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Design of Stitched Sandwich Composites using Response Surface Modeling

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1. SUMMARY

Sandwich composites are widely used in the aerospace industry due to their high flexural rigidity that is associated with their unique material architecture. These structures have superior flexural rigidity due to a light-weight internal core that is bonded to thin, rigid composite facesheets. However, debonding between the facesheet and the core in sandwich composite panels can occur at relatively low out-of-plane loads. Additionally, visual inspection of the outermost surfaces provides little indication of internal delamination that may be present. In this study, sandwich composites are manufactured with through-the-thickness reinforcements, or stitches, to act as crack-growth inhibitors and to improve interfacial properties. The objective of this study was to determine the influence of stitching on the fracture behavior of stitched sandwich composites. Therefore, this research develops a stitch parameter design space for future or existing aircraft programs using stitched sandwich composite parts. A design of experiments (DoE) approach was implemented to maximize the mode I fracture energy based on optimal selection of stitching parameters (stitch density and linear thread density). A response surface model (RSM) based on stitch design parameters was used to inform a finite element model for the prediction of crack-growth arrestment. Optimal stitch design parameters based upon maximizing the fracture energy were incorporated on a structural component, such as a stitched composite strut door.

This report presents the experimental and computational methodologies at a coupon-level for future use in predicting the structural-scale crack-growth arrestment. An extensive literature review [2] was performed to understand the state of the art regarding the influence of stitching on the out-of-plane behavior of polymer matrix composites. In particular, this review provides insight into the mode I and mode II fracture properties, interlaminar properties, and impact behavior of stitched composites. Experimental methods of stitched composites were investigated to inform an experimental test plan that used DoE, which minimized the required number of experiments to fully characterize the out-of-plane behavior of stitched sandwich composites within a selected design space. Based on the DoE approach, a response surface model was developed to determine optimum stitch parameters. These parameters and experimental test results were used to inform a finite element model for predicting crack-growth arrestment.

In summary, the major contributions of this project are listed as follows.

1. An extensive (140 papers) literature survey was conducted on the mechanistic behavior of stitched composites. The key findings include the following.
 - a. Stitch density and linear thread density are the two primary factors that influence the out-of-plane performance of composite materials.
 - b. Few studies were found on sandwich composites and high-temperature composites.

These findings encouraged our research to investigate the mechanical response of stitched polymer matrix composites.

2. A methodology to use embedded optical fibers to estimate the internal crack front within single and double cantilevered beam (DCB) specimens was developed.

3. A statistical model to predict the influence of stitched parameters and their interactions on the out-of-plane behavior of stitched sandwich composites was developed.
4. A discrete cohesive zone modeling approach with unique traction-separation laws to predict crack growth in stitched sandwich composites was developed.
 - a. Performed a 2D sensitivity analysis on the trilinear traction-separation law and provided recommendations for determining the cohesive stiffness, maximum elastic stress, bridging stress, and effective displacement.
 - b. Characterized the crack front curvature near the through-the-thickness reinforcement.
5. Laboratory test results were used to inform the fabrication and testing of a subcomponent
 - a. A stitched sandwich strut door was manufactured.
 - b. Testing setup of the composite strut door has been completed.

2. INTRODUCTION

2.1. Background

Sandwich composites are widely used in the aerospace industry due to their high specific flexural stiffness. However, sandwich composites are limited by low interfacial strength that arises from a material property mismatch between the composite face sheets and the internal core material [5]. Separation of the face sheets from the core material reduces the strength and stiffness of the sandwich composite structures [6]. Furthermore, visual inspection of the outer surface provides little indication of the internal delamination that may be present due to loadings such as low velocity impact.

Novel approaches have been developed to reinforce composite laminates using z-pinning, 3D weaving, and stitching in the through-the-thickness direction [7, 8]. Primarily, stitching has proven to be the simplest and most effective 3D reinforcement to increase the interlaminar strength of composite laminates [7]. For example, the complex Pultruded Rod, Stitched, Efficient, Unitized Structure (PRSEUS) panel, developed by NASA and the Boeing Co., used stitching to improve transverse properties by integrating stitching between skin and stringer joints [9]. It should also be noted that stitching parameters have considerable influence on the mechanical performance in the in-plane and out-of-plane directions of composite structures. These parameters include thread material, thread ply, thread diameter, thread finish, stitching density, and the stitching pattern [4]. Tan et al. [10-12] studied the influence of stitching density and thread thickness on damage progression and failure characteristics of stitched composites and found that stitching is highly effective in resisting damage propagation. However, in quasi-static indentation tests, stitched composites showed lower damage initiation loads when compared to unstitched composites. Liotier et al. [13] characterized the complex morphology of the sewn region in stitched composites, in which resin rich regions form adjacent to the stitching thread. These resin rich areas develop, which effectively reduce the in-plane properties. These microcracks are also intensified under elevated temperatures. A recent publication by Liotier et al. [14] investigated the thermal and moisture influence on the resin rich areas near polyester and polyamide stitched regions in a carbon/epoxy composite laminate. It was shown that thermal cycling enhances the development of microcracks near the vicinity of all stitched regions [14].

Although much research has been performed on the influence of stitching parameters in solid composite laminates, very few studies have investigated the influence of stitching parameters on the mechanical properties of sandwich composites, nor have stitching parameters such as linear thread density (thread diameter), thread material, and thread finish been investigated. Peiyan Wang et al. [15] evaluated the flexural properties of orthogonally stitched foam core sandwich structures using a micromechanical approach. It was determined that the bending failure load, flexural rigidity, and shear rigidity increased with increasing stitch density. Other investigations have involved the effect of the angle of stitching. Lascoup et al. [5] investigated the influence of angled glass stitching in a polyester resin/ glass sandwich composite with an internal polyurethane and polyester foam core under four-point flexure, out-of-plane compression, and shear loading. The failure mechanisms of the stitched sandwich are highly dependent on the angle of the internal stitching [5]. Large stitch angles ($> 60^\circ$) produced internal buckling of the stitches. Furthermore, the trajectory of each stitch randomly intersected adjacent stitching, which

resulted in interpenetration during manufacturing and reduced the capability to resist out-of-plane deformation [5]. Lascoup extended this work by developing analytical approaches to estimate the ultimate shear, compressive strength, and bending stiffness of stitched sandwich composites, but found that the foam limits the rotation of the stitching. It was noted that additional research is needed to understand the influence of foam on the stitches' behavior. Also, stitching creates resin channels within the foam that allow ease of resin impregnation prior to cure [16, 17]. An optimization study is needed to minimize the additional weight due to the resin filled channels.

2.2. Scope

The objective of this research was to develop optimal parameters that can be used in through-the-thickness reinforcing processes of sandwich composites to minimize core-facing separation. Currently, sandwich composites are widely used in the aerospace industry due to their high specific flexural stiffness. However, they are limited by their low interfacial shear strength between the core and facing sheets. Therefore, this research will use a design of experiments (DoE) approach to investigate the use of stitching, and its associated parameters, in the arrestment of core-facing separation. The DoE approach enabled the development of a response surface model (RSM) to minimize core-facing separation and aid in designing redundant delamination arrestment features of lightweight aerospace vehicles. Additionally, a finite element model was developed to include the damage arrestment features to obtain an improved predictive capability for sandwich composites. A subcomponent (sandwich composite strut door) was fabricated using the optimal stitching parameters through the complete acreage of the part and tested under a single point loading condition. The test results were compared to those of a strut door fabricated with perimeter (around the foam core) stitching and tested under the same loading condition. The primary tasks of this research included:

1. Literature Review: Determine the state of the art on the out-of-plane behavior of stitched composites.
2. Experimental: Determine the influence and interaction (coupling effects) of stitching parameters on the core-to-facing shear and fracture behavior of sandwich composites.
3. Computational: Develop a predictive methodology using finite element analysis to assess the arrestment of core to facesheet separation in stitched sandwich composites.
4. Implementation: Incorporate optimal stitching parameters in a structural scale test article to assess their efficacy in arresting core to facesheet separation.

3. METHODS, ASSUMPTIONS & PROCEDURES

Data used in this study comprised of two experimental studies. In the first, a design of experiments approach was used to characterize the influence of through-the-thickness reinforcements on the mode I fracture energy, which was determined from single cantilever beam (SCB) tests. A response surface model was developed to predict the fracture energy of stitched sandwich composites based on the selected input factors (stitch density, linear thread density, and facesheet thickness). Unique fracture surface morphologies are exhibited during mode I testing that are dependent on stitch processing parameters. It is shown that an optimum selection of stitch processing parameters can be obtained to maximize the mode I fracture energy. In the second study, a double cantilever beam test was conducted on a composite laminate specimen from which internal strains were determined from an embedded optical fiber (OF); a Lagrangian cross-correlation approach was used to estimate the delamination front location.

3.1. Experimental Study 1: Influence Of Stitch Parameters on the Fracture Energy of Stitched Sandwich Composites

3.1.1. Design of Experiments

Statistical design of experiments (DoE) can be used to identify and model complex relationships between input factors and output material responses to facilitate the development of a more physics-based model. A phenomenological response surface model (RSM) was developed using a face-centered central composite design (FC-CCD) of experiments.

The selected input factors in this study are stitch density (X_1), linear thread density (X_2), and facesheet thickness (X_3). The FC-CCD is based on a 2^3 factorial treatment design consisting of 15 design points [19] and its design space is based upon nondimensional coded levels (x_i) of the actual input factors (X_i), where $x_i = -\alpha, -1, 0, 1, \text{ or } \alpha$ [19]. A graphical representation of the FC-CCD with treatment combinations of the coded factors x_1, x_2 , and x_3 at each design point is shown in Figure 1. The coded levels, and their corresponding actual levels, are shown for each actual input factor (X_i) in Table 1. At each design point, three replicates were tested to determine the pure error for estimating the lack of fit of the measured fracture energy. A total of 45 experiments were performed. The linear relationship between the coded levels (x_i) and the actual levels (X_i) is given by

$$x_i = \frac{2X_i - (X_{iHigh} + X_{iLow})}{(X_{iHigh} - X_{iLow})} \quad (1)$$

where X_{iHigh} and X_{iLow} correspond to $x_i = 1$ and -1 , respectively, for $i = 1, 2, 3$.

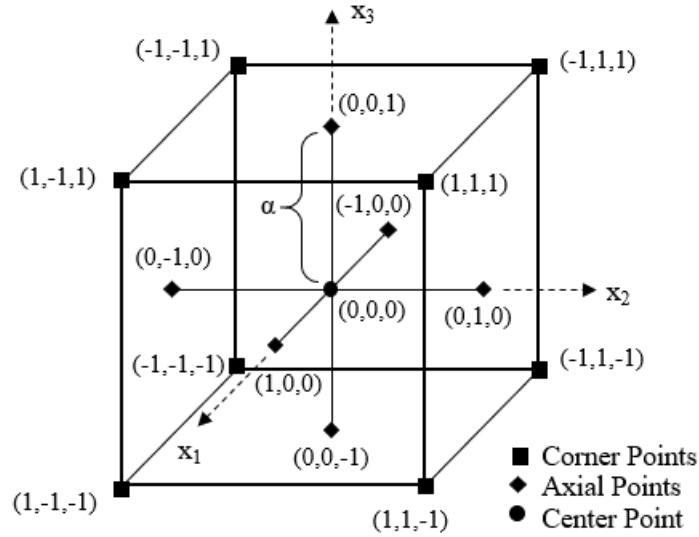


Figure 1. Face-centered central composite design space with non-dimensional coded factors (x_1 = stitch density, x_2 = linear thread density, and x_3 = facesheet thickness)

Table 1. Non-dimensional coded levels and actual levels for each FC-CCD factor

Coded Level	Actual Levels		
	Stitch Density, X_1 (stitches/mm ²)	Linear Thread Density, X_2 (Denier)	Ply Thickness, X_3 (mm)
x_i			
-1	0.00155	400	1.778
0	0.005775	800	2.667
1	0.01	1200	3.556

The range of levels of each factor (X_1 , stitch density; X_2 , linear thread density; and X_3 , facesheet thickness) was determined based on a previous experimental study [20] and from current literature. A stitch density range of $0.0015 \leq X_1 \leq 0.01$ stitches/mm² was used. The upper limit of $X_1 = 0.01$ stitches/mm² was selected based on a study from Wang et al. [15] and corresponds to an equal pitch and spacing of 10 mm. Increasing the stitch density from 0.01 stitches/mm² to 0.04 stitches/mm² was shown to degrade the flexural modulus of the carbon/epoxy sandwich composite when compared to its unstitched counterpart. Furthermore, the lower limit $X_1 = 0.0015$ stitches/mm² was selected from previous studies [5, 21], where the spacing between stitching was approximately 25 mm. The linear thread density ranged from $400 \leq X_2 \leq 1200$ Denier was based on previous studies on stitched composite laminates [11, 22]. The minimum facesheet thickness was selected to prevent facesheet failure during mode I fracture testing in the range of $1.8 \leq X_3 \leq 3.6$ mm. The upper limit of the facesheet thickness $X_3 = 3.6$ mm was based on a sandwich composite redesign of a T 38 strut door [23]. Fracture energy at initiation and steady-

state was determined for each treatment combination in the FC-CCD to develop the RSM. In this study, a full quadratic RSM with a three-factor interaction term was considered and expressed as

$$Y = \beta_o + \sum_{i=1}^3 \beta_i x_i + \sum_{i=1}^3 \beta_{ii} x_i^2 + \sum_{i=1}^3 \sum_{\substack{j=1 \\ i < j}}^3 \beta_{ij} x_i x_j + \beta_{123} x_1 x_2 x_3 + \varepsilon \quad (1)$$

where Y is the fracture energy response, x_i are the coded levels of each input factor (x_1 = stitch density, x_2 = linear thread density, and x_3 = facesheet thickness). The estimates of the model parameters ($\beta_o, \beta_i, \beta_{ii}, \beta_{ij}$, and β_{123}) are determined by the method of least squares. The terms $x_i x_j$ represent two-factor interaction effects and $x_1 x_2 x_3$ represents the three-factor interaction effect, where $i, j = 1, 2, 3$, and $i < j$. The significance of each regressed term in Eq. (2) is evaluated using an analysis of variance (ANOVA) procedure. The error term ε is assumed to be normally distributed with a zero mean and constant variance. The error random variables are independent and identically distributed. These assumptions are verified by determining the studentized residuals, the normal probability plot of the predicted response, and are provided as supplemental data for this study. The regression parameters β are estimated as [19]

$$\beta_m = (A_{ml} A_{lm})^{-1} A_{ml} G_l \quad (2)$$

The nondimensional FC-CCD design space A_{lm} is represented geometrically for each regression term and observation at each treatment combination [19], where $l = 1, 2, \dots, n$ and $m = 1, 2, \dots, p$. The total number of regression parameters and measured observations correspond to p and n , respectively. The measured fracture energy G_l is analytically represented as

$$G_l = \left. \frac{P^2}{2B} \frac{dC}{da^*} \right|_{l=1,2,\dots,n} \quad (3)$$

where P is the reactive load and B is the specimen width. The term dC/da^* is the derivative of the compliance C with respect to the true crack length a^* . A modification of the crack length ($a^* = a + \Delta$) is introduced to ensure that the flexural modulus is independent of the measured crack length [24]. The compliance is given as [25]

$$C = m(a + \Delta)^3 \quad (4)$$

where the slope m and length Δ are evaluated from the linear relationship between $C^{\frac{1}{3}}$ and a [24, 25]. Substituting Eq. (5) into Eq. (4), the fracture energy can be obtained as

$$G_l = \frac{3P}{2B} \frac{\delta}{(a + \Delta)} \Big|_{l=1,2,\dots,n} \quad (5)$$

where δ is the applied displacement during testing.

3.1.2. Fabrication of Stitched Sandwich Composite Specimens for Mode I Testing

Single cantilever beam (SCB) specimens were fabricated from an infused epoxy/carbon fiber sandwich composite panel with 110 kg/m³ foam core. The core was perforated manually in a 6.35 mm grid spacing with a 0.79 mm diameter needle to allow the resin to perfuse through the core during infusion. The carbon/epoxy facesheets were comprised of a cross-ply layout configuration $[0^\circ/90^\circ/90^\circ/0^\circ]_i$, where $i = 2, 3, 4$ correspond to the facesheet thicknesses (X_3) in Table 1. A 0.0127 mm Teflon film was used as the crack initiator at a depth of approximately 76.2 mm from the edge of the laminate. The dry preform with foam core was stitched using VectranTM thread with a stitch density (X_1) and linear thread density (X_2) dictated by the FC-CCD. The stitch pitch is the same distance as the stitch spacing and was determined by the relative stitch distance $D = \sqrt{1/X_1}$. The sandwich composite preforms were stitched using a 2000H Juki industrial sewing machine using a modified lock stitch. A 10 mm distance was maintained between the initial crack length and the first row. The 2.25 mm diameter needle was used to stitch the dry preforms and was selected based on robotic stitching processes [26].

The dry sandwich composite preforms were infused using a one-sided vacuum assisted resin transfer molding process with an out-of-autoclave Hexflow 1078 epoxy resin system. The dry sandwich structure and epoxy resin were separately heated to 88 °C to reduce the viscosity of the resin system before infusion. After infusion, the temperature was increased at a rate of 1.8 °C/min to a temperature of 177 °C, held for two hours, and reduced to room temperature (24 °C). After cure, the samples were sectioned into 20 cm by 5 cm test coupons, as illustrated in Figure 2. The sizing of the SCB coupons was based on preliminary tests and based upon current SCB standards [27]. Piano hinges were bonded to the top facesheet above the initial defect using Loctite Hysol EA 9394 adhesive.

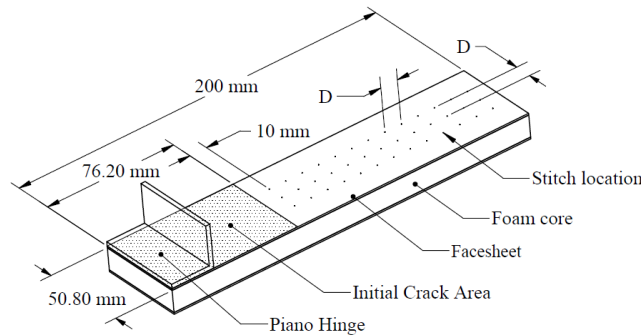


Figure 2. Schematic of a single cantilevered beam (SCB) specimen

3.1.3. Single Cantilever Beam Testing

The single cantilever beam (SCB) testing was conducted in accordance with ASTM STD 5528-13 and based on testing standards developed by Ratcliffe [27] and Cantwell [28]. Force was applied to the piano hinge at a constant displacement rate of 0.5 mm/min. The bottom surface of the specimen was rigidly constrained with a non-rotating base. An example of this test with a specimen subjected to load is shown in Figure 3. A 1 kN load cell was used to measure the reactive load. Crack lengths were recorded using visual measurements with the aid of an ARAMIS digital image correlation system. The SCB tests were performed at each treatment combination (X_1 , X_2 , X_3) as determined by the FC-CCD and a total of three replicates were performed at all design points.

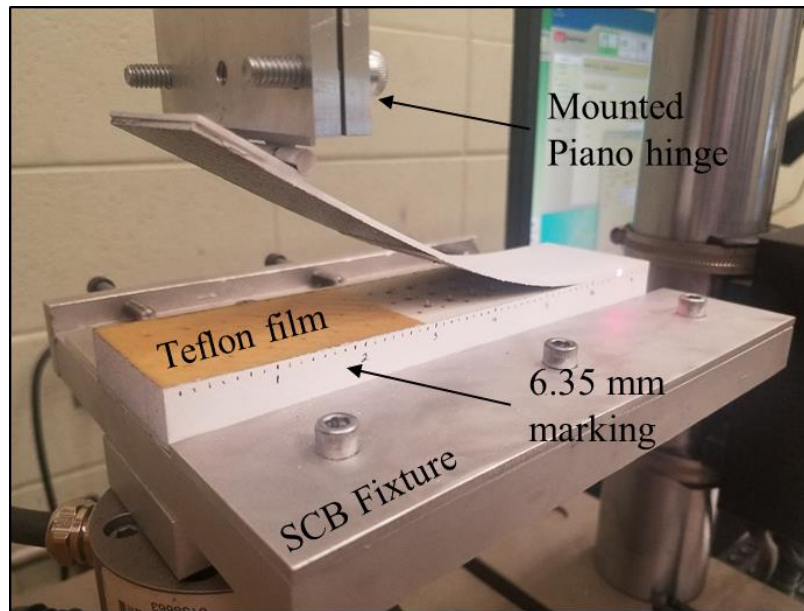


Figure 3. Testing of a single cantilevered beam (SCB) specimen

3.1.4. Stitched Sandwich Composite Fracture Behavior

The load and crack length response for typical stitched SCB specimens is shown in Figure 4. For every test, an initial linear response prior to the start of the propagation of delamination was observed and at crack initiation, a sudden decrease in the reacted load occurred. A layer of epoxy resin formed underneath the Teflon film during fabrication, resulting in unstable crack growth due to an increase in the crack-tip radius. As the crack front approaches the vicinity of the first stitch row, the reacted load linearly increases until stitch failure occurred (Region 2). Upon failure of a stitch row, the crack lengths immediately progressed to the next stitch row. Additionally, multiple stitch rows appeared to fail for high linear thread densities ($X_2 = 1200$ Denier) and low facesheet thicknesses ($X_3 = 1.778$ mm). As subsequent stitch rows failed, a “saw-tooth” pattern was observed in the load-displacement response (Points 2-6). The magnitude of the failure load at each stitching row decreases with an increase in the applied displacement. The required load to develop the necessary tensile stresses to advance the crack front decreases.

This behavior is due to the greater distance between the location of the reacted load and the crack front, which increases with each subsequent stitch row failure.

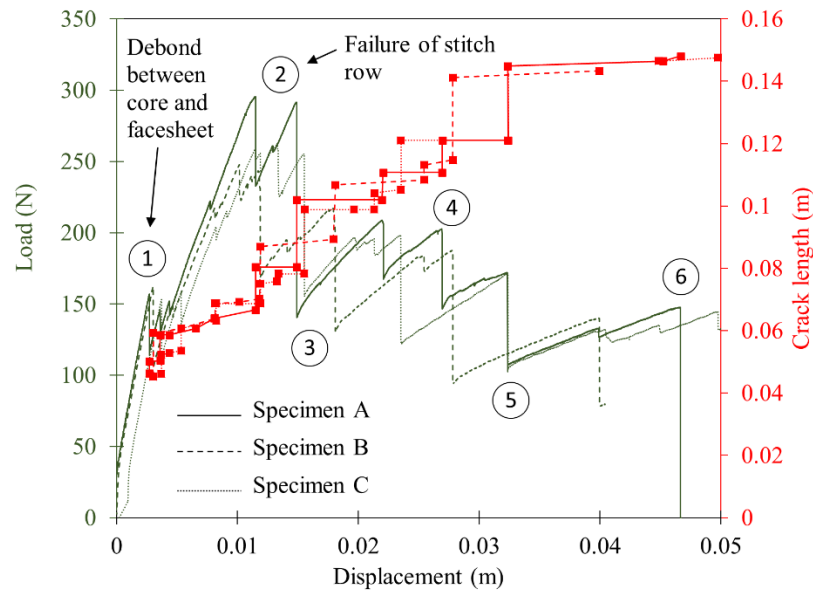


Figure 4. Load and crack length behavior for stitched SCB specimens (X1=0.01 stitches/mm², X2=1200 Denier, X3=1.776 mm)

The fracture surface of a representative SCB sample is shown in Figure 5a. During facesheet to core separation, “candle-like” structures formed on the surface of the facesheet that correspond to the epoxy matrix-stitch columns that internally failed inside the core during each test. Region 1 (R1) is the characteristic resin-stitch column after stitch failure. Region 2 (R2) is the interface between a resin-stitch column and facesheet after stitch failure. It was observed that the stitch primarily failed within the core (developing matrix-stitch columns on the facesheet) or within the facesheet. The diameter of the matrix-stitch column is approximately 6% greater than the needle diameter used in the stitching process. This indicates that resin will bleed into damaged foam cells adjacent to the stitch. In summary, three failure mechanisms are observed: 1) matrix stitch pullout at the facesheet-core interface, 2) matrix-stitch column frictional pullout, and 3) matrix stitch frictional pullout with ductile core failure. These failure mechanisms are illustrated in Figure 5b. The facesheet-core interface failure primarily occurs due to tensile failure of the Vectran™ thread within the facesheet and is consistent with previously observed stitch interface failures for composite laminates [29, 30]. Failure at the facesheet-core interface may also develop secondary in-plane fiber bridging due to carbon fibers that were damaged in the stitching process. Frictional pullout of the matrix-stitch column occurs when the interfacial shear strength between the column and foam core is exceeded due to high linear thread densities ($X2 \geq 1200$ Denier).

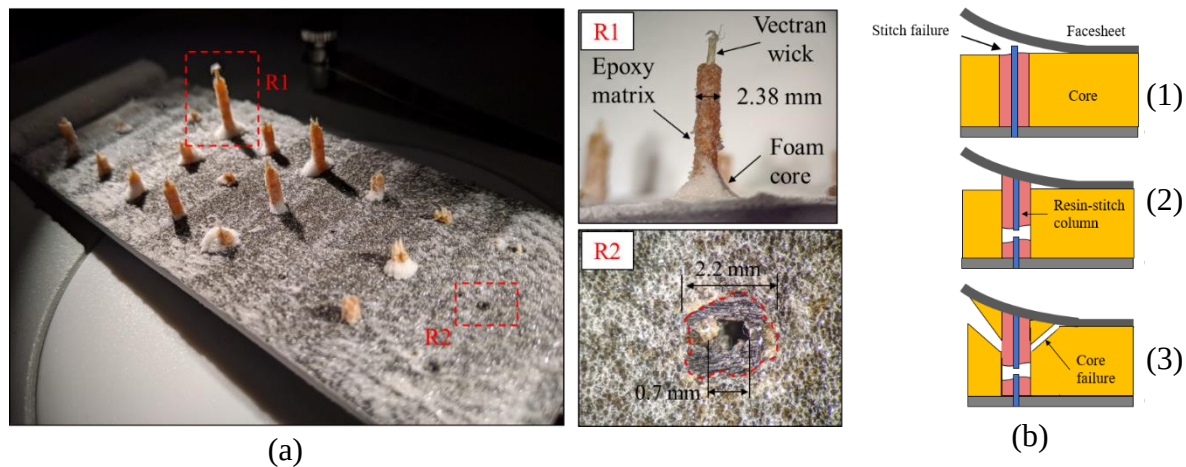


Figure 5. (a) Fracture surface of a delaminated specimen and (b) sandwich composites failure mechanisms.

The fracture energy of representative stitched specimens with respect to the applied displacement is shown in Figure 6. Upon crack initiation (Zone 1), the fracture energy is relatively constant and is consistent with measurements previously reported for unstitched sandwich composites [20]. This is primarily due to the lack of toughening mechanisms to retard crack growth. However, the fracture energy increases (by approximately 600%) linearly as the crack growth approaches the initial stitch row (Zone 2). This behavior is highly dependent on the input factors (X_1 = stitch density, X_2 = linear thread density, and X_3 = facesheet thickness) and will be discussed later. Fracture energies obtained from Zone 2 represent the fracture energy required to propagate crack growth due to the presence of the through-thickness stitching. The maximum fracture energy developed due to each stitch row is relatively constant. The failure of the stitch rows resulted in significant reductions in the fracture energies and produced unstable crack growth between the facesheet and the core (Zone 3). As the stitch density decreases, the fracture energy approaches the unstitched fracture energies.

The maximum fracture energy and unstitched fracture energies were recorded for each coded treatment combination shown in Table 2. The maximum fracture energy was normalized by the fracture energies in Zone 1 (unstitched) to study the relative influence of each input factor (X_1 = stitch density, X_2 = linear thread density, and X_3 = facesheet thickness). These experimental results were then used to develop the response surface model based on the ANOVA procedure.

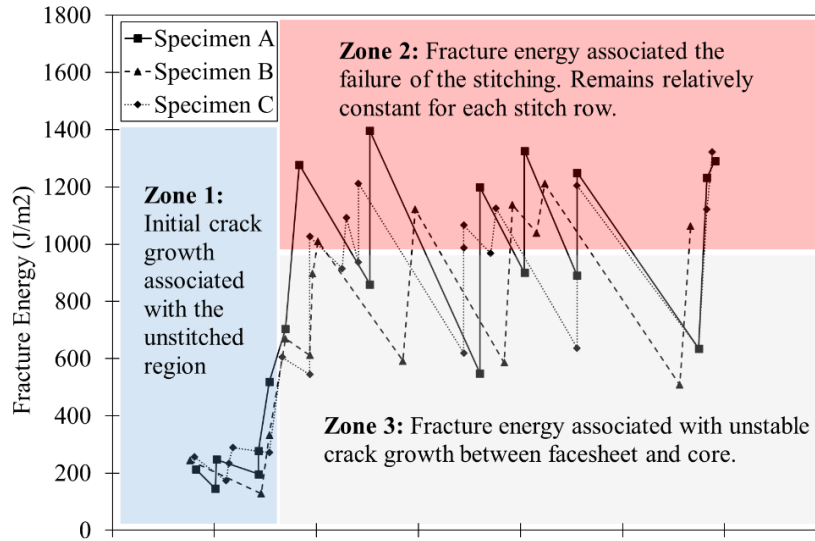


Figure 6. Fracture energy response for stitched sandwich composite specimens (X1=0.01 stitches/mm², X2=1200 Denier, X3=1.776 mm)

Table 2. Non-dimensional coded levels and actual levels for each FC-CCD factor

x_1	x_2	x_3	Max Fracture Energy* (J/m²)	σ (Max)	Fracture Energy at Initiation* (J/m²)	σ (Initial) (J/m²)	Normalized Fracture Energy* (J/m²)	σ (Normalized) (J/m²)
-1	-1	-1	507.6	46.6	280.9	29.3	1.8	0.3
1	-1	-1	706.6	45.8	288.6	31.2	2.5	0.4
-1	1	-1	1441.2	58.2	266.5	21.4	5.4	0.4
1	1	-1	2293.9	160.5	254.9	66.8	9.4	2.1
-1	-1	1	360.1	19.6	292.0	22.2	1.2	0.1
1	-1	1	582.5	40.2	314.3	56.7	1.9	0.4
-1	1	1	660.7	43.6	243.1	15.7	2.7	0.3
1	1	1	2190.8	194.8	268.7	34.5	8.3	1.5
-1	0	0	540.0	71.8	280.0	23.0	1.9	0.1
1	0	0	990.0	36.3	267.9	28.4	3.7	0.3
0	-1	0	570.8	24.5	260.3	10.9	2.2	0.0
0	1	0	1795.7	168.4	267.2	22.4	6.7	0.3
0	0	-1	947.5	56.2	228.9	21.6	4.2	0.6
0	0	1	739.3	26.7	258.5	7.3	2.9	0.1
0	0	0	712.7	36.9	282.5	22.3	2.5	0.3

x_1 : coded levels of stitch density

x_2 : coded levels of linear thread density

x_3 : coded levels of facesheet thickness

σ : standard deviation

*Average from three replicates per design point

3.1.5. Response Surface Model

The ANOVA procedure was used to determine the significance of the response surface model (RSM), each corresponding term, and the overall lack of fit. The ANOVA table for the response surface model is shown in Table 3. The sum of squares (SS), mean square (MS), and F-value are determined for the model, each regression parameter, and the residual. The SS_{Residual} is decomposed into the lack of fit and pure error components. The lack of fit represents how well the predicted response approximates the experimental values. The pure error signifies the variability in the measured response and is estimated at each design point and across all replicated treatment combinations. In this study, an $\alpha = 0.05$ is used to evaluate the significance of the lack of fit F test prior to determining the significance of the model. A non-significant lack-of-fit is desired since that indicates the RSM fits well with the measured values. Following the analysis of variance, the regression parameters were determined using Eq. (3) to develop the final RSM as a function of stitch density (x_1), linear thread density (x_2), and facesheet thickness (x_3). The RSM was evaluated at a 0.05 level of significance. Furthermore, the model's corresponding terms (linear, quadratic, and interaction terms) were evaluated using partial F-tests at a 0.10 level of significance. In this study, all terms with an initial P value < 0.10 were kept in the model to ensure that significant terms were not prematurely removed due to error introduced by other terms [31]. A backward elimination approach was used to remove any nonsignificant terms based on the principle of hierarchy [19]. Nonsignificant lower order polynomial terms were retained because they appeared in significant higher order polynomial terms.

The normalized fracture energy was transformed to lognormal space to obtain an improved fit to the experimental data. The ANOVA table provided in Table 3 shows the development of the F-tests of the model and the corresponding terms. The model was determined to be adequate ($p\text{-value} < 0.0001$) for predicting normalized fracture energies within the design space of the FC-CCD. Furthermore, the lack of fit was determined to be non-significant and shows good agreement to measured data in predicting the normalized fracture energies. Only two terms (x_3^2 and $x_2 \cdot x_3$) were determined to be non-significant; however, $x_2 \cdot x_3$ was retained within the model due to the principle of hierarchy. The $R^2 = 0.9504$ indicates that 95.04% of the total variation in the normal fracture energies is explained by the RSM [19].

Using Eq. (3) with the selected terms from the ANOVA analysis, the RSM was developed for the normalized fracture energy. The nondimensional form (x_1, x_2, x_3) of the RSM is expressed as

$$\ln(G_{\text{normal}}) = 1.14 + 0.30x_1 + 0.58x_2 - 0.18x_3 + 0.11x_1x_2 + 0.09x_1x_3 - 0.02x_2x_3 - 0.17x_1^2 + 0.20x_2^2 + 0.06x_1x_2x_3 \quad (6)$$

This model can be transformed from coded variables (x_1, x_2, x_3) to uncoded variables (X_1, X_2 , and X_3) using Eq. (1). The corresponding uncoded RSM is expressed as

$$\ln(G_{\text{normal}}) = 0.66 + 146.54X_1 + (1.7 \times 10^{-4})X_2 - 0.12X_3 - 0.035X_1X_2 - 8.04X_1X_3 - (2.8 \times 10^{-4})X_2X_3 - 9319.26X_1^2 + (1.23 \times 10^{-6})X_2^2 + 0.0384X_1X_2X_3 \quad (8)$$

Table 3. ANOVA table of the normalized fracture energy model.

Source	SS	df	MS	F-value	p-value	Conclusion
Model	14.7983	9	1.6443	74.5641	< 0.0001	Significant
x_1	2.7178	1	2.7178	123.2469	< 0.0001	Significant
x_2	10.0978	1	10.0978	457.9206	< 0.0001	Significant
x_3	1.0081	1	1.0081	45.7141	< 0.0001	Significant
$x_1 \cdot x_2$	0.3100	1	0.3100	14.0569	0.0006	Significant
$x_1 \cdot x_3$	0.1740	1	0.1740	7.8918	0.0081	Significant
$x_2 \cdot x_3$	0.0089	1	0.0089	0.4049	0.5287	Not significant
x_1^2	0.2325	1	0.2325	10.5417	0.0026	Significant
x_2^2	0.3249	1	0.3249	14.7315	0.0005	Significant
$x_1 \cdot x_2 \cdot x_3$	0.0799	1	0.0799	3.6225	0.0653	Significant
Residual	0.7718	35	0.0221			
Lack of Fit	.2024	5	0.0405	2.1325	0.0887	Not significant
Pure Error	0.5694	30	0.0190			
Total	15.5701	44				
R^2	0.9504					
R^2 Adjusted	0.9377					

SS: Sum of squares
df: degrees of freedom
MS: Mean square

The magnitude and corresponding sign of the coefficient signify the overall contribution of each term to the normalized fracture energy. For instance, increasing the facesheet thickness (x_3 , X_3) decreases the normalized fracture energy. A three-dimensional plot of the normalized fracture energy as a function of stitch density (X_1) and linear thread density (X_2) at a facesheet thickness $X_3 = 1.776$ mm is shown in Figure 7. Increases in the normalized fracture energies are primarily associated with increases in linear thread density and stitch density. Linear thread density (x_2 , X_2) appears to be the most dominant factor to increase the normalized fracture energy, followed by stitch density (x_1 , X_1).

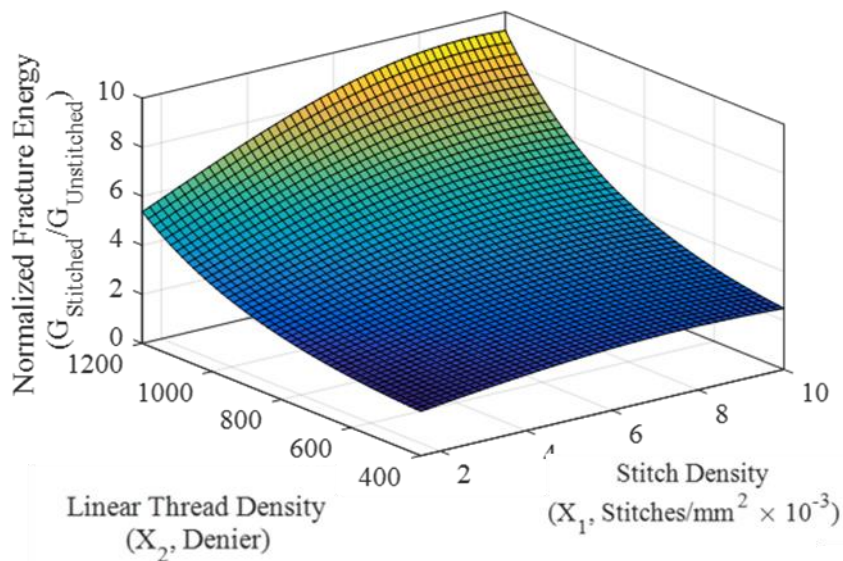


Figure 7. Normalized fracture energy as a function of stitch density (X_1) and linear thread density (X_2) for a thickness of $X_3 = 1.778$ mm ($x_1 = -1$)

3.1.6. Results and Discussion

The normalized fracture energies as a function of stitch density (x_1) for select linear thread densities ($X_2 = 400, 800$, and 1200 Denier) and facesheet thicknesses ($X_3 = 1.776, 2.667$, and 3.556 mm) are shown in Figure 8(a) - 8(c). Experimental data used in the FC-CCD and auxiliary data are provided for comparison to the RSM. Overall, the response surface model shows adequate precision in estimating the normalized fracture energy in comparison to the auxiliary data points identified in Figure 8(c).

For each facesheet thickness (X_3), the fracture energy is improved by up to 400% for low linear thread densities ($X_2 = 400$ and 800 Denier) and is relatively constant with respect to stitch density (X_1). At these linear thread densities, the primary traction-separation response is stitch pullout at the facesheet-to-core interface, which is identified in Figure 5b(a). However, increasing the linear thread density from $X_2 = 400$ Denier to $X_2 = 1200$ Denier greatly increases the normalized fracture energy from a factor of two to a factor of ten for each facesheet thickness (X_3). The relative improvement is attributed to the change in the fracture morphology during mode I facesheet-to-core separation. At high linear thread densities ($X_2 \geq 1200$ Denier), matrix-stitch column pullout and ductile failure of the core occurs (Figure 5b (1) and Figure 5b (3)). Furthermore, the overall contribution to the observed fracture energies is due to differences in the cylindrical surface area of the stitch and matrix-stitch column that develop due to altering the linear thread density. The matrix-stitch column has approximately three times greater surface area than an $X_2 = 1200$ Denier Vectran thread and requires greater fracture energy to promote crack growth.

The contribution of stitch density (X_1) to the overall normalized fracture energy is primarily seen at high linear thread densities ($X_2 = 1200$ Denier). For a facesheet thickness between $2.667 \text{ mm} \leq X_3 \leq 3.556 \text{ mm}$, increasing stitch density linearly increases the normalized fracture energy from approximately 250% to 800%. At a facesheet thickness of $X_3 = 1.778 \text{ mm}$ and linear thread density $X_2 = 1200$ Denier, the normalized fracture energy increases linearly between $X_1 = 1.5 \times 10^{-3}$ stitches/mm² and 7×10^{-3} stitches/mm². At a stitch density of approximately 9.3×10^{-3} stitches/mm², the normalized fracture energy reaches a maximum value of approximately 929%. This stitch density corresponds to an optimum stitch distance $D = 10.38 \text{ mm}$. The relative optimum stitch distance is consistent with previous literature and appears to be dependent on linear thread density. Further increases in the stitch density may result in the degradation of flexural stiffness of the stitched sandwich composite.

For a constant stitch density (X_1) and linear thread density (X_2), the normalized fracture energy decreases with increasing facesheet thickness ($1.778 \text{ mm} \leq X_3 \leq 3.556 \text{ mm}$) by approximately 13%. This may be due to bridging of the stitches near the crack front. Farmand-Ashtiani et al. [32] has shown that fracture energy is dependent on specimen geometry when large-scale bridging occurs at the delaminated interface. Therefore, the fracture energy values in this study should be treated as apparent values due to a geometry dependence. The reduction in the fracture energy has also been observed by Saseendran et al. [33] for unstitched sandwich composite specimens with carbon/epoxy facesheets with a honeycomb core. The single-cantilever beam test is inherently a mixed-mode test, but mode I dominates, due to the material mismatch at the facesheet-to-core interface. The reduction in the fracture energy may be due to a shift in the mode-mixity phase angle, which quantifies the ratio of shear (mode II) to normal (mode I) loading at the crack tip [34]. These shifts in the mode-mixity phase angle may result from

differences in the crack root rotation as the crack growth processes near a stitch row for different facesheet thicknesses. High crack root rotations develop additional shear stresses that can reduce fracture energy of the stitching required to promote crack growth, which is counter intuitive for unstitched laminates. This is primarily because stitched laminates have a greater mode I fracture energy than a mode II fracture energy [35-37]. Therefore, additional shear stresses near the crack front can reduce the effectiveness of the stitching to arrest crack growth.

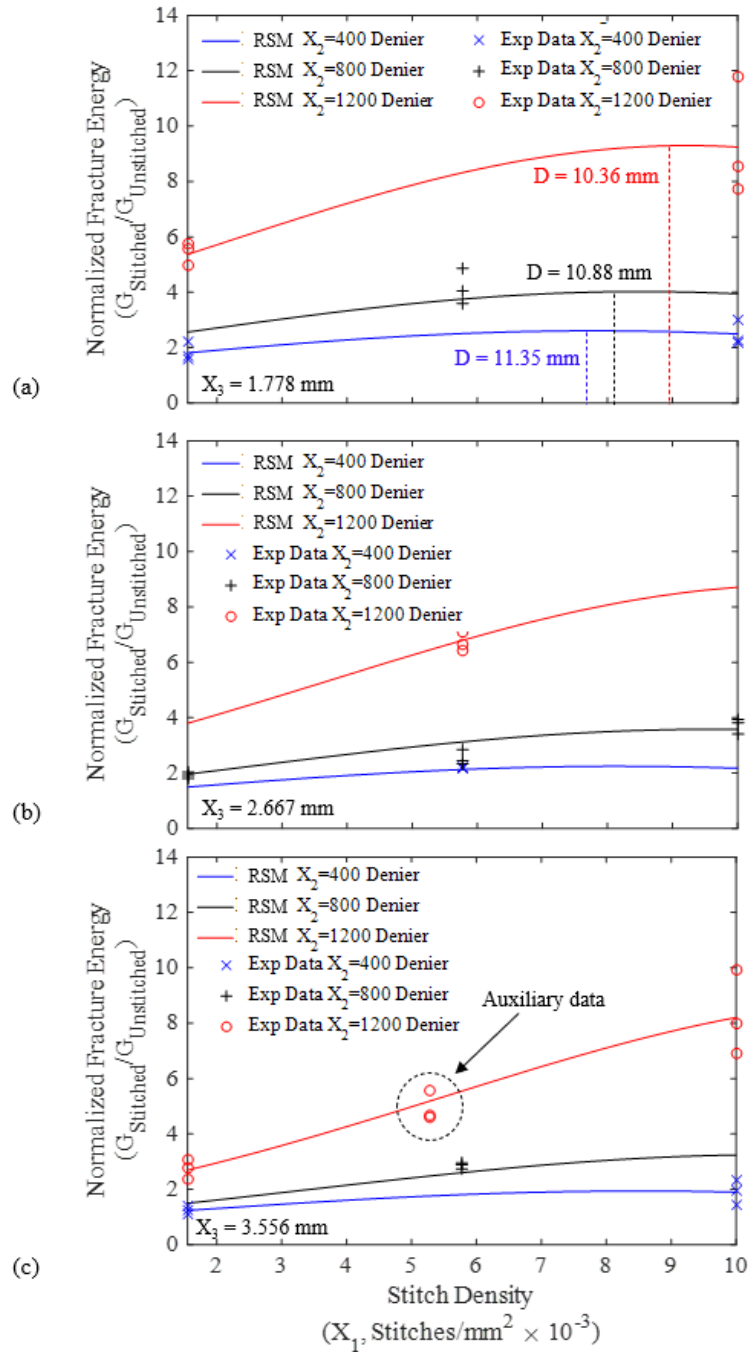


Figure 8. Normalized fracture energy as a function of stitch density (X_1) for select linear thread densities ($X_2 = 400, 800, \text{ and } 1200 \text{ Denier}$) and for thicknesses of (a) $X_3 = 1.778 \text{ mm}$, (b) $X_3 = 2.556 \text{ mm}$, and (c) $X_3 = 3.556 \text{ mm}$

3.1.7. Conclusions

A face-centered central composite design (FC-CCD) of experiments was used to evaluate the influence of stitch parameters (stitch density and linear thread density) and specimen geometry (facesheet thickness) on the mode I fracture energy of stitched sandwich composites. Sandwich composite specimens were manufactured with various treatment combinations of stitch densities (0.0015-0.01 stitches/mm²), linear thread densities (400-1200 Denier), and facesheet thicknesses (1.8-3.6 mm) as dictated by the FC-CCD. The mode I fracture energy was determined by performing single cantilever beam tests. A response surface model (RSM) was developed using an analysis of variance procedure to predict the normalized fracture energy within the design space.

The incorporation of stitching significantly increases the mode I fracture energy and is highly dependent on stitching parameters. Linear thread density is determined to be the most influential factor to improve steady-state fracture energy of stitched sandwich composites. During testing, crack growth exhibits unstable stick-slip behavior that results in a saw-tooth response in the observed fracture energies. The fracture surfaces reveal three different failure mechanisms: (1) stitch pullout at the delaminated interface, (2) frictional sliding of the matrix-stitch column, and (3) ductile foam core failure. Furthermore, the RSM developed in this study reveals that an optimum stitch density of 0.0093 stitches/mm², or a corresponding relative stitch distance of 10.38 mm, is obtained using a linear thread density of 1200 Denier. Increasing the facesheet thickness results in a reduction in the observed fracture energies. This is attributed to fiber bridging and changes in the mode-mixity phase angle, which have been observed to be dependent on the sandwich composite specimen geometry.

This study establishes the overall fracture behavior of stitched sandwich composites due to stitch processing parameters (stitch density, linear thread density, and facesheet thickness). The results indicate that stitching is an excellent method to inhibit crack growth within sandwich composites for damage-tolerant composite designs.

3.2. Experimental Study 2: Estimation of Internal Crack Growth in Polymer Composites using Optical Fibers

A Lagrangian cross-correlation approach was used to estimate the delamination front location without influencing crack growth behavior and to determine the critical strain energy release rate (SERR) variation experimentally using high-spatial resolution optical fibers. As a note, cross-correlation has been used to detect damage based on strain modes under ambient excitation [38] and from mode-converted lamb waves [39]. For this study, cross-correlation was used to identify the location of the delamination front to measure the internal delamination *in situ* within DCB composite specimens. A single DCB specimen was fabricated with multiple passes of an embedded optical fiber (OF) to demonstrate the cross-correlation numerical approach and its efficacy.

3.2.1. Fabrication of Double Cantilever Beam Specimens

Double cantilever beam specimens were fabricated from an infused epoxy/ carbon fiber laminate of [0/90/90/0]_{3s} configuration. The layup configuration was selected to minimize the variation of the SERR across the delamination front [40-42]. This configuration allows for a relatively good comparison to the visually-obtained edge measurements and to assist in establishing the efficacy

of this approach. The epoxy matrix is an out-of-autoclave API-1078 Vacuum Assisted Resin Transfer Molding (VARTM) resin system. The resin was infused into a dry carbon biaxial [0/90] noncrimped fabric (NCF) by SAERTEX, Inc. A Teflon™ film of 0.0127 mm thickness was used as the crack initiator at the midplane of the laminate. The initial starting crack length was approximately 50.8 mm. The laminate was cured using the cure cycle shown in Figure 9. The temperature of the oven, resin, and carbon fiber were increased to 88 °C to reduce the viscosity of the resin before infusion. The temperature was then increased at a rate of 1.8 °C/min to a temperature of 149 °C and held for six hours. The temperature was then raised to 177 °C for a two-hour soak and then reduced to ambient temperature (~24 °C).

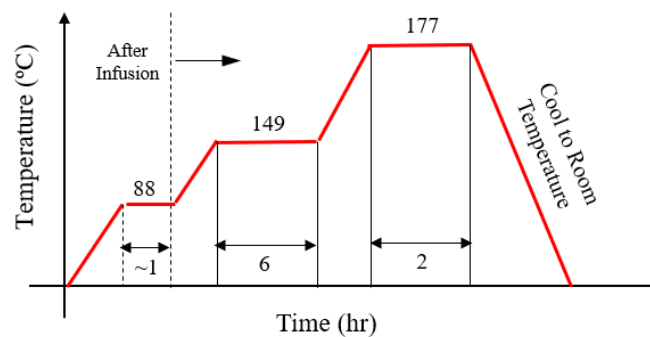


Figure 9. Two-step cure cycle

Prior to embedding the OF sensors in the NCF preform, polytetrafluoroethylene (PTFE) tubes were temporarily woven through the local preform stitching of the NCF fabric as shown in Figure 10 (a). The optical fibers were then passed through these tubes as shown in Figure 10 (b). Once the optical fiber was interlaced into the NCF fabric, the PTFE tubes were removed. Each optical fiber pass was placed within a one degree tolerance relative to the carbon fiber tow direction. Additional PTFE tubing was used at the ingress of the laminate to prevent OF breakage during handling of the cured part. The OFs outside of the laminate were encased in a separate bag to prevent resin buildup on the sensors. The layout of an OF sensor within the DCB specimen is shown in Figure 11. Three equally-spaced fiber passes were used to characterize the delamination front.

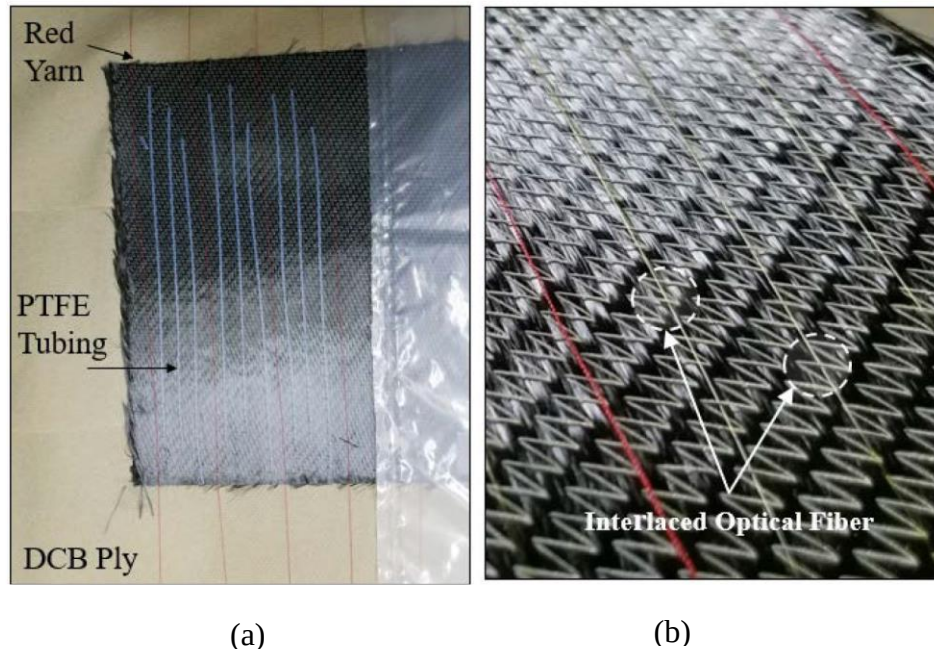


Figure 10. a) Temporary placement of PTFE tubes through local preform stitching and (b) OF threaded through an NCF

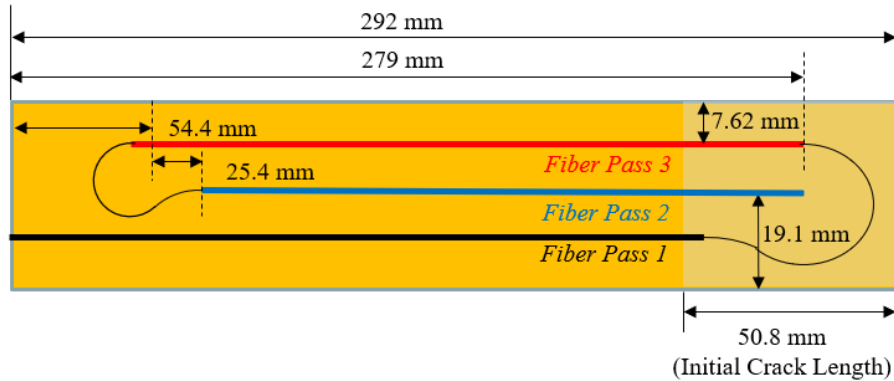


Figure 11. Layout of the OF within the DCB specimen

3.2.2. Testing of Double Cantilever Beam Specimens

A single DCB specimen was clevis-mounted in an Instron model 8872 hydraulic test frame with a 1 kN load cell at a displacement rate of 0.5 mm/min. One side of each test article was marked in 12.7 mm intervals to obtain visual edge measurements; additionally, surface measurements of crack lengths were measured using an ARAMIS camera system. Internal strains were measured using the Luna Technologies ODISI-B fiber optic system. The optical fiber used in this study is a polyimide-coated, low bend loss optical glass fiber (GEOSIL®-SM) with an operating wavelength of 1550 nm. A schematic of the test setup is shown in Figure 12. The strain distributions were measured along three fiber passes that were embedded within the DCB specimen. Due to the

limitations of the testing apparatus, tests were terminated when the crack-tip opening displacement reached approximately 50.8 mm.

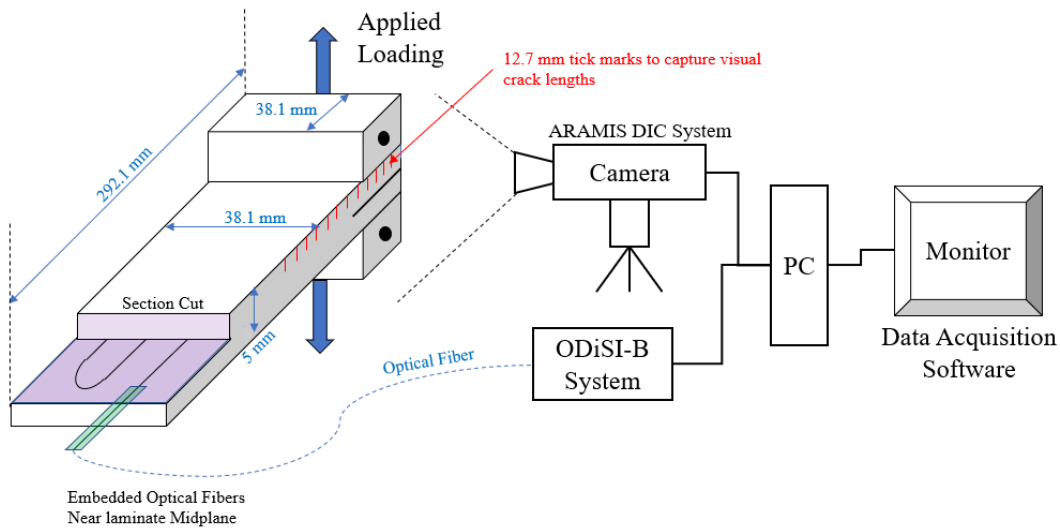


Figure 12. Schematic of the DCB test setup

3.2.3. Results: Internal Optical Fiber Strains

The strain distributions along the OF length and for select applied displacements (0 mm, 15 mm, and 30 mm) are shown in Figure 13. Unique strain distributions are developed to identify each fiber pass as the delamination progresses with an increase in displacement. Due to the looping pattern of the OF, the symmetry between the characteristic “wavy” distributions between each adjacent fiber pass is seen. As expected, the magnitude of strain in each fiber pass increases with increasing displacement and, thus, shifts the strain distribution uniformly as the delamination progresses. Interestingly, as will be shown, the slope of the strain curves (differential strain) in Region 2 for each fiber pass remains relatively uniform with increasing displacement.

The differential strain as a function of location along the OF length for select applied displacements is shown in Figure 14. The differential strain region near the crack front (Region 2) reveals unique peaks that correspond to the location of the crack front. The shift in these peaks is a measure of the change in crack length (Δa) for each fiber pass. The differential strain (slope) was calculated along each fiber pass in Region 2 (perturbed strain region) for each applied displacement and is shown in Figure 15. The differential strain was normalized by the differential strain at the maximum applied load upon the initial occurrence of delamination. The start of delamination was identified by the initial reduction in slope of normalized differential strain with respect to the applied displacement. For each fiber pass, an initial elastic response is developed followed by a relatively uniform distribution of differential strain. This uniformity of the differential strain occurs throughout the propagation of delamination during the DCB test and is relatively independent of the applied loading conditions. Furthermore, the consistent differential strain after delamination indicates that the process zone length is relatively constant after initial propagation. This is due to very little toughening mechanisms, such as fiber bridging, that could increase the SERR during crack propagation [43]. This behavior allows for the

development of a numerical method to identify the delamination front, its progression, and termination point.

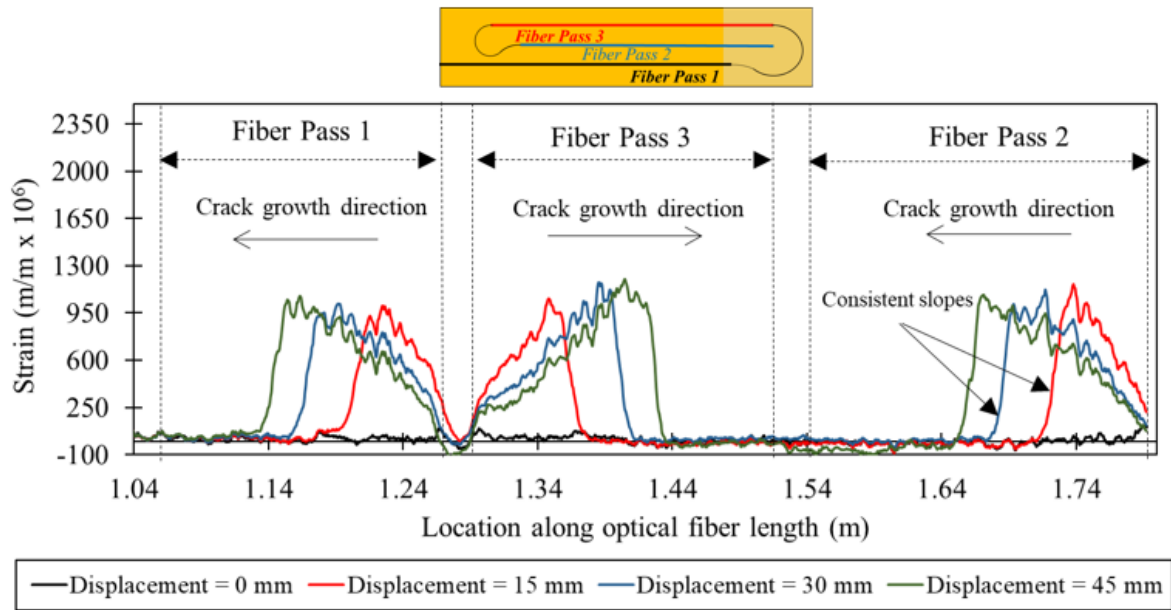


Figure 13. Strain distribution along each fiber pass and select crack-tip opening displacements (0 mm, 15 mm, 30 mm, and 45 mm)

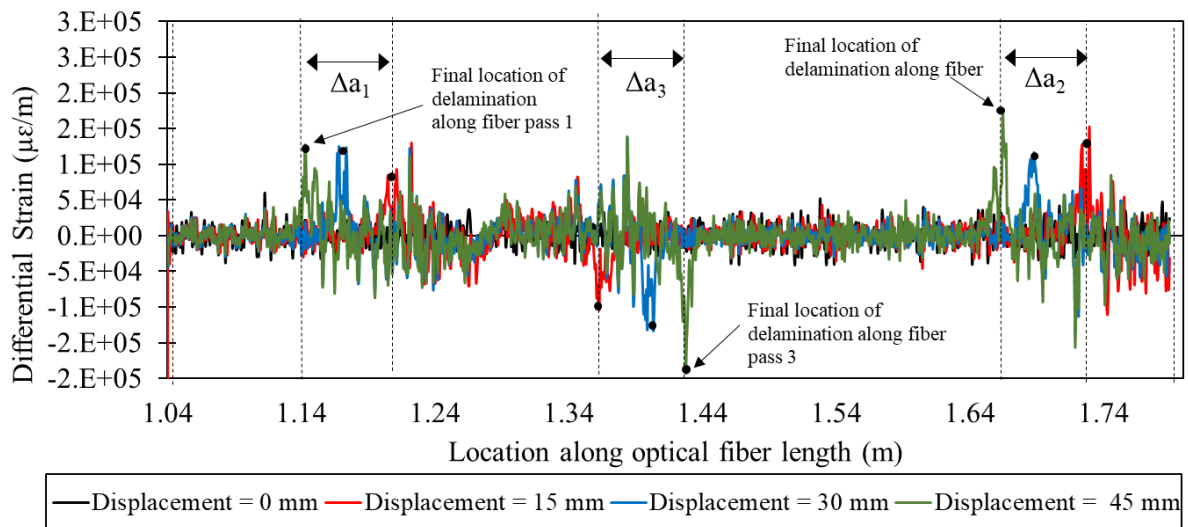


Figure 14. Differential strain distribution along each fiber pass and select crack-tip opening displacements (0 mm, 15 mm, 30 mm, and 45 mm)

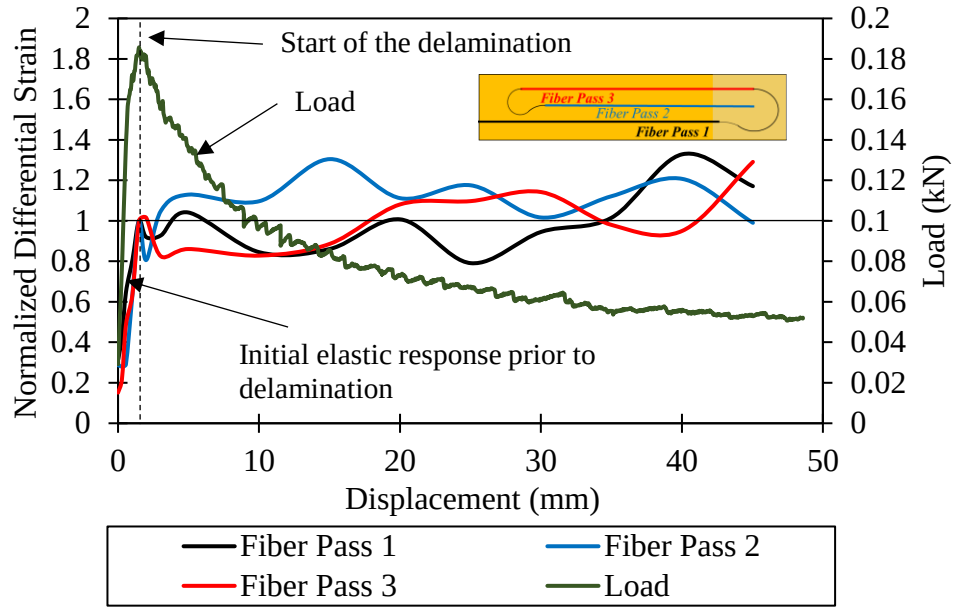


Figure 15 Normalized differential strain in Region 2 as a function of the applied displacement for each fiber pass

3.2.4. Determination of Crack Length and Fracture Toughness

Identification of the delamination front is achieved by measuring the shift, as shown in Figure 14, in the differential strain due to an increase in crack length. The differential strain is evaluated as

$$\frac{d\varepsilon}{dx} \approx \frac{\varepsilon_i - \varepsilon_{i-1}}{x_i - x_{i-1}}, (i = 1, 2, \dots, N) \quad (9)$$

where ε_i and ε_{i-1} correspond to the i^{th} and $i-1$ measurement of strain for a total of N samples. Similarly, x_i and x_{i-1} correspond to the i^{th} and $i-1$ location along the OF. These differential strain peaks correspond to the location of the delamination front along the OF. Furthermore, the distance between the differential strain peaks obtained at the max load and corresponding peaks at subsequent loads can be used to estimate the change in crack length as the delamination progresses.

The crack length is estimated by using a Lagrangian cross-correlation approach, as shown in the flow chart in Figure 16. In this Lagrangian scheme, the measured differential strain during delamination propagation are correlated to the differential strain at the onset of delamination, or max applied load. . Cross-correlation ρ_{12} measures the similarity between two signals [44] and can be expressed as

$$\rho_{12}(\Delta a) = \int_{-\infty}^{\infty} \frac{d\varepsilon_1(x)}{dx} \cdot \frac{d\varepsilon_2(x + \Delta a)}{dx} dx \quad (10)$$

where $\frac{d\varepsilon_1(\Delta a)}{dx}$ and $\frac{d\varepsilon_2(x+\Delta a)}{dx}$ corresponds to the differential strain when the maximum load occurs and for subsequent loadings, respectively, at a location x along the optical fiber. The normalized correlation (ρ_{12}/ρ_{max}) with respect to the normalized distance ($\Delta a/\Delta a_{max}$) is shown in Figure 17(a). The correlation value ρ_{12} becomes a maximum (ρ_{max}) when the differential strain $\frac{d\varepsilon_1}{dx}$ at x has the same gradient in $\frac{d\varepsilon_2}{dx}$ at $x+\Delta a$. The distance Δa corresponds to the change in crack length when $\rho_{12} = \rho_{max}$. The distance Δa_{max} corresponds to the largest distance to obtain a measure of correlation between $\frac{d\varepsilon_1}{dx}$ and $\frac{d\varepsilon_2}{dx}$. To minimize the noise present within the measurements, a beta distribution of the cross-correlation coefficients is used to locate the maximum correlation coefficient, as shown in Figure 17(b). The probability beta distribution function is given by [45]

$$f(\Delta a) = \frac{\rho_{12}^{a-1} [1 - \rho_{12}]^{b-1}}{\beta(a, b)} \quad (11)$$

where $\beta(a, b)$ can be expressed as

$$\beta(a, b) = \int_0^1 \rho_{12}^{a-1} [1 - \rho_{12}]^{b-1} d\rho_{12} \quad (12)$$

The shape parameters a and b were estimated using maximum-likelihood estimation, i.e.,

$$b = \frac{(1 - \bar{x})}{s^2} (\bar{x}(1 - \bar{x}) - s^2) \quad (13)$$

$$a = \frac{\bar{x}b}{1 - \bar{x}} \quad (14)$$

where s^2 and $\bar{x}$ correspond to the variance and mean of normalized correlations, respectively.

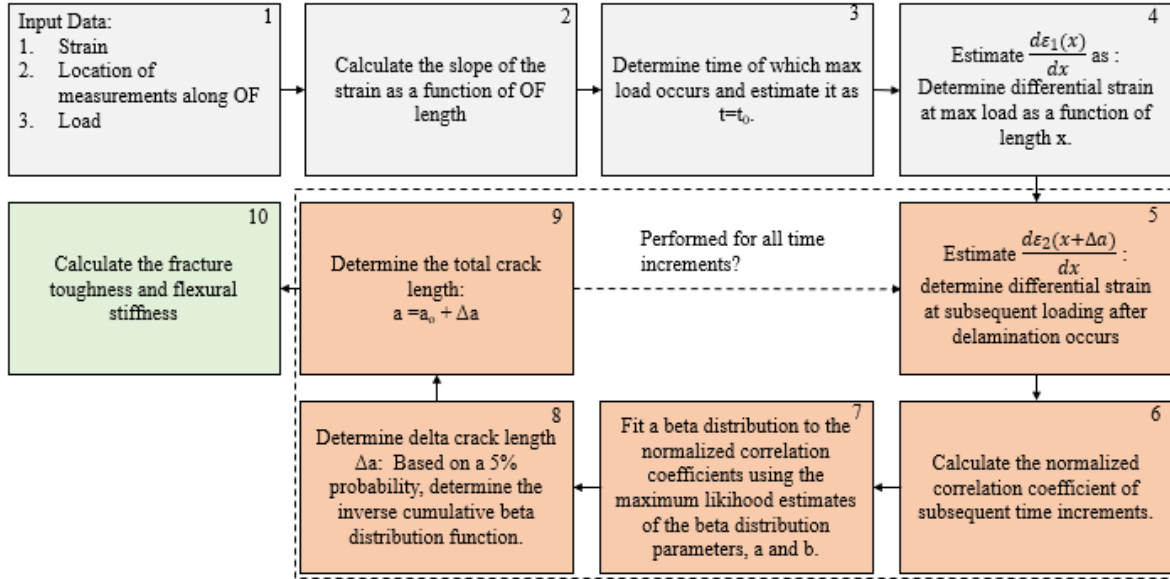


Figure 16. Flow chart for determination of the crack length and fracture toughness

Once the shape parameters for the beta distribution are determined, the maximum correlation can be estimated as the average of all correlation values that exceed a 5% probability that the true correlation can exceed the mean correlation value. Once the true maximum correlation is estimated, the change in crack length can be calculated as

$$\Delta a = \frac{\Delta a}{\Delta a_{max}} \cdot FPL \quad (15)$$

where $\frac{\Delta a}{\Delta a_{max}}$ is the normalized crack length and FPL is the fiber pass length. The total crack length a can then be estimated as

$$a = a_0 + \Delta a \quad (16)$$

where a_0 is the initial crack length. The corresponding mode I fracture toughness G_I is estimated using the modified compliance calibration method using

$$G_I = \frac{3}{2} \frac{P^2 C^{2/3}}{A_1 b h} \quad (17)$$

where P and C are the applied load and compliance, respectively. The compliance is determined from the ratio of the applied displacement (δ) to the applied load (p). The specimen width and thickness are denoted as b and h , respectively. The slope A_1 of the normalized crack length (a/h) is a linear function of $C^{1/3}$.

The flexural stiffness E_{of} is estimated OF strain data using a linear elastic approach as

$$E_{of} = \frac{M \cdot y}{I^* \cdot \varepsilon_{max}} = \frac{P \cdot (a - L_{app}) \cdot y}{I^* \cdot \varepsilon_{max}} \quad (18)$$

where M is the bending moment, I^* is the weighted moment of inertia using classical lamination theory, y is the distance from the neutral axis of the cross-section to the location of the OF, and ε_{max} is the maximum axial strain near the crack tip. The term $(a - L_{app})$ represents the distance from the applied load to the crack front. The distance y is estimated to be approximately 0.8 mm. Comparisons are made to the Modified Beam Theory (MBT) estimate of the flexural modulus E_{1f} as

$$E_{1f} = \frac{64 \cdot P \cdot (a + |\Delta|)^3}{\delta \cdot b \cdot h^3} \quad (19)$$

where $a + |\Delta|$ is the corrected delamination length determined in accordance with ASTM STD D5528.

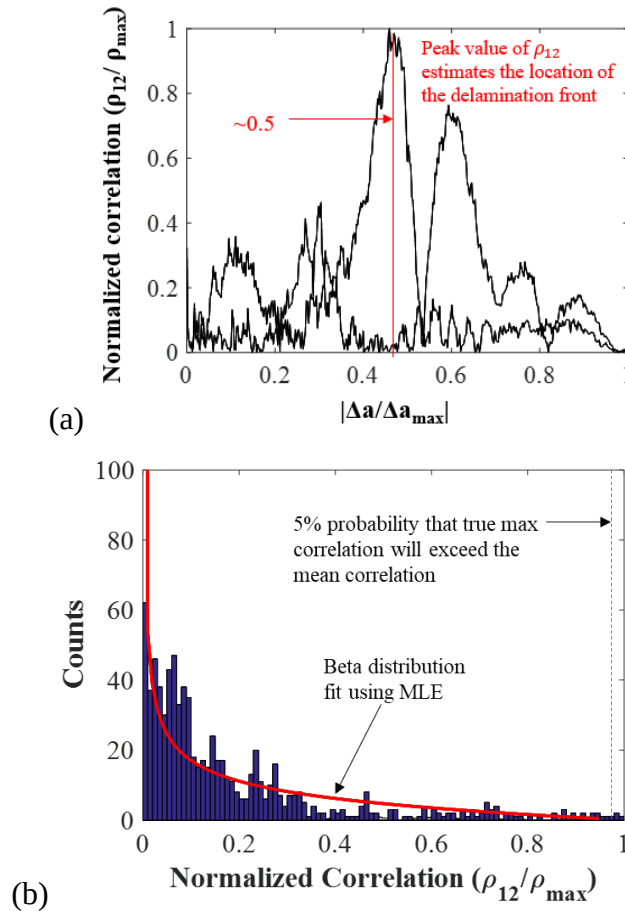


Figure 17. (a) Normalized correlation as a function of the normalized crack length and (b) Beta distribution fit as a function of the normalized correlation

3.2.5. Results and Discussion

The load and crack lengths, from the visual edge measurements and from OF strain measurements are shown in Figure 18. Excellent agreement is obtained between the surface measurement data and OF strain measurements from Fiber Pass 3. A somewhat greater deviation is obtained from OF passes 1 and 2. These greater variations are attributed to the non-uniformity in the crack length at the delamination front.

The fracture toughness values with respect to the crack lengths that are visually-obtained and computed from OF strain measurements are shown in Figure 19. As before, the fracture toughness from Fiber Pass 3 has excellent agreement with the surface measurements as Fiber Pass 3 is the nearest to the edge from which the data was taken. Fiber passes 1 and 2 show a greater variation in the estimated internal crack lengths due to the variation in the SERR across the delamination front. The variation of the SERR for select displacements is shown in Figure 20. As expected, the SERR has a concave curvature along the delamination front. This variation is primarily due to the anticlastic behavior of composite laminates undergoing bending. Furthermore, increasing the applied displacement does not significantly influence the relative variation of the SERR across the delamination front.

The relative percent differences of fracture toughness estimates obtained from the OF estimates and visual edge measurements range from 2.7% to 4.7% and are given in Table 4. These differences agree with analytical predictions obtained from a previous study [46]. It is noted that a maximum of 17.7% difference is obtained at an applied displacement of 11.5 mm. This large error is attributed to noise in the measurements that were used to calculate the differential strain. The statistical significance of these optical fiber fracture toughness estimates, as compared to the visual measurements, depends greatly on the high spatial resolution of strain measurements to determine the internal crack length. The spatial resolution of the crack length measurements using optical fibers is approximately ± 0.625 mm, which is much lower than the crack lengths measured ($\sim 1.25\%$ at initial crack length). For relatively small crack lengths (< 2.5 mm), the optical fiber estimates are statistically insignificant as compared to visual measurements. In addition, optical fiber waviness or inclination along the specimen may influence the crack lengths estimates. The relative error ε due the optical fiber waviness to can be estimated as

$$\varepsilon = \left| \frac{a - a \cdot \cos(\varphi)}{a \cdot \cos(\varphi)} \right| \cdot 100 \quad (20)$$

where φ is the estimated waviness or inclination angle along the length of the DCB specimen. The term $a - a \cdot \cos(\varphi)$ represents the difference in the true crack length as compared to the optical fiber estimates. In Figure 21, the estimated error as a function of the inclination angle. For relatively low angles ($< 2^\circ$), the estimated error is approximately 1.5%. Furthermore, the relative error associated with the measurements is considered negligible due to the total crack growth developed during test. However, optical fibers that have relatively large inclination angles ($> 2^\circ$) will require a correction factor to properly determine the true crack length. Otherwise, an excellent correlation between the OF measurements and visually-obtained measurements is obtained. This

measurement technique allows for the characterization of the internal SERRs for composites with unique ply configurations, where the SERR variation may be much greater.

Flexural modulus was also determined from the OF strain data using Eq. 6.11 and from MBT using Eq. 6.12 and is shown in Figure 22. Both methods indicate a decrease in the flexural modulus with an increase in the applied displacement after the start of delamination. The decrease in flexural moduli is attributed to the plastic effects associated with matrix cracking and filament failure near the delamination zone. In addition, MBT greatly over predicts the flexural modulus even with the corrections proposed by Hashemi [24]. Furthermore, the OF data shows an increase in the flexural modulus before delamination occurs and for displacements less than approximately 0.6 mm as shown in an enlarged view within Figure 22 (Region of Interest, ROI). The flexural moduli from MBT and OFs are compared to experimentally obtained values from reference [47] and are given in Table 5. Flexural modulus predictions from Fiber Pass 2 show a relatively good agreement (approximately 25.5%) with experimentally obtained values before the start of delamination. However, the flexural stiffness predictions from Fiber Passes 1 and 3 are influenced by the variation in the SERR along the delamination front and show greater deviations from experimentally obtained measurements (approximately 20% difference).

Table 4. Percent differences between visual edge measurements and optical fiber predictions

Displacement (mm)	Load (N)	Fracture Toughness (J/m ²)				Percent Difference (%)		
		Edge Measurement	Fiber Pass 1	Fiber Pass 2	Fiber Pass 3	Fiber Pass 1	Fiber Pass 2	Fiber Pass 3
1.4	186	330	325	331	312	1.6	0.1	5.6
2.1	171	389	400	406	383	2.8	4.5	1.4
2.9	157	425	437	444	419	2.7	4.5	1.4
4.3	143	490	513	522	493	4.8	6.6	0.5
5.6	128	505	520	529	499	3.0	4.8	1.1
6.7	119	512	523	532	502	2.1	3.8	2.1
8.4	106	515	524	533	503	1.8	3.5	2.4
9.6	102	534	542	552	520	1.6	3.4	2.5
11.5	88	492	569	579	546	15.7	17.7	11.0
14	87	554	573	582	549	3.3	5.1	0.9
15.8	82	553	562	571	539	1.6	3.3	2.6
18.4	77	568	571	581	548	0.6	2.3	3.5
20.6	70	539	543	552	521	0.6	2.3	3.5
23.1	67	545	588	598	564	7.9	9.8	3.5
26.7	65	579	600	610	576	3.7	5.5	0.5
29.2	60	552	561	571	539	1.8	3.5	2.3
32.3	58	559	571	580	547	2.0	3.8	2.1
34.8	55	559	566	576	543	1.4	3.1	2.8
40.1	56	618	632	643	606	2.2	4.0	1.9
43.3	53	614	622	633	597	1.3	3.1	2.8
Average	-	-	-	-	-	3.1	4.7	2.7

Table 5. Comparison of measured and predicted fracture toughness

Method to estimate Flexural Modulus	AVG (GPa)	STD (GPa)	%Diff from Exp. Measurement
Fiber Pass 1* (Eq. 6.11)	214.4	118.0	360.45
Fiber Pass 2* (Eq. 6.11)	32.1	3.2	31.03
Fiber Pass 3* (Eq. 6.11)	25.6	3.5	45.03
Modified Beam Theory (Eq. 6.12)	34.7	4.6	25.52
Experimental values from Ref. [235]	46.6	2.5	-
*Average values obtained prior to delamination			

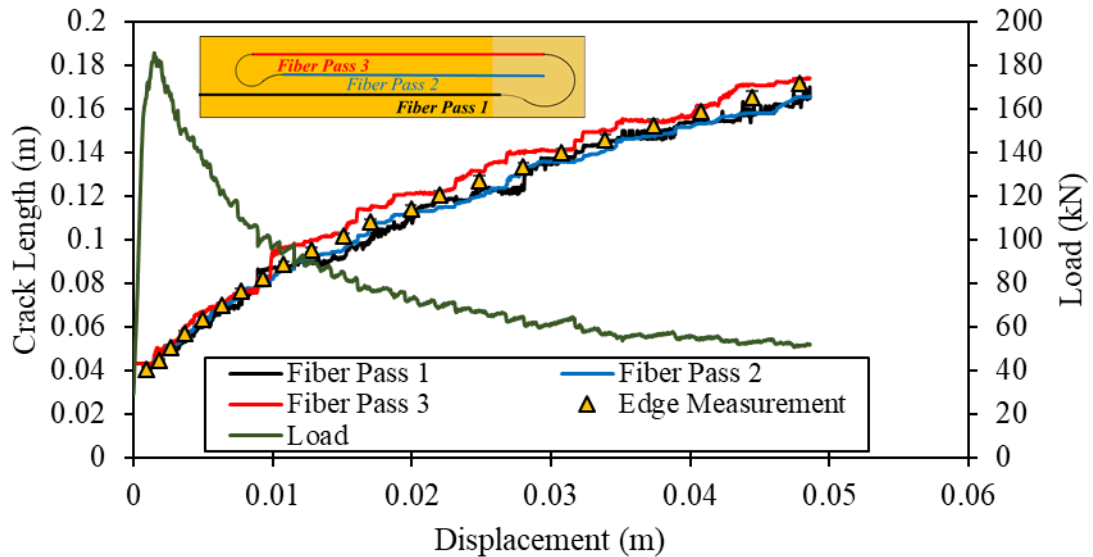


Figure 18. Load and crack length as a function of displacement for each fiber pass and edge measurement

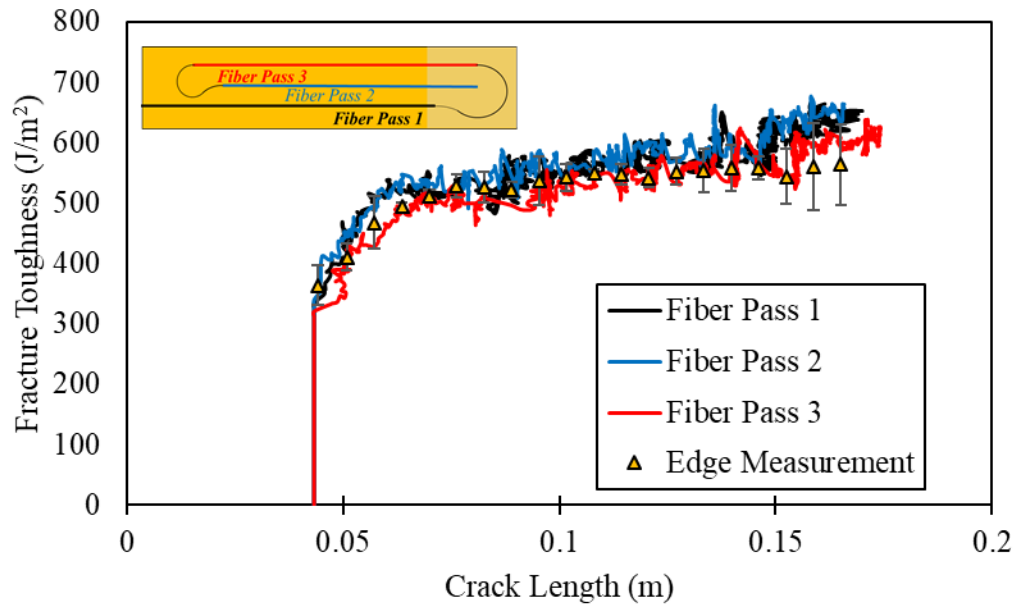


Figure 19. Load and crack length as a function of displacement for Fiber Passes 1, 2, and 3, and edge (visual) measurement

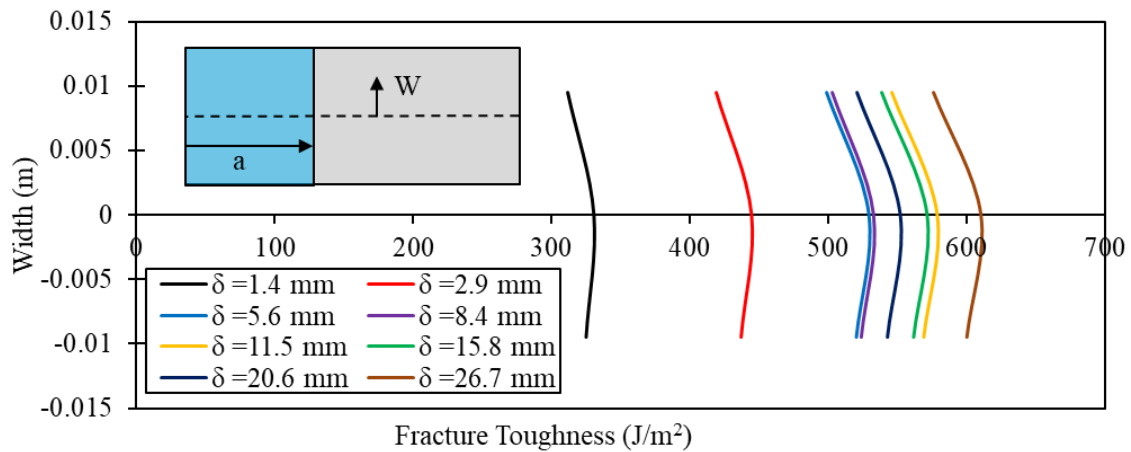


Figure 20. Fracture toughness variation across the specimen width for select applied displacements

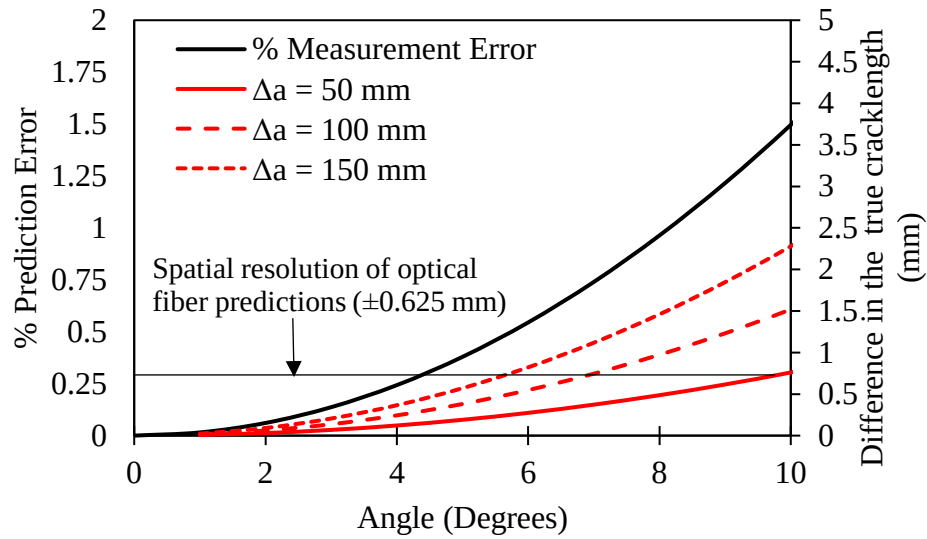


Figure 21. Estimation of the prediction error and relative difference in the true crack length as a function of inclination angle of the optical fiber along the DCB specimen

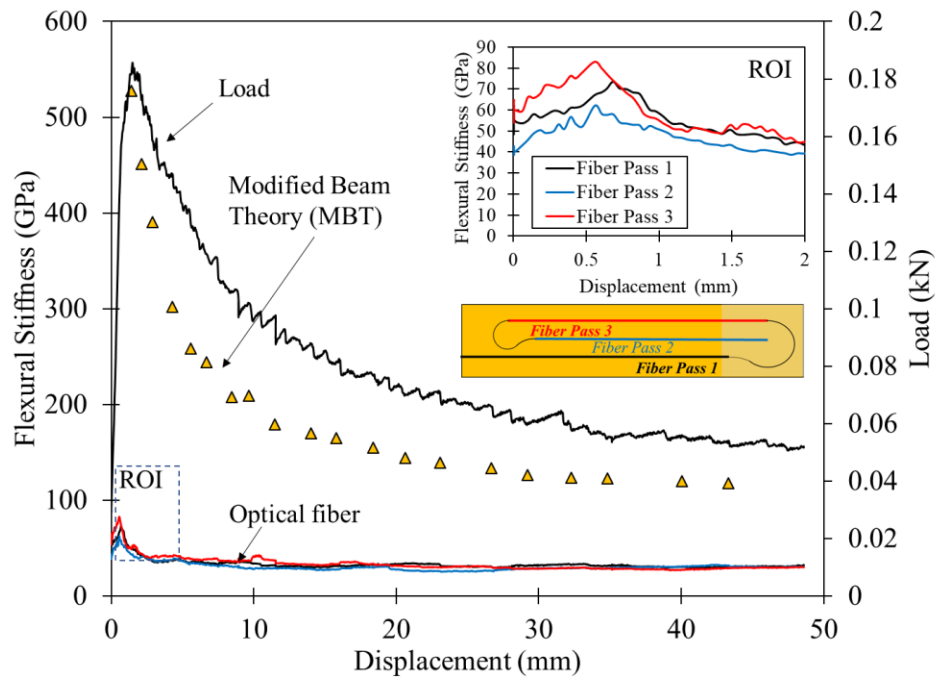


Figure 22. Flexural modulus (optical fiber predictions and measurements using MBT) and load as a function of the applied displacement

3.2.6. Conclusions

In this study, strain distributions were obtained from OF sensors embedded within DCB test articles and correlated to the propagation of delamination. The sensors were woven through the local preform stitching before resin infusion. A unique “wave-like” strain distribution that shifts with the propagation of delamination was obtained. The differential strain along the length of the OF was computed, resulting in unique peaks that correspond to the location of the delamination front.

A cross-correlation based approach was developed to estimate the crack length using the shift in the differential strain gradient. Excellent agreement between SERR surface measurements and OF estimates (<4.7% difference) were obtained. Measuring the fracture toughness across the specimen width revealed a concave curvature that is associated with the anticlastic behavior of laminates undergoing bending. In addition, the flexural moduli (Table 5) before the start of delamination can be estimated with reasonable accuracy (6.93%).

Using OF strain data, the measurement of internal delamination propagation in composite laminates of unique ply configurations can be achieved. This approach alleviates the need to require unidirectional plies near the midplane of composite laminates to determine the SERR of angle-ply laminates. This approach also allows engineers and researchers to improve their predictive capability and design composites from a crack-progression perspective by using internal SERR measurements. Lastly, this approach can be used to monitor crack growth progression, leading to a more condition-based maintenance procedure. Future work includes the investigation of the SERR variation of angled-ply laminates using the Lagrangian cross-correlation method with validation using ultrasonic methods.

4. MODELING APPROACHES

Both analytical and computational models of stitched sandwich composite behavior were developed in this study. The J-integral approach was used to model the mode I fracture energy; finite element models were developed for prediction of the mode I delamination and crack growth during fracture. All models were informed and validated with data from the single cantilever beam testing (Section 4.1).

4.1. Estimation of the Mode I Fracture Energy of Stitched Sandwich Composites using the J-Integral Approach

The J-integral approach was used to develop simplified expressions for mode I fracture energy of the single cantilever beam (SCB) test (Section 4.1). The SCB test [25, 27] is a variant of the tilted sandwich deboned (TSD) method, where the incline of the single cantilever beam specimen is zero degrees. The estimated fracture energies from the J-integral approach are compared to existing linear elastic fracture mechanics approaches and finite element analysis.

4.1.1. J-Integral Modeling

The J-integral relationship is a contour integral that can be expressed as [48]

$$J = \int_{\Gamma} \left(W dy - T_i \frac{du_i}{dx} ds \right) \quad (21)$$

where J is the nonlinear elastic strain energy rate along the contour Γ . The contour Γ is represented by a clockwise segmented path around the boundary of the fractured domain, as shown in Figure 23. The strain energy density and traction vector are denoted as W and T_i , respectively. The traction stresses represent the external stresses acting on the cracked boundary and can be expressed as $T_i = \sigma_{ij}n_j$. The perimeter of the crack is assumed to be stress free under monotonic loading conditions. The term $\frac{du_i}{dx}$ is the rotation vector in the x direction about the incremental path ds along the contour.

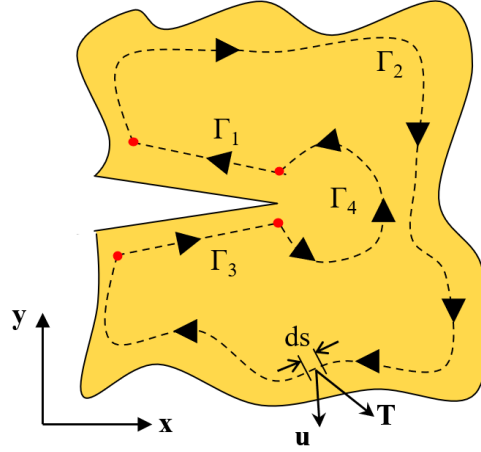


Figure 23. Arbitrary contour around the tip of a crack

Consider the single cantilevered beam with discrete traction above a delaminated arm shown in Figure 24. The bottom surface of the SCB specimen is rigidly constrained. A discrete traction load T_y is applied above the partially debonded region. The perimeter of the SCB specimen is delineated by the segmented contours, $\Gamma_1 - \Gamma_8$. For clarity, the contours are represented slightly internal to the specimen's perimeter. The J-integral is path independent for any closed contour. Therefore, the compatibility relationship between the segmented contours can be expressed as

$$\sum_{i=1}^8 J_i = J_1 + J_2 + J_3 + J_4 + J_5 + J_6 + J_7 + J_8 = 0 \quad (22)$$

where J_i is the SERR for the i^{th} segmented contour Γ_i around the perimeter of the fractured domain.

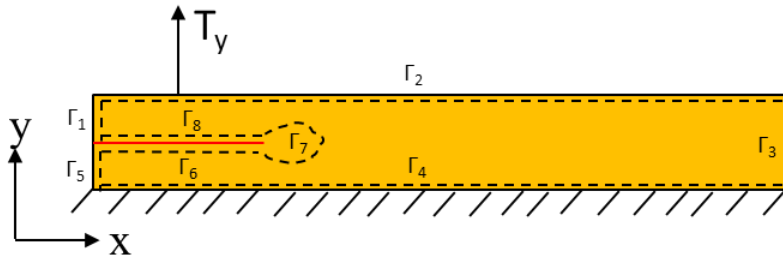


Figure 24. Single cantilever beam specimen with an applied traction

The strain energy density is analytically represented by

$$W = \int \sigma_{ij} d\varepsilon_{ij} \quad (23)$$

where σ_{ij} and ε_{ij} are the stress and strain tensors, respectively. The stresses σ_{ij} are zero along Γ_1 , Γ_3 , and Γ_5 since those surfaces are traction-free. The perimeter of the crack is also assumed to be traction-free under monotonic loading conditions. There are also no rotations along a clamped boundary condition acting on a horizontal path, i.e.,

$$\frac{du}{dx} = 0, \quad dy = 0 \quad (24)$$

Therefore, the following relationships are obtained.

$$J_1 = J_3 = J_4 = J_5 = J_6 = J_8 = 0 \quad (25)$$

and

$$J_7 = -J_2 \quad (26)$$

The remaining segmented paths are on horizontal surfaces ($dy = 0$) with a traction acting only in the y-direction. Therefore, Eq. (21) can be reduced to

$$J_7 = \int T_y \frac{du_y}{dx} dx \quad (27)$$

For finite displacements [49], the rotations $\frac{du_y}{dx}$ can be exactly represented as

$$\frac{du_y}{dx} = \tan(\theta) \quad (28)$$

where θ is the total angle between the rigid beam and the deformed beam at the applied traction T_y . Alternate forms of the rotation have been developed by other researchers [49-51]; however, these formulations assume small strains for a finite displacement of a beam subjected to pure bending. This behavior may not be the case for stitched composites which can develop rotational constraint near the vicinity of the stitching and crack front [21, 35, 52]. The traction stress T_y can be expressed as a distributed load acting over an infinitesimal length ($x_2 - x_1$) and width b . The applied loading is illustrated in Figure 25. Incorporating Eq. (28) into Eq. (27) and representing the traction stress as a distributed load, the J-Integral can be expressed as

$$J = \int_{x_1}^{x_2} \frac{P}{b(x_2 - x_1)} \tan(\theta) dx = \frac{P}{b} \tan(\theta) \quad (29)$$

Herein, the subscript of the nonlinear strain energy release rate J_7 is removed and generally interpreted as the mode I strain energy release rate J near the crack front.

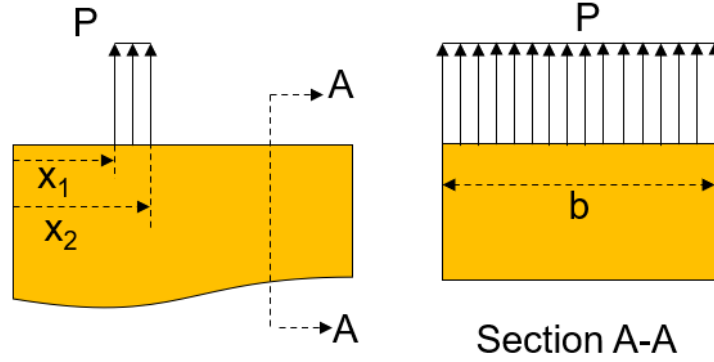


Figure 25. Representation of the discrete traction as a distributed load

For small rotations, Eq. (29) can be expressed as [53, 54]

$$J = \frac{P\theta}{b} \quad (30)$$

The SERR J is primarily a function of the applied loading P , the specimen width b , and the angle θ at the applied traction. The rotation at the applied traction is primarily influenced by the crack root rotation and elastic foundation below the deformed beam. This formulation is independent of crack length and does not assume linear-elastic material behavior. This formulation may be used to determine the traction-separation law of sandwich composites that may have large plastic zone sizes during fracture [55]. Under linear elastic material conditions, Eq. (30) is equivalent to the Mode I fracture energy G_I using modified beam theory (MBT). The corresponding linear elastic rotation θ and displacement δ can be expressed as [56]

$$\theta = \frac{6Pa^{*2}}{Ebh^3} \quad (31)$$

and

$$\delta = \frac{4Pa^{*3}}{Ebh^3} \quad (32)$$

The terms E and h are the axial modulus of the deformed laminate and thickness, respectively. The effective crack length is denoted as a^* . In beam theory, the root of the crack front is assumed to have zero slope. Therefore, an effective crack length $a^* = a + \Delta_f$ is used to compensate for the foundation and rotation effects near the crack front [24]. The Δ_f term can be estimated empirically by evaluating the x-intercept from the cube root of the compliance (δ/P) with respect to the crack length a [24]. Equations (31) and (32) can be used in Eq. (30) to obtain

$$G_I = J = \frac{3P\delta}{2b(a + \Delta_f)} \quad (33)$$

The mode I fracture energy can be calculated using the modified compliance calibration (MCC) method as

$$G_I = \frac{3P^2 C^{2/3}}{2A_1 b h} \quad (34)$$

where A_1 is the slope of the normalized crack length (a/h) as a function of the cube root of the compliance ($C^{1/3}$) and h is the total thickness of the specimen.

4.1.2. Finite Element Modeling

A two-dimensional (2D) and three-dimensional (3D) finite element analyses of an SCB specimen were performed using ABAQUS Standard 2019 commercial software. Material properties were obtained from prior studies [1-4] and are summarized in Table 6. The assigned loads and boundary conditions for each model are shown in Figures 26 and 27. The bottom surface of the 2D model was rigidly constrained. A vertical load was applied above a partially debonded region of the sandwich composite. The compliance of the SCB test fixture was simulated within the symmetric 3D model to determine its influence on the calculated fracture energy.

Surface-to-surface hard contact between the bottom face of the SCB specimen and the test fixture was applied. The test fixture was translationally fixed using a kinematic coupling. The 2D and 3D finite element models were comprised of plane strain shell (CPE4I) and hexagonal elements (C3D8I), respectively, with an approximate element length of 0.2 mm. The J-integral was evaluated across the entire crack front. Due to the influence of Poisson's effect on the estimated fracture energy [42, 57], an averaged value of the fracture energy was used for comparisons to other experimental and computational methods.

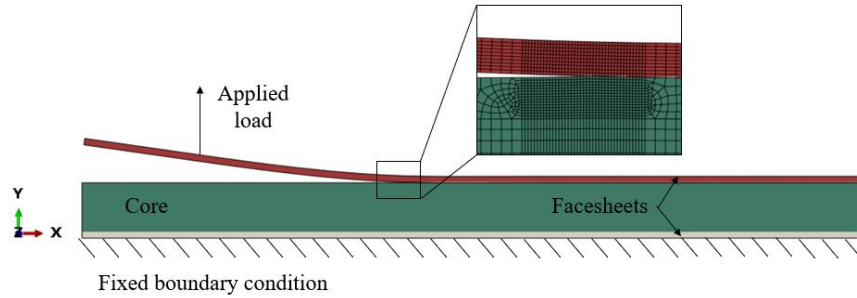


Figure 26. Two-dimensional finite element model of an SCB specimen

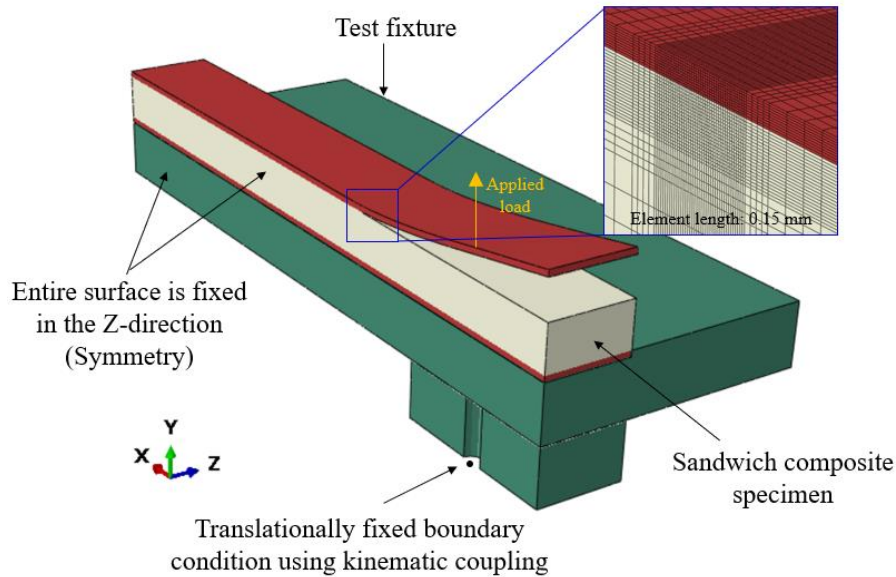


Figure 27. Three-dimensional finite element model of an SCB specimen

Table 6. Bulk material properties for the facesheet and core [1-4]

Facesheet		Core	
$E_{11} = E_{22}$ (Pa)	5.66E+10	E (Pa)	1.86E+08
E_{33} (Pa)	8.64E+09	ν	3.30E-01
$G_{12} = G_{13}$ (Pa)	4.66E+09		
G_{23} (Pa)	4.95E+09		
ν_{12}	0.0619		
$\nu_{13} = \nu_{23}$	0.25		

4.1.3. Results and Discussion

The load and crack length as a function of the applied displacement are shown in Figure 28(a) for an SCB test. An initial linear response prior to the start of the delamination propagation was observed for the SCB test. At crack initiation, unstable crack growth was observed in the unstitched portion of the sandwich composite and a decrease in the reacted load occurred. The load then increased as the crack front approached the initial stitch row. Failure of the initial stitch

row occurred at the maximum measured load and resulted in an immediate progression of the crack to the adjacent stitch row. Subsequent rows of stitching were observed to bridge the crack-plane near the vicinity of the crack front. The stitching primarily pulled out from the facesheet near the facesheet-to-core interface. During each subsequent failure of a stitch row, a saw-tooth pattern in the measured load was observed. Additionally, the crack front did not propagate between failures of stitch rows.

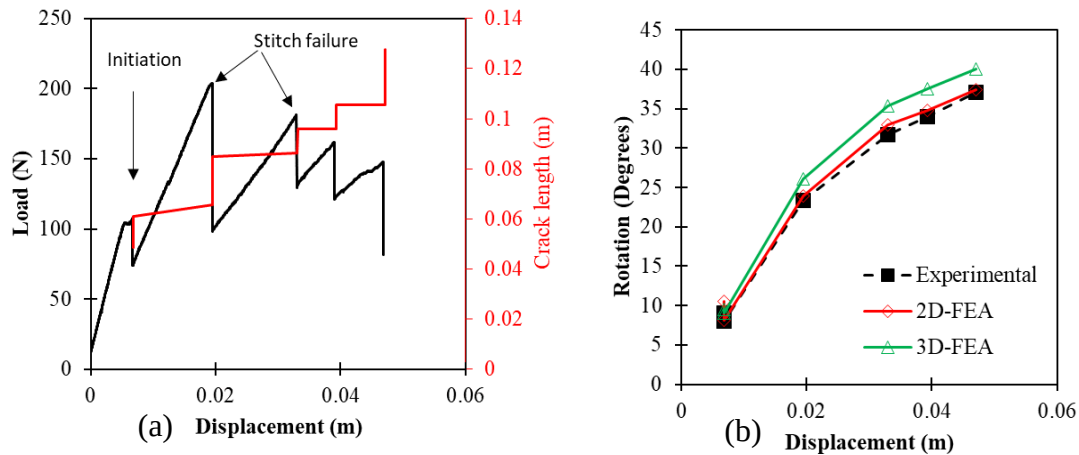


Figure 28. (a) Load, crack length, and (b) rotation with respect to the applied displacement

During testing, the rotations were measured at initiation and at the maximum loads prior to stitch failure. The measured and predicted rotations with respect to the applied displacement are shown in Figure 28(b). The rotation increases with an increase in the applied displacement. Additionally, good agreement ($\sim 1\%$ percent) was obtained between the predicted rotations from the 2D finite element model and experimental measurements. Greater deviations ($\sim 7\%$) between the experimental measurements and the 3D finite element model were obtained; this may be attributed to differences in material properties and boundary conditions. The cube root of the compliance with respect to the crack length is shown in Figure 29. As the crack length increases, a linear variation in the measured compliance is observed. Excellent agreement was obtained for both the 2D and 3D finite element results when compared to the experimental compliance.

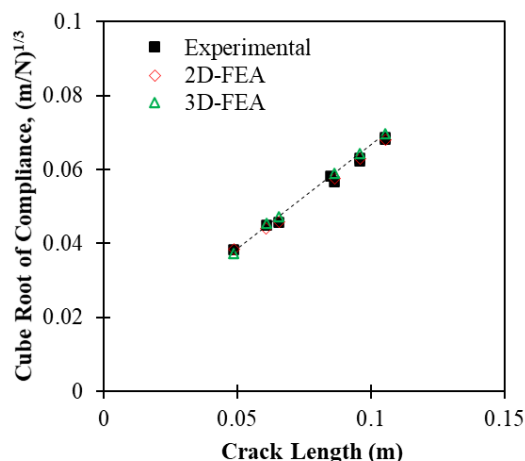


Figure 29 Cube root of the compliance with respect to crack length

The fracture energy with respect to the measured crack length for different experimental (J-Integral, MBT, and MCC) and computational methods (2D and 3D finite element analysis) is shown in Figure 30. During crack initiation, the fracture energy is relatively constant due to the lack of toughening mechanisms to resist crack growth. Experimental and computational results are in excellent agreement in the unstitched region of the sandwich composite. As the crack front approaches the initial stitch row, the fracture energy increases linearly by a factor of 6, and remains relatively constant during the initial and subsequent failures of stitch rows. As noted from previous studies [20, 58, 59], the relative improvement in the crack-growth resistance is highly dependent upon stitch parameters. The percent differences between each method are shown in Table 7. Results from the 2D and 3D finite element models are in close agreement (7%-11%) with the experimentally obtained J-integral estimate (Eq. 29). However, the MBT and MCC methods significantly underpredict (~20%) the fracture energy required to propagate cracks for the selected stitch parameters and material design. The relative differences are primarily attributed to the small-scale yielding that is assumed when using the MBT and MCC methods. The fracture energies calculated from the MBT and MCC methods provide relatively conservative estimates of the fracture energy in the stitched region of the sandwich composite, which may be useful in preliminary designs due to its availability in the open literature.

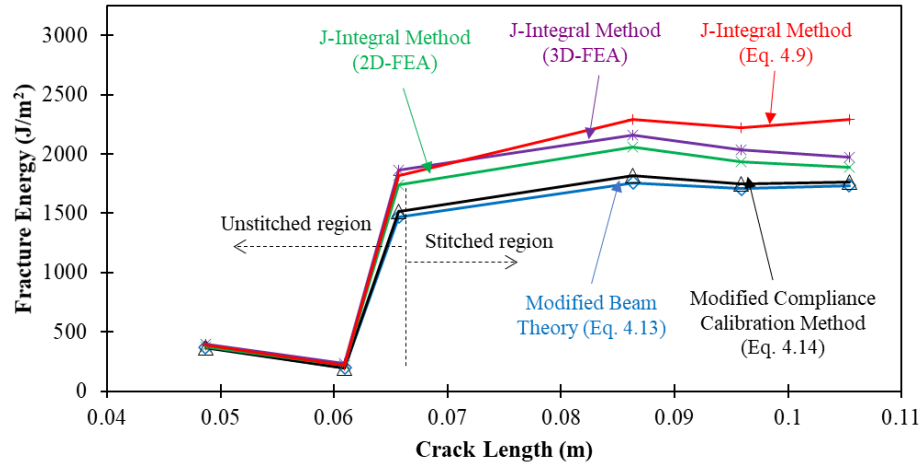


Figure 30. Fracture energy as a function of crack length for different methods

Table 7. Percent difference in the fracture energy for different methods

Method	Unstitched		Stitched	
	Fracture Energy (J/m ²)	Percent Difference (%)	Fracture Energy (J/m ²)	Percent Difference (%)
Modified Beam Theory	372.2	4.98	1733.1	19.64
Modified Compliance Calibration Method	363.2	7.30	1776.2	17.64
J-Integral (2D FEA)	369.8	5.61	1906.4	11.60
J-Integral (3D FEA, Average)	395.8	1.02	2009.5	6.82
J-Integral (Large Rotations)	391.7	-	2156.5	-

4.1.4. Conclusions

Simple analytical expressions for the nonlinear elastic fracture energy of a stitched single cantilevered beam specimen have been developed. Under linear elastic material assumptions, the proposed solution is equivalent to existing solutions such as the modified beam theory (MBT) and the modified compliance calibration (MCC) method. Additionally, 2D and 3D finite element analyses were performed to estimate the fracture energy using the J-integral approach. A single cantilevered beam test of a stitched foam core sandwich composite was performed to quantify the percent differences between experimental and computational methods in the unstitched and stitched regions. Stitching was shown to improve the fracture resistance by a factor of 6. Conservative estimates were obtained using the MBT and MCC approaches. Reasonable agreement of the fracture energy was obtained using the 2D and 3D finite element models and the analytical J-integral method.

4.2. Finite Element Analysis of Mode I Delamination of Stitched Sandwich Composites

Two-dimensional (2D) and three-dimensional (3D) finite element analyses (FEAs), simulating the facesheet-to-core separation process in stitched single cantilever beam specimens, were performed. The facesheet-to-core separation and failure of the through-the-thickness stitching are represented using discrete cohesive zones using a bilinear and trilinear traction-separation law, respectively.

For an unstitched composite laminate, a bilinear traction-separation law (Figure 31) is commonly assumed and is primarily associated with small-scale bridging conditions. This model is described by three parameters: the critical SERR, the maximum traction stress (σ_1), and a penalty stiffness (K_{nn}). Damage occurs during the linear softening region ($\overline{ac}$) and is represented by a reduction of the penalty stiffness, where cohesive failure occurs when the effective displacement (δ_e) is reached (Point c).

A sensitivity analysis is conducted on the components of a unique trilinear traction separation law (TSL) that represents the bridging behavior of through-the-thickness reinforcements using 2D FEA. The facesheet-to-core separation was modeled using 3D finite element models (FEMs) with a discrete cohesive zone modeling approach. Validation of the models is performed by comparing load and crack growth predictions to experimental measurements. Data from SCB testing (Section 4.1) was used to refine and validate the models.

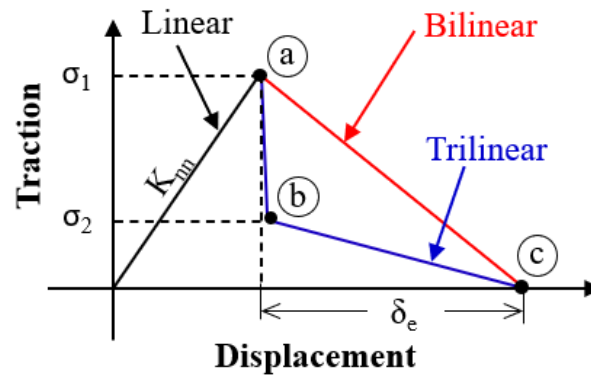


Figure 31. Traction-separation laws to describe brittle cohesive behavior

4.2.1. Computational Modeling

Implicit FEA of stitched SCB specimens was performed using ABAQUS 2019 commercial software. A 2D analysis was performed to assess the influence of a trilinear traction-separation law on the predicted load-displacement response. These results were then used to inform a 3D FEM using a discrete cohesive zone methodology to evaluate the influence of stitch parameters on the crack growth behavior. The facesheet and core interface was discretized to independently simulate the debonding of the foam core from the facesheet and failure of the through-the-thickness reinforcement near the delaminated interface. The material properties used in the FEMs are summarized in Table 8. In the following sections, the 2D and 3D finite element analyses are discussed.

Table 8. Elastic properties of the stitched sandwich composite model [1-4]

Facesheet Material Properties [2, 3, 57]	
$E_{11} = E_{22}$ (Pa)	5.66E+10
E_{33} (Pa)	8.64E+09
$G_{12} = G_{13}$ (Pa)	4.66E+09
G_{23} (Pa)	4.95E+09
ν_{12}	0.0619
$\nu_{13} = \nu_{23}$	0.25
Core Properties [4]	
E (Pa)	1.86E+08
ν	3.30E-01

4.2.2. Two-Dimensional Finite Element Analysis

The 2D FEM is shown in Figure 32. The bottom surface of the SCB model is rigidly constrained for all degrees of freedom and a displacement of 50 mm was directly applied above the partially debonded region. An element mesh size of approximately 0.15 mm was used near the cohesive interface. The cohesive surface is discretized into three zones [2]: 1) initiation, 2) facesheet-to-core interface, and 3) an area to represent stitching, as shown in Figure 32. The relative cohesive zone of the stitch row is approximately 2 mm and based on physical measurements of the stitch column diameter. At the facesheet-to-core interface (unstitched regions), a bilinear traction separation law was assumed to represent the unstable behavior at crack initiation. A fracture energy of 220 J/m² was used in the unstitched regions and were obtained from a previous study [58].

A trilinear traction-separation law is used to represent the bridging behavior of the through-thickness reinforcements in stitched sandwich composites based on an assumed shape from interlaminar tests [29]. A sensitivity analysis of the stitch trilinear traction-separation law on the load-displacement response of an SCB specimen was performed. The influence of the cohesive stiffness (K), maximum elastic stress (σ_1), effective bridging displacement (δ_e), and bridging stress (σ_2) are shown in Figures 33(a)- 33d), respectively. In Table 9, the nominal cohesive parameters used to represent each interface is shown. The trilinear traction-separation law for the stitched region has an initial linear-elastic material behavior, followed by a sharp reduction after the maximum elastic stress to a known bridging stress (Figure 31). The traction stress then decreases linearly from the bridging stress, where cohesive failure occurs when the traction stresses are zero at the maximum effective displacement input. The ratio of the maximum elastic stress to known bridging stress is denoted as the elastic-plastic ratio (σ_1/σ_2). It is important to note that the area under of the curve of the stitch traction-separation law was not maintained to be constant in order to evaluate the influence of each parameter independently. This is unorthodox as compared to traditional cohesive zone modeling approaches, where the area under of a bilinear traction-separation law is assumed to be constant in order to determine the effective displacement by varying the maximum elastic stress.

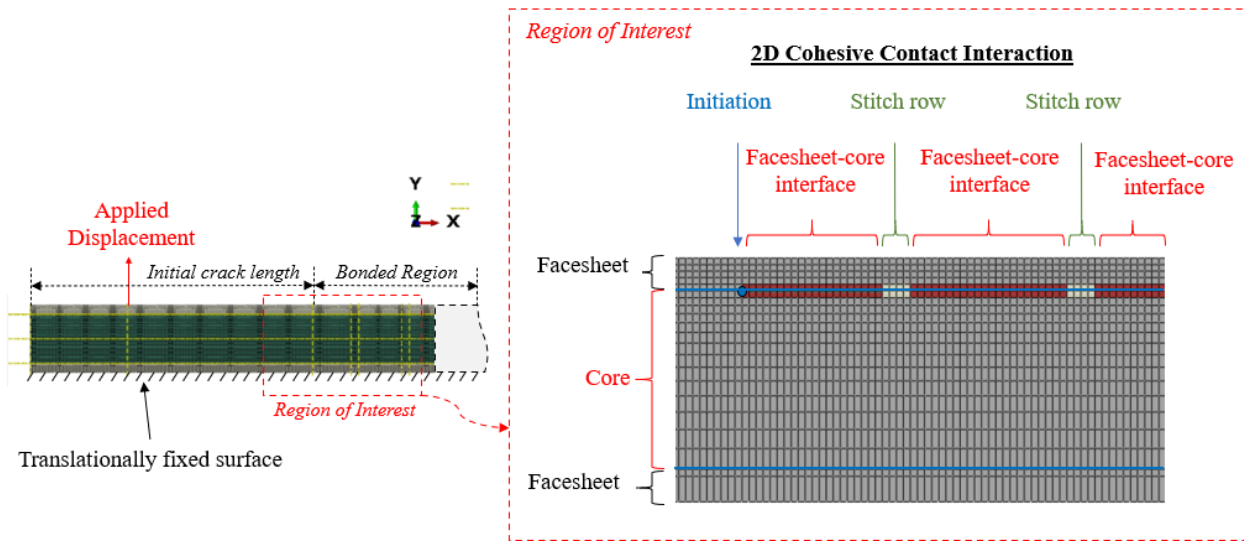


Figure 32. Two-dimensional finite element model to simulate crack growth in stitched sandwich composites

Table 9. Nominal cohesive parameters of the 2D FEM

Interface	TSL* Shape	Penalty Stiffness K_{nn} (Pa/m)	Maximum Elastic Stress, σ_e (Pa)	Elastic-Plastic Ratio (σ_2/σ_1)	Effective Displacement, δ_e (m)
Initiation	Bilinear	1.00E+13	1.58E+06	-	2.78E-04
Facesheet to core	Bilinear	1.00E+13	3.00E+04	-	1.47E-02
Stitch	Trilinear	5.00E+11	2.50E+08	0.1	1.00E-03

TSL: Traction-Separation Law

The load as a function of the applied displacement for select cohesive stiffnesses ($K=1, 2, 5$, and 10×10^{11} Pa/m) is shown in Figure 33(a). Decreasing the mode I stiffness of the cohesive traction-separation law increases the magnitude of the maximum load required to fail the stitch rows. For high cohesive stiffnesses ($5 \times 10^{11} < K \leq 5 \times 10^{12}$ Pa/m), the measured load increases linearly until failure of the initial stitch row. As a note, very high cohesive stiffnesses ($>10^{13}$) may yield spurious results and inaccurately represent the traction-separation behavior during the delamination process. Additionally, premature separation between the facesheet and core may occur beyond the initial stitch row if the cohesive stiffness of the through-thickness reinforcement is too low ($K < 2 \times 10^{11}$ Pa/m). As a result, greater applied displacements are necessary to reach the failure load of the more ductile cohesive interface. This behavior indicates that ductile through-the-thickness reinforcements with high tensile strengths can be beneficial in resisting delamination in polymer composites.

The influence of the maximum elastic stress on the load-displacement response of a stitched sandwich composite is shown in Figure 33(b). Increasing the maximum elastic stress from 250

MPa to 450 MPa linearly increases the maximum load required to fail initial and subsequent stitch rows. Additionally, no significant change in the stiffness (slope) of the load-displacement response is observed. The load as a function of the applied displacement for select effective displacements is shown in Figure 33(d). As expected, the bridging behavior of the cohesive interface only influences subsequent stitch rows after the initial stitch row has failed, as shown in Figure 33(c). Increasing the effective bridging displacement of the trilinear traction separation law increases the applied displacement and load magnitude required to fail subsequent stitch rows. Furthermore, increasing the effective displacement from 1 mm to 2 mm does not show any difference in the predicted response. This is primarily attributed to the spacing of the through-the-thickness reinforcements. Decreasing the spacing may likely develop greater large-scale bridging and increases in the load-magnitude response. Lastly, increasing the ratio of the bridging stress to the maximum elastic stress (σ_1/σ_2) after damage initiation does not globally affect the magnitude of compliance or maximum stitch failure loads, as shown in Figure 33(d). However, the primary influence is associated with the difference in the magnitude of load before and after stitch failure. Increasing the elastic-plastic ratio decreases the load difference during stitch failure, prior to frictional sliding of the through-thickness reinforcement.

Based on the observations revealed by altering the four parameters (cohesive stiffness, maximum elastic stress, effective displacement, and bridging stress), a systematic approach for determining the traction-separation law of a through-thickness reinforcement can be determined. The two primary parameters that influence the load-displacement response are the maximum elastic stress and the cohesive stiffness, which can be altered to appropriately predict crack growth within stitched sandwich composites. In this study, the elastic-plastic ratio is assumed to be approximately 0.1. An effective bridging displacement of 1 mm is used in this study, which is based on interlaminar tensile tests of a single stitch [29]. The cohesive stiffness is increased or decreased to match the stiffness (slope) of the load-displacement response. Further increases or decreases in the effective displacement can alter the magnitude of the load to predict the desired load-displacement response and crack growth behavior.

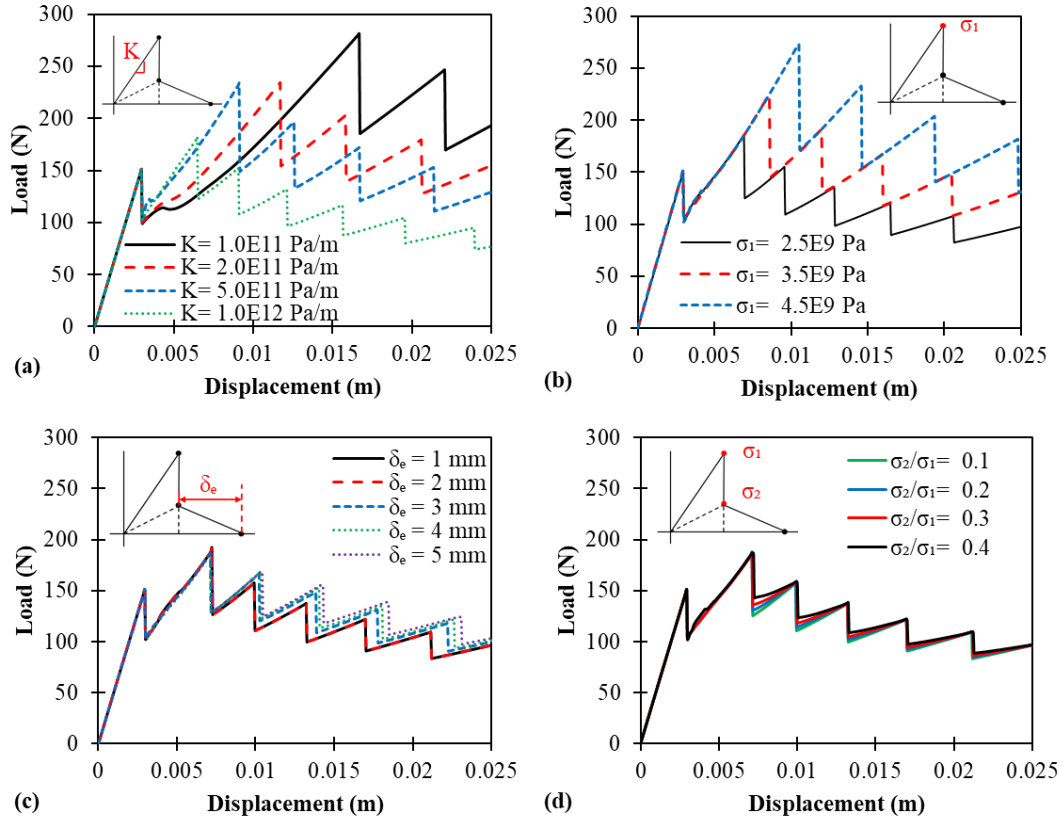


Figure 33. The influence of the (a) cohesive stiffness, (b) maximum stress σ_1 , (c) effective displacement δ_e , and (d) bridging strength σ_2 on the load-displacement response

4.2.3. Three-Dimensional Finite Element Analysis

The 3D FEM used to simulate separation between the facesheet and core is shown in Figure 34. Three different stitch densities (0.0016, 0.0057, and 0.01 stitches/mm²) corresponding to 2, 3, and 4 stitches, respectively, across the specimen width were considered. Incompatible hexagonal elements were used to improve the deformation gradients within the domain of the element when the test article is subjected to bending. Symmetry boundary conditions were imposed to decrease the computational time. A small-time increment, damage stabilization parameter, and a dissipated energy fraction of 10^{-30} seconds, 10^{-4} , and 0.004, respectively, were used to achieve convergence. To improve computational efficiency, the unstable crack growth between the facesheet and core at initiation was not simulated. A bilinear traction-separation law representing the facesheet-to-core interface was used, and a fracture energy of 220 J/m² with a maximum traction stress of 2.75 MPa was applied. The traction-separation laws used to represent the micromechanical damage process of stitching with different linear thread densities (400, 800, and 1200 Denier) are shown in Figure 35. The cohesive parameters used to represent each interface is shown in Table 10. A cohesive stiffness of 200 GPa/m for the through-the-thickness reinforcements was assumed and iteratively determined based on a sensitivity analysis, which is discussed in the following section. The maximum elastic stress was iteratively determined by comparing it to load-displacement measurements obtained from the SCB tests. The bridging

stress and effective displacement after stitch failure were assumed to be 0.1 of the maximum elastic stress and 1 mm, respectively, based on interlaminar tensile tests obtained from [22, 29, 60]. Since the bridging stress and effective displacements were assumed, the area under the curve of the traction-separation law was not maintained to be constant.

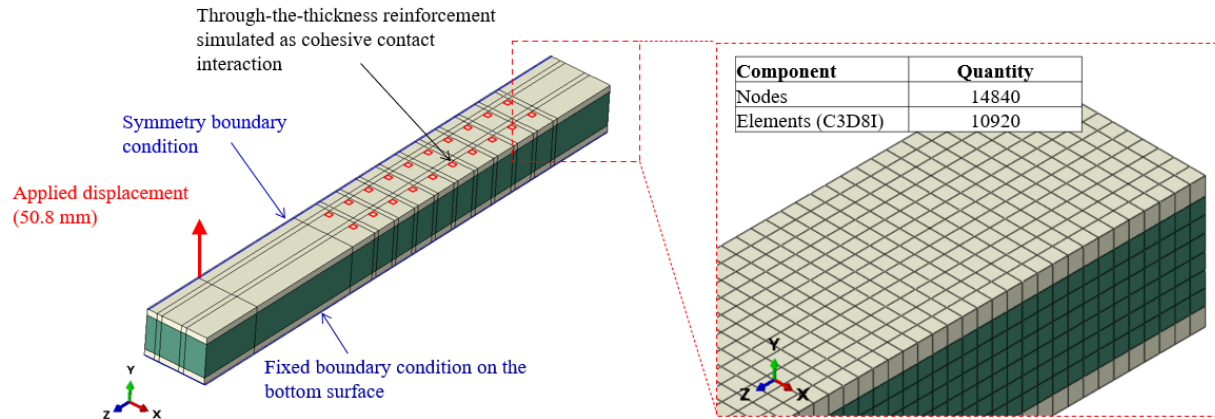


Figure 34. Three-dimensional finite element model to simulate crack growth in stitched sandwich composites

Table 10. Cohesive parameters of the 3D FEM

Times	TSL* Shape	Penalty Stiffness K_m (Pa/m)	Maximum Elastic Stress, σ_1 (Pa)	Elastic- Plastic Ratio (σ_2/σ_1)	Effective Displacement, δ_e (m)
Facesheet to core	Bilinear	1.00E+13	2.75E+06	-	1.60E-04
400 Denier Stitch	Trilinear	2.00E+11	3.00E+07	0.1	1.00E-03
800 Denier Stitch	Trilinear	2.00E+11	5.00E+07	0.1	1.00E-03
1200 Denier Stitch	Trilinear	2.00E+11	8.10E+07	0.1	1.00E-03

TSL: Traction-Separation Law

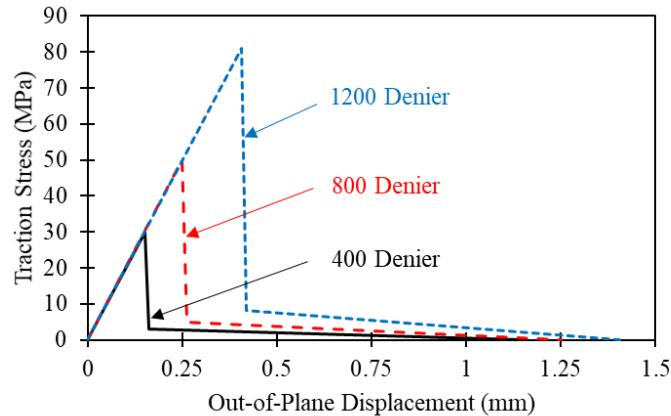


Figure 35. Traction-separation laws to represent stitching of different linear thread densities

A systematic approach for determining the traction-separation law for through-thickness reinforcements was used for stitched sandwich composite laminates that contain different stitch densities and linear thread densities. A comparison of the predicted and experimental values of the load and crack length for SCB specimens containing select stitch densities (0.0016, 0.0057, and 0.01 stitches/mm²) is shown in Figures 36(a) and 36(b). As mentioned previously, unstable crack propagation is observed at crack initiation, which results in a sharp decrease in the reacted load. Correspondingly, an increase in the measured crack length is observed. The reacted load then increases linearly as the crack front approaches the initial stitch row. Upon failure of a stitch row, the crack front immediately progresses to the adjacent stitch row. Sharp decreases in the measured load were observed as subsequent stitch rows failed. The magnitude of the failure load at each stitch row decreased with an increase in the applied displacement. This behavior is due to a greater distance between the location of the reacted load and crack front, which decreases with each subsequent failure of a stitch row. Increasing the stitch density from 0.0016 stitches/mm² to 0.01 stitches/mm² proportionally increases the maximum load prior to failure by approximately 48% and decreases the measured crack growth by approximately 16%.

The influence of select linear thread densities (400, 800, and 1200 Denier) on the load and crack growth of an SCB specimen is shown in Figures 37(a) and 37(b). Increasing the linear thread density from 400 Denier to 800 Denier increases the maximum load at which stitch failure occurs by approximately 18%, but only a 6% decrease is realized in the measured crack lengths. At high linear thread densities (>1200 Denier), the crack lengths decreased by approximately 14% to 30% was observed. The greater reduction in crack growth is mainly attributed to a change in the fracture morphology, as shown in Figures 38(a) and 38(b). Primarily stitch pullout from the facesheet was observed for low linear thread densities (400 Denier). At high linear thread densities (1200 Denier), stitch-column pullout and foam core failure were observed [58]. This change in fracture morphology results in significantly greater capability to resist crack growth. Lastly, multiple stitch rows were observed to fail for high and low linear thread densities.

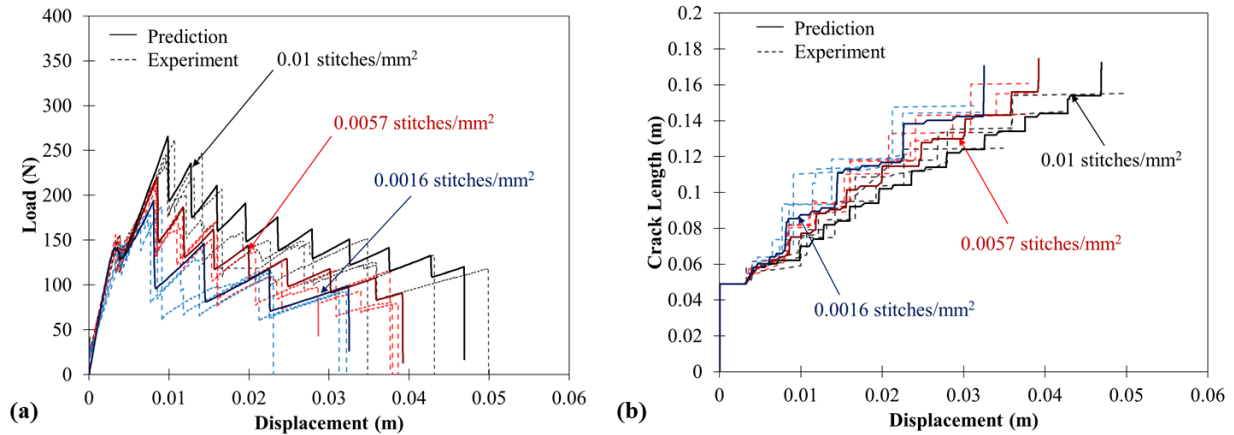


Figure 36. Influence of stitch density on the (a) load and (b) crack growth response

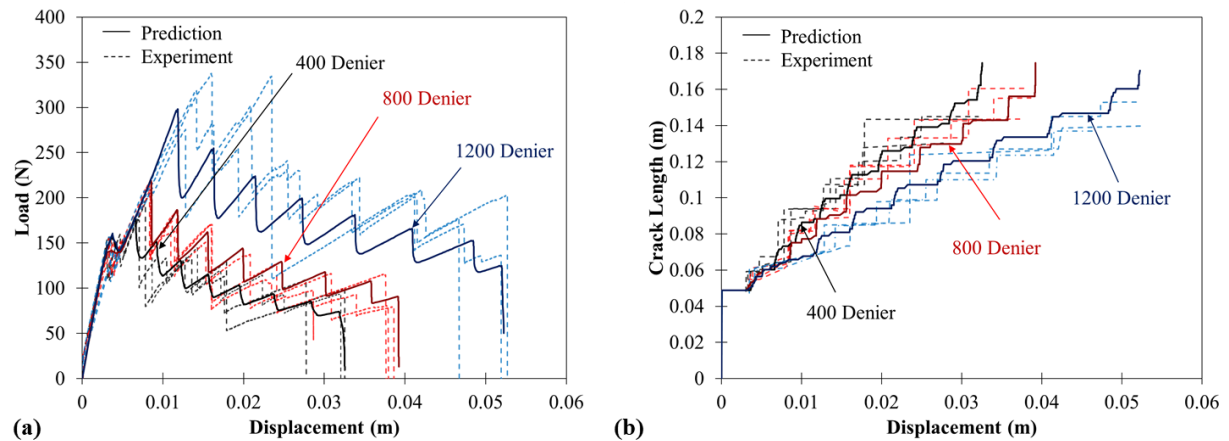


Figure 37. Influence of linear thread density on the (a) load and (b) crack growth response

The discrete cohesive FEM approach shows excellent agreement with the load and crack length measurements for each select stitch density and linear thread density. In this approach, the traction-separation law for the through-thickness reinforcement was assumed to be the same for each stitch row. During some of the tests, the simultaneous pullout of the through-thickness reinforcements along a stitch row did not occur, which caused some discrepancy between the predicted and experimental results, as shown in Figure 37(a). This behavior may occur due to the misalignment of the through-thickness reinforcement. Additionally, compaction of the structural threading can occur during the vacuum bagging process, which can distort the reinforcement in the through-the-thickness direction and result in a variation of the maximum elastic strength. These aforementioned effects induce changes in the applied displacement at which a stitch row failure occurs. Therefore, the load-displacement and crack-growth responses can appear shifted due to a variation in the traction-separation law of each through-the-thickness reinforcement. It may be more appropriate to use a stochastic finite element process to take in account the

variation in the traction-separation response for various linear thread densities and stitch densities used in stitched sandwich composites.

The predicted crack front curvature along the specimen's width for select loads and applied displacements is shown in Figure 39. The initial crack front curvature is straight along the specimen's width prior to crack initiation (Region 1). As the crack propagates (Region 2), a variation in the growing crack front is observed due to the anticlastic curvature of the facesheet subjected to bending [42, 61-64]. This behavior is due to Poisson's effect, which results in

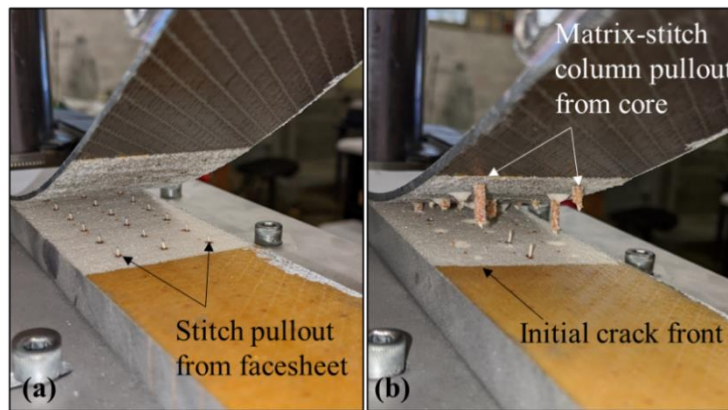


Figure 38. Fracture morphology during the facesheet-to-core separation of a sandwich composite stitched with (a) 400 Denier and (b) 1200 Denier Vectran™ thread

opposing concave and convex curvature along each side of the composite facesheet. Additional curvature in the crack front is observed as the crack front approaches the initial stitch row (Region 3). The through-thickness reinforcements locally constrain the facesheet to resist crack growth and result in some curvature in the crack front near the stitching. Crack front curvature appears to be dominated by the anticlastic deformation of the facesheet away from the through-thickness reinforcements. At the maximum applied load, the crack front moves beyond the initial stitch row (Region 4), which is followed by failure of the initial stitch row. Afterward, the crack front immediately propagates to the second stitch row and stitch bridging is observed (Regions 5-6).

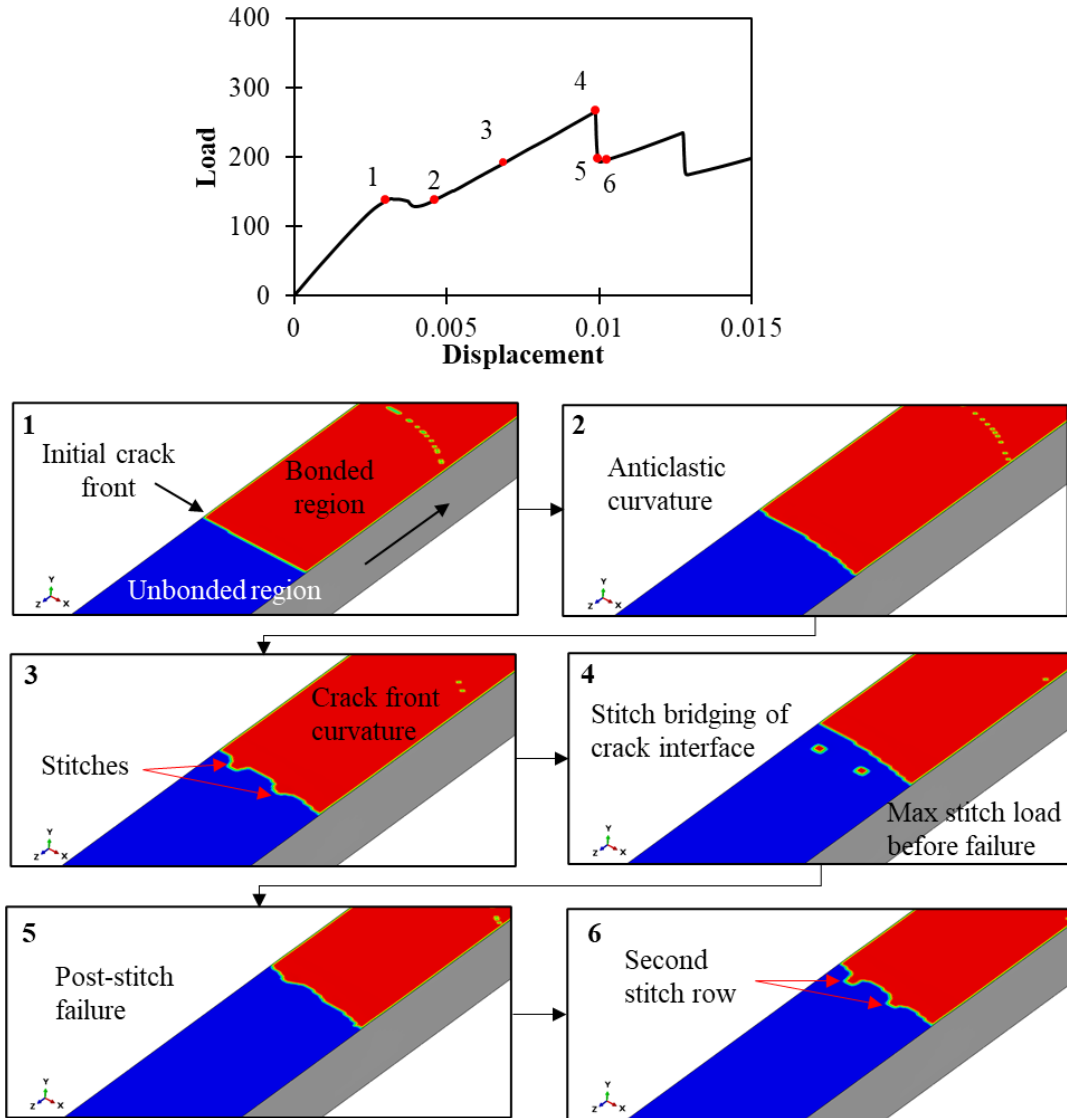


Figure 39. Crack front variation due to the presence of the through-thickness stitching

4.2.4. Conclusions

A discrete cohesive zone modeling approach was used to simulate the mode I load and crack length response of stitched sandwich composites with select stitch densities (0.0016-0.01 stitches/mm²) and linear thread densities (400-1200 Denier). Single cantilever beam (SCB) tests were performed to determine the load and crack growth response as a function of the applied displacement. Experimental tests reveal unique fracture morphologies that are dependent on stitch parameters. Using low linear thread densities (400 Denier), stitch pullout from the facesheet near the facesheet-to-core interface was observed. However, matrix-stitch column pullout from the core was observed at the facesheet-to-core interface for stitched sandwich composites with high linear thread densities (1200 Denier) of Vectran™ thread.

A trilinear traction-separation law was used to represent the failure process of the through-the-thickness reinforcement within 2D and 3D FEMs. The 2D FEA revealed that the cohesive stitch stiffness and elastic maximum traction stress of the stitch are the primary parameters that influence the initial load-displacement response of an SCB test, whereas the bridging stress and effective displacement influenced the load-displacement response after the initial stitch row failure. The predicted load and crack length responses using a 3D FEM have good agreement with experimental measurements. However, the current approach does not consider the variation of the traction-separation law used to represent the through-the-thickness reinforcement. Variation in the maximum elastic stress, bridging stress, and effective displacement after stitch failure may occur due to misalignment, angle of the reinforcement, and stitch compaction that could occur during fabrication. The 3D FEM approach showed that crack fronts may be influenced by the through-the-thickness reinforcements and can lead to crack curvature along the specimen width. Lastly, the methodology presented in this study provides a pathway to guide researchers on the selection of a trilinear traction-separation law that accurately represents the failure process of stitching during delamination events.

4.3. Estimation of Crack Growth during the Fracture of Stitched Sandwich Composites

Mode I characteristics of stitched sandwich composites were examined. Data from single cantilevered beam (SCB) tests (Section 4.1) was used in the model development and validation. The SCB specimens were embedded with optical fibers (OF) to determine internal crack length measurements using a Lagrangian cross-correlation approach [57]. Implicit finite element analysis was performed and compared to OF strain measurements.

4.3.1. Computational Procedure

Implicit FEA of stitched SCB specimens was performed using ABAQUS commercial software. The material and fracture properties used in the FEMs are summarized in Table 11. The unstitched and stitched fracture energies were determined from single cantilever beam testing and were obtained from a previous study [58]. The 3D FEM used to simulate separation between the facesheet and core is shown in Figure 40. Incompatible hexagonal elements were used to improve the deformation gradients within the domain of the element when the test article is subjected to bending. Symmetry boundary conditions were imposed to decrease computational time. A time increment, damage stabilization parameter, and a dissipated energy fraction of 10^{-30} seconds, 10^{-4} , and 0.004, respectively, were used to achieve convergence. To improve computational efficiency, the unstable crack growth between the facesheet and core at initiation was not simulated. A bilinear traction-separation law representing the facesheet-to-core interface was used, and fracture energy of 220 J/m^2 with maximum traction stress of 2.75 MPa was applied. A trilinear traction-separation law was used to represent the failure process of the through-the-thickness reinforcement and is shown in Figure 41. A cohesive stiffness of $2\text{E}11 \text{ Pa/m}$ for the through-the-thickness reinforcements was assumed and based on a sensitivity analysis (Section 5.2). The maximum elastic stress was iteratively determined by comparing it to load-displacement measurements obtained from the SCB tests. The bridging stress and effective displacement after stitch failure were assumed to be 0.1 of the maximum elastic stress and 1 mm, respectively, based on interlaminar tensile tests obtained from [22, 29, 60].

Table 11. Bulk material properties for the facesheet and core [1-4]

Facesheet [2, 3, 57]	
$E_{11} = E_{22}$ (Pa)	5.66E+10
E_{33} (Pa)	8.64E+09
$G_{12} = G_{13}$ (Pa)	4.66E+09
G_{23} (Pa)	4.95E+09
ν_{12}	0.0619
$\nu_{13} = \nu_{23}$	0.25
Core [4]	
E (Pa)	1.86E+08
ν	3.30E-01
Facesheet to Core Fracture Properties [58, 59]	
G_I (J/m ² ; Facesheet to core)	220
K_{nn} (Pa; Mode I Penalty Stiffness)	10E+13
σ_{max} (Pa; Facesheet to core – 3D)	2.75E+6

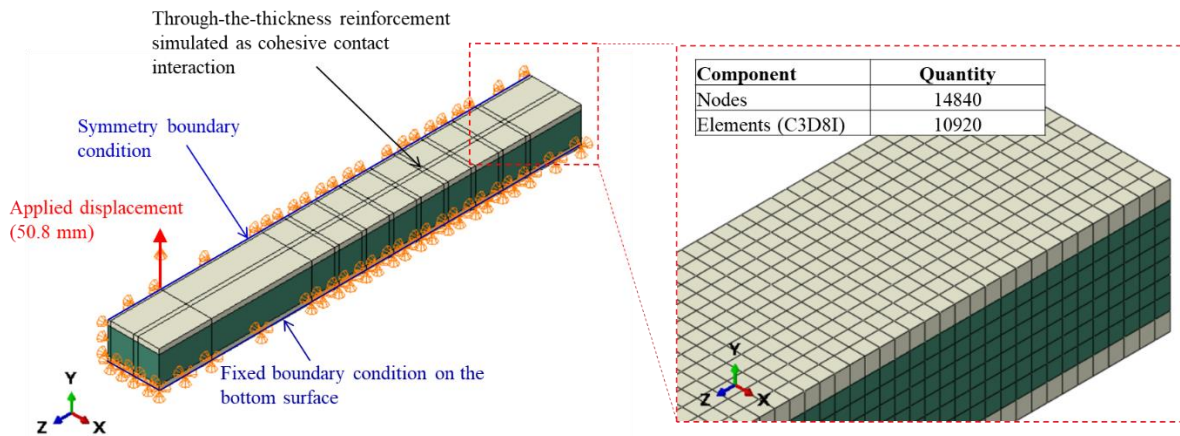


Figure 40. Stitched sandwich composite finite element model

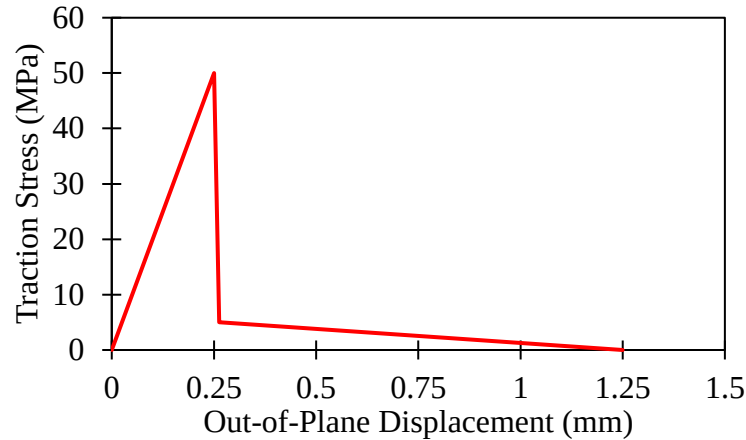


Figure 41. Stitch traction-separation law

4.3.2. Numerical Procedure to Determine Internal Crack Length

The profile of strain along the length of a DCB can be characterized by a “wave-like” strain distribution that propagates with the progression of delamination, as shown in Figure 42 [57]. At crack initiation, the flexure of the cantilevered arm exhibits a linear strain variation along the specimen length ($0.98 \text{ m} < x < 1.02 \text{ m}$). Near the crack front, the strain distribution is perturbed by the stress intensity of the crack front ($0.95 \text{ m} < x < 0.98 \text{ m}$). The magnitude of the strain decreases to zero as the location of the measurement of strain decreases ($x < 0.95 \text{ m}$). As the crack propagates to the nearest stitch row, an increase in the strain magnitude and a shift in the strain distribution is observed. As the initial stitch row fails, the magnitude of the strain decreases with a further shift in the strain distribution. Good agreement is observed between the OF strain measurements and predicted FEA strains.

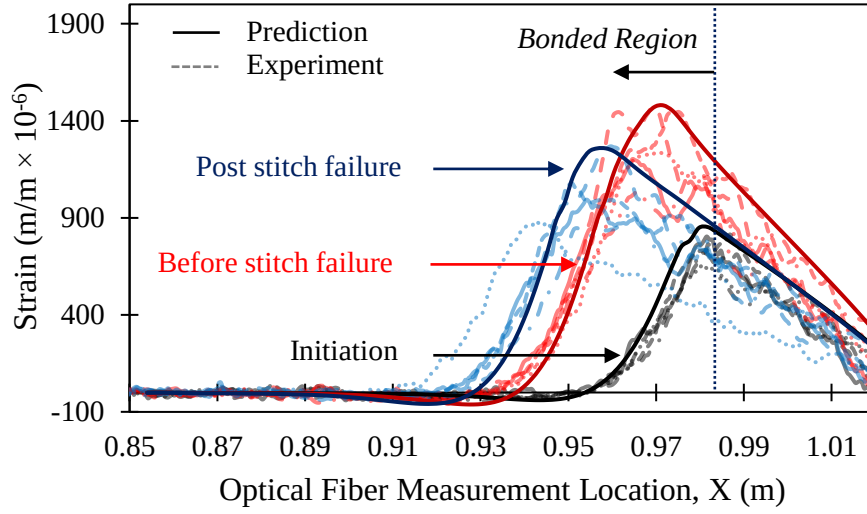


Figure 42. Predicted and measured strain distributions at initiation, before stitch failure, and post stitch failure

Identification of the delamination front is achieved by measuring the shift in the differential strain due to an increase in crack length [57]. The differential strain is evaluated as

$$\frac{d\varepsilon}{dx} \approx \frac{\varepsilon_i - \varepsilon_{i-1}}{x_i - x_{i-1}}, (i = 1, 2, \dots, N) \quad (35)$$

where ε_i and ε_{i-1} correspond to the i^{th} and $i-1$ measurement of strain for a total of N samples. Similarly, x_i and x_{i-1} correspond to the i^{th} and $i-1$ location along the OF length. The crack length is estimated by using a Lagrangian cross-correlation approach. In this Lagrangian scheme, the measured differential strain during delamination propagation are correlated to the differential strain at the onset of delamination, or max applied load. Cross-correlation ρ_{12} measures the similarity between two signals [44] and can be expressed as

$$\rho_{12}(\Delta a) = \int_{-\infty}^{\infty} \frac{d\varepsilon_1(x)}{dx} \cdot \frac{d\varepsilon_2(x + \Delta a)}{dx} dx \quad (36)$$

where $\frac{d\varepsilon_1(\Delta a)}{dx}$ and $\frac{d\varepsilon_2(x+\Delta a)}{dx}$ corresponds to the differential strain when the maximum load occurs and for subsequent loadings, respectively, at a location x along the OF. The correlation value ρ_{12} becomes a maximum (ρ_{max}) when the differential strain $\frac{d\varepsilon_1}{dx}$ at x has the same gradient in $\frac{d\varepsilon_2}{dx}$ at $x+\Delta a$. The distance Δa corresponds to the change in crack length when $\rho_{12} = \rho_{max}$. The distance Δa_{max} corresponds to the largest distance to obtain a measure of the correlation between $\frac{d\varepsilon_1}{dx}$ and $\frac{d\varepsilon_2}{dx}$. Once the change in the crack length is determined, the total crack length can then be estimated as

$$a = a_0 + \Delta a \quad (37)$$

where a_0 is the initial crack length. The corresponding mode I fracture toughness G_I is estimated using the modified compliance calibration method using

$$G_I = \frac{3}{2} \frac{P^2 C^{2/3}}{A_1 b h} \quad (38)$$

where P and C are the applied load and compliance, respectively. The compliance is determined from the ratio of the applied displacement (δ) to the applied load (p). The specimen width and thickness are denoted as b and h , respectively. The slope A_1 of the normalized crack length (a/h) is a linear function of $C^{1/3}$.

4.3.3. Results and Discussion

A comparison of the predicted and experimental values of the load and crack growth for each test is shown in Figures 43 and 44, respectively. Unstable crack propagation is observed at crack initiation, which results in a sharp decrease in the reacted load and a corresponding increase in the crack length. The reacted load then increases linearly as the crack front approaches the initial stitch row. Stitch row failure occurs at each maximum load peak during crack propagation, where the magnitude of the maximum load decreases with an increase in crack length. This behavior is due to a greater distance between the location of the reacted load and crack front, which increases with each subsequent failure of a stitch row. Good agreement was obtained between the experimental measurements and predicted values using the discrete cohesive zone approach.

The OF crack length estimates showed approximately 10% greater crack growth magnitude as compared to visually obtained measurements (Figure 44). This behavior is attributed to the variation of the fracture energy along the delamination front. The crack front is typically non-uniform and exhibits greater crack growth near the centerline of the specimen. This behavior is developed from an anticlastic curvature of the facesheets due to Poisson's effect. However, the fracture energy can also fluctuate due to local differences in fiber-to-resin bond strength, fracture morphology, and porosity [46]. In particular, variation in the stitch failure strength may occur in stitches along the same stitch row due to misalignment and differences in stitch pretension. Therefore, greater crack lengths may be observed along the width of the SCB specimen due to premature failure of stitches along the same stitch row.

The fracture energy of the stitched sandwich composite specimens with respect to the crack length is shown in Figure 45. At crack initiation, a relatively low fracture energy value is observed (250 J/m²). This fracture energy depicts the required energy of separation between the facesheet and core without the presence of through-the-thickness reinforcements. As the crack front approaches the initial stitch row, the fracture energy is increased by approximately 330%. The maximum energy for each stitch row is relatively constant prior to the stitch row failure. Failure of the stitch row results in a significant reduction in the fracture energy and produces unstable crack growth between the facesheet and core. However, the nearby stitch rows arrests the crack growth after each subsequent failure of a stitch row.

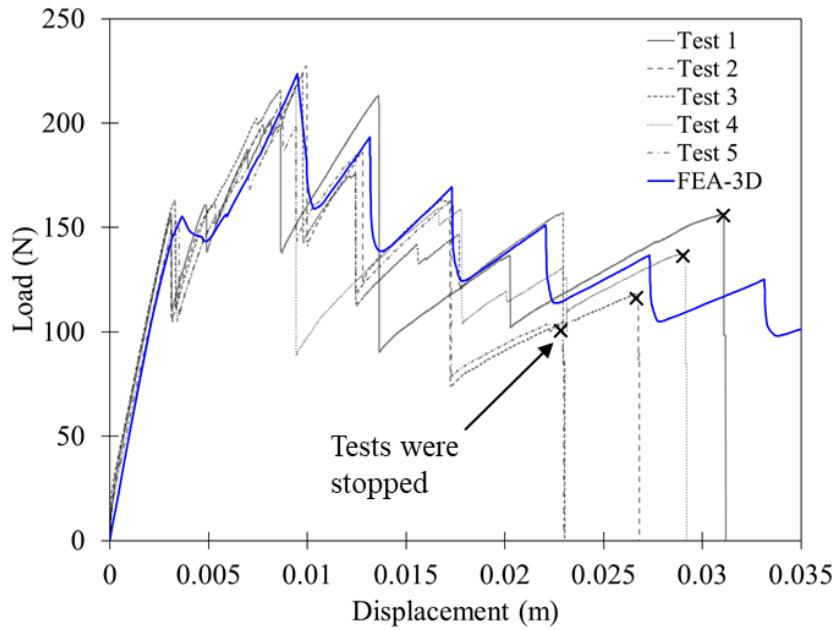


Figure 43. Predicted and measured load-displacement response

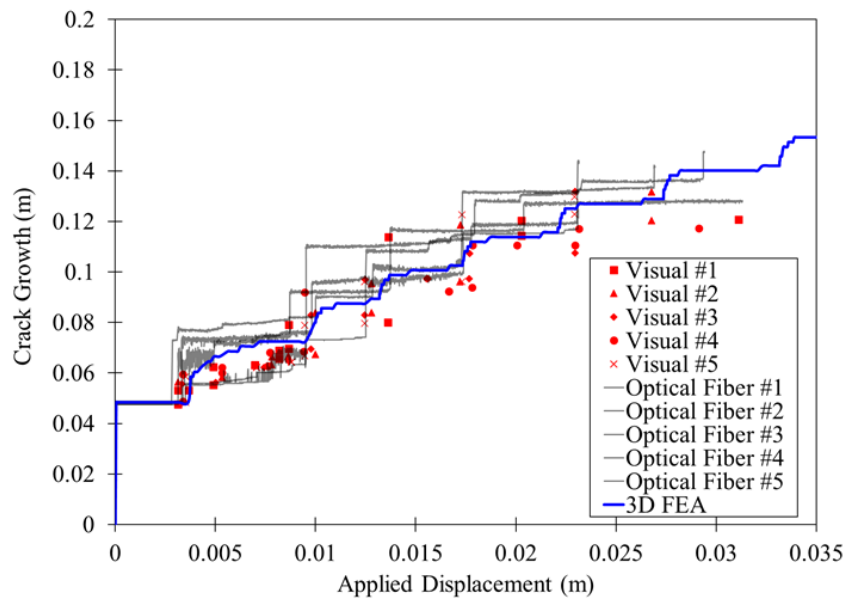


Figure 44. Predicted and measured crack growth response

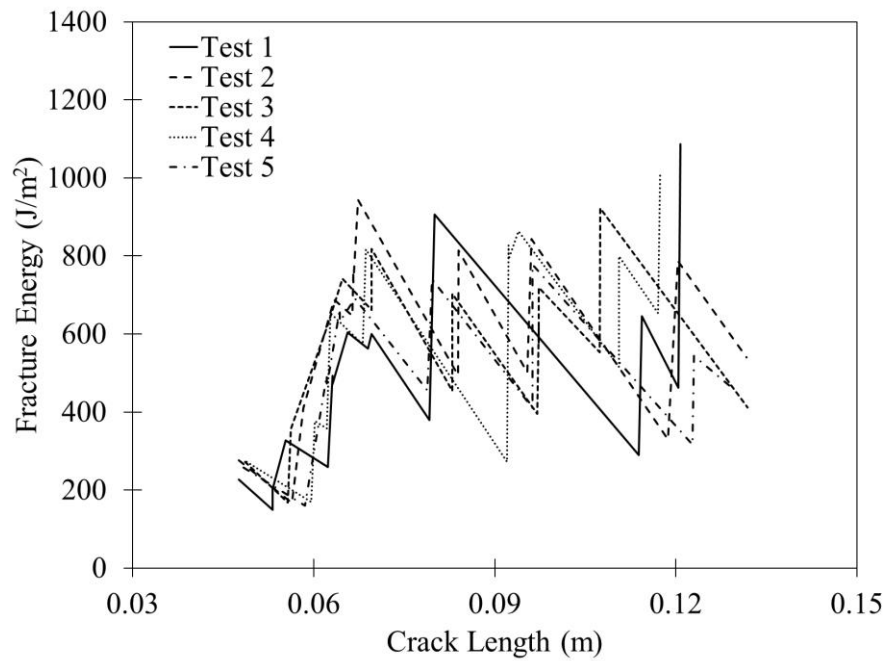


Figure 45. The mode I fracture energy of stitched sandwich composite

4.3.4. Conclusions

The crack growth behavior of stitched sandwich composites is investigated by performing single cantilevered beam tests with embedded optical fibers. Unstable crack growth was primarily observed during each test. Strain distributions obtained from optical fibers were correlated to the crack propagation of delamination. Internal crack growth measurements were obtained using a Lagrangian cross-correlation approach. Internal crack growth was approximately 10% greater in magnitude as compared to visual measurements. Additionally, a finite element analysis of the SCB specimen showed good agreement with load and crack growth estimates obtained from the optical fiber measurements. The calculated fracture energy near the through-the-thickness reinforcement was approximately three times the unstitched fracture energy. These results show the excellent capability of stitching to arrest core-to-facing separation in sandwich composites.

5. IMPLEMENTATION: SUBCOMPONENT (STRUT DOOR) FEA AND FABRICATION

Most research has been limited to coupon-level tests rather than structural or sub-component tests. While coupon-level tests are essential for understanding the fundamental mechanical behavior of the composite, full-scale component testing is necessary to obtain the structural response under operational loads. This allows for a better understanding of the failure mechanisms and leads to an improved design and more reliable structures.

Results from the earlier study informed the design of a stitched sandwich composite main landing gear door. A finite element analysis (FEA) was conducted on the strut door. Also, to demonstrate manufacturability, strut doors were constructed from carbon fiber non-crimp fabric facesheets and closed-cell foam core; the doors were stitched through the thickness using a modified lock stitch technique. Two doors were fabricated, with both doors stitched (using a modified lock stitch) around the perimeter, and one of the doors also stitched through the sandwich core. A vacuum-assisted resin transfer molding process was used to infuse the specimens. The FEA analysis and the manufacturing process are presented.

5.1. Finite element analysis of composite strut door

5.1.1. Material properties

The composite door is comprised of lightweight carbon/epoxy facesheets and an internally bonded foam core with an average thickness of 0.188 inches around its perimeter and 0.688 inches at the thickest location due to the 0.5-inch foam core. The material properties used in the FEA model are listed in Table 12. The strut door model consists of the interior skin, core, and exterior skin, as shown in Figure 46.

Table 12. Material properties used in FEA model of strut door

Composite Properties	
$E_{11} = E_{22}$ (GPa)	56.6
E_{33} (GPa)	8.64
$G_{12} = G_{13}$ (GPa)	4.66
G_{23} (GPa)	4.95
ν_{12}	0.06
$\nu_{13} = \nu_{23}$	0.25
Foam Core	
E (Pa)	0.19
ν	0.33

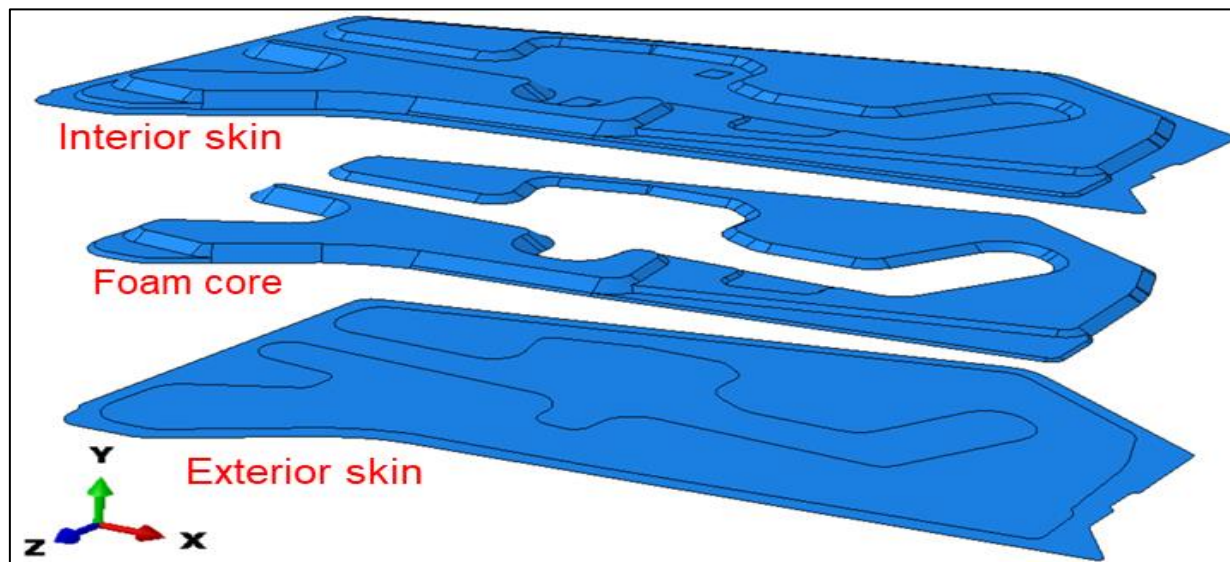


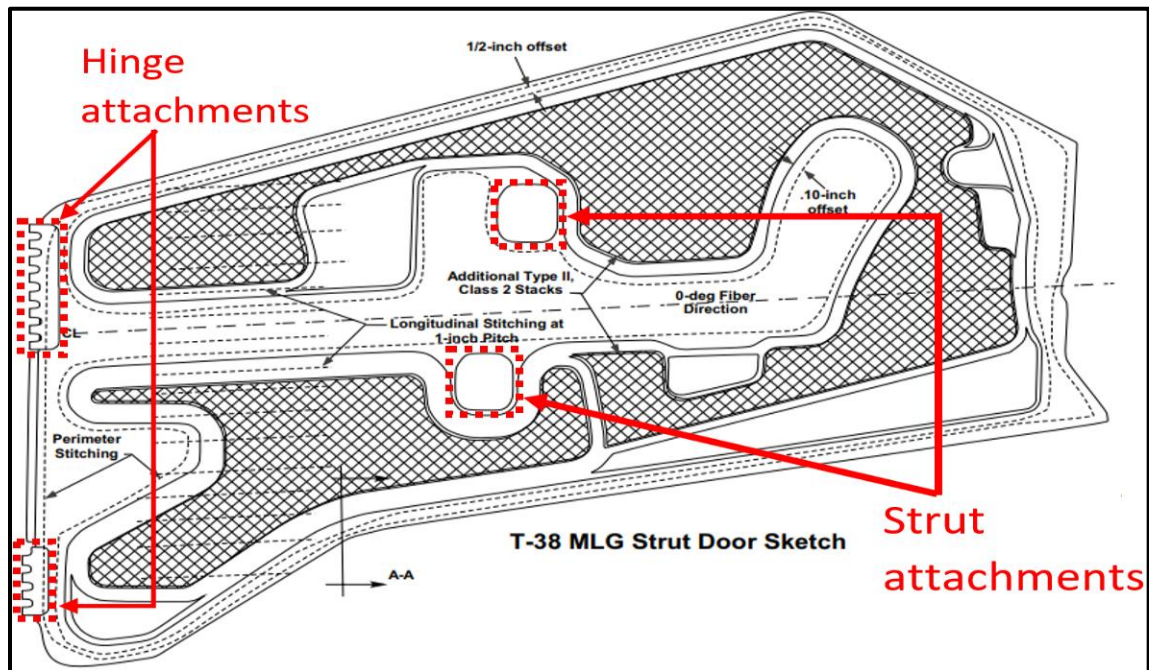
Figure 46. Exploded view of the strut door showing the interior skin, foam core, and the exterior skin

5.1.2. FEA Model

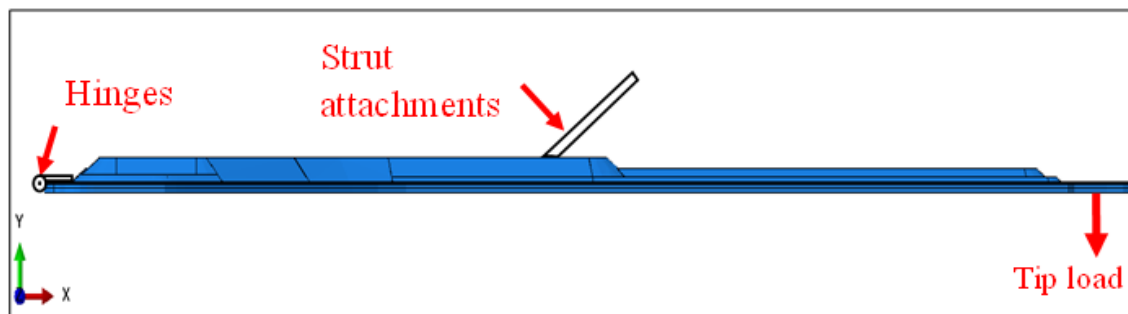
A commercial FEA software (ABAQUS) was used to perform an implicit analysis of the composite strut doors. The FEA analysis was performed for an unstitched composite strut door.

Figure 47 shows the location of the hinges and the strut attachment points. Kinematic coupling was used to attach connector surfaces, representing hinges and struts, to reference points on the door. Both the hinges and strut reference points had an offset of 0.25 inches from the interaction surface. All hinge and strut boundary conditions allowed rotation about the z-axis to simulate flight operations. The analysis was conducted for a tip load of 4450 N, as shown in Figure 47b.

A surface-to-surface tie constraint with hard contact interactions was used for the composite door model between the interior skin, foam core, and exterior skin. The mesh for the complex geometry presented some challenges, but these were resolved by connecting small lengths to form continuous edges. The composite door was modeled using conventional shell elements (S4R) for the composite parts and modified tetrahedral elements (C3D10M) for the foam core region. The meshed portions of the foam core and the skin are shown in Figure 48. Using conventional shell elements for the composite skins allowed easy modification of the composite layups. These elements also automatically accounted for the shell thicknesses in the model, so minimum changes needed to be made to the assembly to investigate a new layup. A mesh convergence study was also performed on the composite door model; an optimum mesh size was determined to be 10 mm for the skins and 7.6 mm for the core. As a result, the composite model had a total of 58,900 elements and 98,460 nodes.

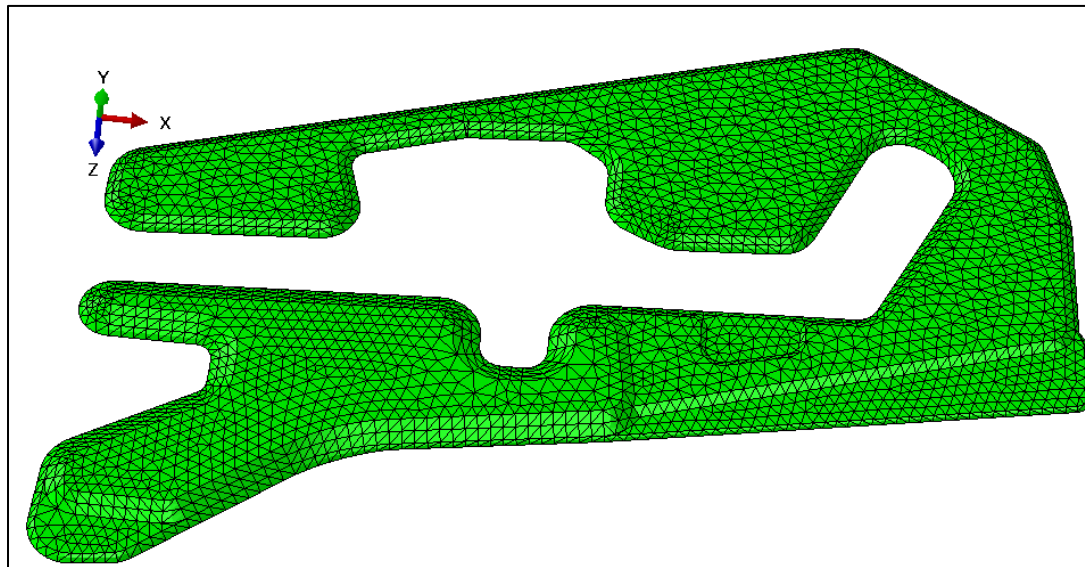


(a)

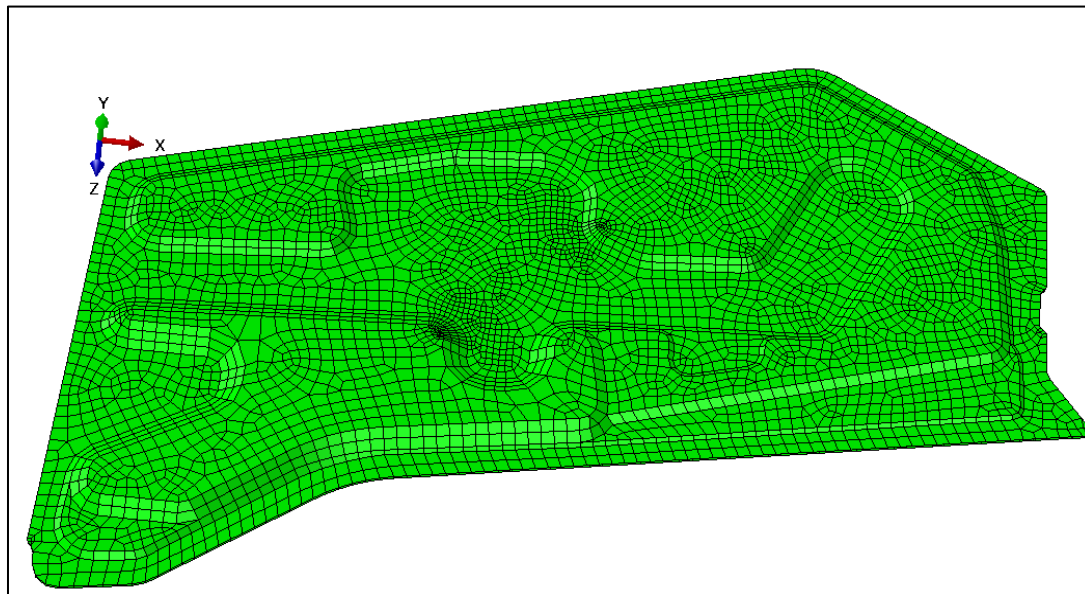


(b)

Figure 47. Schematics showing location of the hinge and strut attachment points for (a) top view and (b) side view of strut door



(a)



(b)

Figure 48. Meshed region of (a) foam core and (b) interior skin

5.1.3. FEA Results

The max principal stress due to the tip load on the strut door is shown in Figure 49. In general, areas of high stress are seen to be around the strut attachment points and the foam core edges. The strut attachment locations also have higher stresses when compared to the hinge locations. The boundary condition in the hinge area in the FEA model are surface-based, leading to the load being distributed to a larger area. Due to the tip loading condition, the struts undergo higher reaction forces than the hinge area.

The out-of-plane deflection of the door is shown in Figure 50. A maximum tip deflection of 40 mm is obtained. As expected, the deflection between the hinged edge and the strut location points is limited by the struts.

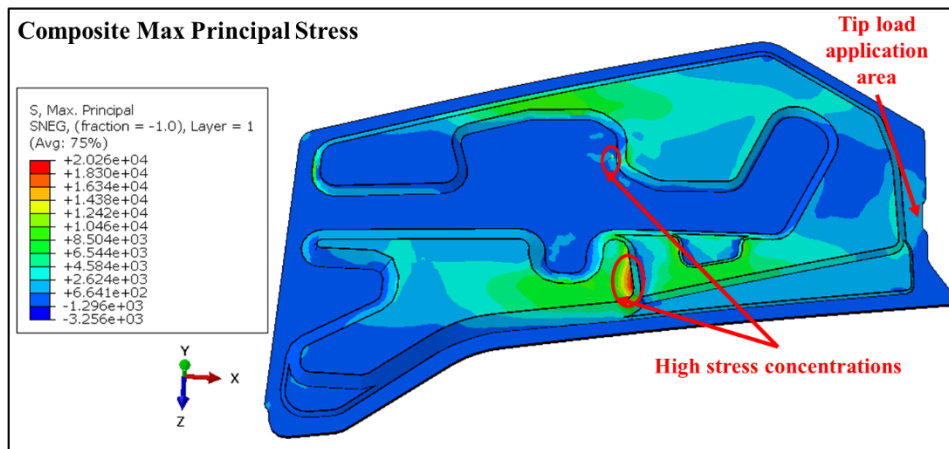


Figure 49. Max principal stress on the interior skin (psi)

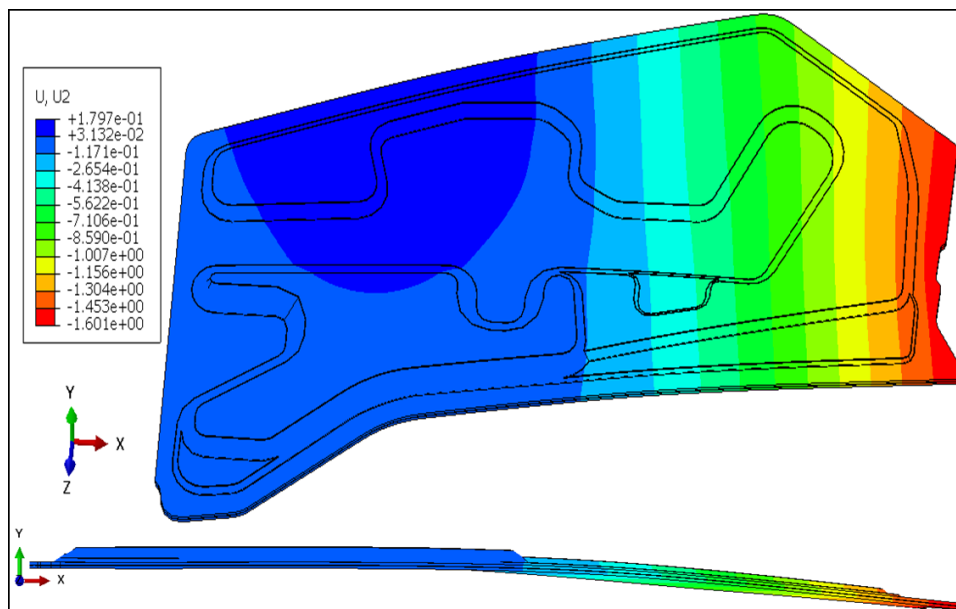


Figure 50. Out of plane deflection due to tip load

5.2. Fabrication of stitched sandwich composite strut door

5.2.1. Layup

CAD drawings of the strut door were made in SOLIDWORKS as shown in Figure 51. The outline was created by flattening the contour of the inside of the door and extending the perimeter by 1.5 inches. This allows the fabric to cover the contours of the foam core and provides trim material that results in clean edges of the final part. The part line was taken from the projected view of the strut door and was used to cut the infused door into the final shape and size. All composite fabric was cut using these drawings and a Gerber cutting machine.

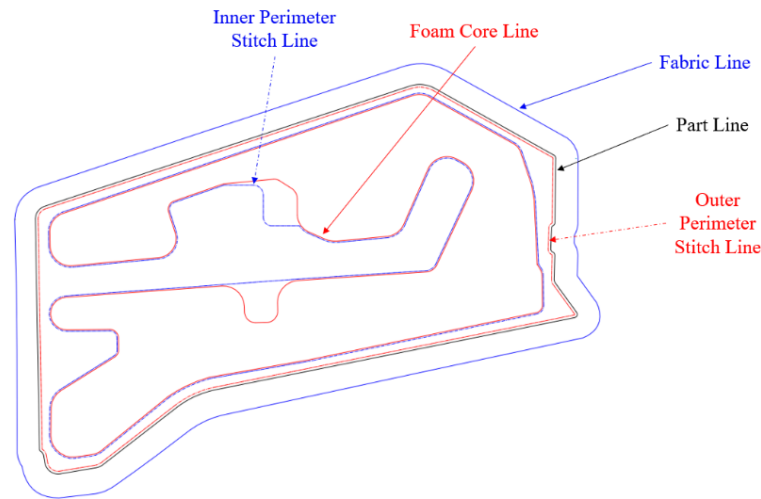


Figure 51. CAD drawing of strut door used for cutting preform stacks

Two strut doors were fabricated using dry carbon non-crimp fabric (NCF) and lightweight (110 kg/m^3) foam core. The layup consists of a Rohacell Hero 110 foam core sandwiched between the interior and exterior SearTex® carbon fiber ply stacks, as shown in Figure 52. The layup orientation for the exterior and interior plies are $[+45/-45/0/90/0/-45/+45]_3$ and $[+45/-45]_2$, respectively. The exterior stacks were stitched 0.5 inch from the edge to prevent shifting and fraying of the fabric (Figure 53). The stitch line was beyond the outer part line, and thus trimmed post cure.

The foam core was placed on top of the exterior stacks, as shown in Figure 54. To ensure proper placement, a template was cut with the Gerber cutting machine on the fabric line; the foam core line was clipped in alignment with the fabric perimeter. The interior stacks were placed on top and manually smoothed to the contour of the foam. Clips were attached to the edges of the preform to limit shifting of the layup before stitching.

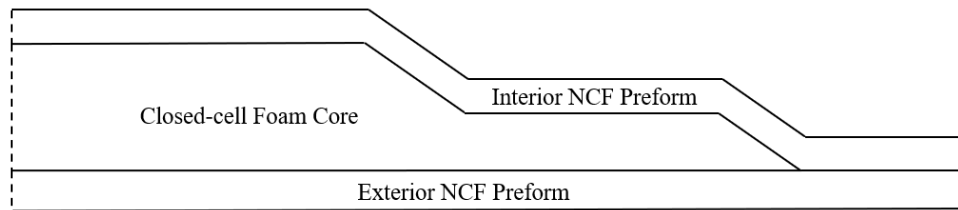


Figure 52. Layup Schematic



Figure 53. Exterior Stack Perimeter Stitching

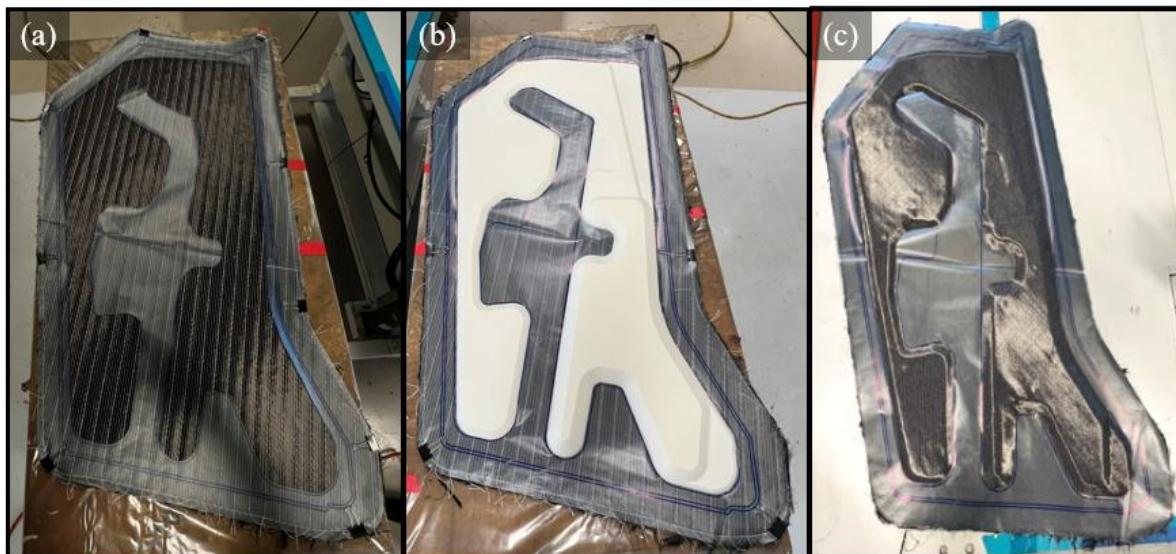


Figure 54. Exterior stacks with (a) placement template (b) foam core and (c) verification of core placement.

5.2.2. Stitching

All stitching used a modified lock stitch and was done on a 2,000H JUKI Pro machine with 1200 Denier linear thread density Vectran thread. A modified lock stitch is a double-sided stitching technique in which a threaded needle penetrates the preforms from the top side and forms a lock on the underside with the bobbin thread. The threaded needle is inserted into the preform and caught with the bobbin thread underneath. As shown in Figure 55, the modified lock stitch knot between the top and bottom threads remains on the bottom of the preform.

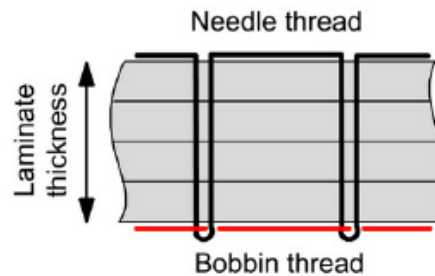


Figure 55. Modified lock stitch schematic[18]

The strut doors were stitched using two different stitching patterns. For Strut Door 1 (SD1), the exterior plies were stacked and stitched using a 10 mm pitch and 25.4 mm spacing, as shown in Figure 56a. SD1 was assembled by placing the core between the stitched exterior preform and the unstitched interior ply stack. SD1 was stitched through the NCF preform with a 0.2 inches offset from the part edge using a pitch of 10 mm, followed by stitching around the outer boundary of the foam core, as shown in Figure 56b. Strut Door 2 (SD2) included the stitching of SD1 and stitched through the thickness of the foam core with a 10 mm pitch and spacing, as shown in Figure 57. All stitching was done on the flat surfaces of the contoured foam core. To ensure consistent stitching, the thread tension settings were adjusted in accordance with the thickness of the foam core.

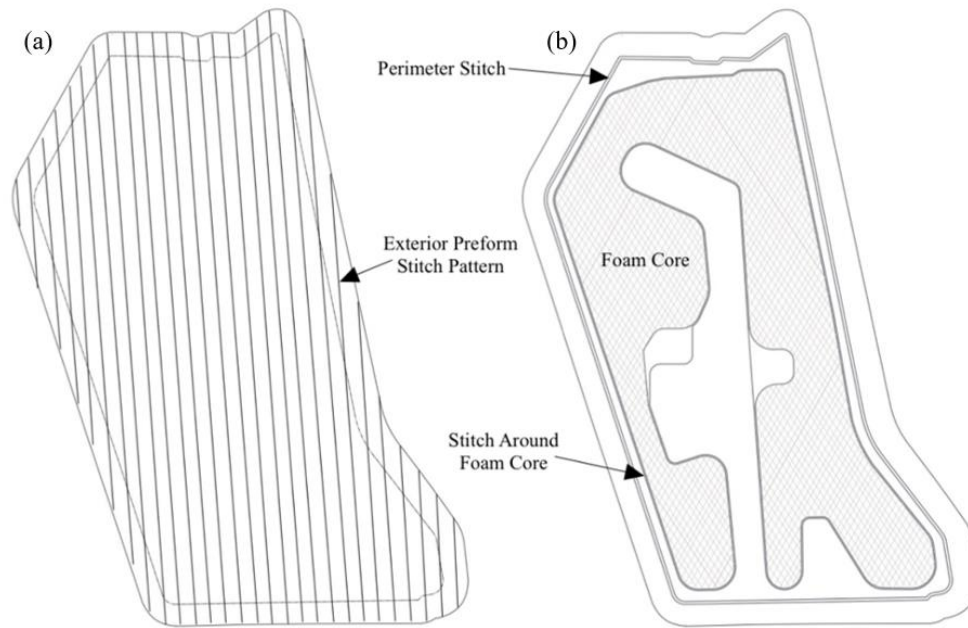


Figure 56. Strut Door 1 Stitching Pattern for (a) inner surface and (b) outer surface

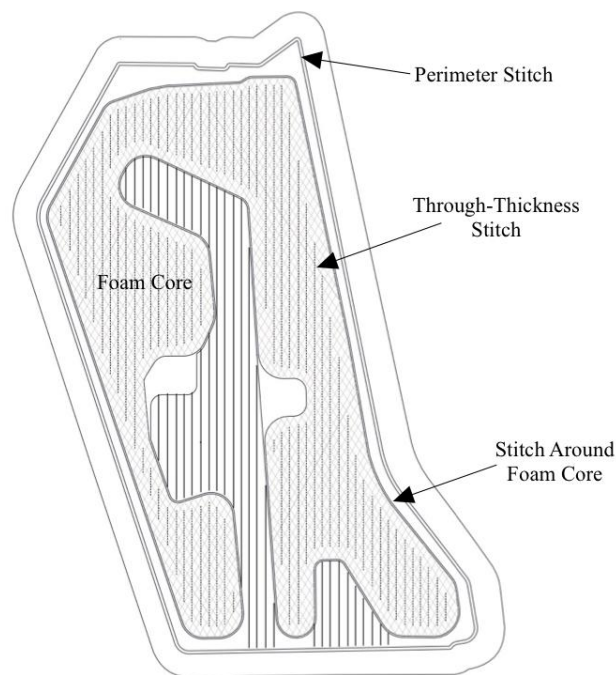


Figure 57. Strut Door 2 Stitching Pattern

5.2.3. Infusion

The strut doors were sealed into separate vacuum bags and a distribution spring was placed along each door with a resin inlet in the center to promote adequate flow of the resin to the parts (Figure 58). Using a vacuum-assisted resin transfer molding process, the composite door preforms were infused with Hexcel HiFlow 1078 resin using a pressure pot and underwent the oven-curing cycle shown in Figure 59. Following infusion, the strut doors were removed from the mold and trimmed using a waterjet cutter with the final parts shown in Figure 60a and 60b.

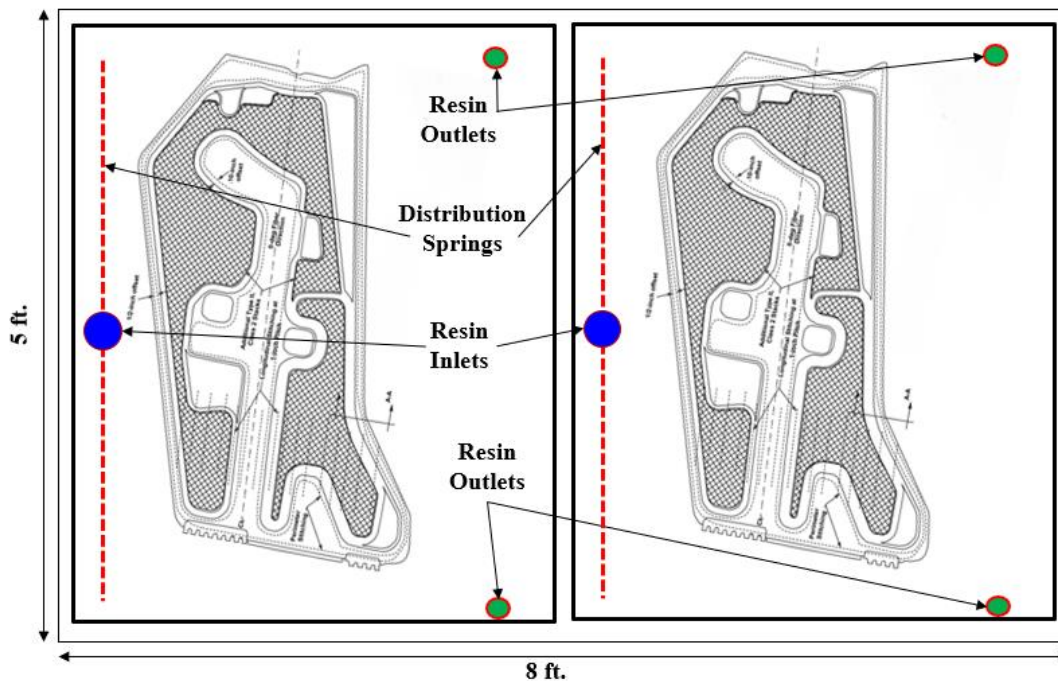


Figure 58. Infusion Layout

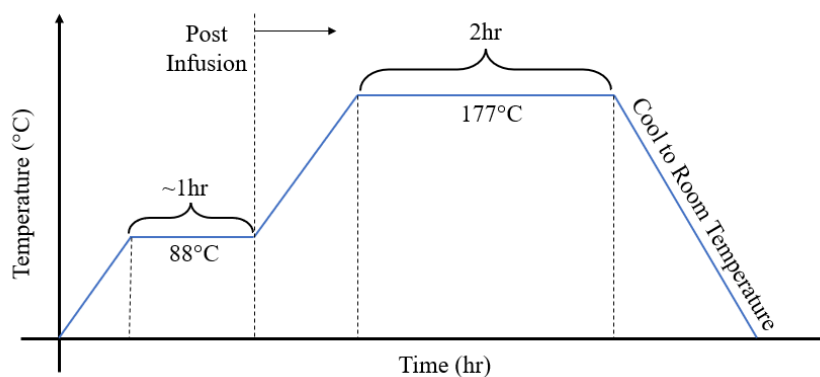


Figure 59. Cure Cycle



Figure 60. Post-cure and trim of (a) SD1 with perimeter stitching and (b) SD2 with perimeter and through foam stitching

5.3. Design and Manufacturing Conclusions

A finite element model was developed for the composite strut door subjected to a tip load. The highest stress concentrations were found to be at the strut attachment locations.

Manufacturability of the strut doors was successfully demonstrated. Two strut doors with two different stitching configurations were fabricated using dry carbon non-crimp fabric. The first composite door was stitched through the dry carbon plies and around the foam. The second door included stitching through varying foam core thickness over the complex geometry, followed by a successful VARTM cure and final trim of both strut doors.

6. RESULTS & DISCUSSION

This research includes an in-depth literature review on the fracture behavior of stitched composite materials, fracture characterization of stitched sandwich composites, optimization of stitch processing parameters, and computational simulation of the mode I fracture of stitched sandwich composites. Additionally, a unique experimental method was developed to determine an internal crack length using optical fibers embedded within composites. As a result of this study, numerous peer-reviewed journal articles, conference papers, and university publications were produced [1, 18, 20, 57, 58, 59]. The primary objective of this study was to assess the influence of stitching on the mode I fracture behavior of stitched sandwich composites. A stitch parameter design space was developed to characterize the fracture energy using a face-centered central composite design approach. A response surface model (RSM) was developed to determine optimum stitch processing parameters to inform a finite element model to predict crack-growth arrestment.

Lightweight composite materials are essential in the aerospace industry to reduce emissions, decrease fuel cost, and increase aircraft range and payload. Furthermore, composites have highly tailorable in-plane mechanical properties, which can be designed by altering individual ply orientations. However, this results in relatively low interlaminar properties between plies of different orientation when compared to metallic alternatives. Through-thickness reinforcements, such as stitching, z-pinning, needling, tufting, and three-dimensional weaving, have been developed in recent decades to enhance composites' interlaminar properties. Stitching is considered to be an efficient and cost-effective method to reinforce composites in the through-the-thickness direction. However, the in-plane and out-of-plane properties of stitched composite materials are highly dependent on stitch processing parameters. These processing parameters include linear thread density, stitch density, stitch pattern, stitch distribution, pretension, stitching style, twist, ply orientation, and the stitch material. In particular, the mode I and mode II fracture energies of stitched polymer matrix composites are dependent on key stitching parameters such as stitch density and linear thread density. The fracture energy is found to increase by a factor up to 15 depending on the selection of stitch density and linear thread density. However, current test standards do not appropriately address how to effectively determine composite materials' modal fracture energies. This is primarily due to the large-scale bridging that occurs during tests. Therefore, the use of linear elastic fracture mechanics methods may not be accurate. Additionally, a high rotational constraint is created by the through-thickness reinforcement that leads to failure of the specimen away from the delaminating zone. Therefore, new test methods or modification of existing test standards have been developed to estimate the fracture energy of composites. The failure of the specimen, due to the high rotational constraint, is prevented by bonding doublers to the outermost surfaces prior to testing.

7. CONCLUSIONS

Based on a literature review based on over a hundred papers, much of the research on the out-of-plane and fracture behavior of stitched composites has been focused on polymer composite laminates. Fewer studies have investigated the impact of stitch parameters on high temperature or sandwich composites. Research indicates that the properties of high-temperature composites and sandwich composites can be improved without impacting in-plane properties by incorporating through- thickness reinforcements. Therefore, this research investigated the influence of stitching processing parameters on the mode I fracture of stitched sandwich composites.

An initial study was performed to explore the use of the J-integral approach to better approximate the fracture energy in stitched sandwich composites that can develop large-scale bridging. Single cantilevered beam tests were performed to estimate nonlinear and linear elastic fracture energies based on the J-integral method and modified beam theory, respectively. This study indicates that the J-integral approach is a promising method to estimate the fracture energy of stitched sandwich composites.

The influence of stitch processing parameters, such as linear thread density and stitch density, were investigated using a face-centered central (FCC) composite design approach. The mode I fracture energy was experimentally determined for each treatment combination of stitch processing parameters by performing single cantilevered beam tests. A response surface model was developed to predict the fracture energy of stitched sandwich composites. Unique fracture surface morphologies were observed that are dependent on stitch processing parameters. The results indicate that stitching is an excellent candidate to inhibit crack growth in sandwich composites. Based on the maximum fracture energy, an optimum stitch density of 0.0093 stitches/mm² was determined.

The stitched sandwich composites' fracture behavior subjected to mode I loading was computationally examined using a discrete cohesive zone modeling approach. A trilinear traction-separation law was used to represent the failure process of the through-the-thickness reinforcement during delamination. Two-dimensional finite element analysis revealed that the cohesive stitch stiffness and elastic maximum traction stress are the two primary parameters that influence the load-displacement response measured during SCB testing. The bridging and effective displacement after stitch failure did not significantly impact the overall load-displacement response. The predicted load and crack length responses obtained from a three-dimensional finite element analysis were observed to have good agreement with experimental measurements. During modal fracture testing, the crack front is not uniform across the width of the specimen. This is primarily due to the anticlastic curvature of the facesheets when subjected to bending and is attributed mainly to Poisson's effect.

The internal crack lengths were obtained from embedded optical fibers within double cantilevered beam and single cantilevered beam test articles. The internal strain distributions from the optical fibers were correlated to the propagation of delamination based on their unique "wave-like" strain distributions that proportionally shift with increasing crack lengths. A cross-correlation approach was developed to estimate the crack length using the shift in the differential strain gradient. Excellent agreement of crack growth was obtained from optical fibers and visual measurements. Measuring the fracture toughness across the specimen's width revealed a concave

curvature that is associated with the anticlastic behavior of laminates undergoing bending. A reasonable correlation between visual measurements was also achieved.

A finite element analysis of the composite strut door subjected to a tip load was conducted. Areas of high stress concentration were found to be around the strut attachment points and foam edges. Manufacturability of a subcomponent was demonstrated by successful fabrication of two composite strut doors. Optimal stitching parameters, obtained from the RSM, were used in the manufacture of the two aircraft composite strut doors with two different stitching configurations.

A methodology for determining the cohesive law of through-the-thickness stitching subjected to mode I crack growth was established. A more generalized form of this cohesive law needs to be developed for mode II and mixed-mode conditions. The current standardized approaches to determine the mode II and mixed-mode fracture energies use a linear elastic fracture mechanics approach that assumes small-scale yielding. This is not the case for stitched composites and can be affected by large-scale bridging that occurs due to the through-the-thickness reinforcement. Furthermore, a high rotational constraint may be generated during modal tests, which can affect the localized plastic zone size assumption. The use of doublers are necessary to prevent failure of the delaminated arms due to the high rotational constraint that may be present due to the stitching. Therefore, an experimental investigation is needed to determine the influence of doubler thickness on the modal fracture energy of stitched sandwich composites.

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APPENDIX A: LITERATURE SURVEY – STATE OF THE ART

An extensive literature review on the out-of-plane behavior of stitched composites was conducted and published [18]. In summary, studies reveal that stitching generally improves the out-of-plane properties of polymer matrix composites. In particular, mode I and mode II steady-state fracture energies of stitched polymer matrix composites have shown to be dependent on stitch parameters and may increase by a factor up to 15 and 3, respectively, for Vectran, carbon, and Kevlar stitching materials. Stitch density and linear thread density are the two primary stitch parameters that have shown to improve the out-of-plane properties. Under mode I delamination, stitches behave primarily as a bridging mechanism to resist crack growth. Stitches have also been observed to mainly deform and plow through the adjacent matrix during mode II loading conditions.

Current test standards do not appropriately address how to effectively determine the modal fracture energies of stitched composites. This is primarily due to the high rotational constraint developed by the through-thickness stitching that leads to facesheet failure and possible significant stitch bridging, which may lead to inaccuracies in the calculated fracture energy. Doublers and new test methods have been developed to prevent failure of the delaminated arms during testing. Standard procedures for determining doubler sizing and estimating appropriate initial crack lengths relative to the stitch location need to be developed.

Additional investigation is needed to understand the influence of unique stitch parameters, such as stitch pretension, stitch twist, and stitching style. Lastly, the influence of stitch parameters on high-temperature composites, such as carbon-carbon composites, CMCs, and sandwich composites, are not well studied. Research indicates that the out-of-plane properties of CMCs can be improved by approximately 30% without impacting in-plane properties by using untwisted low-filament count (1K) carbon fiber tows for stitching thread. Overall, proper selection of stitch parameters such as stitch density and linear thread density can further improve the out-of-plane properties of polymer matrix composites.

LIST OF ACRONYMS, ABBREVIATIONS, AND SYMBOLS

ANOVA	Analysis of Variance
DCB	Double Cantilever Beam
DoE	Design of Experiments
FCC	Face-Centered Central
FC-CCD	Face-Centered Central Composite Design
FEA	Finite Element Analysis
FEM	Finite Element Model
MCC	Modified Compliance Calibration
MS	Mean Square
NCF	Noncrimped Fabric
OF	Optical Fiber
PRSEUS	Pultruded Rod, Stitched, Efficient, Unitized Structure
PTFE	Polytetrafluoroethylene
RSM	Response Surface Model
SCB	Single Cantilever Beam
SD1	Strut Door 1
SD2	Strut Door 2
SERR	Strain Energy Release Rate
SS	Sum of Squares
TSD	Tilted Sandwich Debond
TSL	Traction-Separation Law
VARTM	Vacuum Assisted Resin Transfer Molding
C	Compliance
E	Modulus
J	Nonlinear Elastic Strain Energy Rate
P	Load
X_1	Stitch Density
X_2	Linear Thread Density
X_3	Facesheet Thickness
δ	Displacement
ε	Strain
C	Compliance