Identification of the source of the thermoelastic response from orthotropic laminated composites

S. Sambasivam, S. Quinn and J.M. Dulieu-Barton
School of Engineering Sciences, University of Southampton, Highfield,
Southampton SO17 1BJ, United Kingdom
sshamala@soton.ac.uk

SUMMARY

In previous work, a series of theoretical considerations have been made aimed at identifying the source and assessing prominent factors influencing the thermoelastic response from laminated composites. In this paper four different methods of interpreting the data are investigated and the theoretical thermoelastic response is compared to experimental data to identify the source of the thermoelastic response.

Keywords: Thermoelastic stress analysis (TSA), Glass fibre reinforced polymer (GFRP)

Introduction

Thermoelastic stress analysis (TSA) is based on infra-red thermography, where the small temperature changes resulting from changes in elastic stresses are obtained by measuring the change in infra-red photon emission. The technique has advantages over other experimental techniques through its ability to provide full-field, non-contact stress data from structures under dynamic load in practically real time. A full understanding of the effect of the fundamental uncertainties, such as the source of the thermoelastic signal and the influence of the applied loading conditions, on the response is critical if quantitative stress data is to be extracted from the thermal measurements.

Previous research [1] has shown that the interpretation of the thermoelastic response from orthotropic composite materials requires special consideration. The influence of the surface resin rich layer that occurs in composite components as a consequence of the manufacturing process has been investigated and was used as a basis for calibration. The work [1] showed that for E-glass/epoxy pre-preg laminates consideration of the surface resin layer as the source of the response does not provide satisfactory agreement. In the current paper it is shown that the ‘strain witness’ assumption [2, 3] is only valid for composite laminates with sufficient surface resin thickness to prevent heat conduction. To interpret the thermoelastic response from an orthotropic laminated composite material the usual approach [4, 5, 6], is to assume that the response is a function of the surface ply stresses and their associated coefficients of thermal expansion (CTE). In this work a further two theoretical approaches are explored; (i) the thermoelastic response of the laminate is governed by its global mechanical and thermal properties, and (ii) a combination of the ply by ply mechanical properties combined with global thermal properties. This gives four possible means of interpretation, all of which are discussed in the paper.

Influence of the surface resin rich layer on thermoelastic response

In previous studies [2, 3] it has been assumed that the response is from the surface resin layer, it is said to act as a 'strain witness'. Therefore the resin must be such that it prevents the temperature change that occurs in the surface ply from conducting through the resin to the material surface. To act as a strain witness the surface layer must be thin compared to the thickness of the specimen so that the laminate strains are fully transmitted from the surface ply to the surface of the resin. If the resin is acting as a strain witness then the strain in a given direction in the resin layer is equal to the strain in the same direction in the laminate so the sum of the principal strains in a composite laminate can be related to the stresses in the resin layer as follows:

$$\varepsilon_{xc} + \varepsilon_{yc} = \frac{1-\nu_r}{E_r}(\sigma_{xr} + \sigma_{yr}) \tag{1}$$

where E is Young's modulus and ν is Poisson's ratio, σ_x and σ_y are the change in the principal stresses and the subscript c and r represent composite and resin respectively.

The temperature change, ΔT in the surface resin layer is related to the measured strain by substituting the right hand term in Equation (1) to give:

$$\Delta T = -\frac{\alpha T}{\rho C_p}(\sigma_x + \sigma_y) = -\frac{\alpha_r T}{\rho_r C_{Pr}}\left[\frac{E_r}{1-\nu_r}(\varepsilon_{xc} + \varepsilon_{yc})\right] \tag{2}$$

where T is the surface temperature, ρ is the density and C_p is the specific heat at constant pressure, α is the coefficient of thermal expansion (CTE) in the principal stress directions.

For an orthotropic material the stresses are coupled with CTEs in the direction of interest as follows[4]:

$$\Delta T = -\frac{T}{\rho C_p}(\alpha_x \sigma_x + \alpha_y \sigma_y) \tag{3}$$

It would therefore be very convenient to be able to apply the strain witness assumption to composite components so that the coupling between the stresses and the CTE can be neglected in the analysis.

Equation (2) shows that for laminates made with the same resin with different lay-ups, the thermoelastic temperature change from the surface resin layer should be the same if the sum of the direct strains is constant. Previous work by the authors [1] has shown that this is not the case. It should be noted that the stresses in the orthotropic layers of a composite laminate vary with fibre orientation and moreover it is certain that the stress carried by the resin surface layer will be small compared to that of laminate. This means the stress induced temperature change in the resin surface layer will be different to that in the surface ply of the laminate and depending on the ply orientation may cause large temperature gradients between the surface layer and the orthotropic substrate. Heat transfer between the resin and the laminate and vice versa is therefore a distinct possibility. The measured surface temperature changes could therefore be a result of the ‘strain witness’ effect, a result of heat transfer through the resin giving a response from the surface ply, or a combination of both. The response is clearly dependent on the thickness of the surface resin and the orientation of the subsurface ply.

In TSA, a cyclic load is applied to achieve pseudo adiabatic conditions. The question is at what frequency is heat diffusion through the resin rich surface layer prevented so that it can act as a strain witness. To address this, an aluminium strip specimen of dimensions 105 x 13 x 1.2mm was prepared with part of the surface coated with epoxy resin (60 μm thick) and the other part coated with two passes of RS matt black paint. This specimen was devised as it is impossible to remove the epoxy layer effectively from the surface of a composite laminate. Furthermore, it is extremely difficult to manufacture a fibre reinforced polymer composite without a surface resin layer, particularly if a vacuum consolidation is used. Therefore it was decided to add a resin layer to a simple specimen. To examine if at practical laboratory loading frequencies adiabatic conditions could be achieved the specimen was subjected to a constant uniaxial stress range of 52.2 MPa whilst the loading frequencies was varied from 10 to 60 Hz. Under pure tension loading, the stress is uniform and therefore no heat transfer in the specimen, enabling the diffusion characteristics of the coatings to be examined in isolation. If the response is constant over the frequency range this is a good indication of adiabatic behaviour.

The temperature changes measured from the painted and epoxy coated parts of the specimen are shown in Figure 1. Using the material properties for the aluminium given in Table 1 it is possible to derive a theoretical temperature change for the aluminium; this is shown in Figure 1. It is also possible to derive the temperature change for the epoxy acting as a strain witness using the values given in Table 1. It is assumed that the emissivity of the paint and the epoxy is 0.92 [7, 8]. The response from the painted surface is constant over the frequency range and virtually identical to the calculated ΔT value. This is a clear indication that the response is adiabatic and the paint coating is sufficiently thin to allow complete heat transfer from the surface of the aluminium to its surface. The measurements from the resin do not correspond with the calculated value for the strain witness response and show a monotonic decrease over the frequency range. As the frequency increases the values approach the strain witness value. However, at lower frequencies these results indicate that there is heat transfer from the interface between the epoxy and the aluminium to the surface of the resin. As the resin layer is usually much thinner than 60 μm in a polymer composite this clearly indicates that the orthotropic surface layer has a significant role in the thermoelastic measurements.

To apply the findings of the above to composite laminate, a 2D heat transfer model was constructed to determine the thickness of the surface resin required to prevent heat conduction in order for the ‘strain witness’ assumption to be applicable. The model was constructed using ANSYS with PLANE55 thermal elements. The model showed that a loading frequency above 33 Hz is required for the ‘strain witness’ treatment to be applicable. This finding is somewhat supported by the data in Figure 1 as at around 30Hz the response seems to become uniform. However the difference between the experimental and calculated value could be attributed to differences in mataterial properties and the emissivity. The same model was implemented for a unidirectional laminate (to consider the resin layer and composite material interface). The initial uniform temperature in the surface layer was calculated using Equation (2) and at the interface, using Equation (3), see Table 4. The FE results showing the relationship between the resin thickness and the loading frequency for thermal equilibrium to be achieved in the surface resin layer for UD(0) and UD(90) are shown in Figure 3. This clearly shows that for a loading frequency of 10 Hz, for the given temperature gradient the resin layer thickness should be minimum of 90 μm for UD(0) and 100 μm for UD(90) so that the surface measurements are not affected by the heat transfer. The thickness of the resin layer for the specimens used in this work is 30 μm according to Figure 3, a loading frequency of approximately 67 Hz for UD(0) and 112 Hz for UD(90) are required to achieve adiabatic conditions for the 'strain witness' treatment to be valid. This shows that for the material considered in this work (considering the temperature gradient between the interface and surface layer), the result of heat transfer through the resin gives a response from the surface ply. Therefore, in the next section in the paper explores fully the interpretation of the fibre orientation on the thermoelastic response.

Table 1: Mechanical and physical properties of unidirectional E-glass/epoxy pre-impregnated composite, epoxy and aluminium

Specimen	Young's Modulus (GPa)		Poisson's ratio		Density, ρ (kg/m^3)	CTE, (x 10^{-6}/^{o}C)		Specific heat capacity, C_p (J/(kg^{o}C))	Thermal Conductivity k (W/m^{o}C)
	E_1	E_2	ν_{12}	ν_{21}		α_1	α_2		
UD	34.2	10.0	0.325	0.100	1230	9	31	843	-
Epoxy	4.2	n/a	0.413	n/a	1207	52	n/a	1230	0.175
Aluminium	68	n/a	0.330	n/a	2700	21	n/a	900	-

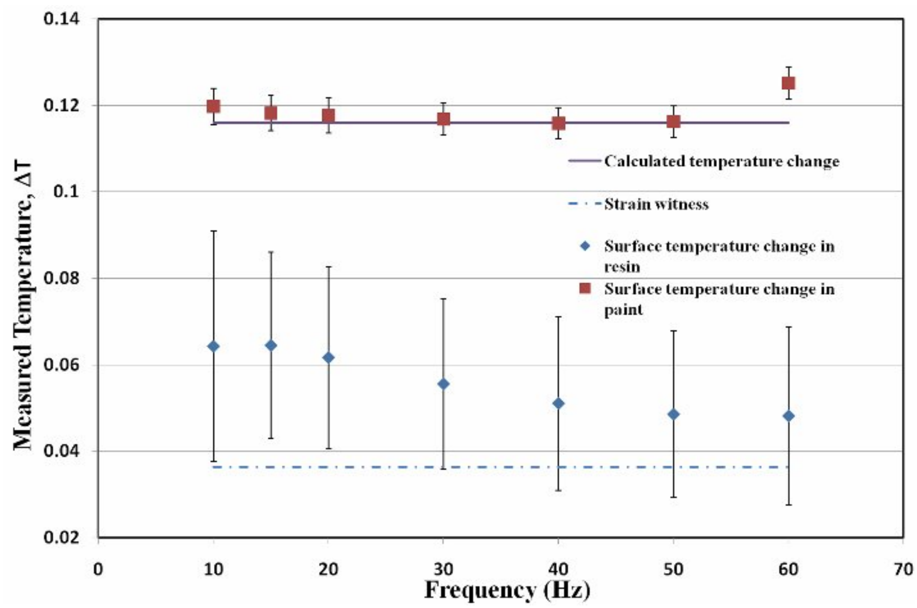

Figure 1: Change in the surface temperature of aluminium coated with an epoxy layer and paint coating

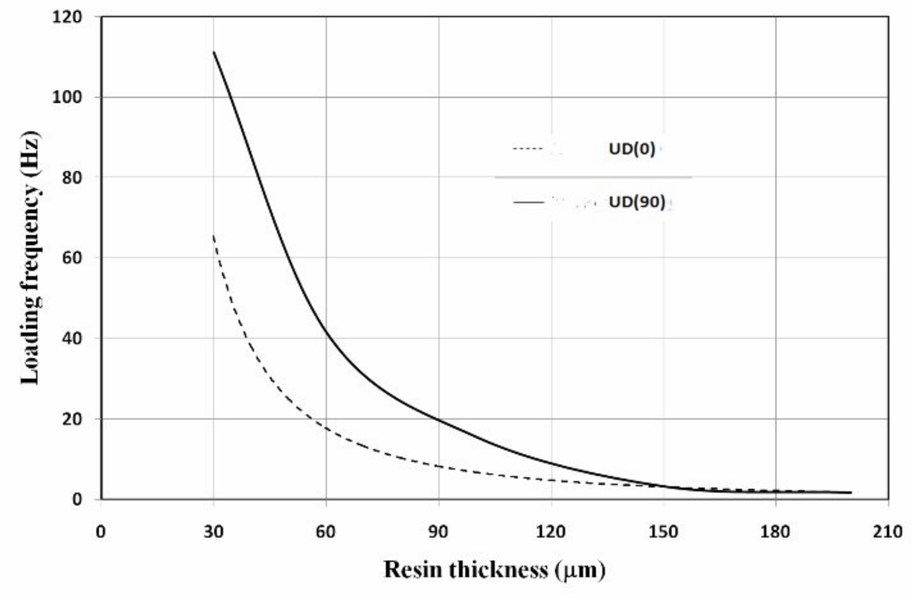

Figure 2: The relation between the thickness of the resin rich and the required loading frequency to achieve adiabatic conditions (from 2D FE analysis)

Thermoelastic response of a composite lamina

In most studies of composite materials using TSA [4, 6] it has been the case that the materials examined and the loading are such that the material axes (1, 2) (i.e. axis system referenced to the fibre direction) coincide with the principal stress axes (x, y) in the lamina, see Figure 3(a). However, when a lamina is subjected to general stress at an angle θ in relation to the principal material direction, the principal stress axes will not coincide with the material axes (see Figure 3(b)). The temperature change measured in TSA is a scalar physical quantity that is independent of the system axis. Therefore an expression of the ΔT in one axis system (e.g. the 1, 2 axes) in terms of another axis system (i.e. the x, y axes) should be equal. The only tensor quantities in Equation (2) are the coefficient of thermal expansion and the stresses. Therefore, the product of these terms in any two arbitrary axes (e.g. i, j) should be equal:

$$[\alpha]_{i,j}^{T}[\sigma]_{i,j} = [\alpha]_{x,y}^{T}[\sigma]_{x,y} = [\alpha]_{1,2}^{T}[\sigma]_{1,2} \tag{4}$$

To prove that the equation is valid, it is necessary to perform the transformation of the CTE and the stress:

$$[\alpha]_{i,j}^{T}[\sigma]_{i,j} = \left[[T][\alpha]_{x,y}\right]^{T}\left[[T][\sigma]_{x,y}\right] \tag{5}$$

where T is the transformation matrix

The coefficients of thermal expansions are second-order tensors and therefore they transform like the strain components, (i.e. $\alpha_s = 2\alpha_{xy}$). In which, α_{xy} is the shear CTE on the x axis along the y direction and α_{yx} is the y axis along the x direction (i.e. $\alpha_{yx} = \alpha_{xy}$) and α_s is the total measure of the CTE in the x-y plane (also known as the engineering shear coefficient of thermal expansion).

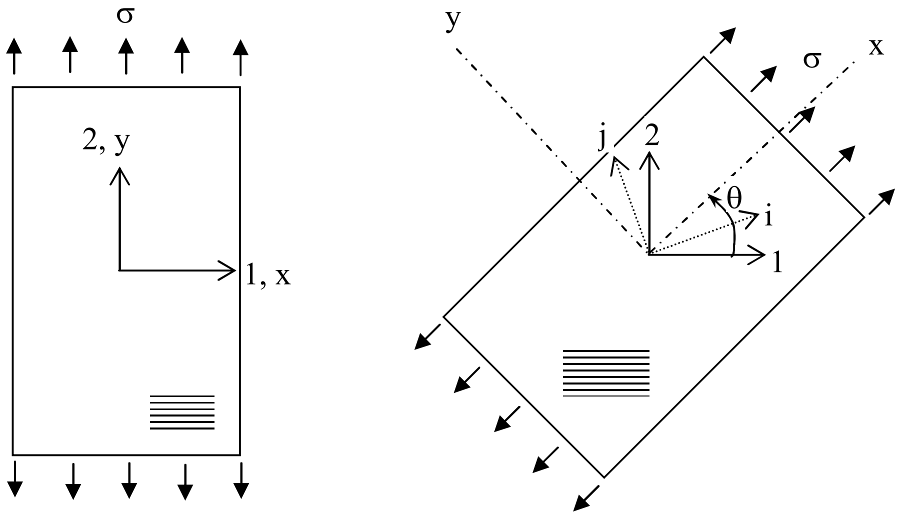

Figure 3: Schematic diagram of the coordinate system and nomenclature (a) on axis laminate (b) off-axis laminate

Therefore, the transformation relation is given as:

$$\left[\alpha_{i,j}\right]^T = \begin{bmatrix} \alpha_x \cos^2\theta + \alpha_y \sin^2\theta + \alpha_s \cos\theta \sin\theta \\ \alpha_x \sin^2\theta + \alpha_y \cos^2\theta - \alpha_s \cos\theta \sin\theta \\ -2\alpha_x \sin\theta\cos\theta + 2\alpha_y \sin\theta\cos\theta + \alpha_s(\cos^2\theta - \sin^2\theta) \end{bmatrix}^T \tag{6}$$

The transformation of stress is given as:

$$\left[\sigma_{i,j}\right] = \begin{bmatrix} \sigma_x \cos^2\theta + \sigma_y \sin^2\theta + 2\sigma_s \sin\theta\cos\theta \\ \sigma_x \sin^2\theta + \sigma_y \cos^2\theta - 2\sigma_s \sin\theta\cos\theta \\ -\sigma_x \cos\theta\sin\theta + \sigma_y \sin\theta\cos\theta + \sigma_s(\sin^2\theta - \cos^2\theta) \end{bmatrix} \tag{7}$$

By multiplying Equations (6) and (7) it can be shown that:

$$[\alpha]_{i,j}^T [\sigma]_{i,j} = \alpha_x\sigma_x + \alpha_y\sigma_y + \alpha_s\sigma_s \tag{8}$$

Therefore the general term $[\alpha]_{i,j}^T [\sigma]_{i,j}$ can be concluded to be an invariant. However, in the lamina principal material directions $\alpha_6 = 0$ and in the principal stress directions σ_s is zero so for these two cases equation (8) reduces to:

$$[\alpha]_{i,j}^{T}[\sigma]_{i,j} = \alpha_1\sigma_2 + \alpha_2\sigma_2 = \alpha_x\sigma_x + \alpha_y\sigma_y \tag{9}$$

To compare the measured ΔT value with a measured strain value for validation purposes , it is necessary to reformulate Equation (8) in terms of applied strain in the laminate:

$$\Delta T = -\frac{T}{\rho C_p}[\alpha]_{1,2}^{T}[Q]_{1,2}[T][\varepsilon]_{x,y} \tag{10}$$

where $[Q]_{1,2}$ is the material stiffness matrix.

Equation (10) can be expanded as:

$$\Delta T = \frac{-T}{\rho C_p}[(\alpha_1 Q_{11} + \alpha_2 Q_{12})(\varepsilon_x \cos^2\theta + \varepsilon_y \sin^2\theta + \gamma_s \cos\theta \sin\theta) + (\alpha_1 Q_{12} + \alpha_2 Q_{22}) \\ (\varepsilon_x \sin^2\theta + \varepsilon_y \cos^2\theta - \gamma_s \cos\theta \sin\theta)] \tag{11}$$

When a uniaxial tensile stress is applied to a balanced orthotropic laminate the shear strain in the laminate (γ_{sL}) is zero, $\varepsilon_x = \varepsilon_{xL}$ and $\varepsilon_y = \varepsilon_{yL}$ so Equation (11) can be simplified as:

$$\Delta T = \frac{-T}{\rho C_p}[(\alpha_1 Q_{11} + \alpha_2 Q_{12})(\varepsilon_x \cos^2\theta + \varepsilon_y \sin^2\theta) + (\alpha_1 Q_{12} + \alpha_2 Q_{22}) \\ (\varepsilon_x \sin^2\theta + \varepsilon_y \cos^2\theta)] \tag{12}$$

For a balanced orthotropic laminate constructed from ±45° angle ply Equation (12) can be further simplified to:

$$\Delta T = \frac{-T}{\rho C_p}\left(\frac{\varepsilon_x + \varepsilon_y}{2}\right)\left[\alpha_1(Q_{11} + Q_{12}) + \alpha_2(Q_{12} + Q_{22})\right] \tag{13}$$

The introduction of stress invariant concept, in order formulate the temperature change from the orthotropic substrate clears any confusion relating to the use of reference axes for the system [9]. It is clear that a consistant use of any axes system is acceptable.

Thermoelastic response from a multidirectional composite laminate

When assessing the behaviour of a general multidirectional composite laminate (consisting of lamina with arbitrary orientations) classical laminate plate theory (CLPT) is used so that the material can be treated as a homogeneous orthotropic plate. Here the mechanical and thermoelastic properties are considered ply by ply and then brought together relative to (say) the laminate axis to provide a 'global' stiffness and CTE. For quasi-isotropic laminates (i.e. $(0, 90)_s$ and $(0, \pm45, 90)_s$) it is evident that the global CTE is equal in the longitudinal and transverse directions (i.e. $\alpha = \alpha_{xL} = \alpha_{yL}$) because of the stacking sequence. Therefore, it may be pertinent to express the thermoelastic temperature change in the following manner:

$$\Delta T = -\frac{T}{\rho C_p}\left\{\alpha_{xL}\left(A_{11}+A_{12}\right)\Delta\varepsilon_{xL}+\alpha_{yL}\left(A_{12}+A_{22}\right)\Delta\varepsilon_{yL}\right\} \tag{14}$$

where A_{ij} is the global stiffness of the laminate.

Equation (14) simply assumes that the material response is that of a homogeneous orthotropic material. A further and as yet unexplored idea is that the CTE is coupled in the stack (i.e. the thermal strain is constant in the through thickness direction) with the surrounding layers and this also may have an effect on the response. To explore this, the orthotropic nature of the surface ply is retained in the treatment but the CTE is treated as a global property as the plies are bonded together and are not free to deform independently, which gives the following equation:

$$\Delta T = -\frac{T}{\rho C_p}\left\{\alpha_{xL}\left(Q_{11}+Q_{12}\right)\Delta\varepsilon_{xL}+\alpha_{yL}\left(Q_{12}+Q_{22}\right)\Delta\varepsilon_{yL}\right\} \tag{15}$$

The different scale of idealisation in each treatment, aims to provide an insight into the thermoelastic behaviour of composite materials. This is achieved by computing the thermoelastic temperature change based on each treatment and comparing it with the measured temperature change from an orthotropic laminate.

Thermoelastic work

The material used for manufacturing the test specimens was a unidirectional glass/epoxy pre-impregnated material. The mechanical properties and physical properties such as density, specific heat capacity and CTE were determined according to respective ASTM standards from unidirectional composite test coupons as provided in Table 1. The global mechanical and physical properties are given in Table 2. To validate the theoretical treatments, Unidirectional (UD), Angle ply (AP), off-axis unidirectional (OA), cross-ply (CP) and quasi-isotropic (QI) panels were manufactured with different stacking sequences and consolidated in an autoclave. Four different sets of CP laminates were manufactured; one with a 0^o surface ply (CP0) and another with a 90^o surface ply (CP90) and also with 0 and 90 ply groups (i.e. $[0_3,90_3]_s$ and $[90_3,0_3]_s$) to evaluate the effect of surface ply on the thermoelastic signal. Two different sets of QI laminate with similar surface layer (i.e. 0^o) and similar mechanical properties (i.e.$[0,\pm45,90]_s$ and $[0,90,\pm45]_s$) were manufactured to evaluate influence of sub-surface plies on depth of the thermoelastic response. The test specimens were mounted in an Instron servo-hydraulic test machine and a cyclic tensile load was applied at a loading frequency of 10 Hz. A strain gauge rosette was attached to the specimens to measure the strain in the laminate principal directions. The thermoelastic temperature change (averaged over a uniform area) were collected from each specimen.

Table 2: Global mechanical and physical properties of the laminates

Specimen	Young's Modulus (GPa), E_L	Poisson's ratio, ν_{LT}	CTE, α (x $10^{-6}/^oC$)
CP	20.0	0.15	10.59
AP	9.5	0.55	16.20
QI	19.7	0.29	9.25

Table 3: Details of applied load, strains and thermoelastic data from the test

Specimen	Load (kN)		Applied strain		Strain sum, $\Delta(\varepsilon_{xL}+\varepsilon_{xL})$	Surface temperature,T (K)
	Mean	Amplitude	ε_{xL}	ε_{yL}		
CP(0)	0.7	0.55	0.001949	-0.000296	0.001653	291.55
CP(0)3	1.7	1.55	0.001750	-0.000209	0.001541	294.90
CP(90)	0.7	0.55	0.001993	-0.000312	0.001681	290.57
CP(90)3	1.7	1.55	0.001750	-0.000209	0.001541	295.26
OA	0.7	0.60	0.002119	-0.000803	0.001316	293.45
AP	0.5	0.41	0.003371	-0.001640	0.001731	294.91
AP3	1.7	1.60	0.004425	-0.002725	0.001700	295.2
QI (45)	0.9	0.88	0.002281	-0.000694	0.001587	292.03
QI(90)	0.9	0.85	0.003210	-0.001021	0.002189	296.09
Epoxy	1.4	1.30	0.002690	-0.00109	0.001600	291.35

RESULTS AND DISCUSSION

Figure 4 show the thermoelastic data collected from each speciment. It is apparent from the thermoelastic images that there are significant differences in the ΔT obtained from the different specimens, with the surface ply orientation and lay-up evident in the images. This indicates that the response is most likely to be from the orthotropic layer as the fibre orientation in the surface layer is clearly visible; however this can only be established using accurate material property data. A further observation from the image data is that there are significant differences in the thermoelastic temperature change obtained from the CP(0) and CP(90) specimens due to the differences in the surface ply orientations, and the data becomes much more uniform when the laminate is thicker. A similar trend is also observed for AP and AP3 plies indicating the possible influence from subsurface plies. This statement is further supported by the observed difference between the two different QI laminates, which have similar surface plies and mechanical properties yet there are clear differences in the thermoelastic data. The measured (with standard deviation) and calculated temperature change are summarised in Table 4. In most cases the values predicted by the four methods are very close. By accounting for the scatter in the data, it is difficult to establish if one prediction is providing better results than another. It is clear that equation (14) generally provides values outside of the scatter bands. For laminates with stacked ply group surface layers there is a good agreement between the measured data and Equation (3). It is clear that for some laminates the stress induced temperature change is less in the surface ply than in the resin and for these cases the heat transfer will be from the surface resin to surface ply.

Table 4: Thermoelastic response from the composite specimens

Specimen	**ΔT, measured**	**Resin (Eq. 2)**	**ΔT, Surface ply (Eq. 12)**	**ΔT, Global (Eq. 14)**	**ΔT, Mixed (Eq. 15)**
UD(0)	0.147 (±0.0090)	0.129	0.141	0.141	0.141
UD(90)	0.104 (±0.0135)	0.124	0.104	0.104	0.104
CP(0)	0.147 (±0.0089)	0.127	0.135	0.175	0.141
CP(90)	0.129 (±0.0214)	0.130	0.106	0.171	0.104
CP(0)3	0.119 (±0.0069)	0.118	0.125	0.174	0.143
CP(90)3	0.101 (±0.0107)	0.118	0.097	0.173	0.106
AP	0.121 (±0.0152)	0.133	0.125	0.132	0.127
AP3	0.127 (±0.0111)	0.130	0.125	0.135	0.131
QI(45)	0.138 (±0.0012)	0.123	0.136	0.144	0.136
QI(90)	0.112 (±0.0090)	0.120	0.133	0.143	0.129

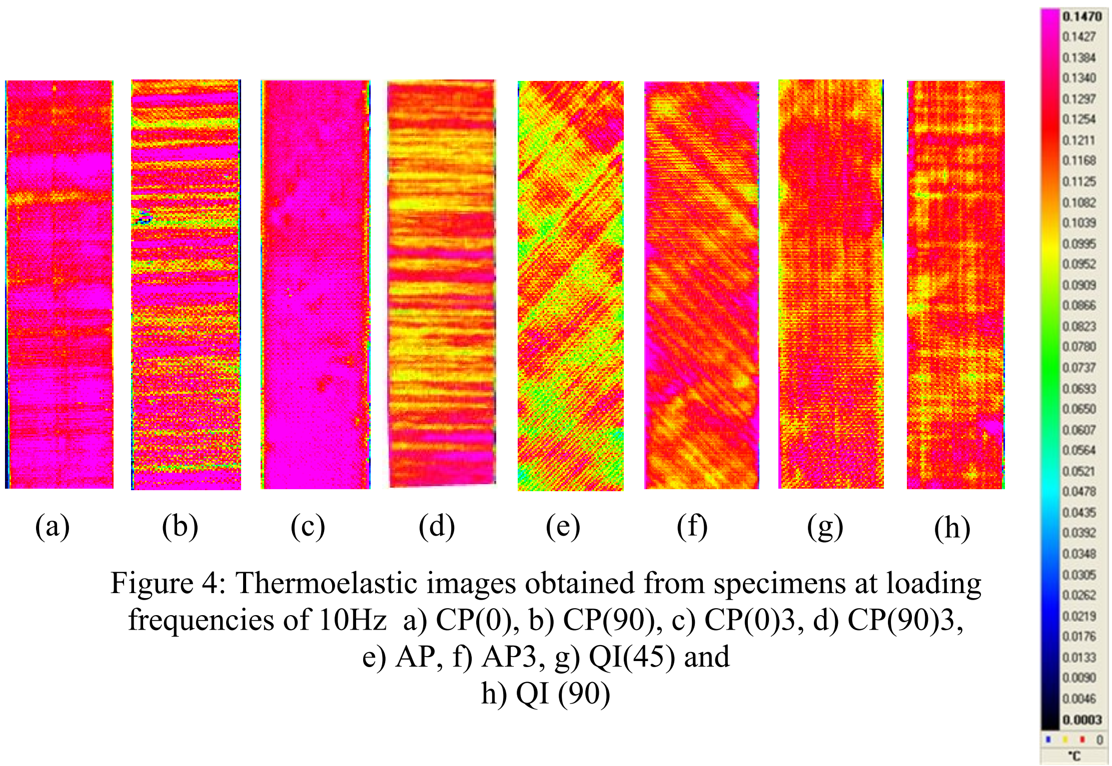

Figure 4: Thermoelastic images obtained from specimens at loading frequencies of 10Hz a) CP(0), b) CP(90), c) CP(0)3, d) CP(90)3, e) AP, f) AP3, g) QI(45) and h) QI (90)

CONCLUSIONS

This study has shown that for the material considered, E-glass/epoxy pre-preg laminates, of the two standard approaches the orthotropic surface layer interpretation provides the best agreement to measured data. The benefit of considering other approaches is demonstrated and this work highlights the importance of careful consideration of the source of the thermoelastic signal while working with composite materials.

References

1. Sambasivam, S., Quinn, S., and Dulieu-Barton, J.M., Proc. SEM XI International Congress on Experimental and Applied Mechanics, 10pp, 2008
2. Emery, T.R., Dulieu-Barton, J.M., Earl, J.S., and Cunningham P.R., Composites Science and Technology, pp. 743-752, 2008
3. Pitarresi, G., Found, M.S., and Patterson, E.A., Composites Science and Technology, Vol. 65, pp. 269-280, 2005
4. Stanley, P., and Chan, W.K., Journal of Strain Analysis, Vol. 23, pp.137-143, 1988
5. Feng, Z., Zhang, D., Rowlands, R. E., and Sandor, B. I., Experimental Mechanics, Vol. 32, pp. 89-95, 1992
6. Pitarresi, G., Conti, A., and Galietti, U., Applied Mechanics and Materials, Vols. 3-4, pp. 167-172, 2005
7. Oliver, D. E. and Webber, J. M. B., Proc. Vth International Congress on Experimental Mechanics, Montreal, pp. 539–546,1984
8. DL003U-I Altair LI User Manual, Cedip Infrared Systems, 2006
9. Potter, R.T., Proc. 2nd Int. Conf. on Stress Analysis by Thermoelastic Techniques., London, Int Soc. Photo Optical Instr. Eng., SPIE (Vol.731), pp. 110-120, 1987