



Multiscale Fatigue Life Prediction for Composite Panels

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Abstract

Fatigue life prediction capabilities have been incorporated into the HyperSizer Composite Analysis and Structural Sizing Software. The fatigue damage model is introduced at the fiber/matrix constituent scale through HyperSizer's coupling with NASA's MAC/GMC micromechanics software. This enables prediction of the micro scale damage progression throughout stiffened and sandwich panels as a function of cycles leading ultimately to simulated panel failure. The fatigue model implementation uses a cycle jumping technique such that, rather than applying a specified number of additional cycles, a specified local damage increment is specified and the number of additional cycles to reach this damage increment is calculated. In this way, the effect of stress redistribution due to damage-induced stiffness change is captured, but the fatigue simulations remain computationally efficient. The model is compared to experimental fatigue life data for two composite facesheet/foam core sandwich panels, demonstrating very good agreement.

1.0 Introduction

Composite sandwich and stiffened panels are common aerospace structural concepts for efficient modern aircraft and spacecraft design. Polymer matrix composites offer excellent material properties per unit weight and, when arranged intelligently to form a panel, full advantage of their properties can be taken to provide a structure with optimal strength, stiffness, and stability. However, care must be taken in the panel design to consider the appropriate local loads as well as the full range of failure modes that may be encountered. The HyperSizer Composite Analysis and Structural Sizing Software (Ref. 1) offers a convenient means to address this problem. As shown in Figure 1, this software relies on a structural scale finite element model to determine element loads for each load case. Panels are defined as groups of elements, and, to arrive at panel design-to loads, the software statistically processes the element loads for each load case. These loads are then localized to determine laminate and ply level stresses and strains. A multitude of static strength and stability failure criteria are checked for the panel and all its subcomponents (e.g., facesheets, core, stiffener web) to find the lightest panel (in terms of materials and geometry) for which no failure criteria are exceeded. The software then writes new material definitions for all the panels to the structural finite element model, which is re-executed to determine a new static solution based on the updated design. This process is repeated iteratively until a converged design is reached.

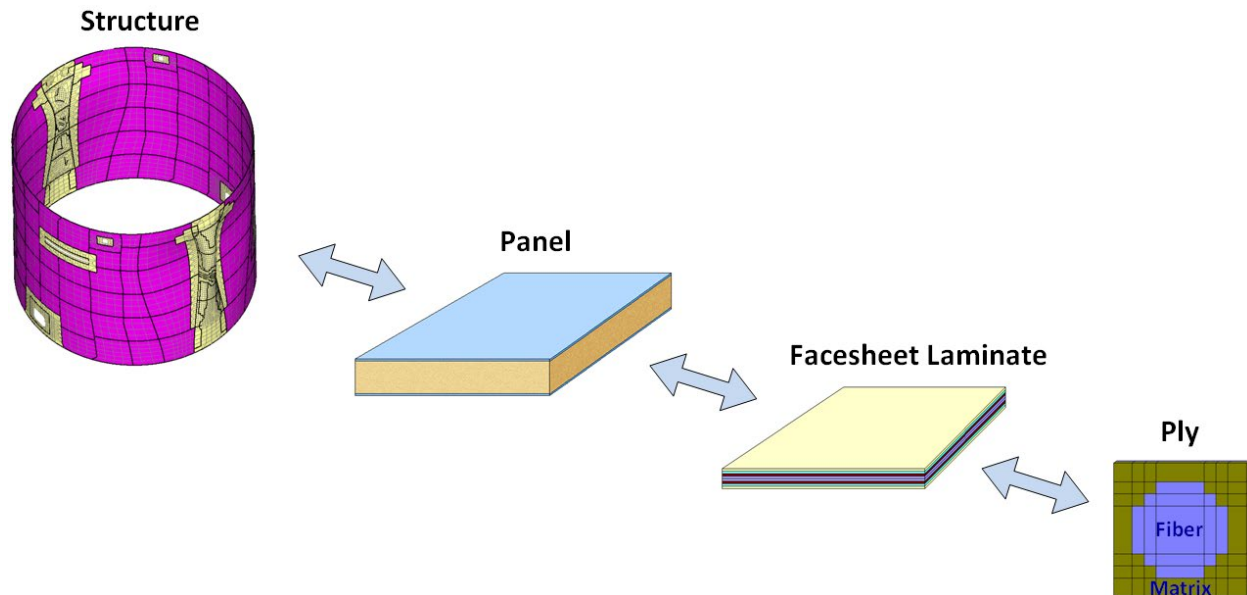


Figure 1.—Schematic of the HyperMAC multiscale design and analysis software that localizes and homogenizes across the scales of the structure, panel, panel subcomponent, and ply, to the composite constituents.

Recently, the standard HyperSizer design and analysis procedure, which localizes to the level of the ply, has been enhanced through coupling with NASA's Micromechanics Analysis Code with Generalized Method of Cells (MAC/GMC) (Ref. 2) to enable localization to fiber and matrix constituent scale (Figure 1). This coupled software, called HyperMAC (Refs. 3 and 4), can now predict the properties of composite materials for use in the standard HyperSizer design process. Further, progressive failure in the fiber and matrix constituents can be simulated, and the effects of this local damage on the higher scale behavior can be predicted. Static failure predictions for composite panels using the HyperMAC software were presented in References 3 and 4. Now, the HyperMAC capability has been further extended to enable cyclic fatigue life predictions for composite stiffened and sandwich panels. The addition of fatigue calculations represents an important additional failure mechanism that was not previously considered by HyperSizer. Furthermore, the presented approach captures the physics of panel fatigue damage and failure at the constituent scale (where damage actually occurs) based on the micro scale stress-strain history throughout the panel. By handling fatigue damage at the micro scale, an isotropic fatigue model can be employed, whereas at the ply level, a more complex anisotropic approach would be required. Further, a ply level model would need re-characterization for every variation in ply fiber volume fraction, whereas the HyperMAC multiscale model can make predictions for any fiber volume fraction.

The remainder of this paper outlines the theoretical underpinnings of the fatigue damage simulation capability, which operates at the fiber/matrix constituent scale, along with its implementation into HyperMAC. The capability is validated via comparison to fatigue data (S-N curves) derived from tests on two types of E-glass/vinylester foam core sandwich beams that have recently appeared in the literature (Ref. 5).

2.0 Composite Static Deformation, Damage, and Failure

The composite laminates considered herein, which form the facesheets of the foam core sandwich beams considered below, are quasi-isotropic E-glass/vinylester manufactured via VARTM. The reinforcement is a DBLT-850-E10 quadriaxial non-crimp E-glass fabric (Ref. 6), while the resin is Reichhold DION 9500 vinylester (Ref. 7). Based on a per ply areal mass of 200 g/m^2 (Ref. 5), an infused ply thickness of 0.1875 mm (Ref. 5), and an E-glass density of 2550 kg/m^3 , the fiber volume fraction of the laminate was estimated to be 42 percent.

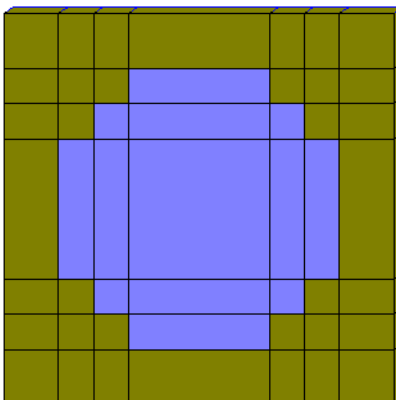


Figure 2.—GMC repeating unit cell used to represent the E-glass/vinylester ply level composite material.

TABLE 1.—COMPOSITE CONSTITUENT ELASTIC PROPERTIES

	E, GPa	ν	Tensile strength, MPa
E-glass (Ref. 9)	80	0.2	2150
Vinylester (Ref. 7)	3.1	0.3	70

The E-glass/vinylester composite laminate was modeled within HyperSizer using the HyperMAC micromechanics capability. This capability enables the ply level materials to be simulated using NASA's Micromechanics Analysis Code with Generalized Method of Cells (MAC/GMC) (Ref. 2), which relies on the Generalized Method of Cells (GMC) (Ref. 8) micromechanics theory. The GMC repeating unit cell used to represent the E-glass/vinylester composite is shown in Figure 2. In GMC, the subvolumes, or subcells, contain constituent materials whose properties are needed as input. The E-glass fiber and vinylester resin were both taken to be isotropic with elastic properties given in Table 1. Loading is applied incrementally, and a given subcell is predicted to fail when its local stress or strain field exceeds a specified failure criterion. The subcell is then assigned very low stiffness components (0.0001 times the original stiffness components), which, when homogenized with the other subcell contributions, causes the composite effective stiffness to decrease. If the composite represents a ply in a laminate, as it does in the present case, the stiffness of the laminate is decreased each time a subcell fails as the incremental loading on the laminate continues. Likewise, if the laminate is part of a stiffened or sandwich panel, as the subcells fail, the panel stiffness will decrease. Final failure can then be predicted using a panel level failure criterion. Herein, this criterion examines the change in the value of the trace of the inverse of the panel ABD matrix, a measure of the panel's compliance. When this value increases by a factor of 10, the panel is considered to be failed (Ref. 3). Note that this is intended to represent acreage panels. Accounting for discontinuities such as ply drops, penetrations, and joints requires additional stress solutions, some of which are provided in HyperSizer (Ref. 1).

A progressive failure analysis of the tensile response of the $[0/45/90/-45]_s$, 42 percent volume fraction E-glass/vinylester solid laminate (described in Ref. 5) was performed using HyperMAC. The simulation modeled the laminate using the classical lamination theory capabilities within HyperSizer, with the results shown in Figure 3. Damage, in the form of subcell failures, initiates at 112 MPa. Because the simulation was performed in strain control (i.e., the axial midplane strain component on laminate was monotonically increased, while all force and moment resultants were kept at zero), each loss of stiffness results in a decrease in stress, followed by continued loading with a reduced slope. Were the complete experimental stress-strain curve provided, one would not expect to observe such obvious stress decreases. Rather, the experimental stiffness decrease would be more gradual than the simulation, which considers the entire specimen as a single point in the plane of the laminate. In addition, because all nonlinearity in the

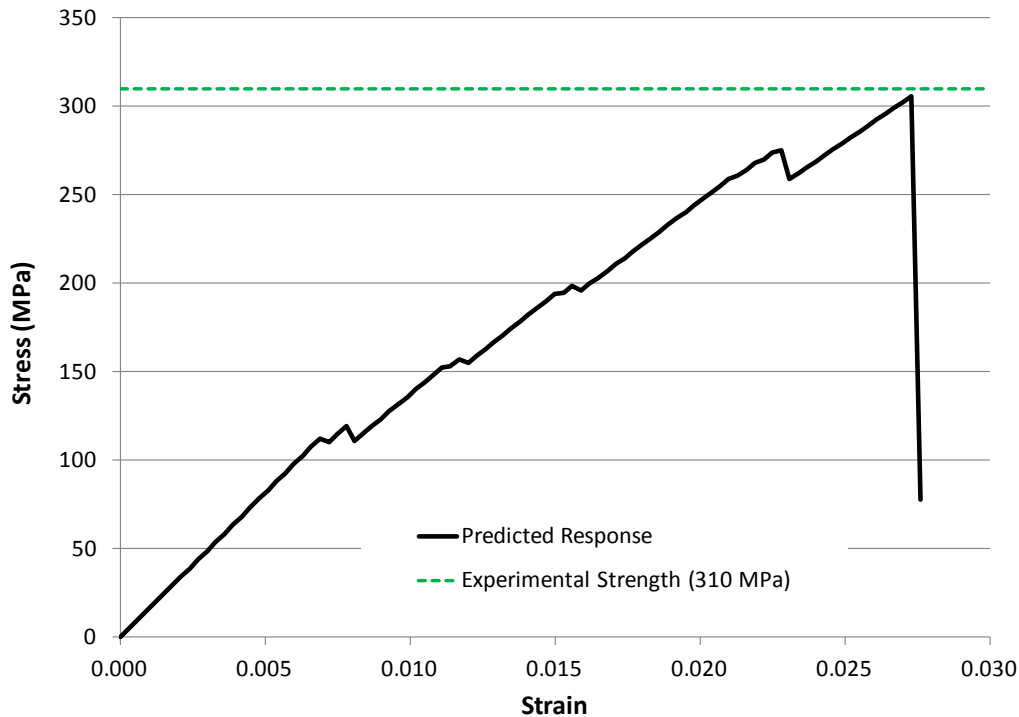


Figure 3.—HyperMAC prediction of the tensile response of a $[0/45/90/-45]_s$ 42 percent volume fraction E-glass/vinylester composite laminate.

simulation is associated with damage in the form of local stiffness decreases, each loading portion of the predicted stress-strain curve follows a line that passes through the origin. Unloading data, were it provided, could provide an assessment of the accuracy of modeling all nonlinearity as stiffness reduction. Several failure events occur within the laminate before a large event that causes a significant decrease in stress and stiffness signals failure of the panel. The predicted laminate tensile strength is 306 MPa, compared to a measured value of 310 MPa (Ref. 5). It should be noted that in this and subsequent simulations, the max stress, max strain, and Tsai-Hill failure criteria were all active for each subcell. If any of these criteria are exceeded, subcell failure is activated. The material failure strains were assumed to be the tensile strength (Table 1) divided by Young's modulus.

3.0 Composite Fatigue

Like the static failure and damage methodology employed within HyperMAC, the cyclic fatigue damage capability considers the fiber/matrix constituent scale by applying damage to the subcells based on the stress and strain history at this scale. Thus, when loading is applied to a panel, the loads are localized to the laminate, ply, and subcell scale to obtain these histories. The fatigue damage model implemented within HyperMAC is a transversely isotropic extension of the NonLinear Cumulative Damage Rule (NLCDR) developed at ONERA (Office Nationale d'Etudes et de Recherches Aérospatiales) for isotropic materials. The development of this model was presented by Arnold and Kruch (Refs. 10 and 11) and its implementation within GMC was described by Wilt et al. (Ref. 12). A damage variable, D , evolves with number of cycles, and, for a given damage level, the stiffness of a subcell is degraded by $(1 - D)$, where $D = 0$ corresponds to a pristine subcell and as D approaches 1, the subcell approaches a failed state. As with static failure, the actual subcell stiffness reduction is capped at a factor of 0.0001 ($D = 0.9999$).

The evolution of damage is governed by,

$$\int_{D_{k-1}}^{D_k} dD = \int_0^N \left[1 - (1-D)^{\beta+1} \right]^\alpha \left[\frac{\hat{F}_m}{1-D} \right]^\beta dN \quad (1)$$

where N is the number of cycles at the current stress state (σ_k), D_k and D_{k-1} are the amount of damage after the current and previous damage increments, respectively, β is a material parameter, and α is a function of the current stress state,

$$\alpha = 1 - \hat{a} \frac{\sigma_{fl}}{\sigma_u} \frac{\langle \Phi_{fl} \rangle}{\langle \Phi_u \rangle} \quad (2)$$

\hat{a} is a material parameter, and $\langle \rangle$ are Macauley brackets. σ_{fl} and σ_u are the uniaxial fatigue limit and ultimate strength material properties, respectively, and Φ_{fl} and Φ_u are the fatigue limit and static fracture surfaces, respectively,

$$\Phi_{fl} = \frac{1}{2} \max_{t_0} \max_t F_{(\sigma_{fl})} [\sigma_{ij}(t) - \sigma_{ij}(t_0)] - 1 \quad (3)$$

$$\Phi_u = 1 - \max_t F_{(\sigma_u)} [\sigma_{ij}(t)] \quad (4)$$

The operator $\frac{\max}{t}$ indicates that the maximum value of the expression to the right up to time t of the current load cycle should be taken. The normalized stress amplitude is defined as,

$$\hat{F}_m = \frac{1}{2} \max_{t_0} \max_t F_{(M)} [\sigma_{ij}(t) - \sigma_{ij}(t_0)] \quad (5)$$

When $\langle \Phi_u \rangle = 0$, static fracture (complete local failure) is indicated, thus α cannot be defined. $\langle \Phi_{fl} \rangle = 0$ indicates that the current stress state is below the fatigue limit and α is set to one. This then represents a special case when integrating Equation (1) that will be considered separately. The t_0 and t terms in Equations (3) to (5) are the time at the beginning of the current load cycle and the current time during the current load cycle, respectively.

The general form for the terms $F_{(\sigma_{fl})}$, $F_{(\sigma_u)}$, and $F_{(M)}$ can be expressed as,

$$F_{(\cdot)} = \sqrt{\frac{1}{(\cdot)_L^2} \left[\left(4\omega_{(\cdot)}^2 - 1 \right) I_1 + \frac{4\omega_{(\cdot)}^2 - 1}{\eta_{(\cdot)}^2} I_2 + \frac{9}{4} I_3 \right]} \quad (6)$$

where,

$$\begin{aligned}
I_1 &= \frac{1}{2} S_{ij} S_{ij} - d_i d_j S_{jk} S_{ki} + \frac{1}{4} (d_i d_j S_{ij})^2 \\
I_2 &= d_i d_j S_{jk} S_{ki} - (d_i d_j S_{ij})^2 \\
I_3 &= (d_i d_j S_{ij})^2
\end{aligned} \tag{7}$$

The current deviatoric stress components are $S_{ij} = \sigma_{ij} - \frac{1}{3} \sigma_{mm} \delta_{ij}$, and d_i are the components of the vector defining the preferred direction in a transversely isotropic material. $\omega_{()}$ and $\eta_{()}$ represent the ratios of longitudinal to transverse normal and shear stresses, respectively, for a transversely isotropic material and are equal to one for an isotropic material. Note that, in calculating $F_{(M)}$ with Equation (6), the value of M , a material constant, is required. The L subscript in Equation (6) indicates the longitudinal direction in a transversely isotropic material.

For the case in which the current local stress state, σ_k , is above the initial fatigue limit, the number of cycles, N , required to damage a subcell from a level of D_{k-1} to D_k is obtained by integrating Equation (1) and is given by,

$$N = \frac{\left\{ \left[1 - (1 - D_k)^{\beta-1} \right]^{1-\alpha} - \left[1 - (1 - D_{k-1})^{\beta-1} \right]^{1-\alpha} \right\}}{\hat{F}_m^\beta (1-\alpha)(\beta+1)} \tag{8}$$

When the local stress level is below the fatigue limit, $\alpha = 1$, integration of Equation (1) yields,

$$N = \frac{\log \left[1 - (1 - D_k)^{\beta-1} \right] - \log \left[1 - (1 - D_{k-1})^{\beta-1} \right]}{\hat{F}_m^\beta (\beta+1)} \tag{9}$$

Equations (8) and (9) can also be solved for D_k in order to determine the current amount of damage developed for a given number of cycles and previous state of damage, D_{k-1} . Further, to determine the remaining cycles to failure for a given previous state of damage, D_k can be set equal to one in Equations (8) and (9).

The implementation of this fatigue model within HyperMAC enables cycle jumping (i.e., not explicitly modeling every fatigue cycle for reasons of computational efficiency) and stress redistribution by using a specified damage increment. HyperMAC applies a single loading cycle to the panel, determines the stress-strain history in each subcell of every ply, and then calculates the number of additional cycles required to damage each subcell by an additional amount equal to the specified damage increment using Equations (8) and (9). The controlling subcell throughout all plies and all laminates within the panel is found as the subcell with the smallest required number of additional cycles. This number of cycles is then applied to the entire panel, and the new damage level in each subcell is calculated. Then, another single loading cycle is applied to the panel, and a new stress-strain history for each subcell is determined. These subcell stress-strain histories will, in general, be different than the histories obtained from the previously applied loading cycle due to the presence of additional damage and the associated stress redistribution. This process is repeated, and the total number of cycles on the panel is summed, until complete failure of the panel is predicted (again, using the trace of the inverse of the ABD matrix criterion described previously). If a static failure of a subcell is detected in response to application of the load cycle, the total number of cycles is incremented by 1, and the load cycle is applied again to capture the redistribution associated with the failure event.

The HyperMAC fatigue implementation is based on constituent material fatigue properties that can be determined from one or more constituent S-N curves. Assuming that the ultimate strength is known as a static failure property, the required fatigue material parameters for an isotropic constituent are: β , \hat{a} , σ_f , and M . However, in the present application to foam sandwich beam tests (Ref. 5), constituent fatigue data were not provided. This reference did, however, provide facesheet quasi-isotropic laminate tensile fatigue data, which were used to back out the vinylester resin fatigue parameters. The tests and simulations both used a value of $R = \sigma_{\min}/\sigma_{\max} = 0.1$. It was assumed that the E-glass fiber is not subject to fatigue damage, but that it could fail statically during the fatigue simulations. A damage increment of 0.2 was used in the simulations.

The characterization of the vinylester fatigue parameters to reproduce the quasi-isotropic laminate tensile fatigue data using HyperMAC is shown in Figure 4. Clearly, the calibrated model does an excellent job of reproducing the laminate fatigue S-N curve. Also plotted is the Basquin's Law representation of the data as presented by Zenkert and Burman (Ref. 5),

$$\sigma_{\max} = B N^{-1/\gamma} \quad (10)$$

where the parameters for the homogenized laminate are: $B = 498 \text{ MPa}$ and $\gamma = 7.88$. The HyperMAC multiscale fatigue model correlates significantly better with the experimental data than does the Basquin's Law characterization, which is linear on the log-log plot. The vinylester fatigue parameters resulting from the correlation shown in Figure 4 are given in Table 2, and the fatigue S-N curve that these parameters represent for the vinylester resin is shown in Figure 5 (again, for $R = 0.1$).

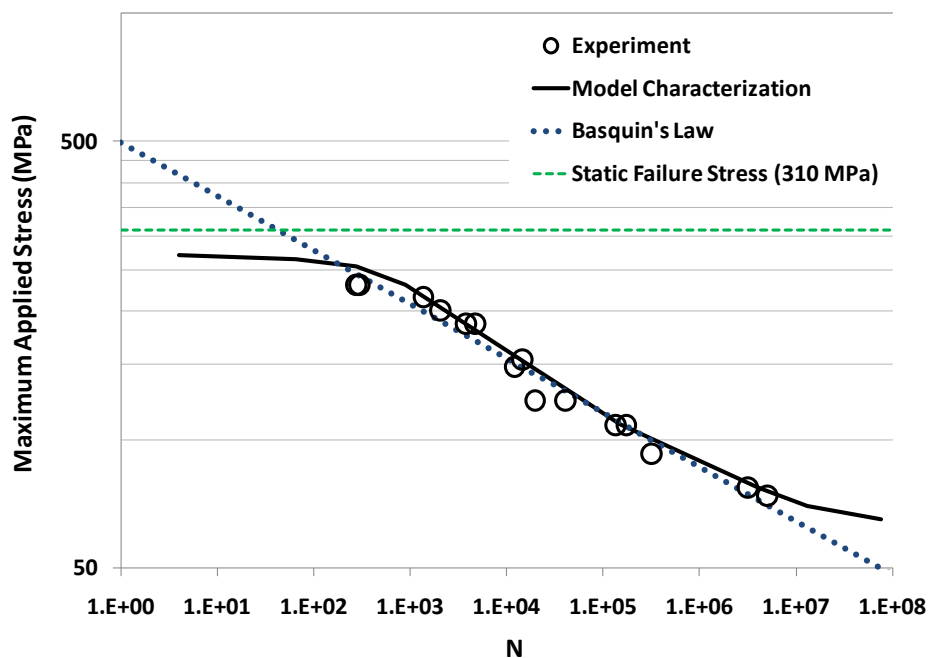


Figure 4.—Correlation of calibrated model with experimental tensile fatigue data (Ref. 5) for a $[0/45/90/-45]_s$ 42 percent volume fraction E-glass/vinylester composite ($R = 0.1$). The Basquin's Law parameters (see Eq. (10)) are: $B = 498 \text{ MPa}$ and $\gamma = 7.88$.

TABLE 2.—CHARACTERIZED NEAT VINYLESTER
RESIN FATIGUE MODEL PARAMETERS

Parameter	Value
ω_u	1.0
ω_f	1.0
ω_m	1.0
η_u	1.0
η_f	1.0
η_m	1.0
β	4.25
\hat{a}	0.3
σ_f	7. MPa
M	135 MPa
σ_u	70. MPa

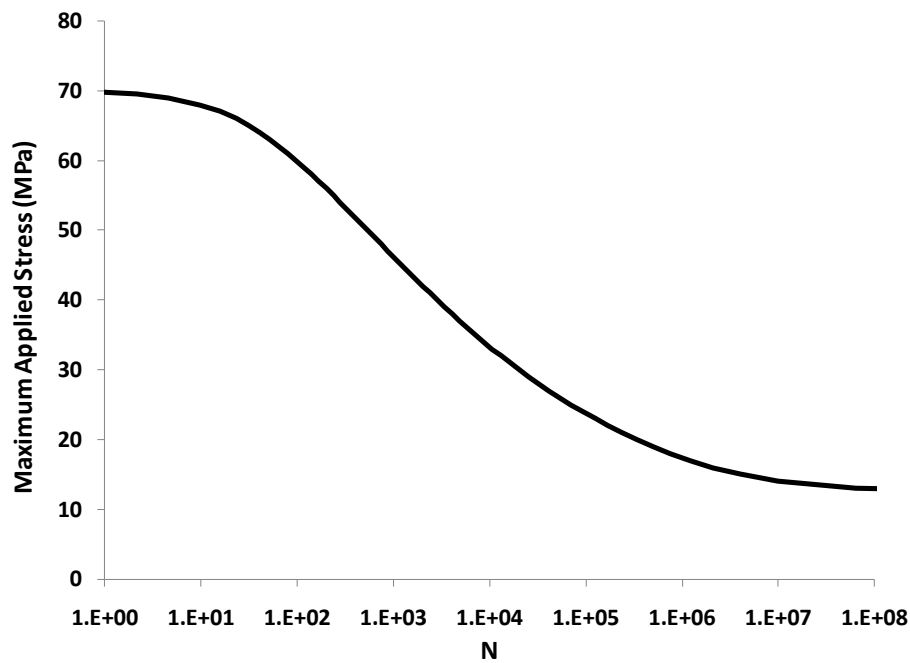


Figure 5.—Characterized neat vinylester resin fatigue S-N curve for $R = 0.1$.

4.0 Foam Core Beam Fatigue

Two types of foam core sandwich beams were tested by Zenkert and Burman (Ref. 5), one with a Divinycell H100 PVC foam core (Ref. 13) and one with a Rohacell WF51 PMI foam core (Ref. 14). The elastic properties of these materials are given in Table 3. Zenkert and Burman (Ref. 5) provided shear fatigue data for these two types of foam, as shown in Figure 6, based on four point bend tests. Also plotted are the Basquin's Law fits to these data, where the parameters are: for Divinycell H100 $B = 2.34$ MPa, $\gamma = 12.08$; for Rohacell WF51 $B = 0.858$ MPa, $\gamma = 19.8$. These Basquin's Law curves are cut off at the shear yield stress for each foam, as given in Table 3.

TABLE 3.—FOAM CORE MATERIAL PROPERTIES (REF. 5)

Property	Divinycell H100	Rohacell WF51
E , MPa	126	75
G , MPa	40	27
τ_{yield} , MPa	1.13	0.66

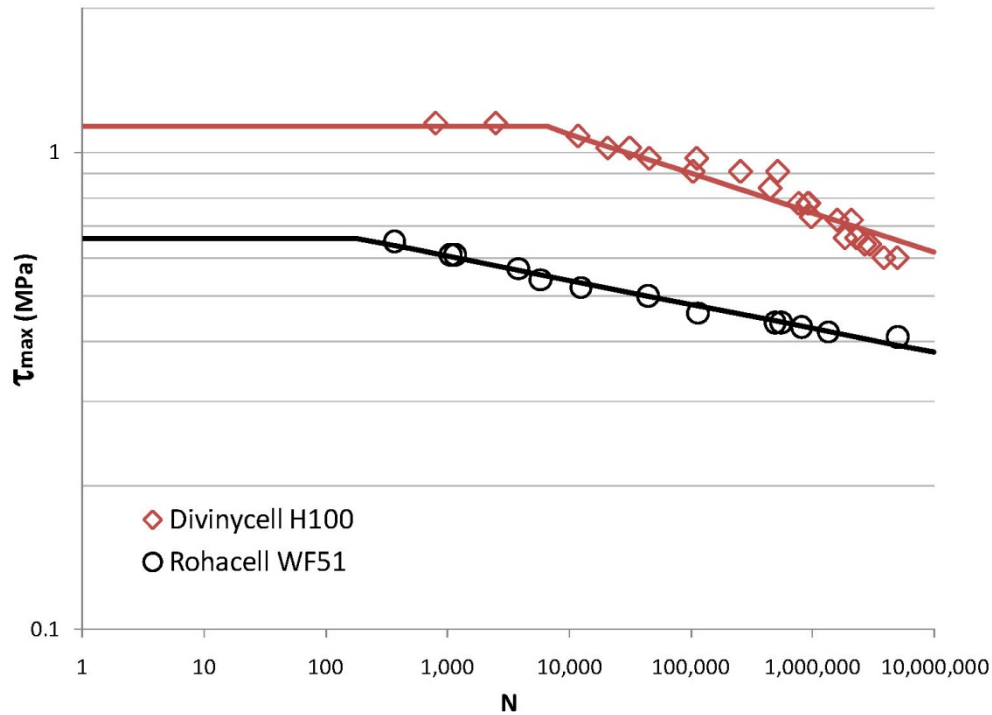


Figure 6.—Shear fatigue test data ($R = 0.1$) for Divinycell H100 and Rohacell WF51 foam materials along with Basquin's Law curves. The Basquin's Law parameters are: for Divinycell H100 $B = 2.34$ MPa, $\gamma = 12.08$; for Rohacell WF51 $B = 0.858$ MPa, $\gamma = 19.8$.

The foam core sandwich beams constructed and tested by Zenkert and Burman (Ref. 5) consisted of a thicker top facesheet and a thinner bottom facesheet, as shown in Figure 7. This was motivated by the desire to have facesheet damage and failure occur only in tension in the bottom facesheet when the beams were tested in four point bend loading as shown in Figure 8 (the top facesheet is in compression). The test beam dimensions are given in Table 4, where, it should be noted that the Divinycell H100 core beam facesheet thicknesses are correct (Ref. 15), despite the fact that the nominal dimensions ($t_1 = 3.0$ mm, $t_2 = 1.5$ mm) were listed in Reference 5. The four point bend tests were conducted using a value of $R = 0.1$.

The two types of foam core beams were modeled in HyperMAC using the software's sandwich panel design/analysis capability. This enables specification of the ply materials, lay ups, core material, and dimensions. The ply materials were represented using the MAC/GMC coupling with the repeating unit cell shown in Figure 2. The E-glass and vinylester constituent material properties given in Table 1 and Table 2 were employed and, similar to the static failure simulation for the facesheets presented above, the max stress, max strain, and Tsai-Hill static failure criteria were active for the constituents. As in the laminate fatigue characterization results presented above, only the vinylester matrix constituent was subject to fatigue damage. Further, since the thicker top facesheet remains in compression in the tests while the fatigue model was characterized for facesheet tension only, damage and failure was not permitted in the thicker top facesheet in the simulations.

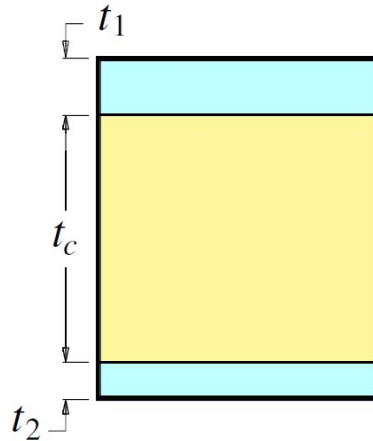


Figure 7.—Foam core sandwich beam cross-section dimensions.

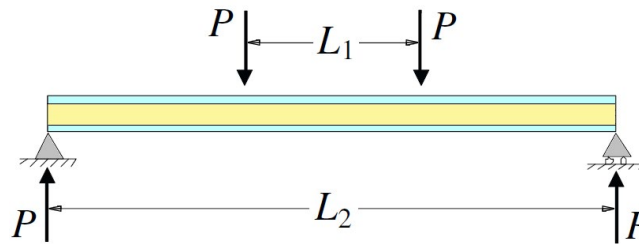


Figure 8.—Foam core sandwich beam four point bend specimen dimensions.

TABLE 4.—FOAM CORE BEAM DIMENSIONS (REFS. 5 AND 15)

	Divinycell H100 Core Beam	Rohacell WF51 Core Beam
Top face	Four layers DBLT-850-E10 quadriaxial non-crimp E-glass fabric (16 plies symmetric)	Four layers DBLT-850-E10 quadriaxial non-crimp E-glass fabric (16 plies symmetric)
Bottom face	Two layers DBLT-850-E10 quadriaxial non-crimp E-glass fabric (eight plies symmetric)	Two layers DBLT-850-E10 quadriaxial non-crimp E-glass fabric (eight plies symmetric)
t_c , mm	50	50
t_1 , mm	2.8	3.0
t_2 , mm	1.4	1.5
L_1 , mm	80	175
L_2 , mm	500	1000

The foam core material was considered to be homogeneous and its fatigue behavior was modeled using the Basquin's Law relation, eq. (10), as plotted in Figure 6. This relation was implemented by lowering the shear allowable of the foam material in HyperMAC as a function of cycles; no core shear stiffness reduction was modeled. The Simplified Shear Solution (Ref. 16) available within HyperSizer was used to calculate the through-thickness shear stress distribution in the core from the panel level through-thickness shear load. This solution results in a more realistic piece-wise parabolic through-thickness shear distribution rather than a constant core through-thickness shear value, as predicted by simpler equations. The core shear stress could then be compared to the allowable (which changes as a function of number of cycles) to predict core failure, which was considered to represent panel failure.

The panel level loading in HyperMAC was applied to simulate the maximum load condition that occurs in the four point bend specimen according to beam theory at the locations directly below the applied load points (see Figure 8). That is, a moment equal to $P(L_2 - L_1)/2$ was applied (equal to the moment between the applied load locations), along with a through-thickness shear load equal to P (equal

to the shear load between the end of the beam and the applied load locations). The simulations thus represent a single location along the four point bend specimen. As in the laminate simulations above, a damage increment of 0.2 was used. Further, as suggested in Reference 5, core failure was predicted using the provided core shear yield stress (see Table 3). The average calculated core shear stress was compared to this yield stress, which was reduced with number of cycles according to Basquin's Law (see Figure 6). The beam was considered to fail either by the standard HyperMAC panel failure criterion based on the inverse of the ABD matrix, or when the core failed (which ever occurred first). Note that the HyperSizer sandwich panel and beam method neglects the contribution of the core material to the panel axial stiffness. However, the core stiffness is used in calculating the through-thickness shear stress distribution (Ref. 16).

Figure 9 compares the HyperMAC predictions with the experimental data provided by Zenkert and Burman (Ref. 5) for the H100 foam core beam loaded in four point bending. Both the experimental and model data show a transition from a core shear failure mechanism to a facesheet tensile failure mechanism. In the experimental data, this transition occurred between 6309 cycles and 8767 cycles. In the model predictions, this transition happened at 7791 cycles. At slightly higher load levels (lower number of cycles) the beam failure mode transitions to static failure of the core ($P_{\max} = 58.9 \text{ N/mm}$). The agreement between the model predictions and experiment is excellent.

Figure 10 compares the HyperMAC model predictions for the H100 foam core beam with Basquin's Law predictions. For the Basquin's Law predictions, the facesheet stress is calculated as,

$$\sigma = \frac{M}{t_2 d} = \frac{P(L_2 - L_1)}{2 t_2 d} \quad (1)$$

where M is the moment between the load application locations, and $d = t_c + t_1/2 + t_2/2$ is the distance between the facesheet centroids. P_{\max} can then be calculated for a given number of cycles by substituting eq. (10) into eq. (1) and solving for P . For the foam core Basquin's Law calculation, the shear load, equal to P , is divided by d to arrive at the average core shear stress. A similar substitution of Basquin's Law then allows the calculation of P_{\max} as a function of number of cycles. Figure 10 shows that, for number of cycles greater than approximately 5000, Basquin's Law predicts a lower failure load for the facesheet compared to core, indicating that facesheet failure should control in the region of the figure. This corresponds to the experimental observation as shown in Figure 9. The Basquin's Law curves for the facesheet reach the core static shear strength before they intersect each other, but they are nearly converged at these points. The Basquin's Law core prediction is very close to the HyperMAC core prediction. For the facesheet, the Basquin's Law prediction is by definition linear (on this log-log plot), and thus there is some deviation from the HyperMAC prediction, which is nonlinear.

A final curve plotted in Figure 10 is the core failure predicted by HyperMAC if, instead of the average core shear stress, the maximum core shear stress is used to predict failure. This may be thought of more as a prediction of damage initiation in the core rather than complete core failure. As expected, at higher loads (lower number of cycles) this would result in a lower predicted P_{\max} value compared to when the average core shear stress is used. Interestingly, when the beam transitions to facesheet failure at a higher number of cycles, the core maximum stress prediction begins to track closely with the facesheet failure prediction. This is because, at a given load level, as the bottom facesheet begins to accumulate damage, it softens. The shear stress in the bottom facesheet decreases, leading to the development of a gradient of shear stress through the thickness of the core where it was previously nearly uniform.

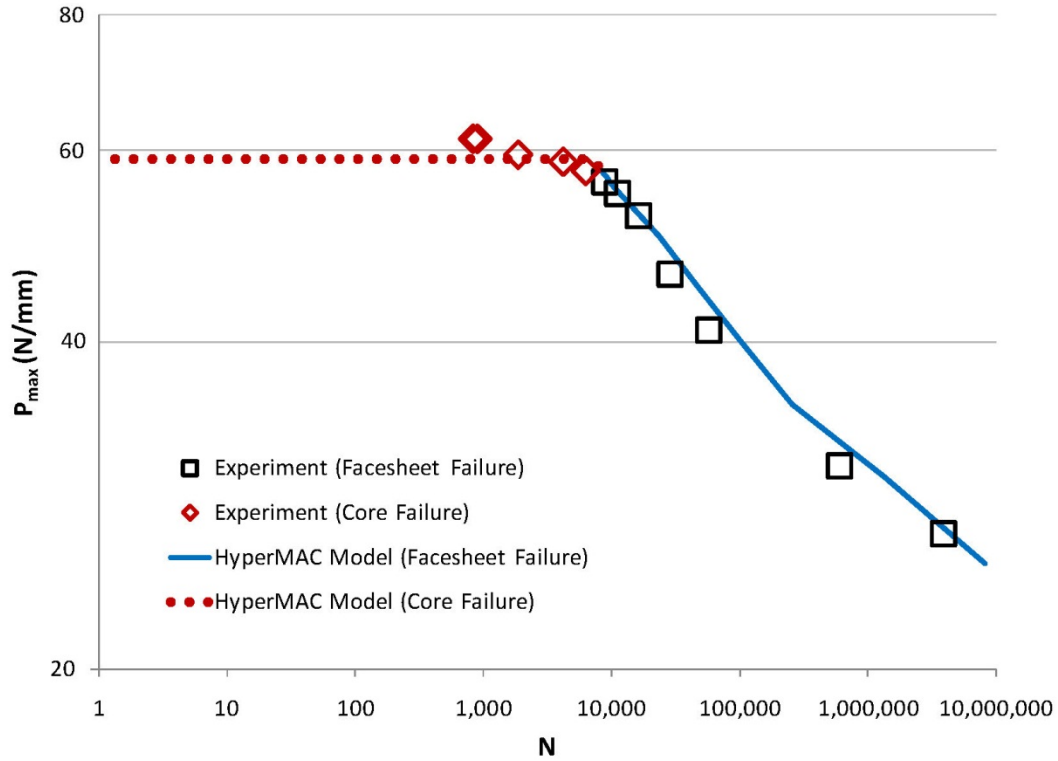


Figure 9.—HyperMAC prediction of the fatigue life of the H100 foam core beam tested in four point bending, along with experimental data (Ref. 5).

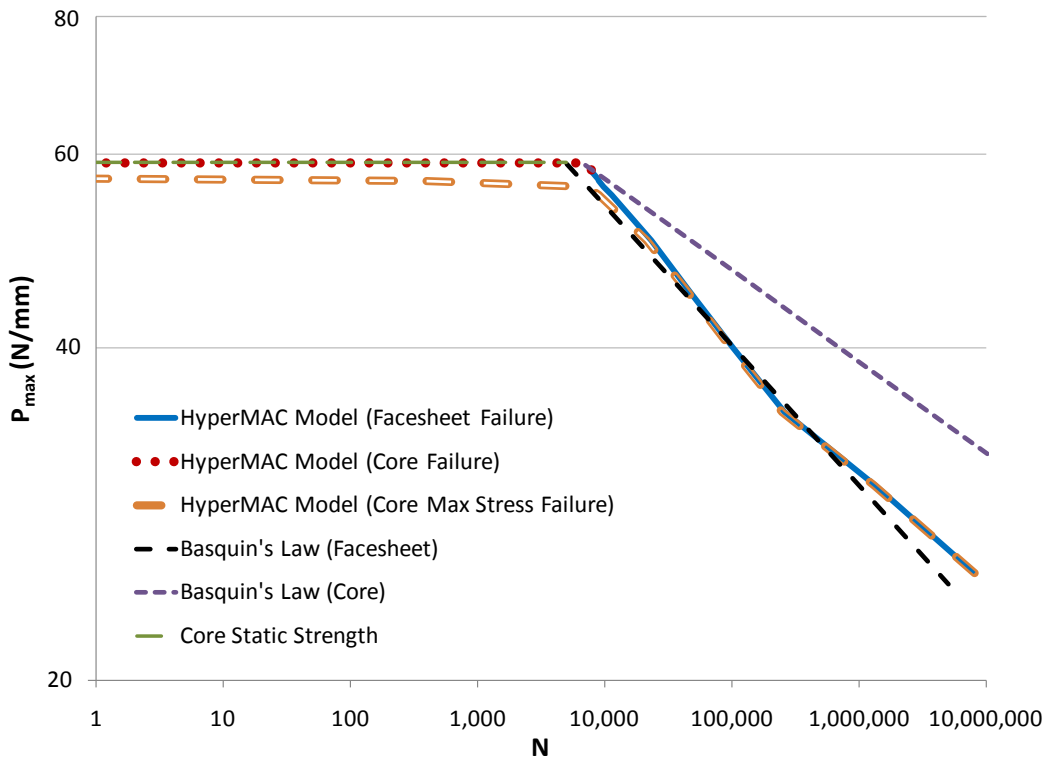


Figure 10.—HyperMAC prediction of the fatigue life of the H100 foam core beam tested in four point bending, compared with Basquin's Law predictions.

The average shear stress in the core, however, is relatively unaffected. This is shown in Figure 11, where an example of the through-thickness shear stress distribution (Ref. 16), as a function of facesheet damage, is plotted (albeit for the WF51 beam). In the pristine beam, the shear stress rises rapidly from zero in the facesheets but exhibits only slight variation from the core average in the core. Both the core average shear stress and the core maximum shear stress are well below the core static shear strength (0.66 MPa in this case of the WF51 core). Note that the shear stress distribution is parabolic in each ply and the core, and it is continuous at the interfaces. After 27,746 cycles, the bottom facesheet has been damaged, and, due to their reduced stiffness, the through-thickness shear stress cannot rise as rapidly in the damaged plies. As a result, the shear stress at the interface between the bottom facesheet and the core is significantly lower than in the pristine beam. Further, because the through-thickness shear stress distribution must still integrate to give the applied through-thickness shear load (which has not changed), the top facesheet and the top portion of the core pick up additional shear stress compared to the pristine case. In fact, at 27,746 cycles, the maximum shear stress in the core (denoted by triangle symbol in Figure 11) exceeds the core shear allowable (which has been reduced to 0.54 MPa after 27,746 cycles based on Basquin's Law). As the loading cycles on the beam continue, the bottom facesheet fails and, with its very low stiffness, it can support very little through-thickness shear stress. This then results in a large through-thickness shear stress gradient in the core and higher through-thickness shear stress in the top facesheet.

In Figure 10, the HyperMAC core failure prediction curve based on maximum shear stress is always (at least slightly) lower than the facesheet failure prediction curve, indicating that using the average core shear stress provides a more realistic failure prediction compared to experiment. However, these results may also indicate that, even when the facesheet failure mode is active, by the time the beam fails, the core may be close to failure and core damage may have even initiated.

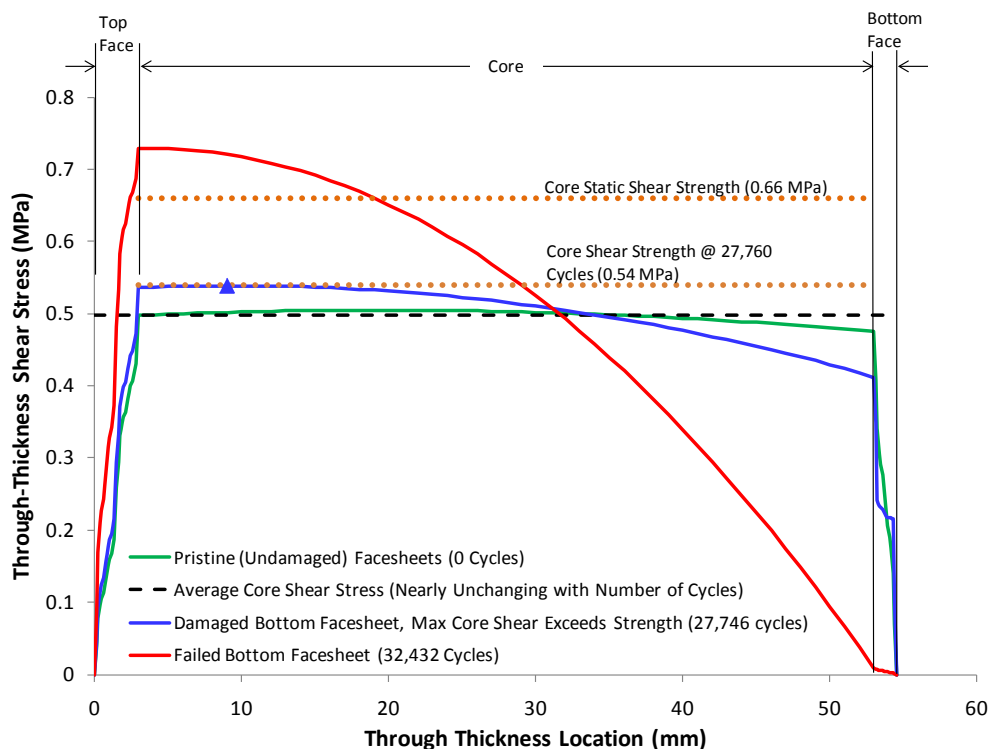


Figure 11.—Example of the variation in the beam through-thickness shear stress distribution as fatigue damage progresses for the WF51 foam core beam.

The HyperMAC predictions for the fatigue life of the WF51 foam core beam are compared to experiment in Figure 12. In contrast to the H100 foam core beam data, the transition from core shear failure to facesheet failure is now well below the core static failure level (34.5 N/mm). The experimental data showed this transition between 16684 and 21073 cycles, whereas the HyperMAC model predicted the transition slightly higher at 26550 cycles. Clearly, the agreement between the model predictions and experiment is excellent.

Figure 13 compares the HyperMAC predictions with Basquin's Law predictions for the WF51 foam core beam. The HyperMAC and Basquin's Law predictions are again in close agreement for core failure. For facesheet failure, the curve predicted using Basquin's Law has a somewhat lower magnitude slope than does that predicted by HyperMAC. This is expected as the Basquin's Law characterization for the facesheet had a lower slope compared to the HyperMAC model characterization (except near the tail) as shown in Figure 4. As in the H100 foam core beam, the HyperMAC model for the core using the maximum core shear stress to predict damage initiation gives a lower predicted curve for core failure. In the region of the fatigue plot controlled by facesheet failure, the core max stress prediction again follows the facesheet failure curve closely and again predicts damage initiation in the core at slightly lower number of cycles than facesheet failure. As before, this indicates that the average core stress is a better indicator of core failure, although some core damage may initiate prior to facesheet failure.

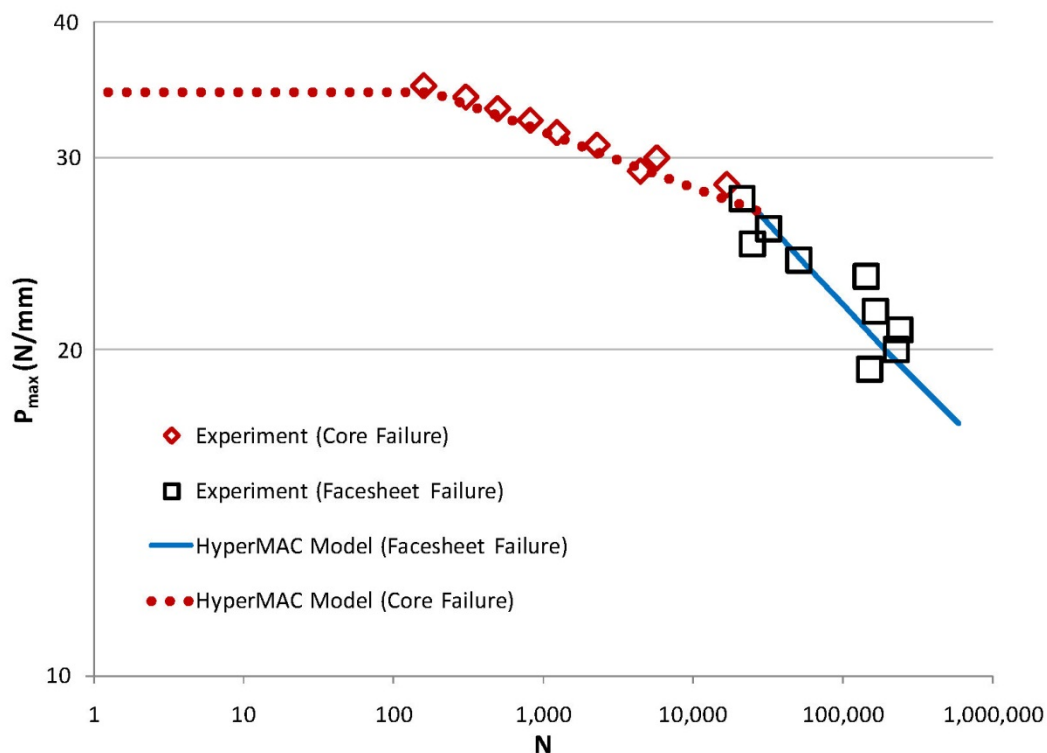


Figure 12.—HyperMAC prediction of the fatigue life of the WF51 foam core beam tested in four point bending, along with experimental data (Ref. 5).

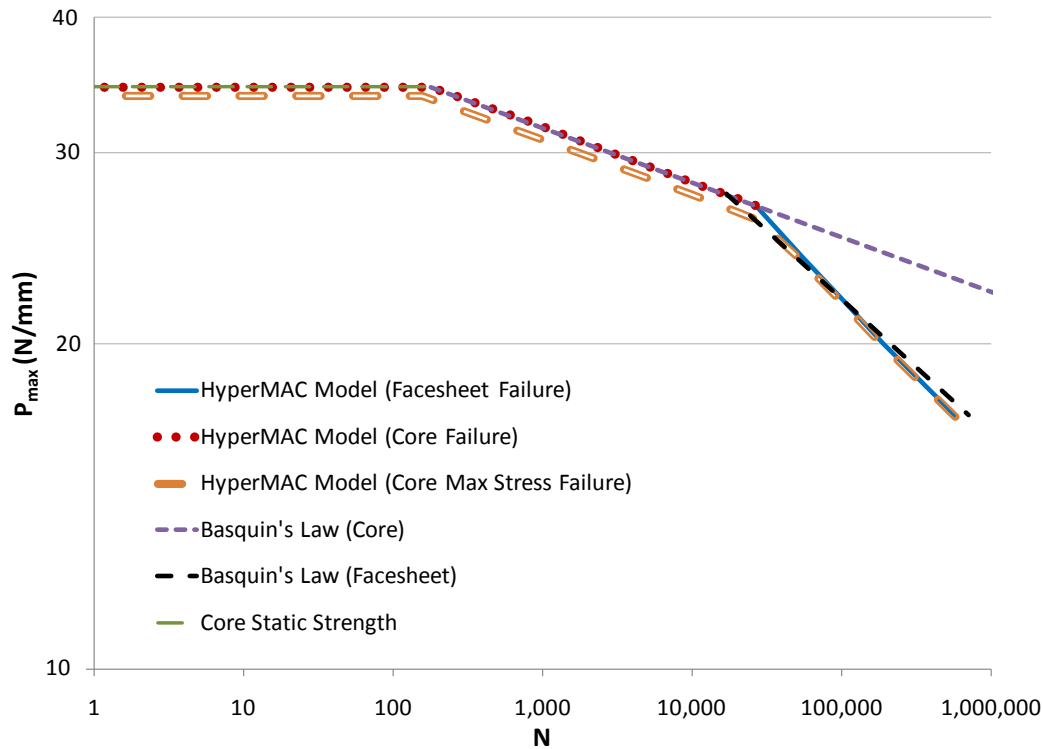


Figure 13.—HyperMAC prediction of the fatigue life of the WF51 foam core beam tested in four point bending, compared with Basquin's Law predictions.

As mentioned previously, the trace of the inverse of the beam's ABD matrix is tracked during the HyperMAC fatigue simulation to serve as a final failure criterion when it changes by an order of magnitude. This also serves as a measure of the beam's overall change in compliance due to damage as the cycles are applied. In Figure 14, this value is plotted for the WF51 foam core beam for a case that failed due to core failure and a case that failed due to facesheet failure. These data are compared to experimental data given by Zenkert and Burman (Ref. 5), wherein the beam's compliance for a given cycle was determined by dividing the applied displacement range (maximum displacement minus minimum displacement) by the applied load range (maximum load minus minimum load). To obtain the compliance change, in both the simulation and experiment, the measure of compliance for a given cycle was divided by the compliance measure of the first cycle. The actual applied load levels for the experimental curves shown in Figure 14 were not given by Zenkert and Burman (Ref. 5). Thus, to select the appropriate applied load level to plot for comparison, the number of cycles at which the compliance change increased rapidly in the experimental data was matched as closely as possible with the number of cycles to failure in a simulation.

Despite the fact that the HyperMAC simulation considers only a single point along the beam (where the load is applied), it matches the measured compliance change history in the beam quite well. The agreement for the beam that failed due to core failure is excellent. For the beam that failed due to facesheet failure, the HyperMAC simulation overpredicts the compliance change somewhat, although it matches the initiation point quite well. Figure 14 clearly demonstrates that the HyperMAC fatigue simulation captures the primary characteristics of the four point bend sandwich beam behavior. Obviously, this type of information is not available from the phenomenological Basquin's Law approach.

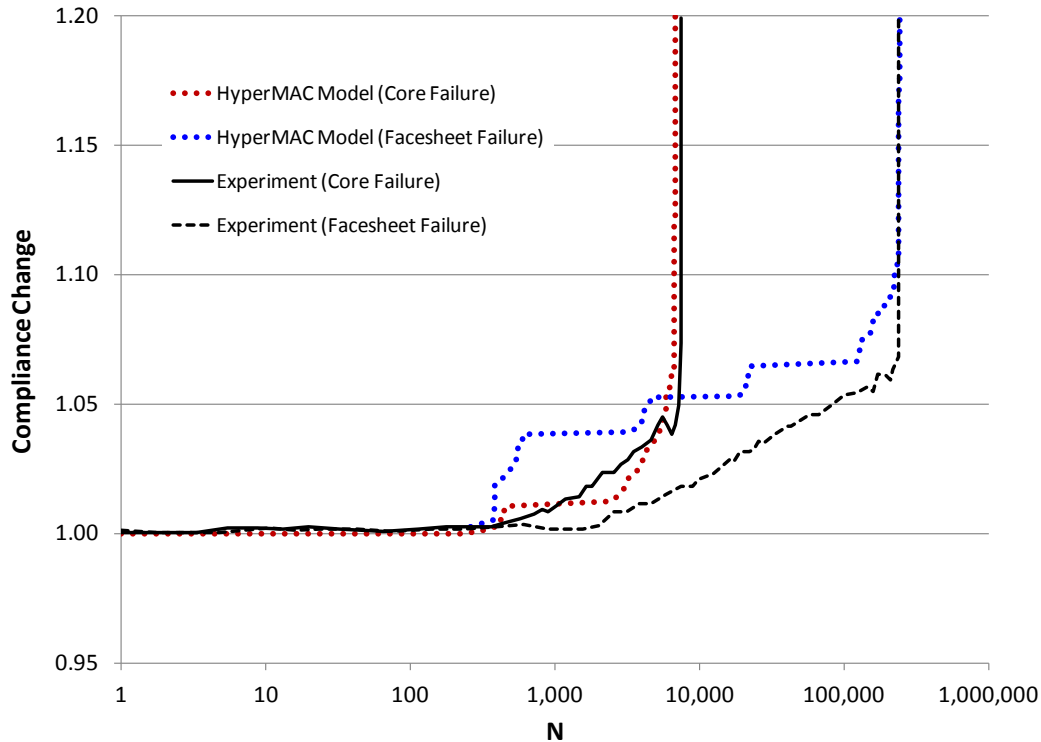


Figure 14.—HyperMAC prediction of the WF51 foam core beam compliance change (trace of the inverse of the ABD matrix) compared to experimental data (Ref. 5).

As a final example, we consider the case in which the fiber volume fraction of the WF51 foam core beam has been varied from 42 percent to plus and minus 8 percent. In VARTM manufacturing, like that used to produce the foam core beams considered herein, some fiber volume fraction variability can be expected as this is controlled by the amount of infiltrated resin. Further, modeling changes in fiber volume fraction cannot be easily accommodated by the Basquin's Law approach on the scale of the laminate. Rather, new laminate level fatigue life test data, like that shown in Figure 4, would need to be generated in order to determine the new Basquin's Law parameters for each laminate with a different fiber volume fraction. Likewise, altering the laminate layup (e.g., adding a ply or altering the layup angles) would also require generation of new test data for the macro Basquin's Law approach, whereas, the present HyperMAC model can easily be altered to accommodate such changes. Note that, it would be possible to apply Basquin's Law at the ply level, but this would require, at a minimum, a generalization of Basquin's Law to handle the stark differences in the ply response in the fiber direction, transverse to the fiber direction, and in shear.

Figure 15 shows the effect of the significant changes in fiber volume fraction of the facesheets on the WF51 core beam fatigue life. As expected, increasing the fiber volume fraction extends the life at a given load level, while decreasing the fiber volume fraction decreases the life. The regions of the fatigue life curves controlled by core failure are unaffected, although the location of the transition of the failure mode is significantly affected by the facesheet fiber volume fraction. It is also interesting to note the relatively large scatter in the experimental data for facesheet failure (compare to Figure 9) is now bounded by the 34 and 50 percent facesheet fiber volume fraction cases. Hence, this or some other microstructural related variation (e.g., void content, fiber angle) could be responsible for the observed scatter. An approach that actually considers the microstructure is needed to examine such effects.

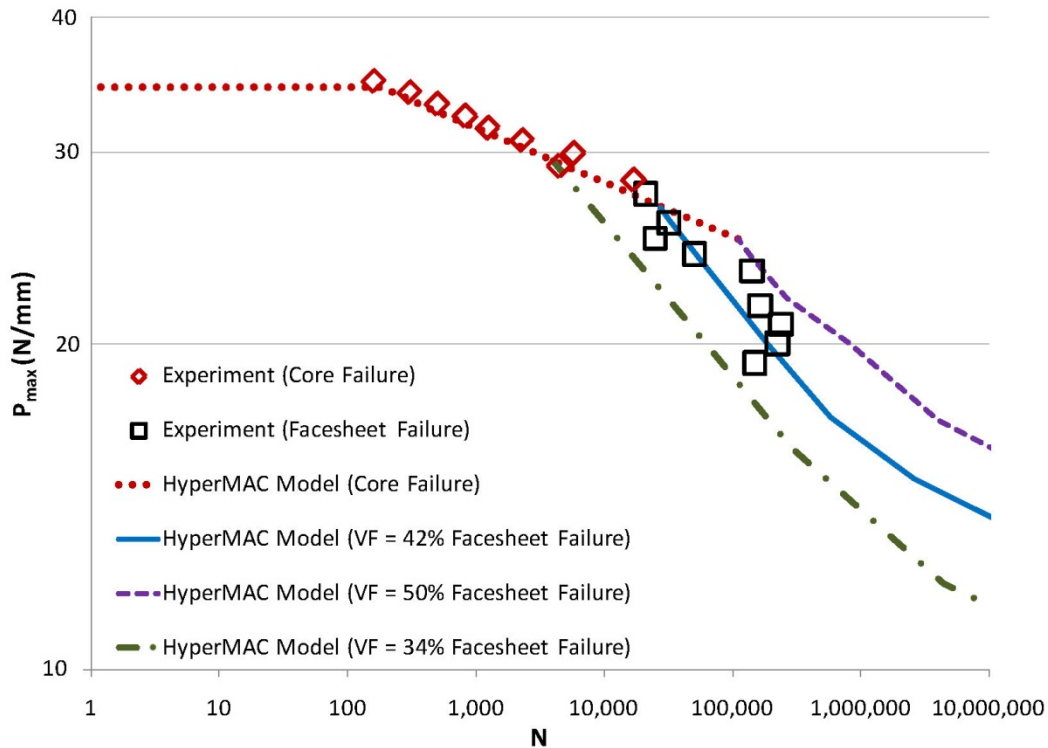


Figure 15.—HyperMAC prediction of the fatigue life of the WF51 foam core beam tested in four point bending where the facesheet fiber volume fraction has been varied.

5.0 Conclusion

An important failure mode, cyclic fatigue failure, has been newly incorporated within the HyperSizer Composite Analysis and Structural Sizing Software. The capability predicts panel fatigue failure based on the accumulation of damage from the fiber/matrix constituent scale. Based on the constituent scale stress and strain history for a given panel level load cycle, local damage is predicted, and the effects of this damage are homogenized to the ply, laminate, and panel levels. Global failure is then predicted based on the resultant loss of load carrying capability on the panel level. The micro scale calculations use NASA's Micromechanics Analysis Code with Generalized Method of Cells (MAC/GMC) software, which has been coupled with HyperSizer in a code called HyperMAC.

HyperMAC multiscale fatigue life predictions were made for E-glass/vinylester composite facesheet – foam core sandwich panels with two types of commercially available foam core materials and compared with experimental data from the literature (Ref. 5). Static and cyclic failure data were provided for the quasi-isotropic laminate facesheets and the core materials. Based on published stiffness and strength data for the E-glass fiber and the vinylester resin, HyperMAC predicted the static tensile strength of the facesheet laminate within 1.3 percent of the experimental value. For the fatigue life predictions, the fatigue life data for the quasi-isotropic laminate were used to back out the four required fatigue model parameters for the isotropic vinylester resin material. The foam core material was treated as homogeneous, with a shear strength that decreased as a function of cycles according to a Basquin's Law fit to the core experimental fatigue data.

The HyperMAC predictions agreed very well with experimental fatigue life data for two types of sandwich beams. For the WF51 core beam, which showed the clearer failure mode shift, compliance change data was also captured well by the HyperMAC model. In comparison to a completely phenomenological approach, wherein the facesheet fatigue response is modeled using a curve fit to the experimental facesheet fatigue data (e.g., using Basquin's Law), the HyperMAC approach captures the

physics of the problem to a much greater extent. Further, once the resin material has been characterized, any microstructural changes can be easily accommodated without needing to re-test the facesheet laminate. The same is true for changes in the type of loading. Compared to a ply level approach, the HyperMAC methodology still has the advantage of progressing to a more fundamental scale, where damage actually occurs, and the associated ability to handle more types of microstructural variations. In addition, by capturing fatigue damage at the constituent scale, HyperMAC can utilize an isotropic fatigue model formulation for the constituents and then naturally predict the effects of damage at the ply scale. As the resin damages due to the cyclic loading, the differences in the behavior of the 0° and 90° plies is predicted by the physical arrangement of the damaged matrix. In contrast, at the ply level, a much more complex fatigue model would be needed, requiring characterization of significantly more parameters.

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