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A novel and robust method to quantify fatigue damage in fibre composite materials using thermal image analysis

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ABSTRACT

This work proposes a novel and robust thermal imaging-based method suitable to quantify damage evolution stages in fibre-reinforced composite materials under fatigue loading. The method uses adiabatic hysteretic heating of the material to calculate the change of enthalpy by thermal imaging when the material undergoes fatigue damage. This work analytically corroborates the validity of using the calculated change of enthalpy to identify the three different fundamental fatigue damage stages of the material. The method is applied to two different types of notched sandwich specimens subject to constant amplitude fatigue loading. It is practically demonstrated that the characteristic curve progressions of the mechanically measured hysteretic dissipation curves are in good agreement with those of the enthalpy change computed by the proposed method.

1. Introduction

Fatigue damage is one of the most important root causes of structural failure of composite structures such as aerospace structures and wind turbine blades. Structural health monitoring (SHM) is needed to provide necessary information about the structural integrity of such composite structures especially when they are in operation. It is important to develop non-contact, quantitative, non-destructive evaluation (NDE) methods to assess the damage state of composite structures under fatigue load. In the past half century, many studies have worked on SHM of composite materials and structures. Comprehensive review studies have been reported in an early work by Scott and Scala [1] and a more recent one by Wang et al. [2]. As this study only focuses on the application of thermography to the assessment of fatigue damage in composite materials and its underlying theoretical framework, the literature review in this work is limited to the studies in recent years. Toubal et al. [3] developed an analytical model based on cumulative damage to predict the damage evolution in woven composite laminates with holes and showed a good correlation between the dissipation of heat and the damage of the composites under fatigue. Montesano et al. [4] used infrared thermography to investigate the fatigue behavior of a carbon fibre reinforced polymer composite and identified a relationship between the dissipated heat, the intrinsic energy dissipation and the number of cycles to failure. Dattoma and Giancane [5] used a combination of digital image correlation (DIC) and thermography to investigate energy balance between heat sources and dissipation sources of notched composite laminates under fatigue loading. A noticeable work, although focusing on metals rather than composites, has been done by Naderi and Khonsari [6]. It presented an experimental approach to fatigue damage quantification based on thermodynamic entropy and showed that the cyclic energy dissipation in the form of thermodynamic entropy can be effectively utilized to determine the fatigue damage.

Palumbo et al. [7] proposed a method to assess the fatigue limit and the monitoring of damage in glass fibre composite materials by means of thermography. Similarly, Huang et al. [8] proposed a model to predict the fatigue limit of composite materials by combining stabilized temperature rising and normalized stiffness degradation in laminates. On a larger structural scale, Chen et al. [9] demonstrated the effective application of thermography to detect damages of a large-scale composite structure that was subject to fatigue loading. Chen et al. [10] further developed an automated approach, AQUADA, to qualify fatigue damage in large-scale composite structures using thermography and computer vision, showing that the damage evolution of composite materials quantified by thermography and computer vision agrees well with mechanically measured stiffness degradation and material damping. The current study explores the underlying thermodynamic theory of using thermography for fatigue damage assessment of composite materials.

As such, the contributions of this work to the current knowledge base are as follows:

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1. Provides a theoretical basis for the validity of using thermography to recover the fatigue damage states in fibre composite materials.
2. Provides a thorough experimental validation of the theoretical prediction.
3. Proposes a simple and robust numerical method to assess the high cycle fatigue damage stage from thermal videos.

This paper is organized as follows: Section 2.1 presents the experimental work of this study, where the composite specimens and the test set up are described in detail. Section 2.2 presents the theoretical framework used to describe mechanical energy, hysteretic energy dissipation and heat generation. Section 2.3 presents the numerical implementation of the developed method for damage assessment using thermal imaging. Section 3 presents the results of the comparative study, followed by a discussion in Section 4. Concluding remarks addressing the key findings and potential application of the proposed method are presented in Section 5.

2. Methodology

2.1. Experimental study

In this study, two different notched sandwich specimens were tested under uniaxial constant amplitude fatigue loading. The sandwich comprises two equally thick glass fibre/epoxy composite skin layers and a PVC foam core. The skins were made from biaxial glass fibre fabric and an epoxy resin. The material and manufacturing specifications are: Biaxial glass fibre fabrics from Saertex with an area weight of 980 g/m² and a fibre density of 2600 kg/m³. The epoxy resin from Araldite LY1568 and a hardener Aradur 3489 with a mix ratio of 100:28. Cross-linked PVC foam cores from AIREX C70 with closed cells with a nominal density of 60 kg/m³, a tensile modulus of 45 N/mm², a tensile strength of 1.3 N/mm² and thermal conductivity of 0.031 W/mK. It is noted that the foam is neither grooved nor perforated. The specimens were manufactured by vacuum infusion with a curing procedure of 19 h at 40 °C followed by 5 h at 75 °C.

The layup is [(±45)₂/Foam/(±45)₂]. The nominal outer dimensions of the specimens are 100 mm × 400 mm × 13 mm with a skin layer thickness of 1.5 mm and a PVC core thickness of 10 mm. The edges of the specimens were notched with a CNC milling machine in two different configurations referred to as 'symmetric edge cracked' (designation HGA05A-02) and 'asymmetric edge cracked' (designation HGA04A-03) geometry c.f. Fig. 1. In order to avoid grip failure inside the clamped regions, both ends of all specimens were reinforced with plywood inserts with a length of 50 mm. This was done by removing PVC foam material in the grip area with subsequent gluing of the plywood inserts with a nominal thickness of 10 mm.

An Instron test machine 8801 equipped with a Dynancell 2527-111 load cell with a load capacity of ±100 kN was used to test the specimens under fatigue. The free length of the specimen between the grips measured 280 mm. The test was conducted under tension–tension in force control with a peak force of F_{max} = 15 kN, R = 0.1 and a harmonic loading frequency of 3 Hz. The corresponding nominal stress range (i.e., with respect to the total cross-sectional area) was Δσ = 10 MPa. A thermal camera Optris P640 (480 × 640 pixel) and an RGB camera Nikon D7200 (4000 × 6000 pixel) were used to take thermal images and visual images respectively. Both cameras were located approximately 55 cm remote from the specimen surface. The thermal camera is located.
directly in front of the specimen, perpendicular to the front face of the specimen, while the RGB camera was located next to the thermal camera, see Fig. 2. The test was continued until a complete fracture of the specimen occurred.

Regarding key measurement data and post-processing procedure: a measure of the hysteretic energy dissipation is the enclosed area of the so-called hysteretic loop that forms in a cyclic load–displacement history plot as depicted in Fig. 3. The hysteretic energy flux \( \mathcal{H} \) was calculated from the force \( F \) measured in the load cell and the corresponding crosshead displacement \( u \) for all data points \( k \) pertaining to one individual loop by using the trapezoidal rule as follows:

\[
\mathcal{H} = \frac{1}{2} \sum_{k=1}^{m} (F_{k+1} + F_{k})(u_{k+1} - u_k) \quad \text{if} \quad k = m \quad \text{then} \quad F_{m+1} = F_1 \quad \text{and} \quad u_{m+1} = u_1
\]  

(1)

In the present case, Eq. (1) was numerically evaluated in the clockwise direction (following consecutive timestamps) for a number of \( m = 250 \) data points contained in one hysteresis loop, c.f. Fig. 3.

### 2.2. Theoretical framework

The theoretical framework presented in this section rests on the following assumptions:

- The change in enthalpy over time corresponds to the change in hysteretic energy dissipation
- The Biot number in the presented problem is small which justifies the use of a lumped mass model
- Radiative- and conductive heat transfer are negligible in the presented problem

Mechanical material fatigue damage is associated with hysteretic energy dissipation. Fig. 4 qualitatively depicts the typical features of the hysteretic energy dissipation function over time, which bear characteristic information on the fatigue damage evolution stage currently prevailing in the material [11]. Fig. 4 shows that the entire fatigue life span of a fibre composite material can be subdivided into three main stages: Initiation (Stage-1) with negative curvature, constant growth (Stage-2) with a constant slope and fast growth (Stage-3) with positive curvature. A predominant amount of external mechanical work the test machine induced into the specimen is transformed into heat due to frictional effects on: (i) a molecular level, (ii) a micro level due to friction between matrix crack surfaces and during debonding in the matrix-fibre interface area and (iii) a macro level between the rough surfaces of large debonded regions. These frictional effects can be considered as intrinsic heat sources that - owing to the low thermal conductivity - are locally heating up the material with a pronounced temperature gradient which is commonly referred to as hot-spot. Energy dissipation of mechanical work into heat is therefore a manifestation of damage accumulation in fatigue-loaded specimens. Since heat is a fundamental physical entity that can accurately be quantified, the authors suggest its use to assess the location of the damage and the severity of the damage.

In this work, the term damage is used as a hypernym. Damage in fibre composite materials passes through three archetypal stages each acting on different characteristic length scales spanning from molecular level over microcrack evolution to discrete macroscale cracks c.f. Fig. 4. The latter ultimately manifest themselves as debonding or delamination cracks. It is for the method proposed in this work per se ineffectual whether the different stages are treated within the realm of continuum damage mechanics or fracture mechanics. It is noteworthy to mention

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**Fig. 2.** Experimental setup of the fatigue test; notched sandwich specimen installed in hydraulic grips and a load cell coupled to the upper grip; thermal camera and an RGB camera taking images at different fatigue cycle numbers according to the evolution stage; a vacuum extraction system was used to collect the debris and to provide air-cooling.

**Fig. 3.** The hysteretic loop formed in a load–displacement diagram and the dissipated energy constituted by the enclosed area hatched in blue; data points indicated as black dots used to integrate the dissipated energy in Eq. (1). (For interpretation of the references to colour in this figure legend, the reader is referred to the web version of this article.)

**Fig. 4.** Typical development of fatigue damage in fibre composite materials under cyclic loading reflected in the hysteretic energy dissipation curve. The dissipated energy per cycle is the integrated area enclosed by one complete hysteretic loop (blue) depicted in a load–displacement (F–u) diagram. (For interpretation of the references to colour in this figure legend, the reader is referred to the web version of this article.)
that Fig. 4 should not be confused with the well-known Paris-Erdogan law for discrete cracks. However, it should be emphasized that in all three stages, the cyclic loads induce heat through material damage entailed by the different physical processes prevailing on the different characteristic length scales – it is the heat induction that is of central importance for the proposed method.

While the hysteretic dissipation is rather straightforward to obtain under laboratory conditions where load input and the associated displacement can be accurately measured, this hardly the case for large-scale composite structures serving in real-life conditions. It should however be possible, to infer the damage state to a sufficient degree of accuracy from the thermal response of the material inside the structure.

The determination of the portion of converted heat is a complex matter and depends on a series of different coupled conditions such as material, loading, frequency, etc. but can be grossly estimated in the order of 90%. However, this energy can be considered as a source of internal heat increasing the enthalpy of the specimen. The change in enthalpy $\Delta h$ is proportional to the hysteretic energy dissipation and hence directly proportional to the temperature variation during damage evolution. From this chain of causality, the following conjecture can be postulated: ‘The fatigue damage evolution stage in a glass/epoxy composite material can be inferred from the enthalpy change calculated from temperature measurements.’

To corroborate the conjecture, an analytical model is used to demonstrate that the characteristic hysteretic energy input is qualitatively preserved in the enthalpy change obtained from thermal imaging. The damage induced in the specimens by mechanical cyclic loading evolves symmetrically in both skin layers where the PVC foam core with closed cells acts as a potent isolator. Therefore, with good approximation the heat transfer problem is symmetric. It is generally accepted that a lumped model [12,13] can be used in the case of a small Biot number $\ll 0.1$ defined as follows:

$$ \text{Bi} = \frac{hV}{kA} = \frac{hV}{kL} $$

(2)

The Biot number represents the internal temperature gradient compared to temperature change at the surface of the body which in the present case is in the order of $10^{-8} \ll 0.1$. If both the $V/A$ ratio and the $h/k$ ratio are small, this implies that the through-thickness temperature gradient inside the material is negligible and hence the temperature is uniform through the thickness. Furthermore, by assuming a uniform temperature in the damaged area of the skin, the whole heat transfer problem can therefore be reduced from a partial differential equation to a linear ordinary differential equation, describing the lumped model as a function of time only.

It is desirable to simplify the thermal model by utilizing the fact that the radiative heat transfer is proportional to the fourth power of the temperature difference between the body and the environment. Due to the small temperature difference in the present case, the effect of radiative heat transfer is proportional to the fourth power of the temperature difference between the body and the environment. Due to the radiative heat transfer being negligibly small compared to convective heat transfer. Furthermore, the thermal conductance of the body along the length measuring between the grips is insignificant because of the following circumstances: (i) the thermal conductivity of the foam core material is negligible (ii) the thermal conductivity of the skin material is low $0.4 \text{ W/mK}$ and (iii) the skin layer thickness is small $1.5 \times 10^{-3} \text{ m}$. For these reasons, only the convective heat transfer governs the analytical thermal model.

For the lumped capacitance model, it is assumed that an intrinsic adiabatic heat source is present inside the skin material indicated as a red ellipse in Fig. 5, which heats the skin uniformly. The intrinsic heat source is caused by hysteretic dissipation of mechanical energy per unit of time $d(t)$ which in this model is given in the polynomial form. The incremental conservation of energy flux for the lumped system described above is expressed as follows:

$$ \rho c_p V \frac{dT}{dt} = -2hA(T(t) - T_e) + q(t) $$

Eq. (3) can be re-written into an inhomogeneous linear first-order ordinary differential equation taking the following form:

$$ \frac{dT}{dt} + \frac{2h}{\rho c_p V} (T(t) - T_e) = \frac{q(t)}{\rho c_p V} $$

Substituting the time constant $\tau = \frac{hV}{\rho c_p A}$ and the input function $f(t) = \frac{q(t)}{\rho c_p V}$, Eq. (4) can be cast into the standard form, notably

$$ \frac{dT}{dt} + \tau (T(t) - T_e) = f(t) $$

The complete solution for Eq. (5) can be written as the sum of the homogeneous solution $T_h(t)$ and the particular solution $T_p(t)$. The homogeneous solution of Eq. (5) can straightforwardly be obtained by direct integration and is readily given by Eq. (6) [14]:

$$ T_h(t) = T_e + \alpha e^{-\alpha t} $$

The right-hand side hysteretic heat source function is stipulated as a complete $n$th-order polynomial:

$$ f(t) = \frac{q(t)}{\rho c_p V} = c_0 + c_1 t + c_2 t^2 + c_3 t^3 + \cdots + c_n t^n \quad \forall n \in \mathbb{N} $$

The ansatz is hence, as well chosen to take the form of an $n$th-order polynomial. The particular solution can be obtained with the method of undetermined coefficients [15]; by omitting the intermediate steps for the sake of brevity, the particular solution is given as follows:

$$ T_p(t) = \frac{1}{\tau} \int_0^t f(t) dt = \frac{1}{\tau} \left( \frac{1}{\tau} \int_0^t f(t) dt \right) $$

The complete solution of the ordinary differential equation can eventually be obtained by adding Eqs. (6) and (8) as follows:

$$ T(t) = T_e + \alpha e^{-\alpha t} + \sum_{n=1}^{N} \left( -1 \right)^{n-1} \frac{1}{\tau^{n-1}} \frac{d^{n-1}}{dt^{n-1}} f(t) $$

The integration constant $\alpha$ is obtained by satisfying the initial condition $T_{h,0} = T_0$ which yields the following expression:

$$ \alpha = T_0 - T_e - \sum_{n=1}^{N} \left( -1 \right)^{n-1} \frac{1}{\tau^{n-1}} \frac{d^{n-1}}{dt^{n-1}} f(t)_{t=0} $$

Fig. 5. Skin layer of the specimen with width $w$ and thickness $t$ with the damaged zone indicated by the red boundary. The damaged area is discretized with small volume elements each having a uniform temperature whose in-plane size is proportional to the pixel size. The volume can be obtained by the specimen width and the corresponding image resolution $N$ in the same direction. (For interpretation of the references to colour in this figure legend, the reader is referred to the web version of this article.)
Eq. (9) shows that with increasing time, the first – transient – term decays away and the second term i.e. the particular solution prevails. Meaning, that in the long run – i.e., for $t \gg \tau$- eigenperiod of the load cycle - the hysteretic input heat dominates the thermal response. In fact, it can be seen from Eq. (8), that $f(t)$ in the first term dominates the particular solution and the contribution of the successive terms containing its derivatives become decreasingly influential with increasing order of the derivative.

Eqs. (9) and (10) conclude the derivation of the lumped capacitance model and we proceed with the presentation of the proposed thermal imaging method. Eq. (9) proofs that the signature of the change in enthalpy is strongly associated with the signature of the hysteretic heat input produced by mechanical fatigue damage. This suggests measuring the change of enthalpy in the specimen defined in general as follows:

$$\Delta h = \rho c_v V \Delta T$$  \hspace{1cm} (11)

According to Eq. (11), measurements of the change in temperature $\Delta T$ are required to obtain the desired enthalpy change, which can be done using a thermal camera. The latter measures the surface temperature $T$ of the specimen in each thermal image frame assuming a uniform through-thickness temperature distribution. Due to the adiabatic heating conditions mentioned at the beginning of this section, the measured temperature of the hotspot reflects the level of mechanical damage in the hotspot location (the damaged area). Fig. 5 shows that the skin surface in real space can be discretized into a finite number of sufficiently small volume elements corresponding to the number of pixels in the thermal image space; the temperature in these small volume elements is quasi-uniform.

Consequently, Eq. (11) can be re-written as an accumulation of the enthalpy change for each volume element at thermal image frame $j$ expressed as follows:

$$\Delta h_j = \rho c_v V_{pix} \sum_{i=1}^{n} (T_{ij} - T_s)$$  \hspace{1cm} (12)

In Eq. (12) conservation of mass holds since the debris material loss due to fatigue damage is minor compared to the total skin material mass. Furthermore, the specific heat capacity of a solid is hardly temperature-dependent for which reason it is assumed that $c_v = \text{const}$. Please note that $T_{ij}$ in Eq. (12) denotes the pixel temperature $i$ of frame $j$ and should not be confused with a derivative in Einstein (index-) notation.

The volume $V_{pix}$ can be obtained from the image space using the pinhole camera model [16] under the proviso that both the image plane and the specimen plane are parallel and the region of interest is close to the optical axis of the thermal camera. These conditions were fulfilled in the experimental setup. It can therefore be assumed with sufficient approximation that (i) the size of a pixel in the image space is linearly proportional to the length in the real space and (ii) that the proportionality is isotropic i.e. the same in the horizontal and vertical direction. Therefore, the volume $V_{pix}$ can be calculated by a calibration procedure using the following relationship:

$$V_{pix} = t_i \left( \frac{w}{N} \right)^2$$  \hspace{1cm} (13)

2.3. Numerical framework

Eq. (12) was implemented in the commercial software package MATLAB [17]. The enthalpy change is calculated in a loop through all image frames according to the following procedure:

1. Extracting the thermal image frames from the thermal video
2. Converting the region of interest i.e. the specimen area between the grips into greyscale. The greyscale values were subsequently converted into pixel temperatures by linear interpolation and the information of the temperature scale bar.

3. The hotspots in each frame are identified by finding the indices of the maximum temperature values in the matrix and adding them to a so-called evaluation set.
4. Calculation of the enthalpy change per frame according to Eq.12 and 13 for the evaluation set by using the first frame as the reference temperature. The enthalpy per frame is calculated by summing the enthalpy contributions of each pixel over the damaged area according to Eq. (12).

Fig. 6(a) shows the greyscale image (procedure point 2) at approx. half the lifetime of the specimen. By choosing two reference temperatures $T_{\text{min}}$ and $T_{\text{max}}$ retrieving the corresponding greyscale values $G_{\text{min}}$ and $G_{\text{max}}$ from the scale bar (not shown) it is possible to compute the pixel temperature using linear interpolation with the slope $k = T_{\text{max}} - T_{\text{min}} / G_{\text{max}} - G_{\text{min}}$ and the offset $o = T_{\text{min}} - G_{\text{min}}k$. Fig. 6(b) shows the evaluation set (procedure point 3) for which the enthalpy is calculated (procedure point 4).

3. Results

Fig. 7 shows two different solutions of Eq. (9) with the thermal material properties for glass fibre epoxy composites taken from [18] and reproduced herein as $c_v = 903 \text{ J/kgK}$ and $\rho = 1835 \text{ kg/m}^3$. The dimensions of the body were chosen according to the specimen geometry notably $l = 2.8 \times 10^{-1}$ m, $w = 0.1 \times 10^{-1}$ m, and $t_s = 1.5 \times 10^{-3}$ m. The coefficients of the third-order polynomial hysteretic heat input are $c_3 = 3.159 \times 10^{-13}$, $c_2 = -2.275 \times 10^{-9}$, $c_1 = 5.896 \times 10^{-6}$ and $c_0 = 7.778 \times 10^{-3}$ and have been chosen to resemble the experimental conditions.

Fig. 7(a) and (b) shows that both the enthalpy change and the hysteretic heat input follow a similar trend corroborating the conjecture that the mechanical fatigue damage stage and the enthalpy change are indeed strongly affiliated. Consequently, all three stages namely initiation growth (Stage-1), constant growth (Stage-2), and fast growth (Stage-3) can be determined from the enthalpy change.

Fig. 8 shows the symmetric double edge cracked sandwich specimen at the onset of Stage-2 growth and close to ultimate fatigue failure at the end of Stage-3 (c.f. Fig. 10(a)). The thermal image shows the evolution of the damaged regions initiating at the notch root under 45° and eventually leading to ultimate shear failure after 29,303 cycles. In Stage-
close to failure the maximum hotspot temperature reached approx. 40 °C.

Fig. 9 shows the asymmetric double edge cracked sandwich specimen at the onset of Stage-2 growth and close to ultimate fatigue failure at the end of Stage-3 (c.f. Fig. 10(b)). The thermal image clearly shows the damaged regions initiating at the notch roots under 45° with whitening in the visible light spectrum. The specimen failed after 14,890 cycles in a global shearing mode.

Fig. 10 shows that the computed enthalpy change from thermal imaging closely resembles the shape of the hysteretic input similarly to the theoretical response depicted in Fig. 7. Comparison of the curves shows that the enthalpy change reproduces the negative curvature in the damage initiation phase (Stage-1), the constant slope with close to zero curvature during constant growth (Stage-2) as well as the exponential increase with positive curvature (Stage-3). The noise seen in the enthalpy curve is most likely caused by the thermal camera sensor. The deviations between the slopes of the enthalpy curve (black) and the input function (blue) in Stage-2 are caused by the different convective cooling conditions of the specimen surface in the laboratory during testing.

4. Discussion

The geometrical and material properties allowed the generation of a lumped capacitance model, which allows the reduction of a partial differential equation into an ordinary differential equation providing a convenient means to shed light on the thermal response. It is at first glance counterintuitive to compare the enthalpy change, which is accumulative over time with a cycle-by-cycle hysteretic energy input. However, the solution of the ordinary differential equation shows that the signature of the hysteretic heat input function is preserved in the externally measured enthalpy if the gradient of the heat input flux is moderate and if the time span is long enough for the transient to die out. These conditions can cautiously be termed as ‘steady state’ and are quite typical for large composite structures subject to cyclic loading conditions such as large utility wind turbine rotor blades. The enthalpy change is a strong function of the convective heat transfer coefficient itself highly dependent on the flow conditions in the vicinity of the
surface. The latter depends on the Nusselt number and consequently on the Reynolds number. Therefore, the convective heat transfer coefficient is a function of the fluid velocity and viscosity, which is not a trivial task to determine. On the other hand, the exact value of the heat transfer coefficient is not required for the proposed method to recover the fatigue damage stages from the thermal response.

A sensitivity study on the hot-spot identification has shown that despite the magnitude, the shape of the enthalpy change is rather insensitive towards the pixel sampling method. Even an evaluation window encompassing the entire specimen has recovered the desired shape as long as all the hotspots are consistently contained within the pixel evaluation set. The preservation of the signature of the hysteretic input allows a qualitative assessment of the fatigue damage evolution stage inside the material providing a reliable means to judge the structural health state as part of structural health monitoring.

In the proposed method, all the required geometrical and thermal properties of the structure can be obtained with high accuracy and low uncertainty. This renders the proposed method a robust, simple and reliable one. In addition, thermal imaging is a mature and cost-efficient technology making long-term thermal measurements of structures affordable. The corroborating evidence in this work facilitates a faithful real-time fatigue damage stage assessment of composite structures from thermal image analysis.

It is noted that the proposed method might not work as accurately in the case of thick laminates in which case adiabatic heating conditions do not hold. In the latter case, the temperature varies through the thickness, therefore using the measured surface temperature to calculate the enthalpy reduces the accuracy of the calculation. In this case, a 3D heat conduction problem should be solved which adds complexity but is theoretically possible. On the other hand, it can be argued that the method is still applicable to large-scale thin-walled composite structures such as aerospace structures and wind turbine blades, as the assumption of approximately small variation of temperature through the wall thickness can be held with reasonable accuracy.

Although quantitative knowledge of the heat transfer coefficient is not required for the proposed method, in its current formulation the convective heat transfer coefficient should be fairly constant over time. While this is more likely the case under indoor laboratory conditions, this requirement is hardly fulfilled under outdoor conditions. This means that long-term thermal measurements of large-scale composite structures need to be corrected for the prevailing convective heat transfer coefficient which will be a function of the environment such as
the weather conditions e.g. wind speed and local turbulence conditions for wind turbine blades. One possible approach could be a calibration of the method for different airflow conditions as part of future research activities.

5. Conclusions

The following conclusions can be drawn from this work:

- Proposition for a thermal imaging method to quantitatively assess fatigue damage in fibre composite materials.
- Good agreement between mechanical fatigue test data and the proposed thermal imaging method.
- Fatigue damage stages can be assessed by computing the change of enthalpy in the specimen using a thermal camera.
- The particular solution of the thermal response prevails under ‘steady state’ conditions which are closely affiliated with the hysteretic heat input.
- The method is particularly useful to determine the fatigue damage stage in fibre composite materials and structures as part of non-contact, robust structural health monitoring techniques.

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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