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Finite-element modeling of a composite bridge deck

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Finite-Element Modeling of a Composite Bridge Deck

Suraj Suraj

Thesis submitted to the

College of Engineering and Mineral Resources

at West Virginia University

in partial fulfillment of the requirements

for the degree of

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in

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Keywords: Bridge Deck, Composites, *ANSYS*, Finite-Element Modeling

ABSTRACT

Finite-Element Modeling of A Composite Bridge Deck

Suraj Suraj

Fiber Reinforced Polymer (FRP) materials are being widely used for structural applications, an example being bridge decks. In this study a finite-element model using the software *ANSYS* is developed for an 8"-thick low-profile FRP bridge deck (Prodeck 8) made of E-glass fiber and Polyester resin. The bridge deck is subjected to a patch load at the center and the finite-element results obtained in the form of deflections, strains, and equivalent flexural rigidity are compared with experimental results. A good correlation is found to exist between the finite-element results and the experimental results. A failure analysis, based on maximum stress, maximum strain and Tsai-Wu theories of the Prodeck 8 is carried and first ply failure is determined. Finally, the Prodeck 8 is evaluated for critical load by performing a buckling analysis.

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CHAPTER 1

INTRODUCTION

1.1 PROBLEM STATEMENT

The engineering world is constantly striving to come up with new materials that can improve the efficiency, enhance the performance, and increase the durability of structures. One such material being investigated and researched is the Fiber Reinforced Polymer (FRP) material for structural applications, specifically bridges. According to the Federal Highway Administration publications^{*}, 28.6% of the total inventories of highway bridges are structurally deficient and over \$55 billion funding has been allocated for bridge replacement and rehabilitation programs to improve the condition of the nation's bridges.

Over the past few years a considerable amount of research work has been carried out on FRP materials for bridge deck systems and a couple of all-composite bridge decks are already in use: Laurel Lick Bridge in Lewis County, WV and Wickwire Run Bridge in Taylor County, WV [GangaRao et.al (1999)]

Composites are primarily made of fibers and matrix. While fibers account for most of the stiffness and strength, the matrix binds the fibers together enabling the transfer of loads. Composite materials have superior properties like light weight, high strength to weight ratio, good corrosion and fatigue resistance and ease of manufacturing,

^{*} (www.fhwa.gov/policy/2002cpr/ch11)

compared to steel and concrete, the traditional construction materials. Though steel and concrete are widely used for structural applications, they require routine maintenance and should have to be replaced eventually compared to FRP's, which have a longer life span.

However the application of composite materials to infrastructure applications has been limited due to the lack of industry recognized design criteria and standards and standardized test methods. One key reason is due to the lack of a reliable failure theory; or more precisely, a consistent failure analysis methodology. This happens despite of the fact that advanced composites have been widely used in aerospace and other strength critical structures for nearly half a century [Wang, (2004)]. Also the design of composite structures is quite complicated and needs specialized training.

1.2 OBJECTIVES

- The primary objective of this research is to develop a finite-element model using the software *ANSYS* for a composite bridge deck subjected to a patch load of 10" x 20" in three point bending.
- To determine the equivalent flexural rigidity and Young's Modulus of the deck.
- To carry out a failure analysis and determine the first ply failure.
- To carry out a buckling analysis and determine the critical load.

1.3 SCOPE

Chapter 2 contains a brief review of published literature relating to composite bridge decks.

Chapter 3 gives a detail description of the composite bridge deck being analyzed.

Chapter 4 deals with the finite-element model generation, the loading conditions and the types of solution.

Chapter 5 contains the results generated, comparison with existing experimental results and a discussion of the results.

Chapter 6 lists the conclusions and also contains some recommendations for future work.

CHAPTER 2

LITERATURE REVIEW

In 1983 the US department of transportation initiated a research project named, “Transfer of Composite Technology to Design and Construction of Bridges”, which marked the beginning of research on FRP decks. Since then, a number of research works has been conducted on FRP decks and a number of papers have been published. This chapter presents a brief review of the relevant references pertaining to experimental and numerical modeling of FRP decks. It should be noted that the magnitude of research work conducted on the analytical front is smaller compared to that of the experimental work.

McGhee et al. (1991) have performed a numerical analysis on FRP decks of different shapes as shown in Fig. 2.1, based on the experimental studies carried out by Henry (1985) and Ahmad and Plecnik (1989). The purpose of the research conducted by McGhee et al. is to determine the shape that would be the best fit for practical application. Based on the mathematical formulation and a finite element analysis, the authors conclude that the FRP deck with Type III cross-section is the most efficient shape.

Mongi (1991) has conducted experimental and numerical studies for determining the deflections of an FRP bridge deck system with different sizes, joint types, and loading conditions. On comparing the experimental and numerical results the author concludes that there exists a good correlation between the two and finds that the FRP bridge deck system made of 2 cellular box sections connected by an I-beam to be the best fit.

Fig. 2.1 FRP bridge deck cross-sections considered by Henry (1985) and Ahmad and Plecnik (1989)

Zurieck (1997) has conducted a finite-element analysis on simply supported FRP decks (40' long, 8' wide and 11" deep) with two stringers and subjected to a patch load. Decks with four different shaped cells as shown in Fig. 2.2 have been analyzed for deflections. The author concludes that the box shaped cell and V shaped cell deflected less compared to the trapezoidal and cross-shaped cells

Fig. 2.2 Cross-sections of the FRP decks analyzed by Zurieck (1997)

Brown (1998) has conducted experimental and analytical studies of FRP bridge decks consisting of cellular box decks and wide flange I-beam as stringer. FRP decks of two different shapes have been tested. The first deck tested had dimensions of 5'x 5'x 5" and consisted of box beams that are 5"x 5"x 3/8". The second deck tested has dimensions of 10'x 10'x 8" and consists of bonded box beams that are 4"x 8"x 1/4" and the decks are in turn placed on FRP wide flange I-beams (12"x 12"x 1/2") and tested as a deck and stringer bridge system. Simplified design equations are developed for the analysis of FRP beams under bending and compared with experimental and finite-element results. The author finds a good correlation between experimental and analytical results and concludes that the simplified design approach developed could be effectively used in the systematic analysis and design of FRP deck and stringer bridge systems.

Shekar (2000) has conducted 3-point bending tests on second-generation trapezoidal and hexagonal FRP bridge decks. The outer dimension of the component cross-section is 12"x 8" with a clear span of 108". The equivalent flexural rigidity is found to be 8.28×10^8 (lb x in²) and the ultimate bending stress is found to be 30.8 ksi.

The author reports the failure mode to be the web flange separation. The stiffness and ultimate bending stress are also calculated using approximate classical lamination theory and the results are compared to experimental results.

Chandrashekhara and Nanni (2000) have conducted an extensive experimental study and a finite-element analysis to evaluate the performance of an all-composite bridge deck. They have stiffness and strength properties and failure modes for a deck with the dimensions 30'x 9' consisting of 3" square box cells 0.25". The authors report a good correlation between their experimental results and finite-element results. Based on their study they conclude that all-composite bridge decks made of pultruded glass and carbon FRP tubes to be a suitable replacement for short span bridges made of conventional materials.

Temeles (2001) has conducted an experimental research on two 7"-thick FRP deck panels, 15'x 5' rectangular panes comprising of ten 15'-long, 6"x 6"x 3/8" standard pultruded FRP tubes. The deck is made of E-glass fiber with Polyester matrix. The deck is tested for fatigue and bending loads. For bending the deck is subjected to AASHTO (American Association of State Highway and Transportation Officials) design loads under simulated HS25 axle with impact effects. The maximum deflection of the deck is found to be $L/470$ (where L is the clear span of the deck). For the fatigue analysis the deck is subjected to approximately 4 million load cycles over a period of 8 months and the author reports no loss in stiffness.

Zhou (2002) has conducted an experimental and analytical study on FRP bridge decks and determined their strength and stiffness. The FRP bridge deck is 15' 3" long and 5' wide comprising of 6" x 6" x 3/8" orthotropic box beams. An analysis procedure based

on classical lamination plate theory (CLPT) using the method of elastic equivalence (MEE) is developed for the stiffness analysis of the FRP bridge deck. The author concludes that while this method predicts the bending deflections reasonably well for uniformly distributed load and rectangular patch loads, the bending deflection of the deck under the point load could not be accurately predicted and suggests the use of finite-element analysis or higher order plate theories. Also a deck failure function method is developed for the failure analysis of FRP decks and concludes that the failure function method gives a good damage and failure predictions for central loading compared to Tsai-Wu and Tsai Hill criteria.

Howard (2002) has conducted experimental tests on an FRP bridge deck called Prodeck 8 with a fiber volume fraction of 54%. The bridge deck is evaluated for its adequacy under AASHTO's HS25 loading case with minimum stringer spacing of 5'. Two different multicellular decks (E-glass fiber with polyester and vinyl ester resins) is designed and tested. Testing has been performed to determine both elastic and failure response at the component level and elastic response at the system level. The experimental results are compared with theoretical results obtained using a micro-mechanics approach. Also buckling test is carried out on 12" long FRP decks to determine the ultimate load and failure modes. From the buckling tests the author concludes that the vinyl ester specimen outperformed polyester specimens pertaining to both the ultimate load and the failure values. Bending tests have been performed on polyester and vinyl ester specimens of width 24" and clear span of 120" loaded with a patch loads of 10"x 20" and 15"x 24" at the center. Linear behavior until failure is

reported and punching shear failure is observed for the 10"x 20" patch load and failure due to web flange separation is observed for the 15"x 24" patch load.

Punyamurthula (2004) has conducted experimental tests on 4"-deep FRP bridge decks made of E-glass fiber and vinyl ester resin with a fiber volume fraction of 50%. The decks are evaluated for structural performance to determine their adequacy under AASHTO's HS25 loading. Failure strength and stiffness of the deck are determined experimentally and the experimental results are compared with theoretical results obtained using CLPT. The author concludes that the FRP bridge decks could resist AASHTO's HS25 type of loading with a stringer spacing of 48".

The work described in this chapter gives a brief overview of the kind of research work that has been carried out on FRP bridge decks. It is evident from the published work that the suitability of Prodeck 8 for bridge deck application has been evaluated experimentally. Structural analysis of the Prodeck 8 has not yet been performed using a finite-element model. The present study aims to fill this need for a numerical modeling and analysis of the Prodeck8.

CHAPTER 3

DESCRIPTION OF PRODECK 8

The composite bridge deck being analyzed is named Prodeck 8 and is made of E-glass/Polyester. Figure 3.1 shows the cross section of the deck used for experimental tests. Figure 3.2 shows the cross section of the deck used for the finite-element model in present research. As shown in Fig. 3.1 the deck is 8” in height and 24” in width. The thickness of top and bottom flange is 0.5”, while that of the diagonal stiffener and the two webs are 0.25” and 0.35” respectively. While the top and bottom flange are made of 50 layers of 0^0 fibers, $\pm 45^0$ fibers, continuous strand mat (CSM) and 56-Yield Rovings (3 per inch), the web and stiffener have 36 and 22 layers, respectively.

For the Prodeck 8 being analyzed the material properties obtained from the manufacturer is:

Modulus of elasticity of fiber (E_f) = 10.5×10^6 psi

Modulus of elasticity of matrix (E_m) = 4.9×10^5 psi

Shear modulus of fiber (G_f) = 4.18×10^6 psi

Shear modulus of matrix (G_m) = 2.37×10^5 psi

Fig. 3.1 Cross-Section of Prodeck 8 [Howard, (2002)]

Fig. 3.2 Cross-Section of Prodeck 8 used for the present analysis

Fig. 3.3 Global coordinate system for the Prodeck 8

Figure 3.3 shows the global coordinate system for the Prodeck 8. The traffic direction is along the X -axis in the global coordinate system. The 0^0 fibers run along the X -axis in the material coordinate system, i.e. is perpendicular to the traffic direction. The fiber architecture for the Prodeck 8 is shown in Fig. 3.4.

Fig. 3.4 Fiber architecture of polyester component [Howard, (2002)]

The detailed product specifications for the 0^0 fibers and $\pm 45^0$ fibers and the mat (CSM) are summarized in Tables 3.1 and 3.2.

Table 3.1 TVM-3408/Version 1001 product Specifications

Fiber Type	Nominal Wt. (oz/ yd²)	Thickness (in)	W_f (lb)	L_v (in³)
0^0 Fabrics	16.90	0.0135	0.1174	1.94
+ 45^0 Fabrics	9.05	0.0073	0.0629	1.05
- 45^0 Fabrics	9.19	0.0074	0.0638	1.06
Mat (CSM)	6.75	0.0054	0.0469	0.778
Totals	41.89	0.0336	0.2910	4.83

Table 3.2 NEMP-120/2001 product Specifications

Fiber Type	Nominal Wt. (oz/ yd²)	Thickness (in)	W_f (lb)	L_v (in³)
+ 45^0 Fabrics	6.11	0.005	0.0423	0.72
- 45^0 Fabrics	6.11	0.005	0.0423	0.72
Totals	12.22	0.01	0.0846	1.44

Where,

W_f = Weight of CSM/fabric per square foot (lb)/ft²

L_v = Volume of 1' x 1' composite laminate (in³)

CHAPTER 4

FINITE ELEMENT ANALYSIS

4.1 INTRODUCTION

This chapter contains the finite-element model generation of the Prodeck 8 in the general-purpose finite element software *ANSYS*. In the following sections a detailed description of the element type, computation of the lamina properties, solid modeling, meshing, boundary conditions and failure criteria is presented.

4.2 ELEMENT TYPE

The SOLID46 element from *ANSYS* 7.1 is used for the finite-element analysis of the deck. To begin with, both solid elements (SOLID46) and shell elements (SHELL99) have been considered for the layered composite bridge deck. The SHELL99 is an 8-node, 3-D shell element with six degrees of freedom at each node. It is designed to model thin to moderately thick plates (*ANSYS* Ref. Manual v7.1) and simple shell structures. While the SHELL99 is good for simple shell structures, solid elements are preferred for structures with complex shapes. The Prodeck 8 has a complex shape and SHELL99 does not very well simulate the web-flange connectivity. As a result SOLID46 is chosen over SHELL99 and this solid element is explained in detail in this section.

Fig. 4.1 Element type SOLID46 (ANSYS Ref. Manual v7.1)

The element type SOLID46 has 8 nodes, one at each corner, and three degrees of freedom at each node: translations in the global X , Y , and Z directions (U_X , U_Y and U_Z). The geometry, node locations, and the coordinate system for this element are shown in Fig. 4.1. Eight nodes, layer thickness, layer material axis orientation, and orthotropic material properties define the element. Every element type has a default element coordinate system. The default element x -axis for SOLID46 is the projection of side I-J and side M-N. The material x direction corresponds to the local layer x direction. The default element coordinate system can be changed for area and volume elements by previously defined local coordinate systems before and/or after meshing.

The material properties may be input either in matrix form or layer form; since failure criteria are not available with matrix input, the layer input option is chosen. The

material properties of each layer may be orthotropic or isotropic or anisotropic in the plane of the element. For isotropic materials only Young's Modulus (E) and Poisson's ratio (ν) need to be defined. The layer configuration is defined layer-by-layer from bottom to top. The bottom layer is designated as layer 1, and additional layers are stacked from bottom to top in the positive z (normal) direction of the element coordinate system.

For each orthotropic layer, the following properties are specified

- Material properties ($E_1, E_2, E_3, \nu_{12}, \nu_{23}, \nu_{13}, G_{12}, G_{23}, G_{13}$)

where

E_1, E_2 and E_3 represent the Modulus of elasticity in the x, y , and z directions of the element co-ordinate system.

ν_{12}, ν_{23} and ν_{13} represent the Poisson's ratio in the xy, yz and xz directions, respectively.

G_{12}, G_{23} and G_{13} represent the Shear modulus in the xy, yz and xz directions, respectively.

- Layer orientation angle (THETA): this defines the fiber orientation of the layer, in degrees with respect to the element coordinate system
- Layer thickness (THK) in the positive z direction.

4.3 COMPUTATION OF LAMINA PROPERTIES

Micro-mechanics is used to calculate the lamina properties. These properties are calculated taking into account the interaction of the constituent materials in detail. The equations that are used for the computation of the lamina properties are presented in the following sections. The component cross-section details and fiber architecture have been presented in Chapter 3.

4.3.1 Material Properties

The basic fiber (E-glass) and matrix (polyester) properties, i.e., Modulus of elasticity (E), Shear Modulus (G) and Poisson's ratio (ν) that are needed in order to compute the lamina properties are obtained from the manufacturer. For the Prodeck 8 being analyzed the properties obtained from the manufacturer is:

Modulus of elasticity of fiber (E_f) = 10.5×10^6 psi

Modulus of elasticity of matrix (E_m) = 4.9×10^5 psi

Shear modulus of fiber (G_f) = 4.18×10^6 psi

Shear modulus of matrix (G_m) = 2.37×10^5 psi

From the above properties, Poisson's ratio for both the fibers and matrix can be obtained as

$$\text{Poisson's ratio of fiber: } \nu_f = \frac{E_f}{2G_f} - 1 \quad (4.1)$$

$$\text{Poisson's ratio of matrix : } \nu_m = \frac{E_m}{2G_m} - 1 \quad (4.2)$$

These equations result in $\nu_f = 0.22$ and $\nu_m = 0.38$.

Fiber Volume Fraction

The fiber volume fraction (V_f) is defined as the ratio of volume of fiber to the total volume of the layer. The Prodeck 8 has rovings, mat and fabrics. A roving is a collection of parallel continuous strands; a strand is an untwisted bundle of continuous fibers used as a unit [Barbero, (1998)]. A mat is formed by randomly oriented chopped filaments (chopped strand mat), short fibers, or swirled filaments (continuous strand mat, CSM) loosely held together with a very small amount of adhesive [Barbero, (1998)].

Depending on whether the fiber reinforcement is in the form of rovings, mats or fabrics, the fiber volume fraction is calculated as follows:

For Rovings

$$V_f = \frac{n\pi D^2}{4bt} \quad (4.3)$$

where,

n = Number of bundles

b = Width of lamina (in)

t = Thickness of composite layer (in)

D = Diameter of fiber = $\sqrt{\frac{1}{\rho_f Y 9\pi}}$

ρ_f = Density of fiber.

Y = yield (a number in yards which weighs 1 lb)

The values for n , ρ_f , and Y are typically obtained from the manufacturer.

For CSM (Continuous Strand Mat) and Fabric

$$V_f = \frac{W_f}{\rho_f L_v} \quad (4.4)$$

where,

W_f = Weight of CSM/fabric per square foot (lb)

L_v = Volume of 1' x 1' composite laminate (in³)

ρ_f = Density of CSM or fabric.

Once the fiber volume fractions for the fiber, rovings and mat have been calculated, the Lamina properties can be calculated as shown in the next section.

4.3.2 Lamina Properties

Properties for Fabric and Rovings

Longitudinal Modulus:

The Longitudinal modulus (E_1) can be calculated using the rule of mixtures (ROM) formula. The ROM formula assumes that the strains in the direction of fibers are the same in the matrix and fiber, implying that fiber-matrix bond is perfect [Barbero, (1998)].

$$E_1 = E_f V_f + E_m (1 - V_f) \quad (4.5)$$

Transverse Modulus

The transverse modulus (E_2) that is the modulus in the direction transverse to the fibers can be calculated using inverse ROM formula. The inverse ROM assumes that the transverse stress is same in the fiber and matrix implying that the fiber-matrix bond is perfect [Barbero, (1998)].

$$E_2 = \frac{E_f E_m}{E_f (1 - V_f) + E_m V_f} \quad (4.6)$$

The inverse ROM does not accurately predict the transverse modulus in general and may not be used for design calculations. An accurate prediction would be the Periodic Microstructure Model (PMM) [Barbero, (1998)]. Since the PMM formula is relatively complex, it is not represented here.

Inplane Shear Modulus

The inplane shear modulus (G_{12}) can be calculated using the inverse ROM formula.

$$G_{12} = \frac{G_f G_m}{G_f(1 - V_f) + G_m V_f} \quad (4.7)$$

Once again the inverse ROM does not accurately predict the transverse modulus in general and may not be used for design calculations. An accurate prediction would be the Periodic Microstructure Model (PMM)

Interlaminar Shear Modulus

The interlaminar shear modulus (G_{23}) is also calculated by PMM.

In-plane Poisson's Ratio

The Inplane major Poisson's ratio (ν_{12}) is calculated using the ROM formula, which is an accurate prediction and is sufficient for design calculations.

$$\nu_{12} = \nu_f V_f + \nu_m (1 - V_f) \quad (4.8)$$

Then the minor Poisson's ratio (ν_{21}) can be calculated as

$$\nu_{21} = \frac{\nu_{12} E_2}{E_1} \quad (4.9)$$

For Continuous Strand Mat

Continuous Strand Mat (CSM) consists of randomly placed continuous rovings held together by a binder. The CSM is used to obtain bi-directional properties. The material properties of CSM are determined assuming them as random composites. A layer of composite with randomly oriented fibers can be idealized as a laminate with a large number of thin unidirectional layers, each with a different orientation from 0° to 180° [Barbero, (1998)].

$$\text{Elastic Modulus} \quad E_{csm} = \frac{3}{8}E_1 + \frac{5}{8}E_2 \quad (4.10)$$

$$\text{Shear Modulus} \quad G_{csm} = \frac{1}{8}E_1 + \frac{1}{4}E_2 \quad (4.11)$$

Where E_1 and E_2 are the longitudinal and transverse moduli of a fictitious unidirectional layer having the same volute fraction as the CSM layer [Barbero, (1998)].

$$\text{Poisson's Ratio} \quad \nu_{ran} = \left(\frac{E_{csm}}{2G_{csm}} \right) - 1 \quad (4.12)$$

Note: Continuous Strand Mats are assumed to behave in an isotropic manner; so only one modulus value is needed.

4.3.3 Lamina Material Specifications

The Prodeck 8 being analyzed contains three different types of lamina:

56-Yield Rovings (3 per inch)

TVM-3408/Version 1001

NEMP 120/2001

Both the TVM-3408/Version 1001 and NEMP 120/2001 contains different types of fiber configurations. For example, TVM-3408/Version 1001 contains both fabrics and a mat. Using the micro-mechanics approach, the material properties for each layer are calculated separately using the equations that have been describe above. The material properties so calculated are input for each kind of fiber. The following tables summarize the lamina properties for the Prodeck 8.

Table 4.1 Lamina properties with fiber volume fraction of 0.64

Fiber type	V_f	$E_1 \times 10^6$ (psi)	$E_2 \times 10^6$ (psi)	ν_{12}	ν_{23}	$G_{12} \times 10^6$ (psi)	$G_{23} \times 10^6$ (psi)
Fibers/rovings	0.64	6.936	2.373	0.343	0.549	0.703	0.704
Mat (CSM)	0.64	4.05		0.399		1.449	

It should be noted that the fiber volume fraction (V_f) obtained using the formulas previously described yields a value of 0.64 as shown in Table 4.1, which is high compared to the fiber volume fraction of 0.54 [Howard, (2002)] obtained from the burn-out test conducted on an element of the Prodeck 8. As a result the analysis is also run using fiber volume fraction of 0.54 and the corresponding results have been presented in chapter 5. For the fiber volume fraction of 0.54 the material properties calculated separately using micro-mechanics approach is summarized in Table 4.2.

Table 4.2 Lamina Properties with fiber volume fraction of 0.54

Fiber type	V_f	$E_1 \times 10^6$ (psi)	$E_2 \times 10^6$ (psi)	ν_{12}	ν_{23}	$G_{12} \times 10^6$ (psi)	$G_{23} \times 10^6$ (psi)
Fibers/rovings	0.54	5.795	1.642	0.36	0.58	0.525	0.531
Mat (CSM)	0.54	3.37		0.404		0.12	

4.4 MODELING AND MESHING

After defining the material properties, a finite element model for the Prodeck 8 is generated. The finite element model is generated as a solid model, i.e., the model is

generated using volumes and then map-meshed. The volumes are created using key points, which are defined in the global co-ordinate system. After meshing the volumes, the orientation of the layers are checked manually and reoriented if needed as desired.

Fig. 4.2 Solid model of the Prodeck 8

The solid model of the Prodeck 8 (Fig. 4.2) consists of 34 volumes, which are glued together. The gluing operation redefines the volume so that the volumes share areas resulting in the merging of key-points, nodes, elements and areas along their common boundaries. Also it enables the transfer of load to all the volumes. Local co-ordinates are defined for all the volumes to enable the proper orientation of the elements after meshing.

After the solid modeling, the Prodeck 8 is map-meshed resulting in a total of 8008 elements with 12560 nodes. Figure 4.3 shows the mesh for the Prodeck 8. The elements generated have their own co-ordinate systems and proper care should be taken to make

sure that the resulting orientation is as desired in order to align fiber directions. Those elements, which are not oriented as desired, are manually reoriented. The process for checking and reorienting the co-ordinate system is explained next.

Fig. 4.3 Meshed Prodeck 8

Figure 4.4 depicts the orientation of each layer for a randomly selected element and the material number assigned to it. The element orientations are with respect to the locally defined co-ordinate systems for all the elements.

Finally for each volume the element co-ordinate systems are graphically checked and reoriented if needed in the desired direction. Figure 4.5 shows the graphical representation of the element co-ordinate system for the Prodeck 8.

Fig. 4.4 Element orientations of individual layers

Fig. 4.5 Graphical representation of the element co-ordinate system

4.5 BOUNDARY CONDITIONS

Fig. 4.6 Schematic representation of the applied boundary conditions on the Prodeck 8

The Prodeck 8 is simply supported as shown with a clear span of 120". Coupling restraints are applied at the center of the top flange with a cross-section of 10" x 20" to get equal displacement in the Y direction, Y being the loading direction. This is done with the assumption that the steel plate, which is used for patch loading, is considered to be rigid and doesn't deform on being loaded. Figure 4.6 shows the boundary conditions applied on the Prodeck 8.

4.6 LOADS

A surface load is applied across a cross-section of 10" x 20" at the center of the Prodeck 8. For Experimental testing, a steel plate of 10" x 20" size is used to simulate the tire. For the finite-element model using *ANSYS* this is achieved by restricting those elements in contact with the steel plate to have the same deflection in the loading direction. This is based on the assumption that the steel plate acts as a rigid body and is always in contact with the deck. Figure 4.7 shows the applied load at the center of the Prodeck 8.

Fig. 4.7 Schematic representation of the 10" x 20" patch load applied on the Prodeck 8

4.7 DESIGN FOR FAILURE

A Structural element can be considered as failed, when it no longer performs the intended function, even though the structure might not have collapsed, partially damaged structures can still be considered as failed. Several failure theories exist for predicting the failure of composite materials, but none can be considered to predict the failure accurately at all times particularly at the component level. Therefore great care must be taken for failure design of composite structures and if possible the results should be experimentally verified. There are many contributing factors for the lack of a good failure theory because the internal constitutions of composites are complex, endowed with micron-scale entities materially and geometrically [Wang, (2004)].

4.7.1 Strength Ratio (R)

The failure criteria can be expressed in terms of strength ratio defined as the ratio of ultimate stress to applied stress. Also in terms of strain it can be defined as the ratio of ultimate strain to applied strain.

$$R = \frac{\sigma_{ultimate}}{\sigma_{applied}} \quad \text{Or} \quad R = \frac{\epsilon_{ultimate}}{\epsilon_{applied}} \quad (4.13)$$

4.7.2 Failure Criteria

Failure criteria are used to assess the possibility of failure of a material. Determining the mode of failure or failure strength for composite materials is very different from that of isotropic materials. For isotropic materials two constants are

sufficient to determine the failure strength, because they do not have any preferential orientation, implying they have the same properties in all the directions. For composite materials, however, the stresses in all the directions must be examined before arriving at any conclusion on the possible causes of failure. This allows the consideration of orthotropic materials, which might be much weaker in one direction than another. The determination of strength using failure criteria is based on the assumption that the material is homogeneous and its strength can be experimentally measured with simple tests [Tsai and Hahn, (1980)].

4.7.2.1 Maximum Stress Criterion

According to this criterion, Failure occurs when one of the following equalities is met [Barbero, (1998)].

$$\begin{aligned}
 \sigma_1 &> F_{1t} && \text{if } \sigma_1 > 0 \\
 \text{abs}(\sigma_1) &> F_{1c} && \text{if } \sigma_1 < 0 \\
 \sigma_2 &> F_{2t} && \text{if } \sigma_2 > 0 \\
 \text{abs}(\sigma_2) &> F_{2c} && \text{if } \sigma_2 < 0 \\
 \text{abs}(\sigma_4) &> F_4 \\
 \text{abs}(\sigma_5) &> F_5 \\
 \text{abs}(\sigma_6) &> F_6
 \end{aligned} \tag{4.14}$$

where,

$\sigma_1, \sigma_2, \sigma_3, \sigma_4, \sigma_5, \sigma_6$ are the stresses in the material coordinates.

$F_{1t}, F_{1c}, F_{2t}, F_{2c}, F_4, F_5, F_6$ are the failure stress values.

In terms of strength ratio the maximum stress criterion can be defined as follows [Barbero, (1998)].

$$\begin{aligned}
R_1 &= F_{1t} / \sigma_1 && \text{if } \sigma_1 > 0 \\
R_1 &= - F_{1c} / \sigma_1 && \text{if } \sigma_1 < 0 \\
R_2 &= F_{2t} / \sigma_2 && \text{if } \sigma_2 > 0 \\
R_2 &= - F_{2c} / \sigma_2 && \text{if } \sigma_2 < 0 \\
R_4 &= F_4 / |(\sigma_4)| \\
R_5 &= F_5 / |(\sigma_5)| \\
R_6 &= F_6 / |(\sigma_6)| && (4.15)
\end{aligned}$$

The strength ratio for the layer is the minimum of all the R_i .

4.7.2.2 Maximum strain criterion

If the material is linear elastic up to failure, the strains to failure are directly related to the ultimate strengths as follows [Barbero, (1998)].

$$\begin{aligned}
\varepsilon_{1t} &= F_{1t} / E_1 \\
\varepsilon_{1c} &= F_{1c} / E_1 \\
\varepsilon_{2t} &= F_{2t} / E_2 \\
\varepsilon_{2c} &= F_{2c} / E_2 \\
\gamma_{4u} &= F_5 / G_{13} \\
\gamma_{6u} &= F_4 / G_{23} \\
\gamma_{4u} &= F_6 / G_{12} && (4.16)
\end{aligned}$$

Where ε_{1t} , ε_{1c} , ε_{2t} , ε_{2c} , γ_{4u} , γ_{5u} , γ_6 are the failure strains.

In terms of strength ratio the maximum strain criterion can be defined as follows

$$\begin{aligned}
R_1 &= \varepsilon_{1t} / \varepsilon_1 & \text{if } \varepsilon_1 > 0 \\
R_1 &= - \varepsilon_{1c} / \varepsilon_1 & \text{if } \varepsilon_1 < 0 \\
R_2 &= \varepsilon_{2t} / \varepsilon_2 & \text{if } \varepsilon_2 > 0 \\
R_2 &= - \varepsilon_{2c} / \varepsilon_2 & \text{if } \varepsilon_2 < 0 \\
R_4 &= \gamma_{4u} / |(\varepsilon_4)| \\
R_5 &= \gamma_{5u} / |(\varepsilon_5)| \\
R_6 &= \gamma_6 / |(\varepsilon_6)|
\end{aligned} \tag{4.17}$$

The strength ratio for the layer is the minimum of all the R_i .

4.7.2.3 Tsai-Wu failure criterion

This criterion can be represented using the following quadratic equation [Barbero, (1998)].

$$\begin{aligned}
f_1 \sigma_1^f + f_2 \sigma_2^f + f_{11} (\sigma_1^f)^2 + f_{22} (\sigma_2^f)^2 + 2f_{12} (\sigma_1^f \sigma_2^f) + f_{22} (\sigma_2^f)^2 \\
+ f_{66} (\sigma_6^f)^2 + f_{55} (\sigma_5^f)^2 + f_{44} (\sigma_4^f)^2 - 1 = 0
\end{aligned} \tag{4.18}$$

where,

$\sigma_1^f, \sigma_2^f, \sigma_6^f, \sigma_4^f, \sigma_5^f$ are the components of stress at any point of the failure envelope.

$$f_1 = \frac{1}{F_{1t}} - \frac{1}{F_{1c}}$$

$$f_2 = \frac{1}{F_{2t}} - \frac{1}{F_{2c}}$$

$$f_{11} = \frac{1}{F_{1t} F_{1c}}$$

$$f_{22} = \frac{1}{F_{2t}F_{2c}}$$

$$f_{66} = \frac{1}{(F_6)^2}$$

$$f_{44} = \frac{1}{(F_4)^2}$$

$$f_{55} = \frac{1}{(F_5)^2}$$

4.7.3 Failure Criteria using *ANSYS*

Using *ANSYS* possible failure of a material (first ply failure) can be evaluated by up to six different criteria. Three failure criteria are predefined in *ANSYS*; they are maximum strain, maximum stress and Tsai-Wu failure criteria. Each of these failure criteria is evaluated at the top and bottom (or middle) of each layer at each of the in-plane integration points. Since the criteria are orthotropic, the failure stress or failure strain values for all directions are input, i.e. if we do not want the failure stress or strain to be checked in a particular direction, a large number in that direction is to be specified. The maximum stress, maximum strain, and Tsai-Wu failure criteria are predefined in *ANSYS* and the failure values are calculated in terms of ζ ,

where

$$\zeta = 1/R. \tag{4.19}$$

The properties that are required to define the above mentioned failure criteria are explained next.

4.7.4 Lamina failure properties

Longitudinal Tensile Strength (F_{1t})

The longitudinal Tensile strength is calculated using the formula

$$F_{1t} = \sigma_{fa} \left[V_f + \frac{E_m}{E_f} (1 - V_f) \right] \quad (4.20)$$

σ_{fa} is the fiber average strength

This equation assumes that the strain in the matrix and the fibers are the same [Barbero, (1998)].

Longitudinal Compressive Strength (F_{1c})

The longitudinal compressive strength is calculated using the formula

$$F_{1c} = \left(\frac{\chi}{a} + 1 \right)^b G_{12} \quad (4.21)$$

where,

$\chi = \frac{G_{12}\Omega}{F_6}$, a dimensionless quantity and a and b are two constants chosen to fit Eq.

(4.23): a=0.21 and b = -0.69.

Transverse Tensile Strength (F_{2t})

The transverse tensile strength is a matrix dominated property and is calculated using the formula

$$F_{2t} = \sigma_{mu} \left[1 - \sqrt{\frac{4V_v}{\pi(1-V_f)}} \right] \left[1 - V_f^{1/3} \right] \frac{E_2}{E_m} \quad (4.22)$$

Where

σ_{mu} is the tensile strength of the bulk matrix and V_v is the void volume fraction

Transverse Compressive Strength (F_{2c})

The transverse compressive strength can be calculated using the same formula as Eq. (4.25), replacing σ_{mu} by σ_{muc} where σ_{muc} is the compressive strength of the bulk matrix.

Inplane Shear Strength (F_6)

Inplane shear strength is calculated using the formula

$$F_6 = \tau_{mu} C_v \left[1 + (V_f - \sqrt{V_f})(1 - \frac{G_m}{G_f}) \right] \quad (4.23)$$

Where τ_{mu} is the bulk shear strength of the matrix.

The strain values are calculated using equation 2.16 and the failure strength and failure strain values for the fibers are summarized in Tables 4.3 and 4.4.

Table 4.3 Strength values for Rovings, TVM and NEMP

Fiber type	F_{1t} (ksi)	F_{1c} (ksi)	F_{2t} (ksi)	F_{2c} (ksi)	F_6 (ksi)
Fibers/rovings	130.969	51.778	5.81	9.862	5.801
Mat (CSM)	22.9	22.9	22.9	22.9	11.0

Table 4.3 Ultimate strain values for Rovings, TVM and NEMP

Fiber type	ϵ_{1t}	ϵ_{1c}	ϵ_{2t}	ϵ_{2c}	ϵ_6
Fibers/rovings	2.26×10^{-2}	8.93×10^{-3}	3.53×10^{-3}	6.01×10^{-3}	1.12×10^{-2}
Mat (CSM)	6.79×10^{-3}	6.79×10^{-3}	6.79×10^{-3}	6.79×10^{-3}	9.17×10^{-3}

CHAPTER 5

RESULTS AND DISCUSSIONS

5.1 INTRODUCTION

The results obtained from the finite element model of the Prodeck 8 are presented in this chapter. The first set of results corresponds to a static analysis performed on the Prodeck 8 under a patch load; two different fiber volume fractions are considered. The values of maximum deflection and strains at selected locations and directions are obtained and compared with earlier experimental results. The load versus maximum deflection and load versus strain plots and contour plots of stress and strain are provided. Also, tables and plots showing a comparison between the finite element and experimental results are presented.

The results of a failure analysis, using maximum stress criterion, maximum strain criterion and Tsai-Wu criterion of the Prodeck 8 are presented. A description of the buckling analysis performed on the Prodeck 8 and an isolated web section of the deck is provided.

5.2 DEFLECTION ANALYSIS

Fig. 5.1 Deformed shape of the Prodeck 8 under central patch load of value 24 kips

The finite-element model generated in *ANSYS* (see Fig. 4.5) is simulated as simply supported (along the entire width of the deck) at two symmetrically located positions with a span of 120". This is subjected to a central patch load over an area of 10"x 20". The deflected shape at a resultant load value of 24 Kips is shown in Fig. 5.1 and the maximum deflection is seen at the center of the span, as expected.

Table 5.1 Maximum deflection of Prodeck 8 for a central patch load

Resultant load (kips)	Maximum deflection (in.)	Maximum deflection (in.)
	present	experiment (Howard)
0	0.0000	0.0000
2	0.0555	0.0720
4	0.1109	0.1360
6	0.1664	0.2120
8	0.2219	0.2950
10	0.2774	0.3570
12	0.3328	0.4210
14	0.3883	0.4970
16	0.4438	0.5630
18	0.4992	0.6360
20	0.5547	0.7140
22	0.6102	0.7750
24	0.6656	0.8530
26	0.7211	0.9240
28	0.7766	0.9970
30	0.8321	1.0810
32	0.8875	1.1600
34	0.9430	1.2640
36	0.9985	1.3670

Fig. 5.2 Variation of maximum deflection with load for a central patch load

The values of the maximum deflection of the deck at different load levels are shown in Table 5.1 and Fig. 5.2. The corresponding experimental results generated by Howard (2002) are also presented in this table and the figure for the sake of comparison. As seen from Table 5.1 and Fig. 5.2 the present finite element results are consistently lower than the experimental ones indicating that the finite element model is stiffer. The finite element plot from the present analysis is linear as expected whereas the experimental curve is basically linear upto 32 kips and then begins to

exhibit nonlinearity. This nonlinearity is probably attributable to accumulation of damage as the load increases. One possible reason for the difference in the deflection values between the numerical and experimental results is the fact that the fiber volume fraction (V_f) used in the present analysis of 0.64 (see Section 4.3) is based on theoretical calculations, which is higher than the fiber volume fraction of 0.54 obtained from a burn-out test [Howard, 2002]. To test this hypothesis the analysis is run with a fiber volume fraction of 0.54 and the contour plot of the transverse deflection at a resultant load of 24 Kips is shown in Fig. 5.3.

Fig. 5.3 Displacement plot in the Y direction for the Prodeck 8 with fiber volume fraction of 54% and 24 Kips patch load

Table 5.2 Maximum deflection of Prodeck 8 for two different fiber volume fractions under a central patch load

load (kips)	Maximum deflection (in.)	Maximum deflection (in.)	Maximum deflection (in.)
	$\nu_f = 0.54$	$\nu_f = 0.64$	experiment (Howard)
0	0.0000	0.0000	0.0000
2	0.0691	0.0555	0.0720
4	0.1382	0.1109	0.1360
6	0.2073	0.1664	0.2120
8	0.2765	0.2219	0.2950
10	0.3456	0.2774	0.3570
12	0.4147	0.3328	0.4210
14	0.4838	0.3883	0.4970
16	0.5529	0.4438	0.5630
18	0.6221	0.4992	0.6360
20	0.6912	0.5547	0.7140
22	0.7603	0.6102	0.7750
24	0.8295	0.6656	0.8530
26	0.8986	0.7211	0.9240
28	0.9677	0.7766	0.9970
30	1.0370	0.8321	1.0810
32	1.1059	0.8875	1.1600
34	1.1750	0.9430	1.2640
36	1.2442	0.9985	1.3670

Fig. 5.4 Variation of load with maximum deflection for two different fiber volume fractions under a central patch load

The mid-span deflection for different load levels for $v_f = 0.54$ and $v_f = 0.64$ are presented in Table 5.2. The corresponding experimental results from Howard (2002) are also included in this table. The data from Table 5.2 is plotted in Fig. 5.4. It is seen from Fig. 5.4 that the plot corresponding to a fiber volume fraction of 0.54 closely matches the experimental one. For the load values of 0 kips to 32 kips (linear portion of the experimental curve), the maximum difference in the maximum deflection values between

the finite element result with a fiber volume fraction of 0.64 and experimental result is about 23%, whereas when the analysis is run with a fiber volume fraction of 0.54, the maximum difference between the two is only about 6%.

The equivalent flexural rigidity and Young's Modulus are calculated based on the fundamental principles of mechanics of materials for bending and Hooke's law. The formulas used for the calculation are presented below.

$$\delta = \frac{PL^3}{48EI} \quad (5.1)$$

$$\sigma = E\varepsilon = \frac{Mc}{I} \quad (5.2)$$

Solving (5.1) and (5.2) for EI and substituting $M = \frac{PL}{4}$ we get,

$$EI = \left(\frac{P}{\delta} \right) \left(\frac{L^3}{48} \right) \quad (5.3)$$

or

$$EI = \left(\frac{P}{\varepsilon} \right) \left(\frac{Lc}{4} \right) \quad (5.4)$$

where,

$\frac{P}{\delta}$ = Slope of the load versus maximum deflection curve

$\frac{P}{\varepsilon}$ = Slope of the load versus strain curve

L = Clear span = 120"

E = Young's modulus or modulus of elasticity

I = Moment of inertia of deck cross section = 300 in.⁴

c = Distance from the neutral axis to the outermost fiber = 4"

Using Eq. (5.3) and slopes from Fig. 5.4 the equivalent flexural rigidity and Young's modulus are calculated and shown in Table 5.3. The corresponding experimental results generated by Howard (2002) are also presented in this table. As seen from Table 5.3, for the present analysis using maximum deflection, Young's Modulus is calculated as 4.30×10^6 psi, whereas the experimental Young's Modulus reported from Howard (2002) is 3.30×10^6 psi. And when the analysis is performed using a fiber volume fraction of 0.54, the Young's Modulus is calculated to be 3.47×10^6 psi. This shows that the fiber volume fraction used for calculating the material properties makes a considerable difference to the equivalent flexural rigidity and Young's modulus values and hence great care should be taken in computing the fiber volume fraction. It should be noted that all the results presented hereafter for the Prodeck 8 are based on a fiber volume fraction of 0.54

Table 5.3 Equivalent flexural rigidity and Young's modulus in the longitudinal direction
based on maximum deflection

Type	Equivalent Flexural Rigidity $E_z I_X (10^9 \text{ lb} \cdot \text{in}^2)$	Equivalent Young's Modulus $E_z (10^6 \text{ psi})$
$v_f = 0.64$ (present)	1.29	4.30
$v_f = 0.54$ (present)	1.042	3.47
Experiment (Howard)	0.99	3.30

5.3 TORSIONAL ANALYSIS OF PRODECK 8

The transverse deflection analysis included in the previous section has not accounted for shear effects, which may be important for composite structures. To quantify the shear effect, a torsional analysis of the Prodeck 8 is carried out. First the torsional constant of the cross section of the deck (Fig. 3.2) is computed. The torsional constants of the closed (J_c) and open (J_o) segments of the section are given, respectively, by

$$J_c = \frac{4\bar{A}^2}{\oint_t \frac{ds}{t}} \quad (5.5)$$

$$J_o = \sum \frac{1}{3}bt^3 \quad (5.6)$$

$$\text{and } J = J_c + J_o \quad (5.7)$$

where $\bar{A}$ = enclosed area of the closed segment.

The calculated values for the Prodeck 8 cross section are $J_c = 341.36 \text{ in.}^4$, $J_o = 0.658 \text{ in.}^4$, and $J = 342.02 \text{ in.}^4$. Now the Prodeck 8 is simulated for the torsional analysis as follows.

Fig. 5.5 Prodeck 8 subjected to torsion

Fig. 5.6 Contour plot of showing angle of twist of the Prodeck 8 from the torsional analysis

One end of the deck of length 128.75 in. is fixed and on the other end equal and opposite distributed shear loads are applied to the top and bottom flanges as shown in Fig. 5.5. Figure 5.6 shows the angle of twist of the Prodeck 8 under the torsional load. As shown in Appendix A, the twist angle of the free end is computed as 4.5125×10^{-5} rad. Then the equivalent shear modulus G is calculated from the equation,

$$G = \frac{TL}{J\theta} \quad (5.8)$$

where

L = Length of the fixed-free deck

T = Torque applied to the free end

J = Torsional constant of the deck cross section

θ = Twist angle of the free end.

The calculated value of the equivalent shear modulus is 0.9465×10^6 psi; this value is in close agreement with the G_{YZ} value of 0.9×10^6 psi calculated by Howard (2002). Then the deflection, δ_s due to the transverse shear strain can be obtained from

$$\delta_s = \frac{PL}{4GA} \quad (5.9)$$

where

P = Magnitude of the applied central load on a simply-supported beam

L = Span of the simply-supported beam = 120"

G = Equivalent shear modulus of the cross section

A = Area of cross section of the deck = 27.04 in.²

The calculated central shear deflection for a resultant load of 24 kips is 0.02813 in. and when this is added to the corresponding bending deflection obtained using the

ANSYS model of 0.2895 in., the total deflection is 0.8576 in. This deflection is used in Eq. (5.3) to obtain the equivalent flexural rigidity of the deck as $1.007 \times 10^9 \text{ lb} \cdot \text{in.}^2$. Note this value is very close to the value of $0.99 \times 10^9 \text{ lb} \cdot \text{in.}^2$ obtained experimentally by Howard (2002). The shear-corrected equivalent Young's modulus in the longitudinal direction is computed to be $3.358 \times 10^6 \text{ psi}$, which is nearly identical to Howard's (2002) value of $3.30 \times 10^6 \text{ psi}$.

A cantilever model of the Prodeck 8 is constructed and subjected to a pure bending moment at the top (see Appendix B). Based on this model the equivalent flexural rigidity is calculated to be $1.277 \times 10^9 \text{ lb} \cdot \text{in.}^2$ and this is comparable to the theoretical value of $1.36 \times 10^9 \text{ lb} \cdot \text{in.}^2$ found by Howard (2002).

5.4 STRAIN AND STRESS ANALYSIS

Strain Gages #1, #4, #6, #7, #9, and #12 measure strain in the 120" direction (Z)

Strain Gages #2, #3, #5, #8, and #11 measure strain in the 24" direction (X)

All Units are in inches, not to scale.

Fig. 5.7 Position of strain gages [Howard, (2002)]

Figure 5.7 shows the positioning of strain gages from the experimental set up by Howard (2002). The strains have been measured in the top and bottom flanges in the X and Z directions and in the webs. Table 5.4 summarizes the strain values from the present *ANSYS* model and the experimentally measured quantities by Howard (2002) in the bottom flange in the Z direction (cell direction) at the Gage # 4 location.

Table 5.4 Strain values at the Strain Gage #4 location for a central patch load

load (kips)	ϵ_z	ϵ_z
	(micro-strains)	(micro-strains)
	Bottom	Bottom
	Present	Experiment (Howard)
0	0	0
2	181	201
4	363	406
6	544	628
8	726	845
10	907	1059
12	1088	1261
14	1270	1482
16	1451	1688
18	1633	1901
20	1814	2119
22	1996	2320
24	2177	2535
26	2358	2740
28	2540	2939
30	2721	3160
32	2903	3365
34	3084	3605
36	3265	3831

Fig. 5.8 Variation of ϵ_z at Gage #4 location for a central patch load

Figure 5.8 shows the load versus strain plot of the data in Table 5.4. As seen from the plot the present finite element results are linear, as expected, and the experimental strains can also be considered to exhibit mostly linear behavior. Tables 5.5 – 5.7 and Figs. 5.9 – 5.17 show the strain values at the other gage locations Fig. (5.7) on the Prodeck 8.

Table 5.5 Strain values at the locations of Strain Gages #1, #2, #5 and #6 for a
central patch load

Note: Expt. denotes experiment.

Load (Kips)	$\mu\epsilon_z$ (#1)		$\mu\epsilon_x$ (#2)		$\mu\epsilon_x$ (#5)		$\mu\epsilon_z$ (#6)	
	Present	Expt. (Howard)	Present	Expt. (Howard)	Present	Expt. (Howard)	Present	Expt. (Howard)
0	0	0	0	0	0	0	0	0
2	111	126	-53	-42	-53	-57	111	138
4	221	252	-106	-91	-105	-111	221	274
6	332	384	-159	-140	-158	-173	332	421
8	443	513	-212	-186	-211	-237	443	567
10	554	642	-264	-231	-264	-300	554	720
12	664	763	-317	-279	-316	-361	664	848
14	775	893	-370	-329	-369	-432	775	990
16	886	1017	-423	-379	-422	-496	886	1126
18	997	1141	-476	-430	-475	-568	997	1270
20	1107	1268	-529	-484	-527	-642	1107	1415
22	1218	1392	-582	-541	-580	-715	1218	1555
24	1329	1518	-635	-598	-633	-794	1329	1711
26	1439	1640	-687	-661	-685	-871	1439	1849
28	1550	1763	-740	-724	-738	-949	1550	1987
30	1661	1892	-793	-797	-791	-1035	1661	2131
32	1772	2017	-846	-865	-844	-1124	1772	2288
34	1882	2167	-899	-969	-896	-1249	1882	2484
36	1993	2281	-952	-1074	-949	-1355	1993	2710

Table 5.6 Strain values at the locations of Strain Gages #7, #8, #11 and #12 for a
central patch load

Note: Expt. denotes experiment.

Load (Kips)	$\mu\epsilon_z$ (#7)		$\mu\epsilon_x$ (#8)		$\mu\epsilon_x$ (#11)		$\mu\epsilon_z$ (#12)	
	Present	Expt. (Howard)	Present	Expt. (Howard)	Present	Expt. (Howard)	Present	Expt. (Howard)
0	0	0	0	0	0	0	0	0
2	-105	-127	36	9	36	20	-105	-122
4	-210	-255	71	24	72	35	-210	-242
6	-314	-389	107	49	107	61	-314	-367
8	-419	-521	143	78	143	92	-419	-485
10	-524	-650	179	110	179	126	-524	-606
12	-629	-769	214	144	215	154	-629	-713
14	-734	-898	250	193	251	194	-734	-829
16	-838	-1021	286	245	287	233	-839	-937
18	-943	-1147	321	310	322	281	-943	-1043
20	-1048	-1274	357	387	358	333	-1048	-1150
22	-1153	-1392	393	465	394	387	-1153	-1250
24	-1257	-1513	429	563	430	450	-1258	-1354
26	-1362	-1628	464	653	466	506	-1363	-1443
28	-1467	-1733	500	771	501	566	-1467	-1511
30	-1572	-1849	536	902	537	633	-1572	-1585
32	-1677	-1951	572	1050	573	703	-1677	-1646
34	-1781	-2037	607	1217	609	744	-1782	-1681
36	-1886	-2145	643	1462	645	939	-1887	-1739

Table 5.7 Strain values at the location of Strain Gage #10 for a central patch load

load (kips)	ϵ_y (micro-strains)	ϵ_y (micro-strains)
	Present	Experiment (Howard)
0	0	0
2	18	15
4	37	35
6	55	72
8	74	118
10	92	157
12	111	190
14	129	220
16	148	241
18	166	257
20	185	264
22	203	267
24	222	272
26	240	280
28	259	284
30	277	419
32	296	510
34	314	554
36	333	270

Fig. 5.9 Variation of ϵ_z at the Strain Gauge #1 location for a central patch load

Fig. 5.10 Variation of ϵ_X (compressive) at the Strain Gauge #2 location for a patch load

Fig. 5.11 Variation of ϵ_X (compressive) at the Strain Gage #5 location for a central patch

load

Fig. 5.12 Variation of ϵ_Z at the Strain Gage #6 location for a central patch load

Fig. 5.13 Variation of ϵ_z (compressive) at the Strain Gage #7 location for a central patch

load

Fig. 5.14 Variation of ϵ_x at the Strain Gage #8 location for a central patch load

Fig. 5.15 Variation of ε_X at the Strain Gage #11 location for a central patch load

Fig. 5.16 Variation of ε_Z (compressive) at the Strain Gage #12 location
for a central patch load

Fig. 5.17 Variation of ϵ_Y at the Strain Gage #10 location for a central patch load

Figures 5.9, 5.11, and 5.16 represent the longitudinal direction (Z) strains, ϵ_Z . The finite-element plots in all cases of ϵ_Z are in very good agreement with the experimental curves. The experimental plots are linear for the most part. The inplane transverse direction (X) strains, ϵ_X , are shown in Figs. 5.10, 5.11, 5.14 and 5.15. There is very good agreement between the present and experimental results in three of the four ϵ_X plots; the one exception is Fig. 5.14, which shows a large discrepancy, particularly at loads of 24 kips and higher. Strain Gage # 8 (Fig. 5.14) is at the mirror-image location of Strain Gage # 11 (Fig. 5.15) and thus should have identical strain values. In fact, Table 5.6 confirms this as regards to the present (*ANSYS*) results; but the experimental results for Gages # 8 and #11 are far apart and thus Gage # 8 is most likely a bad one and its readings should be discarded. In general the ϵ_X plots tend to become nonlinear at low load levels whereas

the ε_Z plots stay mostly linear until about 32 Kips. Figure 5.17 represents the transverse direction strains ε_Y for the web. It is seen that the experimental strain starts to diverge after a load of 5 kips.

Using Eq. (5.4) and the slope from Fig. 5.8, the equivalent flexural rigidity ($E_Z I_X$) and Young's modulus of the Prodeck 8, based on strain are presented in Table 5.8 and compared with the corresponding values obtained experimentally by Howard (2002). It is seen that the equivalent flexural rigidity of $1.32 \times 10^9 \text{ lb} \cdot \text{in.}^2$ based on strain is 15.8% higher than that of Howard (2002). Also, comparing with Table 5.3 the present $E_Z I_X$ based on strain is 26.9 % higher than that based on deflection.

Table 5.8 Equivalent flexural rigidity and Young's modulus in the cell direction based on strain

Type	Equivalent Flexural Rigidity $E_Z I_X (10^9 \text{ lb} \cdot \text{in.}^2)$	Equivalent Young's Modulus $E_z (10^6 \text{ psi})$
Present	1.32	4.40
Experiment (Howard)	1.14	3.8

Fig. 5.18 Contour plot of ϵ_z for a central patch load of 24 Kips

Fig. 5.19 Contour plot of ϵ_z in side view for a central patch load of 24 Kips

Fig. 5.20 Contour plot of ϵ_x for a central patch load of 24 Kips

Fig. 5.21 Contour plot of ϵ_x for a section of the Prodeck 8 for a central patch load of 24

Kips

Fig. 5.22 Contour plot of ϵ_y for a central patch load of 24 Kips

Fig. 5.23 Contour plot of ϵ_y in the side view for a central load of 24 Kips

Fig. 5.24 Contour plot of γ_{XY} for a central patch load of 24 Kips

Fig. 5.25 Contour plot of γ_{XY} in the side view for a central patch load of 24 Kips

Fig. 5.26 Contour plot of γ_{XZ} for a central patch load of 24 Kips

Fig. 5.27 Contour plot of γ_{XZ} in the side view for a central patch load of 24 Kips

Fig. 5.28 Contour plot of γ_{YZ} for a central patch load of 24 Kips

Fig. 5.29 Contour plot of γ_{YZ} in the side view for a central patch load of 24 Kips

Fig. 5.30 Contour plot of σ_Z for a central load of 24 Kips

Fig. 5.31 Contour plot of σ_Z in the side view for a central load of 24 Kips

Fig. 5.32 Contour plot of σ_X in the side view for a central patch load of 24 Kips

Fig. 5.33 Contour plot of σ_X for a central patch load of 24 Kips

Fig. 5.34 Contour plot of σ_y for a central patch load of 24 Kips

Fig. 5.35 Contour plot of σ_y in the side view for a central patch load of 24 Kips

Fig. 5.36 Contour plot of τ_{XY} for a central patch load of 24 Kips

Fig. 5.37 Contour plot of τ_{XY} for a central patch load of 24 Kips

Fig. 5.38 Contour plot of τ_{XZ} for a central patch load of 24 Kips

Fig. 5.39 Contour plot of τ_{XZ} for a central patch load of 24 Kips

Fig. 5.40 Contour plot of τ_{YZ} for a central patch load of 24 Kips

Fig. 5.41 Contour plot of τ_{YZ} for a central patch load of 24 Kips

Contour plots for the strain components ϵ_X , ϵ_Y , ϵ_Z , γ_{XY} , γ_{YZ} , and γ_{ZX} , are presented in Figs. 5.18-5.29. The longitudinal strain ϵ_Z , is represented in Fig. 5.18 as a top view and in Fig. 5.19 as a side view. The phenomenon of top flange in compression, bottom flange in tension, and the maximum absolute strain occurring at mid-span are all as expected. For the applied load of 24 Kips, the maximum compressive strain is 0.002463. The in-plane transverse normal strain (ϵ_X) is displayed in Figs. 5.20 and 5.21. While most of the deck undergoes low ϵ_X values, strain concentration occurs at the corners of the patch load with a maximum value of 0.005971. Figures 5.22 and 5.23 depict the out-of-plane transverse normal strain ϵ_Y . The maximum positive values occur at the edges of the patch load. The contour plots of the shear strain components γ_{XY} , γ_{YZ} , and γ_{ZX} are shown in Figs. 5.24-5.29. It is worthwhile to point out that the maximum strain γ_{YZ} due to transverse shear load occurs at the middle of the webs, whereas the components γ_{XY} and γ_{ZX} are mainly concentrated near the loading zone. The maximum values of γ_{XY} and γ_{XZ} are about 0.0035 while that of γ_{XZ} is about 0.0025.

Figures 5.30-5.41 display contour plots of the normal and shear stress components for a resultant load of 24 Kips. These plots are similar in nature to their strain counterparts. It is seen from Figs. 5.30 and 5.31 of σ_Z that the maximum compressive stress occurs in the top flange, and the maximum tensile stress in the bottom flange, both at the mid-span of the deck. The maximum absolute value is about 20,500 psi. It is seen from Figs. 5.32 to 5.35 that σ_X (in-plane) and σ_Y (transverse) are mostly concentrated at

the edges of the loading zone with the maximum values around 20,000 psi. The shear stress plots, Figs. 5.36-5.41 indicate that the maximum values are about one-fourth that of normal stress components. Contrast this with the maximum shear strains being nearly of the same value as the normal strains; this affirms the low shear modulus of composites.

5.5. FAILURE ANALYSIS

In this section an attempt is made to predict the first ply failure of the Prodeck 8 using the maximum stress criterion, maximum strain criterion and the Tsai-Wu criterion. It should be noted that an automatic progressive ply failure analysis is not featured in *ANSYS* and thus only the first ply failure is investigated. The failure analysis is performed for patch loads of size 10"x 20" and 14"x 20".

Figure 5.42 shows the failure plot for the Prodeck 8 subjected to a patch load of 10"x 20" with a resultant load value of 24 kips. The values listed in the failure plots are that of the quality ξ , defined in Eq. (4.19), which is the inverse of the strength ratio (R). The maximum ξ value in Fig. 5.42 is 2.372 which implies that according to the maximum stress criterion, the first ply failure occurs at a load value of $P = 24/2.372 = 10.11$ kips. It should be noted that first ply failure does not imply failure of the bridge deck. Usually the first ply fails in the transverse direction but is still capable of resisting higher loads in the fiber direction. Also, the other plies can carry increasing loads as evidenced by the failure load of 36 kips from Howard's (2002) experiments. As seen from Fig. 5.42, the failure seems to occur at the corners of the applied patch load, but this view does not clearly show the actual region where the maximum failure value is reported. As a result, more views are explored to see the regions with maximum failure values.

Fig. 5.42 Failure plot of the Prodeck 8 using maximum stress criterion

Fig. 5.43 Failure plot for a part of the Prodeck 8 using maximum stress criterion

Figure 5.43 shows the failure plot for the bottom segment of the top flange and right side of the left web. From Fig. 5.43 we can clearly conclude that the maximum failure value occurs beneath the applied load at the bottom of the top flange. A close analysis of the ξ values of all the layers of the Prodeck 8 indicates that the 0^0 plies (fiber orientation along the Z-axis, longitudinal direction) beneath the applied load at the bottom of the top flange fail first. The *ANSYS* does not indicate the mode of failure. A careful scrutiny of the stress plots (Figs. 5.30-5.41) along with the strength values (Table 4.2) of the plies indicates that the 0^0 plies fail in the transverse direction. It can be reasonably concluded that failure is due to matrix cracking since the transverse stress (σ_x) in the 0^0 plies is tensile and the failure strength, F_{t2} of 5,900 psi is the lowest among the failure strengths.

The failure analysis is also carried out using the maximum strain criterion and the corresponding failure plots are shown in Figs 5.44 and 5.45.

Fig. 5.44 Failure plot of the Prodeck 8 using maximum strain criterion

Fig. 5.45 Failure plot for a part of the Prodeck 8 using maximum strain criterion

Fig. 5.46 Failure plot of the Prodeck 8 using Tsai-Wu Criterion

Fig. 5.47 Failure plot for a section of the Prodeck 8 using Tsai-Wu criterion

It is seen from Figs 5.44 and 5.45 that the failure plots using maximum strain criterion are similar to that of the maximum stress criterion with both criterion predicting failure to occur beneath the applied load at the bottom of the top flange. According to the maximum strain criterion, the maximum value of the failure parameter ξ is 2.075 as compared to a value of 2.372 based on the maximum stress criterion. The Tsai-Wu criterion is applied next with the failure plots being as shown in Figs. 5.46 and 5.47. While the failure plots based on the Tsai-Wu criterion are similar to the other two criteria the maximum ξ value of 3.739 is more than one and a half times that of the either of the other two criteria. While the applicability of Tsai-Wu criterion is well established at the coupon level of unidirectional composites, in the current case of a relatively complex structure made of many layers with three different architectures (Fig. 3.3) of 2-D stitched composites, it appears that the Tsai-Wu criterion may not yield accurate results. The experiments conducted by Howard (2002) indicate a Punching shear failure at the edges of the loading plate when the resultant load is about 36 kips. The three theories considered in the present analysis do predict failure in the same region, at least qualitatively.

The above failure analysis does not indicate the web or the web-flange intersection to be very critical. This may be due to the fact that the patch load of 10"x 20" does not span the web locations. To test this hypothesis, the failure analysis is repeated with a patch load of 14"x 20" with a resultant load value of 24 kips.

Figures 5.48-5.53 show the failure plots for the Prodeck 8 subjected to a patch load of 14" x 20" using the maximum stress criterion, maximum strain criterion and Tsai-Wu criterion. It can be seen from these figures that that failure is predicted to occur at the web section near the top flange or at the web flange intersection. Howard's (2002) experiments also indicate failure due to web flange separation in the case of the 14"x 20" patch load. It should be noted that the maximum ξ values are highest (0.795), implying the first ply failure to occur at a load of $P = 24/0.795 = 30.2$ kips for the maximum stress criterion and lowest (0.519) for the Tsai-Wu criterion, implying the first ply failure to occur at a load of $P = 24/0.519 = 46.24$ kips.

Fig. 5.48 Failure plot of the Prodeck 8 using maximum stress criterion subjected to a patch load of 14"x 20"

Fig. 5.49 Failure plot for a part of the Prodeck 8 using maximum stress criterion
subjected to a patch load of 14"x 20"

Fig. 5.50 Failure plot of the Prodeck 8 using maximum strain criterion subjected to a
patch load of 14"x 20"

Fig. 5.51 Failure plot for a part of the Prodeck 8 using maximum strain criterion
subjected to a patch load of 14"x 20"

Fig. 5.52 Failure plot of the Prodeck 8 using Tsai-Wu criterion subjected to a patch load
of 14"x 20"

Fig. 5.53 Failure plot for a part of the Prodeck 8 using Tsai-Wu criterion

On detailed observation and analysis of the layers, the conclusion is that the 0^0 fiber fails first. It is believed that this might be due to the matrix cracking in the transverse direction.

5.6 BUCKLING ANALYSIS

A buckling analysis is conducted on the present *ANSYS* model of the Prodeck 8. The Prodeck 8 of 12" length is subjected to a patch load of 12" x 12". The top flange is restricted to have same displacement in the Y direction to achieve pure buckling. Eigenvalue buckling analysis method is used to obtain the critical load for the deck. The buckled shape is shown in the Fig. 5.54. The critical load is found to be 183 kips, which is much larger than the experimental critical load of 45 kips obtained by Howard (2002). Also buckling analysis is carried out on just the web section without the stiffener as shown in the Fig. 5.55. The considered web has dimensions of 0.35" thickness, 12"

length and 7" height and is subjected to a load at the top over the area of 12"x 0.35". The critical load for the web section is found to be 33 Kips.

Fig. 5.54 Buckled shape of the Prodeck 8

Fig. 5.55 Buckled shape of the web

CHAPTER 6

CONCLUSIONS AND RECOMMENDATIONS

6.1 INTRODUCTION

Prodeck 8 has been evaluated for static response under 3-point bending and buckling load types using the finite-element software *ANSYS*. Also a first-ply failure analysis has been carried out. This chapter contains conclusions drawn from the current finite-element analysis as well as recommendations on further analysis.

6.2 CONCLUSIONS

- Successfully modeled the Prodeck 8 using *ANSYS*.
- The deflection analysis has showed excellent correlation with experimental results obtained by a previous researcher.
- Comparisons of present strain values at selected locations with previous experimental values has indicated very good correlation.
- The equivalent flexural rigidity and Young's modulus of the prodeck 8 based on a transverse load model are found to be $1.04 (10^9) \text{ lb} \cdot \text{in.}^2$ and $3.47 (10^6) \text{ psi}$.
- When corrected for shear effects, the equivalent flexural rigidity value of $1.007(10^9) \text{ lb} \cdot \text{in.}^2$ was nearly identical to that of the experimentally obtained value of the previous research.
- The equivalent flexural rigidity and Young's modulus of the prodeck 8 based on strain values corresponding to the transverse load case are found to be $1.26 (10^9) \text{ lb} \cdot \text{in.}^2$ and $4.21 (10^6) \text{ psi}$ respectively.

- Due to the present limitations of *ANSYS*, only the first ply failure analysis has been done.
- Based on the failure analysis performed using maximum strain criterion, maximum stress criterion and Tsai-Wu criterion, it is found that the 0^0 fibers at the bottom of the top flange, directly under the applied patch load of 10"x 20" are the first layers to fail. And it is believed that the failure occurs due to the matrix cracking in the transverse direction.
- For a patch load of 14"x 20" it is found that the failure occurred at the web flange intersection and again it is observed that 0^0 fibers are the first to fail.
- Based on the buckling analysis of the Prodeck 8, the critical load is found to be 183 kips which is very high compared to the experimentally determined critical load of 45 kips reported by the previous researcher.
- The buckling analysis of just the web section resulted in a critical load of 33 kips.

6.3 RECOMMENDATIONS

- Progressive ply failure of the Prodeck 8 can be studied by writing some finite-element codes to be used with *ANSYS* or using another commercially available finite-element software, which is capable of progressive ply failure.
- A thorough study can be carried out to determine the failure modes of the Prodeck 8 under different load types.
- The critical load of the Prodeck 8 should be verified by performing a suitable buckling analysis.

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APPENDIX A

SHEAR MODULUS AND SHEAR CORRECTION

A.1 Shear Modulus

The equivalent shear modulus G of the Prodeck 8 is calculated as follows. First, the angle of twist at the free end of the beam of the web (θ_w) and that of the flange (θ_f) from the torsional analysis (Section 5.3) are obtained using Figs. A-1 and A-2 and are found to be

$$\theta_w = 5.026 \times 10^{-4} \text{ rad}$$

$$\theta_f = 3.999 \times 10^{-4} \text{ rad.}$$

The average angle of twist of the Prodeck 8 is calculated as

$$\theta = \frac{\theta_w + \theta_f}{2} = 4.5125 \times 10^{-4} \text{ rad.} \quad (\text{A-1})$$

The shear modulus of the Prodeck 8 is calculated as

$$G = \frac{TL}{J\theta} = \frac{1135(128.75)}{342.02(4.5125 \times 10^{-4})} = 0.9465 \times 10^6 \text{ psi.} \quad (\text{A-2})$$

Note that the applied torque at the free end is 1135 lb • in. and the length of the deck considered for the torsional analysis is 128.75 in.

A.2 Shear correction in transverse loading analysis

Using the shear modulus value obtained using Eq. (A-2), the deflection due to shear (δ_s) is calculated as

$$\delta_s = \frac{PL}{4GA} = \frac{24000(120)}{4(0.9465 \times 10^6)27.04} = 0.02813 \text{ in.} \quad (\text{A-3})$$

Thus the total deflection (δ), due to shear deflection (δ_s) and bending deflection (δ_B) (Section 5.2), is calculated as

$$\delta = \delta_s + \delta_B = 0.02813 + 0.8295 = 0.8576 \text{ in.} \quad (\text{A-4})$$

The equivalent flexural rigidity and Young's modulus for the Prodeck 8 based on total deflection are calculated as

$$EI = \frac{PL^3}{48\delta} = \frac{24000(120^3)}{48(0.8576)} = 1.007 \times 10^9 \text{ lb} \cdot \text{in.}^2 \quad (\text{A-5})$$

$$E = \frac{EI}{300} = 3.358 \times 10^6 \text{ psi} \quad (\text{A-6})$$

Fig. A-1 Linear fit of the angle of twist for the web of the Prodeck 8

Fig. A-2 Linear fit of the angle of twist for the flange of the Prodeck 8

APPENDIX B

PRODECK 8 SUBJECTED TO PURE BENDING MOMENT

Figure B-1 shows the boundary conditions and load applied on the Prodeck 8 to achieve pure moment. As shown in the figure, the deck of length 130 in. is fixed at one end and on the other end, equal and opposite forces of 120 psi are applied on the top and bottom flanges over an area of 11.55"x 0.398", thus creating a pure bending moment of magnitude 4193 lb • in.

Figure B-2 shows the contour plot of the transverse deflection for the Prodeck 8 under the pure bending moment. The maximum deflection is found to be 0.0278 in. and the equivalent flexural rigidity and Young's modulus are calculated as shown below.

$$\delta = \frac{ML^2}{2EI} \quad (B-1)$$

Using Eq. (B-1) the equivalent flexural rigidity is found to be $1.274 \times 10^9 \text{ lb} \cdot \text{in}^2$ and the Young's modulus is found to be $4.24 \times 10^6 \text{ psi}$.

Fig. B-1 Prodeck 8 subjected to pure bending moment

Fig. B-2 Contour plot of displacement in the Y direction