined compression and bending loads successfully showed that differences between types of artificial defect could be identified. Results from two nominally identical C-spars, differing only in insert type (uncoated steel insert and Teflon film) showed only a 5% difference in failure load, whereas the local temperature measurements revealed ~ 50% higher energy release for the steel insert. Therefore, it was demonstrated that the thermal imaging approach provides spatially resolved energy release measurements, enables post-failure analysis of complex structures, and requires only basic material thermal properties (ρ , C_p) for quantitative comparisons, provided that the main failure mechanism is rapid propagating delamination. These capabilities position the methodology as a valuable tool for *in situ* energy release rate evaluation in realistic aerospace structures where standard coupon tests may not capture representative failure behaviour under complex loading scenarios.

CRediT authorship contribution statement

Riccardo Cappello: Writing – review & editing, Writing – original draft, Visualization, Validation, Software, Methodology, Investigation, Formal analysis, Data curation, Conceptualization. **Rafael Ruiz-Iglesias:** Writing – review & editing, Investigation, Data curation. **Spyros Spyridonidis:** Visualization, Software, Investigation, Formal analysis, Data curation. **Neha Chandarana:** Writing – review & editing, Supervision, Resources. **Tobias Laux:** Writing – review & editing, Supervision, Investigation, Data curation. **Janice M. Dulieu-Barton:** Writing – review & editing, Writing – original draft, Supervision, Resources, Project administration, Methodology, Funding acquisition, Conceptualization.

Declaration of competing interest

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

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