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Impact characterization of RTM composites

Part I *Metrics*

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The use of textile based architectures and the dry compaction of preform layers prior to resin infusion creates the potential for highly tailored resin transfer-moulded composites that have significantly different response to impact than traditional prepreg-based composites. The response of a number of resin transfer moulding (RTM) composites is characterized using both traditional and new metrics of performance. Impact performance maps are used to differentiate between materials response at a fundamental level and key differences are traced to differences in preform fabric architecture. Global and local differences in response based on architecture are elucidated through the determination of damage and energy absorbance, and are related to materials' specific characteristics in an attempt to allow comparison of impact response of composites on a more quantitative basis.

1. Introduction

Although the issues related to the determination and prevention of impact-induced damage in composites have existed ever since composites were first used in aircraft, the characterization of both the impact event and the resulting damage are still part of an emerging science, rather than a well-defined and understood phenomenon. Composites, especially glass-reinforced thermosets are seeing increased use in structures ranging from boat hulls and superstructure to automotive fenders (bumpers) and panels, civil infrastructure elements and even aircraft components. Whereas only a few applications such as in the composite armoured vehicle (CAV) will face ballistic level threats, most are likely to experience intended or accidental impact events during their service life. The use of tailored textile structural composites through the resin transfer moulding (RTM) process allows the composites designer a great deal of flexibility in designing for various load conditions, including impact. However, in order to truly tailor architecture it is essential that a global understanding of the impact event is developed, including that related to energy absorption through gross deformation and through failure mechanisms at the constituent level such as crack initiation, fibre breakage, pullout, delamination and debonding. While RTM composites offer the capability of tailoring global and local response through appropriate selection of fibre architecture, sizing and resin, the current level of understanding does not allow one to actually use these possibilities at any level other than the global one, because of the dual lack of fundamental understanding related to the micromechanics of impact and the lack of tools to analyse and assess the impact event itself.

Three issues embody the overall challenge in the area, namely: (i) to identify and characterize the relevant failure mechanisms; (ii) to understand their interactions; and (iii) to be able to predict the extent of damage within a given composite system under a set of specified load and boundary conditions. Whereas there is a considerable, albeit still incomplete and inconclusive, body of knowledge about these aspects for laminated composites [1, 2], there is a total lack of such a base for textile-based RTM composites. It should be noted that the RTM process is fundamentally different from autoclaving in that rather than prepreg, dry fibre preforms are compacted in a tool cavity bringing fabric layers into varying degrees of intimate contact before any resin is injected. The compaction step allows for nesting of neighbouring layers and for mechanical interlocking between layers. Consequently, the resin-rich interlaminar zones seen in prepreg-based composites are absent, with RTM composites showing no distinct macroscale regions of differentiation between layers. At the macrostructural level this leads to the formation of an almost monolithic material with damage mechanisms considerably different from those seen in laminated prepreg-based composites [3]. These mechanisms are largely initiated by the actual intricacies of the fabric used in the preform itself. The potential for tailoring global response through preform architecture has significant attractiveness but will not become a reality until methods are devised that allow investigation of the response to external loading from a microstructural point of view and beyond the stage of using gross damage as a tool for qualitative comparison between different materials systems. This is particularly true in the case of impact, where the usefulness of textile structural composites in providing enhanced impact and damage resistance will not be fully realized until a method for interpreting materials response at a level higher than merely comparative is developed.

This paper examines the use of inelastic energy curves [4] and other metrics as a means of interrogating the impact events so as to differentiate between responses of different reinforcing fabric architectures. A number of metrics are used to analyse damage and energy absorption and to draw conclusions related to architectural effects, in order to emphasize the use of developing methods of characterizing impact response at a materials level, rather than merely at a level of relative comparison. This is especially needed so as to be able to appropriately select fabric architecture (knits, weaves, mats etc.) for the desired level of impact response.

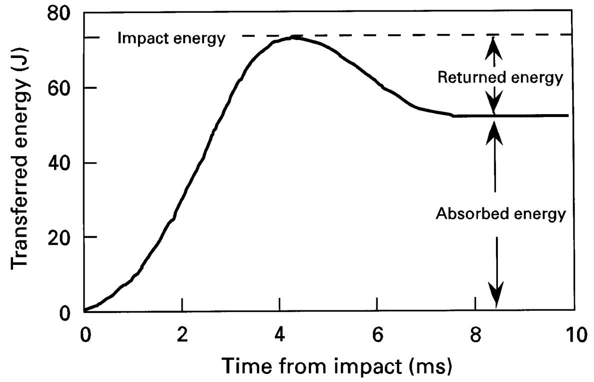

Figure 1 Transient impact response and the components of energy.

2. Characterization of impact response

In sharp contrast to the impact response of metals, impact response in composites reflects a complex failure process that is highly dependent on the constituent materials (fibre and matrix) and the fibre architecture (orientation, fabric type). While the occasional impact may be readily absorbed in a tough monolithic plate without danger of catastrophic failure, composites, by nature of the preferential in-plane reinforcement, are susceptible to potentially debilitating damage caused by out of plane loads. Even relatively small deflections caused by low velocity impact may be sufficient to initiate matrix cracking which is a precursor to delamination and other failure modes. The current lack of full understanding of the effect of material constituents and architecture (such as related to the relative efficiency of woven fabric reinforced composites as compared to non-woven ("knitted") fabric reinforced composites) is partially due to the lack of appropriate means for interrogation of the materials response at a level greater than that akin to a general comparison of behavior on the basis of relative ranking. In fact, until recently, the impact behaviour of composite materials has been characterized by the same test methods originally developed for metals [5]. It is also perhaps telling, that to date no ASTM standard exists specifically detailing an impact test method for composites, which has resulted in general confusion among the community regarding the identification and determination of critical impact test parameters or response variables. It should however be noted that a number of potential test methods have been proposed over the past decade, some of which have considerable merit and bear further consideration, despite the significant arguments *vis-à-vis* the appropriateness of one suggested method over another. Although impact phenomena can be studied over a large range of velocities and regimes including the low velocity, hypervelocity and ballistic regimes, this paper will be restricted to discussions related to low velocity regimes with the drop weight test apparatus defining the limits of use. Details related to other methods and comparisons between these [5, 6] are hence not repeated herein. It is emphasized at the outset, that impact response in general, is not merely dependent on the characteristics of the materials being tested, but is rather a function of the interplay between materials, structural configuration and test method.

Impact tests produce different measured characteristics such as force, deflection and energy, and every test measures a characteristic at a specified failure limit. Fig. 1 depicts a standard energy trace resulting from an impact event. Metrics such as the returned (or inelastic) energy and the absorbed energy, peak load and deflection at peak load are often used to characterize impact response. Some investigators use metrics such as load at incipient damage, energy absorbed at incipient damage, maximum load, and energy absorbed at maximum load to compare the impact response of composites [7]. Still others have used the slope (or thickness normalized slope) of the initial part of the transient response to determine materials response to impact [6, 8], which intrinsically limits characterization to the linear portion of the behaviour, which in itself could be only a small portion of the overall response envelope. In order to gain an understanding of true materials response over a range of impact energies Sierakowski *et al.* [9] introduced the concept of a linear relationship between impact energy and damage area. Although useful, this technique is handicapped since accurate measurement of the projected damage area (PDA) is a source of considerable argument and at best is approximate and global in nature. Others, such as Lagace and Wolf [10] and Jackson and Poe [11] have attempted to use the impact force as a parameter for assessment of impact damage and response. In a recent set of papers [4, 12, 13] a new approach related to the determination of inelastic energy has been proposed and combined with the use of PDA as an interrogational tool to enable the use of impact performance maps [4, 14].

Transient response curves (such as in the schematic of Fig. 1) intrinsically show the amount of energy transferred between the cross-head and plate as a function of time. In doing this, it goes one step further than the load–deflection record (or the load–time record) which indicates deformation response, since the former also shows the breakup of absorbed and returned energy as a function of time. Attempts have been made in the past [6, 15, 16] to differentiate between regions of the load–time record, so as to determine metrics of comparison between events. These, however, have to be studied as records of isolated events (i.e. one trace per impact event) and hence it is difficult to derive materials based response to impact, rather than a specific energy, from them. In order to investigate materials response over a range of energy levels, a number of previous investigators have attempted to plot absorbed energy as a function of impact energy. However, this has not proved to be an efficient interrogational tool since the plot attempts to derive an understanding of materials response from a complex function of overall test response, which includes a number of variables such as test fixture geometry, fixture and frame stiffness, thereby showing low sensitivity for distinguishing variations in material degradation behaviour. The use of inelastic energy curves (IEC [4]) changes the bases by plotting returned (or inelastic, since internal damage accrues during loading) energy as a function of impact energy to give increased interrogational sensitivity to characterization of materials impact response. A general form of the IEC is shown in Fig. 2 which depicts three distinct regions and two transitional zones, which roughly correspond to the five stages in [8]. It is, however, emphasized that the stages in [8] relate to one impact event, whereas the IEC is over a range of impact energies till final puncture.

- *Region I* purely elastic in nature with a one-to-one correspondence between incident and returned energy.
- *Region II* actual dynamic response characterized by a linear relationship between incident and returned energy. Although localized visible damage can be seen the specimen essentially reacts elastically at the outset. Point C determines the linear inelastic limit (LIL), at which point permanent indentation and damage contributions become significant. It is important to note that till LIL in Region II there is actually very little crushing under the contact point (which is very different from the conical crush zone observed in laminated prepreg-based composites) for RTM composites.
- *Region III* embodies the puncturing of multiple fabric layers which are uncoupled from their adjacent layers as a result of prior damage development. Whereas the returned energy response is well behaved and predictable in Region II, it shows diametrically opposite reproducibility in Region III. The region is describable as partially chaotic because small perturbations can significantly influence returned energy levels. Cognizance of this behaviour is critical for

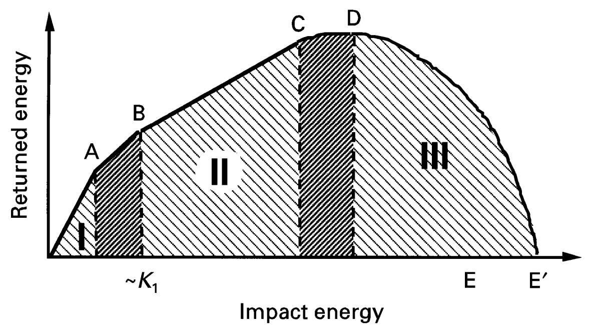

Figure 2 Schematic of the general form of the IEC. A, elastic yield limit; B, transitional elastic limit; C, linear in elastic limit; D, transitional inelastic limit; E, E′, complete puncture. (▨) transition zones.

designers who heretofore have used a "puncture energy" as a design metric, but will be faced with a range for RTM composites.

3. Materials and experimental methodology

A set of composite panels 0.64 cm thick and 24.5×24.5 cm in size were fabricated using the six preform fabrics listed in Table I. Two Derakane vinylester systems, 411–C50 and 8084 were used for infusion, with both being catalysed with 1.75 wt % bisphenol peroxide and containing 1% internal mould release. The 411–C50 system has a nominal tensile strength and modulus of 75–84 MPa and 3300–3500 MPa, respectively, and is capable of 4–7% elongation, whereas the 8084 system has a nominal tensile strength and modulus of 69–75 MPa and 3000–3200 MPa, respectively, with a 10–12% elongation capacity. Further, typical reverse impact values to first crack on 8 mm thick plates results in 6.44 N m and 23.39 N m for the 411–C50 and 8084 systems respectively (data from Dow Chemical Company). The 8084 system is thus a tougher system which compromises tensile and flexural performance for toughness and elongation. Both systems are widely used for resin transfer molding applications. The resin system was premixed and then injected into the preform (already compacted to the 0.64 cm thickness) using a pressure-pot at a pressure of 2.75×10^5 Pa. Plates were cured in the tool initially at 100 °C for 30 min, and were then postcured in an oven for 3 h at 125 °C after demoulding. In further discussions the composites will be referred to by the preform fabric and resin system together (18 oz–411 would mean the system using the 18 oz plain weave infused with the 411–C50 resin system).

Low velocity impact events were administered to specimens of 90×90 mm size cut from the composite panels using a Dynatup model 8200 drop weight impact tower linked to a GRC model 730-1 data acquisition system (as shown in Fig. 3) in order to obtain real time contact force histories. Contact force was determined using a 50 kN load transducer located between the cross-head and the 12.7 mm diameter tup nose. Impacts were administered over a range of energies until complete puncture. The cross-head had an unladen weight of 3625 g, effectively fixing the lower limit of attainable impact energies. It should be noted that although widely used for composites, the instrumented drop weight impact test is essentially

TABLE I Details of preform structure

Preform fabric and layup	Resultant mass fraction in composite	
	411–C50	8084
18 oz ($\approx 610.33\ g/m^2$) plain weave (4s)	47.1	–
24 oz ($\approx 813.78\ g/m^2$) plain weave (4s)	73	74
36 oz ($\approx 1220.66\ g/m^2$) plain weave (3s)	61.1	75
CDM 1808 knit (5s)	75.6	76
Knit and weave hybrid [3(0/90/C)3PW]	69.9	–

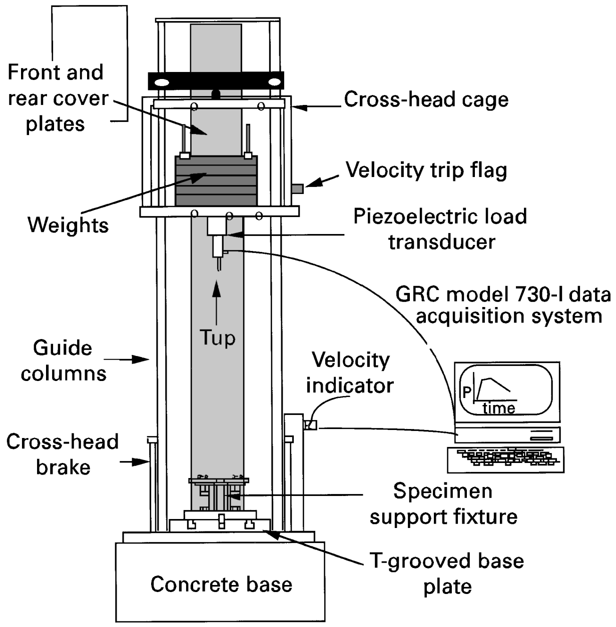

Figure 3 Schematic of the drop weight impact tower apparatus.

designed for isotropic materials. The extent to which a given test specimen thus approaches in-plane isotropy is a concern that needs to be kept in mind; the constraining fixture is circular and the indentor is hemispherical, whereas the plain weaves and knits being considered could be assumed to be orthotropic by nature, at best. A consequence of forcing an orthotropic plate to adapt to isotropic boundary conditions is the occurrence of localized crimping that occurs to accommodate membrane type deformation [17]. The consequence of this to composite materials in general is significant; the departure from an isotropic materials' global response influences damage progression, as well as the initiation and growth of inter-layer separations, which are accompanied by fibre breakage and pullout/tearout occurring from nested layers in textile structural composites fabricated using the RTM process.

Post-test analysis was conducted through an investigation of the damaged area that included determination of damage area, ashing of specimens to determine bundle breakage, use of dye penetrants and microscopic examination of failure areas.

4. Results and discussion

In order to investigate and interpret data obtained from the drop weight tests, inelastic energy curves and projected damage area (PDA) plots were employed. These make it possible to seek a comparison based on intrinsic materials response, rather than through a relative indication of impact performance, and allows for comparisons based on differences in fabric architecture, resin type and fibre volume fraction. IEC are created by plotting the energy returned to the cross-head during rebound as a function of impact energy over the range of attainable impact energies. Returned (or inelastic) energy represents the difference between the impact energy and energy "absorbed" within the impacted plate either by conservative or non-conservative means. An example of an IEC, for the 8084 infused 24 oz plain weave composite is depicted in Fig. 4. It should be noted that as shown in [4] the LIL reflects the end of the linear relationship between active energy absorption modes and accumulating damage. Damage progression after LIL is seen to be random since global response variables and properties lose considerable influence over the failure process once LIL is exceeded, and local interaction between tup and fabric architectural details govern subsequent behaviour. Fig. 5 shows a comparison of LIL for six different fabric–resin systems under consideration. The use of the tougher 8084 system is seen to result in a higher LIL than achieved through the use of the 411–C50 system. Impact performance is often assessed through the measurement of the projected damage area, which can be misleading because of the ambiguity associated with its determination. This is especially true of resin transfer-moulded systems where delamination is not necessarily the most significant or initiating mode of damage. In fact, in RTM systems, the first damage modes are those of intrabundle cracking, wherein cracks propogate within the bundle and can be spaced 1–2 mm apart with

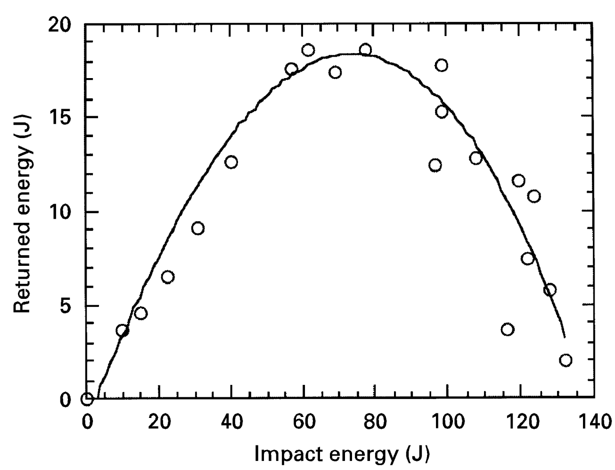

Figure 4 Representative IEC plot (24 oz plain weave fabric, ($\approx$813.78 g/m^2), Derakane 8084 vinylester resin).

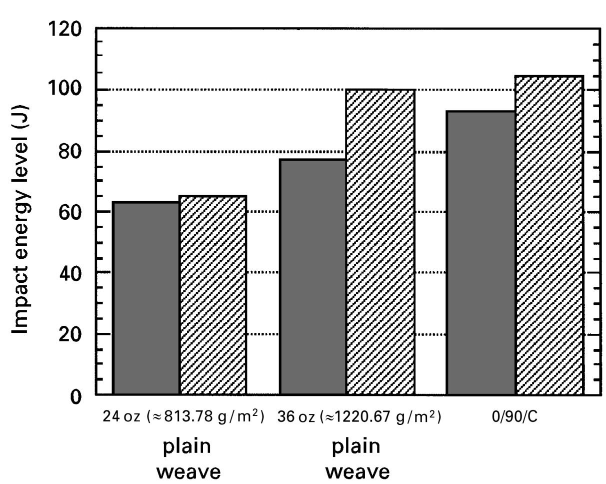

Figure 5 Comparison of LIL levels based on resin type. (▩) 411–C50; (▨) 8084.

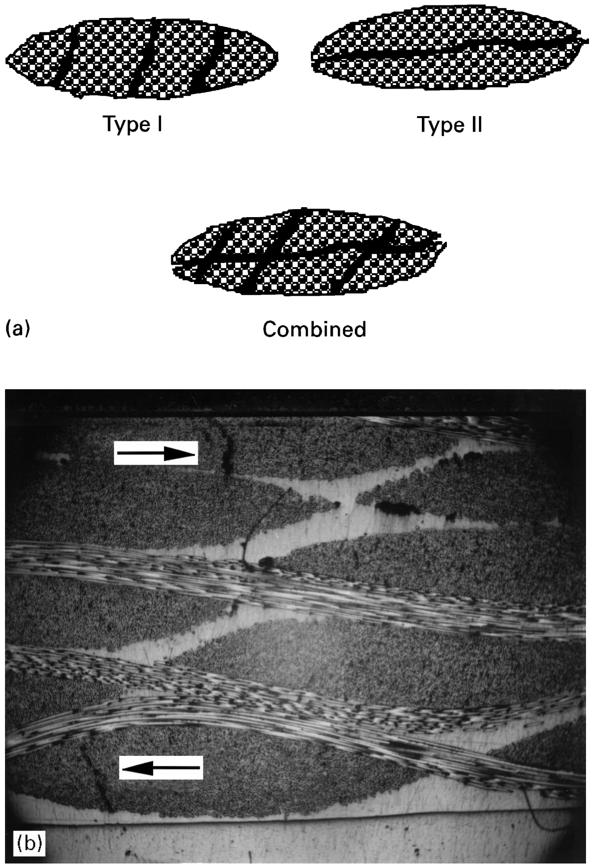

Figure 6 (a) Schematic of intrabundle cracking; (b) Micrograph of intrabundle cracking (1.08 kg plain weave).

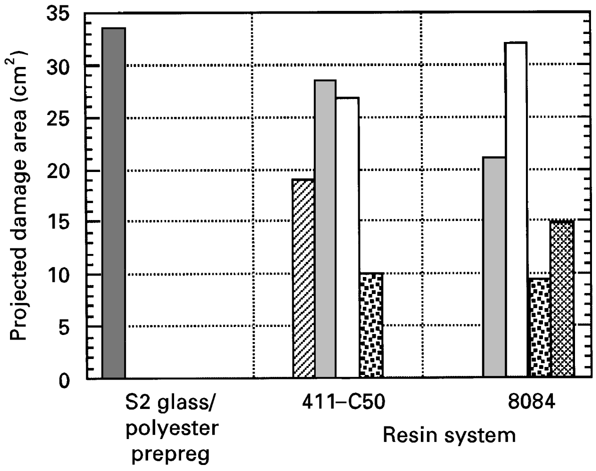

Figure 7 Comparison of projected damage area (PDA) levels at LIL. (▩) prepreg; (▨) 18 oz plain weave; (▩) 24 oz plain weave; (□) 36 oz plain weave; (▩) 0/90/C "Promat" non-woven; (▩) 0/90 non-woven.

crack widths of less than 0.1 mm (Fig. 6). Once these cracks propagate to the bundle surface they can act as initiation sites for interbundle cracking. It should be noted that intrabundle cracking is actually a manifestation of fibre debonding due to deformation as a result of transverse tensile strains and bundle compaction under load. Both intra- and inter-bundle cracking can absorb significant levels of energy, without showing the gross damage levels seen in delamination. Fig. 7 gives a comparison of PDA for the different systems at LIL and compares levels of damage with that seen for a S2 glass polyester prepreg (plain weave) at the same thickness and having a fibre mass fraction of 67.2%. It should be noted that the 33.50 cm^2 PDA for the prepreg is the largest of all the systems, although the LIL is comparable to that of the (0/90/C)–8084 and the 36 oz plain weave–8084 systems. Both the RTM systems, however show lower PDAs, with the 0/90/C being significantly less in dimensions (i.e. less than 10 cm^2 for both resin systems). An explanation for this, beyond the level of intra- and inter-bundle cracking lies in the difference in the ultimate modes of ply separation seen in the two systems. Composite "layered" plates (prepreg based) generally show interply separation through the resin rather easily under out-of-plane loadings (such as impact), and it is significant that the PDA in this case reaches a level of above 30 cm^2 at impact energies as low as 25 J (although complete puncture is not achieved until levels of over 200 J are reached). In contrast to the clean delamination seen in this system, zones of separation in the RTM composites were not contiguous. Compaction of the dry preform layers causes significant interpenetration and nesting of adjacent fabric layers resulting in intermingling of fibres and fibre bundles from adjacent layers, creating a tight and intermeshed microstructure. Interply separation is then accompanied by significant surface roughness (including hackling of the matrix [3]), fibre pullout, bridging and fibre breakage. Due to this intermeshing and nesting, resin transfer-moulded fabric forms (especially the Promat) variety, such as the CDM 1808 which is a biaxial non-woven with a chopped strand mat backing) behave as more-or-less macroscopically monolithic structures. Initial damage modes/mechanisms thus have a minimal effect on the dominant deformation up to LIL, after which sufficient damage is seen to accumulate, allowing ply separation. It should be noted that the fibres from the chopped strand mat in the (0/90/C) Promat fabric increases the intermeshing between layers as a result of which PDA is significantly lower for the composite (Fig. 7). It can also be noticed from Fig. 7 that the presence of the mat which adds very little to overall weight (627.28 g/m^2) of the fabric causes a significant decrease (9.5 cm^2 from 14.94 cm^2) from the PDA measured for the corresponding 0/90 non-woven fabric. This is also seen to result in an almost 100% increase in LIL levels at equivalent fibre loadings, indicating the higher damage tolerance afforded by these fabrics when used in highly compacted preforms. As may be apparent already and will be shown through the use of impact performance maps, non-wovens may offer significant advantages over plain weaves in the area of impact response, due to intermeshing and interlayer compaction in RTM.

In the past, based on impact data from laminated composites, investigators, including Sierakowski *et al.* [9, 18], showed the existence of a linear relationship between impact energy (K), and projected damage area, A, of

$$K = K_1 + A(K_2)$$

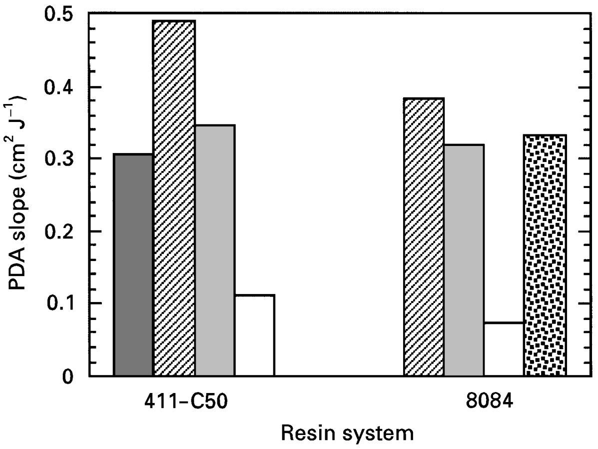

Figure 8 Comparison of performance based on PDA slopes. See Fig. 7 for key.

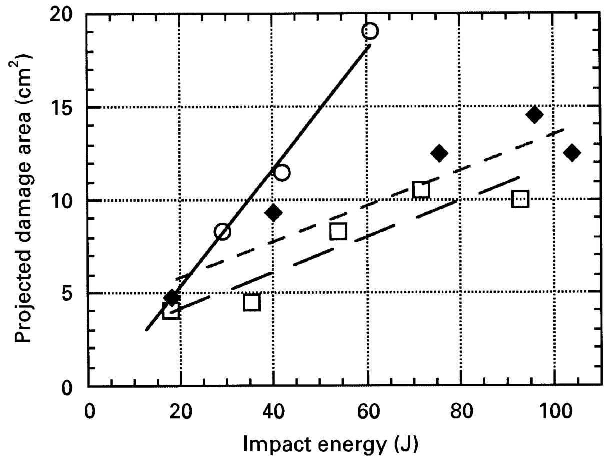

Figure 9 Projected damage area as a function of impact energy (within the linear region, i.e. until LIL). (○) 18 oz plain weave; (□) 0/90/C "Promat"; (◆) hybrid.

where K_1 represented an initiation energy and K_2, the slope representative of the sensitivity of the material to damage accumulation. Obviously, a high slope indicates greater ease of damage accumulation for a material system. It is of considerable interest to compare results obtained from plots of the different materials investigated based on their values of K_2: the PDA slope, with results reported on the basis of IEC slope and damage area. Fig. 8 shows a comparison of PDA slopes from the RTM materials systems considered and on the basis of the resin system used. As with results seen in Figs 5 and 7, it can be noticed that the 8084 based systems are tougher and show less propensity for damage. It is interesting that with the 411–C50 system a clear trend with plain weave fabric weight is not possible, unlike that seen with the use of the IEC slope. However, it is emphasized that PDA slopes and IEC slopes are not equivalent with the former indicating ease of damage propagation, whereas the latter indicates ability for damage absorbance. These two metrics are obviously contrasting in nature and a material that is a good energy absorber need not simultaneously be intrinsically impact damage resistant – in fact often these are in opposition to each other. It is however noted that the 0/90/C Promat fabric based composites have a much lower PDA slope than the 0/90 biaxial non-woven, again suggesting the greater viability of the Promat. In fact, overall the Promat would seem to be significantly more damage resistant than the plain weaves. Fig. 9 shows a comparison of the PDA traces for three different preform architectures infused with the 411–C50 Vinylester system: the Promat, 540 g plain weave, and a hybrid consisting of a symmetric layup of Promat and an S2 glass plain weave. It can be seen that the Promat-based composite performs the best in terms of the lowest PDA slope. Interestingly it also has the lowest IEC slope of the three. The use of the S2 glass was not seen as a great benefit since it provided cleaner surfaces for delamination (interply separation) between layers of the finer S2 architecture. It should be noted that initial damage was seen at low impact levels primarily due to matrix crazing on the resin rich top surface, which should not be misconstrued as giving a realistically low damage threshold since the composite is as such unaffected.

It should be noted at this point that the PDA curves as introduced by Sierakowski *et al.* [9, 16] are characterized by the initiation point, K_1, and the slope, K_2. Further, based on experimental data from RTM composites, it would appear that the linear portion terminates at the LIL as defined by IEC curves. Since the slope of both curves characterizes a different type of materials response till LIL (i.e. over their linear regimes) it is reasonable to expect that the combined usage of both the IEC slope and the PDA slope could result in the development of an interrogational and classification tool for impact response. A high IEC slope is characteristic of a material that behaves as a stiff plate with little internal damping or energy absorbance capacity, whereas a low slope is indicative of the capacity of a material for significant internal damping and absorbance of impact energy. Higher PDA slopes meanwhile characterize the susceptibility of the material to damage propagation, suggesting the higher the value of the PDA slope, the greater is the projected damage area for the same impact energy level. One could therefore conclude that the IEC slope reflects the fraction of impact energy returned, while the PDA slope indicates sensitivity to internal damage accumulation. The use of these two as axes of a plot forms an impact performance map, from which it is possible to determine, quantitatively, the difference in materials response to impact. A high IEC slope and a low PDA slope are characteristic of damage-resistant materials, whereas material systems characterized by a low IEC slope and a high PDA slope indicate high capability for energy absorbance with global damage. A low IEC slope and a low PDA slope suggests that the material response is characterized by the ability for significant energy absorption through local damage events (rather than a global event such delamination). Fig. 10 depicts an impact performance map for the materials systems investigated in this study. It is clear that the representation allows for easier characterization of response than by the

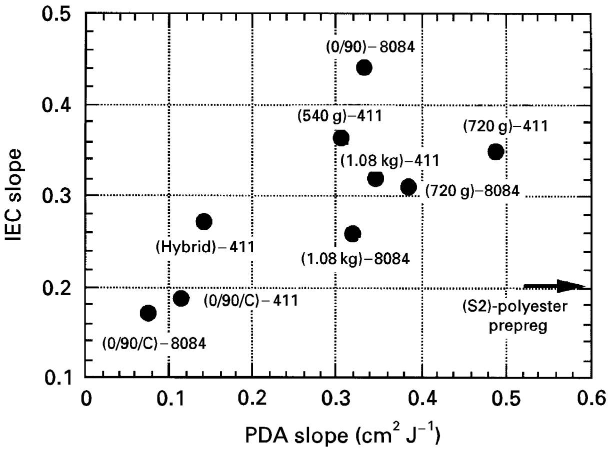

Figure 10 Representation of materials response through an impact performance map.

comparison based on a set of single metrics, as in Figs 5, 7 and 8. The maps also allow the designer to optimize for energy absorption, damage tolerance, or impact damage resistance, or an appropriate combination of the above; this can be done through appropriate tailoring of preform architecture, resin system, or even size and weight of fibre bundles used. The further use of interrogational tools is being continued with emphasis on determination of the effect of fibre bundle size, sizing and overall fabric weight per layer, on impact response and will be reported at a later date.

It is instructive to the present discussion to compare fibre bundle breakage at a number of impact energy levels for the three representative systems under consideration: non-woven (RTM), plain weave (RTM) and prepreg, as shown in Fig. 11a–c. It can easily be seen that the prepreg-based system shows the lowest level of overall bundle breakage, although as reported earlier it showed the greatest PDA through early delamination. The role of the chopped strand mat in absorbing damage is clearly seen on comparison on the number of broken bundles equivalent energy levels in the 0/90/C and the 540 g plain weave composites. The contrasting behaviour – local versus global, in the RTM and prepreg systems as reported in the earlier plots and discussion is again emphasized in Fig. 11. It is perhaps more illustrative to also mention that if the cumulative bundle breakages were tallied in the RTM composites and the prepreg composites through the thickness, it can easily be seen that for the systems considered there is significantly more local damage (as seen through breakage of fibre bundles) in the 0/90/C–411–C50 composite than in the S2 glass/polyester prepreg-based samples as seen in Fig. 12. It should however be noted that as seen in Figs 7 and 10, global damage as measured through the projected damage area is considerably greater for the S2/polyester prepreg-based system due to delamination-induced separation.

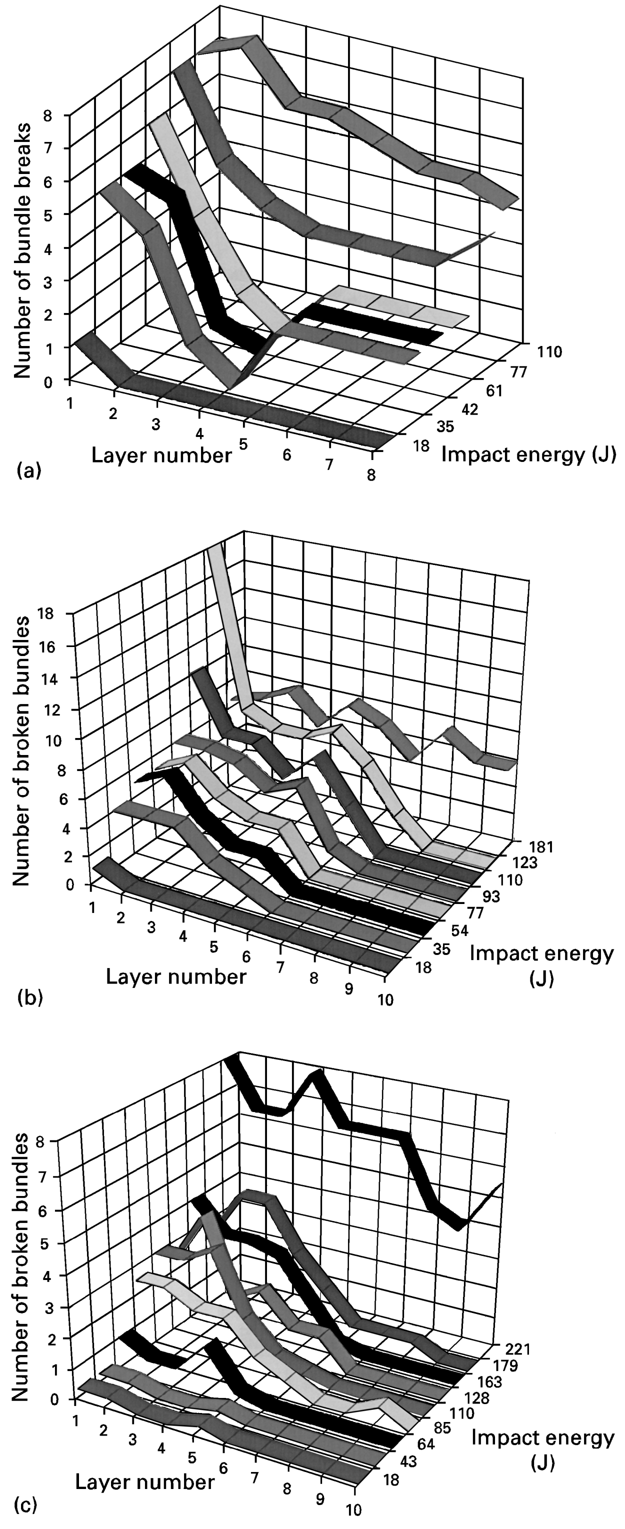

Figure 11 Number of fractured bundles through the thickness as a function of impact energy for 0.64 cm composite specimens: (a) plain weave (411–C50 Vinylester Resin); 18 oz (b) 0/90/C (411–C50 Vinylester Resin); (c) S2 glass/polyester prepreg.

5. Summary

Resin transfer-moulded composites show a very different materials based impact response as compared to prepreg-based systems due to the significant compaction, fibre entanglement and nesting of adjacent layers in the preform. Due to the inherent capacity of microstructural tailorability of RTM composites at the preform level through the use of sizings and textile architectures, there is a critical need to be able to determine metrics of performance through which this tailoring can be accomplished and measured. The

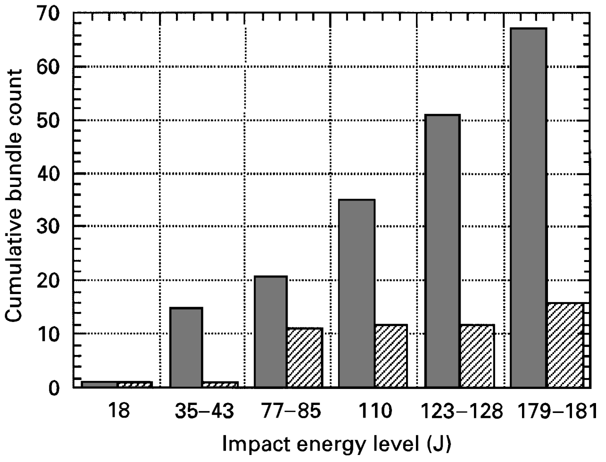

Figure 12 Comparison of cumulative bundle breakage as a function of impact energy. (▩) 0/90/C; (▨) prepreg.

paper describes the response of a set of RTM composites fabricated using both woven (plain weave) and non-woven architectures, in terms of a set of pre-established and new metrics. The use of IEC curves and impact performance maps is shown to give more materials specific data from an impact event. Further interrogation of the response of a materials system in terms of the often conflicting requirements of energy absorption (with globally debilitating damage modes such as delamination) and impact resistance is shown to be possible through the use of damage performance maps. Results of the limited tests conducted show that the use of non-woven fabric architectures backed with chopped strand mat perform better than plain weaves over equivalent impact energy levels due to the additional nesting and interlayer penetration achieved during compaction.

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References
1. S. ABRATE *Appl. Mech. Rev.* **44** (1991) 155.
2. W. J. CANTWELL and J. MORTON *Composite Struct.* **12** (1989) 39.
3. R. W. RYDIN and V. M. KARBHARI in proceedings of the 10th Annual ASM/ESD Advanced Composites Conference, Dearborn, MI (ASM International, Metals Park, OH, 1994) p. 117.
4. *Idem., J. Reinforced Plastics Composites* **14** (1995) 1175.
5. S. N. KAKARALA and J. L. ROCHE ASTM STP 936 (1987) 144.
6. T. L. LINDSAY, Masters Thesis submitted to the Materials Science Program, University of Delaware, (1990).
7. L. W. GAUSE and L. J. BUCKLEY ASTM STP 936 (1987) 248.
8. C. W. KNAKAL and D. R. IRELAND ASTM STP 936 (1987) 44.
9. R. L. SIERAKOWSKI, L. E. MALVERN and C. A. ROSS in "Failure modes in composites, III", edited by T. T. Chiao (AIME, 1976 New York) p. 73.
10. P. A. LAGACE and E. WOLF *AIAA J.* **33** (1995) 1106.
11. W. C. JACKSON and C. C. POE *J. Composites Technol. Res.* **15** (1993) 282.
12. R. W. RYDIN, M. B. BUSHMAN and V. M. KARBHARI *ibid.* **14** (1995) 113.
13. R. W. RYDIN, A. LOCURCIO and V. M. KARBHARI *ibid.* **14** (1995) 1199.
14. V. M. KARBHARI and R. W. RYDIN in Proceedings of the 40th International SAMPE Symposium, Anaheim, May 1995, p. 1971.
15. J. E. McCARTY and R. S. WHITEHEAD USAF Contract # F33615-82-C-3213, Fourth Government/Industry Briefing, San Antonio, TX, 22 May, 1986.
16. R. H. TOLAND, *AIME* (1973) 271.
17. H. MATSUHASHA, M. J. GRAVES, J. DUGUNDJI and P. A. LAGACE AIAA-1993-1611-CP, (1993) p. 2668.
18. R. L. SIERAKOWSKI, L. E. MALVERN and C. A. ROSS *AIAA J.* **23** (1976) 442.

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