

Nonlinear Tensile and Shear Behavior of Macro Fiber Composite Actuators

R. Brett Williams,^{*} Marc R. Schultz,[†] Michael W. Hyer,[‡] Daniel J. Inman,[§]
Virginia Polytechnic Institute and State University
Blacksburg, VA 24061, USA

W. Keats Wilkie[¶]
NASA Langley Research Center
Hampton, VA 23681, USA

ABSTRACT: The Macro Fiber Composite actuator, developed at the NASA Langley Research Center, offers much higher flexibility and induced strain levels ($\sim 2000 \mu\epsilon$, peak-to-peak) than its monolithic piezoceramic predecessors. As a result, a significant amount of nonlinear mechanical behavior occurs over the applicable strain range of the device. This work experimentally determines the four independent linear elastic engineering constants of the orthotropic MFC actuator and characterizes nonlinear tensile and shear stress-strain behavior and transverse Poisson effects using various plastic deformation models. The results can then be readily incorporated into structural actuation models that accurately consider nonlinear mechanical behavior.

KEY WORDS: piezoceramic fibers, elastic properties, material nonlinearities

INTRODUCTION

The Macro Fiber Composite (MFC) actuator is part of an emerging technology that strives to improve the current state of the art for structural actuation, which currently relies heavily upon monolithic piezoceramic materials. The MFC is a layered, planar actuation device that employs rectangular cross-section, unidirectional piezoceramic fibers (PZT) embedded in a thermosetting polymer matrix. This active, fiber-reinforced layer is then sandwiched between copper-clad Kapton[®] film layers that have an etched interdigitated electrode pattern. During manufacturing, these layers are laid-up by hand and then cured in a vacuum hot-press. After the epoxy matrix that bonds the package together is fully cured, a high DC voltage (~ 1500 Volts) is applied to the electrodes, thereby poling the piezoceramic material in the plane of the actuator and establishing the poling direction parallel to the PZT fibers. This in-plane poling and subsequent in-plane voltage actuation allows the MFC to utilize the d_{33} piezoelectric effect, which is much stronger than the d_{31} effect used by traditional PZT actuators with through-the-thickness poling [1]. A detailed description of the MFC manufacturing process is given by Williams et al. [2] and, while the current work focus on room-temperature properties only, elastic behavior over a wide range of temperatures is presented by Williams, Inman and Wilkie [3].

^{*} Author to whom correspondence should be addressed, 310 Durham Hall, MC 0261, Blacksburg, VA 24061
ph: (540) 231-2910 fax: (540) 231-2903, rowilli6@vt.edu. Graduate Research Assistant, CIMSS, Department of Mechanical Engineering

[†] Graduate Assistant, Department of Engineering Science and Mechanics

[‡] Professor, Department of Engineering Science and Mechanics

[§] G.R. Goodson Professor and Director, CIMSS, Department of Mechanical Engineering

[¶] Research Scientist, Structural Dynamics Branch

Since the MFC is composed of both isotropic and orthotropic layers as seen in Figure 1, and is most adequately represented as a symmetric, hybrid, cross-ply laminate, or in standard composites notation, $[\text{Iso}_{\text{Kapton}}/\text{Iso}_{\text{acrylic}}/90^{\circ}_{\text{copper}}/\bar{0}^{\circ}_{\text{PZT}}]_S$. Mechanical behavior of this orthotropic laminate is fully described by four independent stiffness quantities, E_1 , E_2 , ν_{12} and G_{12} . In a previous work, micromechanical models were used to determine the elastic constants of each orthotropic layer, and then classical lamination theory was used to determine the effective mechanical properties of the MFC [2]. The present effort serves to verify these linear values experimentally, and then to further characterize plastic deformation behavior.

EXPERIMENTAL PROCEDURE

Three sets of tensile test experiments were performed on MFC actuators in order to characterize their tensile and shear constitutive behavior and mechanical properties. The first set was designed to measure the stress-strain behavior in the PZT fiber direction, while the second set captured such behavior transverse to these fibers. The third set measured the in-plane shear stress-shear strain performance. This section describes the specific preparation of the various types of specimens, strain gage and Instron testing machine set-up and analysis of the raw data.

Specimen Preparation

The first set of tests loaded the standard MFC tensile test specimen, seen in Figure 2, in the direction of the PZT fibers (1-direction). The tabs near the ends of the specimen were for loading into pneumatic grips, and a fine mesh screen was inserted between the specimen and the grips to prevent slipping during the test. The second set of tests loaded the MFC perpendicular to the PZT fibers using 2-direction test specimens, which were cut as depicted in Figure 1 and fitted with specially designed fiberglass tabs as seen in Figure 3. This particular region was selected because it contained only an active portion of the MFC (area containing copper electrodes), along with the solder tabs and leads which preserved the electrical connectivity of the device. Clearly, the 2-direction test specimen was much shorter than those used for the 1-direction tests, and having enough area to fit into the grips while uniformly distributing the clamping pressure was an issue that was resolved by bonding fiberglass tabs onto the specimen using strain gage epoxy. Fiberglass was chosen as its stiffness closely resembled the stiffness of the MFC in the 2-direction, however, the increased thickness of the specimen required the use of mechanical instead of pneumatic grips. The slots seen in Figure 2a were cut in order to allow the MFC leads to escape from the grips and be twisted together without damage so that the desired short-circuit properties were measured. The third set of tests utilized 45° off-axis tensile test specimens, seen in Figure 4, to measure the in-plane shear stress-shear strain behavior of the MFC. Again, the end tabs were for loading into the grips, however, off-axis tensile specimens exhibit shear-extension coupling. Since traditional mechanical or pneumatic grips prohibited such shear deformation, special pivoting grips [4] that allowed the ends of the specimen to rotate freely were used to ensure a uniform state of stress across the specimen width.

Strain Gage Instrumentation and Instron Testing Machine Setup

For the 1 and 2-direction tests, strain gages capable of measuring both longitudinal and transverse strains simultaneously, type CEA-13-250WQ-350 from Vishay Measurements Group, were aligned with the 1-2 coordinate system seen in Figure 2 and then bonded to the center of the top and bottom of each MFC tests specimen. For the off-axis tests, 0° - 45° - 90° strain gage rosettes, type CEA-13-250UR-350 from Vishay Measurements Group, were aligned with the x-y coordinate system and bonded to the top and bottom of the specimens as shown in Figure 4. All strain gages were bonded using M-Bond AE-15 strain gage epoxy, which was cured at 150°F for two hours as suggested by the manufacturer, and strain gage leads attached using solder. Each MFC specimen was then tensile

tested using an Instron 4468 universal testing machine with a self-calibrating 1000 kN load cell and the appropriate grips. All specimens were loaded at a crosshead speed of 0.2 mm/min until failure occurred in the specimen or near the grips. During the tensile tests, a ten channel stand-alone strain conditioner-amplifier and an analog to digital converter were connected to a PC, on which a Labview Virtual Instrument was used to collect numerous sets of data simultaneously, namely load from the testing machine and four (or six, for off-axis test) channels of voltage from the four (or six) strain gages.

Comments on Data Analysis

For an engineering analysis, stress and strain values must be computed from the acquired load and voltage data. The actual strains are easily calculated from the recorded voltages as the strain gage amplifier is calibrated such that one output volt equals 1000 microstrain ($\mu\epsilon$). The strain values are then averaged from the top and bottom gages to remove any bending strain effects. The stress developed from the applied load is calculated by dividing the load data by the cross-sectional area of the active region of the MFC. However, the chosen thickness represent the *maximum* value for the specimen, as the copper electrodes are spaced out, rather than covering the entire active region. Thus, the actual thickness can vary slightly depending on the amount of epoxy that is expelled from between the electrodes during the final MFC curing phase. Nevertheless, a photomicrograph of the MFC cross-section indicates that using this maximum thickness is a reasonable approach [1]. Lastly, the stress-strain curves are shifted horizontally so that the linear region extrapolates exactly through the origin in accordance with ASTM Standard D3039/D3039M-00.

NONLINEAR 1 AND 2-DIRECTION BEHAVIOR

Most engineering materials exhibit a simple, proportional relationship between stress and strain and between transverse and longitudinal strain at low load levels. The elastic moduli and Poisson's ratios are extracted from these linear relationships. However, at higher stress/strain levels, this linear relationship is no longer valid as plastic deformation begins to occur. It is important to characterize both the elastic and plastic behavior since the MFC is subjected to such high strains, whether from actuation or mechanical loading (or most likely, a combination of both). This section focuses on experimentally characterizing such behavior in the 1 and 2-directions using the elastic-linear hardening and Ramberg-Osgood plastic deformation models. Also, the use of biaxial strain gages for these tests allow transverse strain data to be collected, from which, Poisson's ratios can be calculated. However, only the ν_{12} is considered independent and of particular importance to the current characterization of the MFC.

Elastic-Linear Hardening Relationship

The elastic-linear hardening relationship describes the stress-strain behavior as linear at a slope of E_i ($i = 1, 2$) up to the onset of plastic deformation, and then linear again at a slope of $\delta_i E_i$ through the plastic region, where δ_i is the slope reduction factor for the i -direction test [5]. For materials displaying hardening type plastic deformation, i.e. the MFC when tested in the 1 and 2-directions, δ_i has a value between zero and one. Mathematically, this type of constitutive behavior is given as

$$\sigma_i = \begin{cases} E_i \epsilon_i & (\sigma_i \leq \sigma_{0i}) \\ (1 - \delta_i) \sigma_{0i} + \delta_i E_i \epsilon_i & (\sigma_i \geq \sigma_{0i}) \end{cases} \quad (1)$$

where σ_i is the normal stress, ϵ_i is the normal strain, and σ_{0i} is the stress at which plastic deformation begins. Application of this model to the data from the 1 and 2-direction tests requires a linear

regression of both the elastic and plastic regions, which provides values for E_i and δE_i for each specimen, respectively. The onset of yielding is determined graphically as the point where the stress-strain curve begins to exhibit plastic deformation, in contrast to an offset yield point typically used with engineering metals. These characteristic values are averaged for each of the two directions and appear in Table 1. Figure 5 shows a plot of Equation 1 using these parameters, along with the experimental data. Clearly, the models adequately represent the experimental data from the 1 and 2-direction tests.

Ramberg-Osgood Relationship

The Ramberg-Osgood plastic deformation model represents the total strain as the sum of the elastic and plastic strains [5]. Again, the elastic strain component is proportional to stress, however, the exponential relationship between stress and plastic strain is

$$\sigma_i = H_i \epsilon_{pi}^{n_i} \quad (2)$$

where H_i is a material constant, n_i is a strain hardening exponent and ϵ_{pi} is the plastic strain. Solving Equation 2 for ϵ_{pi} and adding to the elastic strain gives the total strain as

$$\epsilon_i = \frac{\sigma_i}{E_i} + \left(\frac{\sigma_i}{H_i} \right)^{\frac{1}{n_i}} \quad (3)$$

From the exponential nature of Equation 2, a plot of stress versus plastic strain is a straight line on a log-log plot. Values of H_i and n_i for a particular tensile test are found either graphically from this plot (where H_i is equal to σ_i at a ϵ_{pi} equal to one and n_i is the slope of the line if the logarithmic decades in the two directions are of equal lengths) or using a linear curve fit of logarithmic data. The current effort uses the more precise latter approach to find H_i and n_i for each of the experimental stress-strain curves. The averaged results for both the 1 and the 2-direction tests are presented in Table 1. Figure 6 plots Equation 3 with these values, one curve for each direction, along with the corresponding experimental data. Again, the curves closely match the data.

Nonlinear ν_{12} Behavior

The experimental transverse strain data is used to determine Poisson's ratio, ν_{12} . As seen in Figure 7, the slope of the linear fit, ν_{12} , is constant in the linear elastic region, however, at the onset of plastic behavior, it changes sign and magnitude. This change is indicative of broken fibers returning to a lower or stress-free state at which time the transverse strain is eventually recovered. Beyond this strain level, ν_{12} is essentially zero. Poisson's ratio values for the elastic and plastic regions are given in Table 1. It should be noted that the tension tests from this research effort are monotonic, and fiber breakage is a one-time event. Thus, Poisson's ratio could change for subsequent tensile loading and unloading scenarios, as would be the case for actuating the MFC under large harmonic voltage.

Comments on ν_{21}

Since the minor Poisson's ratio, ν_{21} , is not an independent material property, it is not essential to verify its value experimentally. However, if one wishes to do so, it is determined in the same manner as ν_{12} , except that data from the 2-direction test is substituted for the 1-direction data. In the current experiments, transverse strain data was collected for the 2-direction tests, however, since these test specimens are so short, the fiberglass tabs *severely* restrict the transverse contraction, thus resulting in

incorrect experimental values that are much too low. However, the results presented in Table 1 and 3 are from calculated from

$$\nu_{21} = \nu_{12} \frac{E_2}{E_1} \quad (4)$$

where the quantities on the right-hand side are from experimental and analytical values, respectively.

NONLINEAR IN-PLANE SHEAR BEHAVIOR

While the 1 and 2-direction tests create a uniaxial state of stress by applying the load at 0° or 90° to the PZT fibers, respectively, loading at any intermediate angle creates a biaxial stress-state consisting of longitudinal, transverse and in-plane shear stresses on the off-axis plane [6]. Since this set of tests is concerned with characterizing shear behavior, the load should be applied at an angle which maximizes the shear strain in the material coordinate system. While previous work shows this optimal angle is 32° for the MFC [7], special tensile-test coupons were manufactured with the PZT fibers oriented at 45° because such components are readily available from NASA Langley. However, this deviation in angle results in only a 5% reduction in shear strain [7] and simplifies the subsequent data analysis.

In order to obtain a shear stress-shear strain curve, the collected stress-strain data from an off-axis specimen must be manipulated in the following manner. First, stress transformations give the shear stress in the material coordinates as

$$\tau_{12} = \frac{\sigma_x \sin 2\theta}{2} \quad (5)$$

where σ_x is the applied unidirectional stress. Next, the shear strain in the material coordinate system is

$$\gamma_{12} = (\epsilon_y - \epsilon_x) \sin 2\theta + \gamma_{xy} \cos 2\theta. \quad (6)$$

Here, for 0°-45°-90° rosettes, ϵ_x and ϵ_y are equal to the strains from gages 1 and 3 (ϵ_{g1} and ϵ_{g3}) in Figure 3, respectively, while the shear strain in the geometric coordinates, γ_{xy} , is

$$\gamma_{xy} = -\epsilon_{g1} + 2\epsilon_{g2} - \epsilon_{g3}. \quad (7)$$

However, since $\theta = 45^\circ$ for these MFC coupons, Equation 5 simplifies to

$$\tau_{12} = \frac{\sigma_x}{2} \quad (8)$$

and γ_{xy} is not required as the last term in Equation 6 vanishes. Now, the desired curve is found by plotting the shear stress from Equation 8 versus the shear strain calculated in Equation 6 at each load level. The in-plane shear modulus, G_{12} , is the slope of the initial tangent to this curve.

Ramberg-Osgood Relationship

The Ramberg-Osgood model developed above easily adapts for use with nonlinear in-plane shear deformation by substituting shear stress and strain parameters for their normal stress and strain counterparts. The resulting expression for the intralaminar shear stress, τ_{12} , is

$$\tau_{12} = H_{12} \gamma_{p12}^{n_{12}} \quad (9)$$

where H_{12} is a material constant, n_{12} is a strain hardening exponent and γ_{p12} is the plastic shear strain. Solving Equation 9 for γ_{p12} and adding to the elastic strain gives the total strain as

$$\gamma_{12} = \frac{\tau_{12}}{G_{12}} + \left(\frac{\tau_{12}}{H_{12}} \right)^{\frac{1}{n_{12}}} \quad (10)$$

Following the curve fitting procedure described above, average values for H_{12} and n_{12} are found from five test specimens and the corresponding shear stress-shear strain curve from Equation 10 is plotted along with the experimental data in Figure 8.

Quadratic Least-Squares Fit

Clearly, the Ramberg-Osgood model accurately represents the shear stress-shear strain data. However, it is preferable to have two expressions for all stress-strain curves, at least one of which can be solved explicitly for stress. Examination of the data shows only a small amount of plastic strain, so a power hardening type of relationship will not fit the data well. However, the shear stress-shear strain data does appear to possess a quadratic relationship of the form

$$\tau_{12} = C_1 \gamma_{12} + C_2 \gamma_{12}^2 \quad (11)$$

where the constants C_1 and C_2 are found using a quadratic curve-fitting program for each specimen. The average values from five tests are presented in Table 2 and are used to plot Equation 11 along with the experimental data in Figure 8.

Unlike the elastic-linear hardening and Ramberg-Osgood models, this quadratic regression is purely mathematical and not based on any physical behavior. However, Equation 11 provides a good fit to the data and gives a simple shear stress prediction that can be readily applied for simulation and control models.

COMPARISON WITH THEORETICAL LINEAR VALUES

In a previous work [2], classical lamination theory was used to predict effective laminate stiffness properties of the MFC. These values are presented in Table 3, along with the percent error when compared to the experimental values obtained from the current effort. Clearly, the traditional mechanics of composite materials theories predict, with reasonable accuracy, the four independent engineering properties in the linear elastic region. This experimental verification of these well-known equations is significant because future researchers can employ such techniques for alternative MFC lamination configurations, including various PZT fiber orientations and geometries.

CONCLUSIONS

This research effort experimentally determines the four independent orthotropic engineering properties in the linear elastic region as well as characterizes the nonlinear constitutive behavior of the MFC actuator under short-circuit conditions. The experimental values of E_1 , E_2 , ν_{12} and G_{12} agree with published values [2] to within a few percent in most cases. Therefore, researcher can use traditional mechanics of laminated composites to develop new types of MFC actuators with various piezoelectric materials and geometries. Also, the models in Figures 5 and 6 depict high fidelity quantitative relationships for the nonlinear stress-strain behavior in the 1 and 2-directions, while the off-axis test specimens provide accurate representation of the in-plane shear behavior of the MFC. However, these models all have both advantages and disadvantages. For the 1 and 2-direction tests, one could argue that the Ramberg-Osgood is a better fit overall, particularly in the elastic-plastic transition region, yet it involves increased complexity, the inability to explicitly solve for stress and no distinct yield point. While in general, either model is acceptable for design use, one concerned with overall stress-strain behavior would likely prefer the simpler elastic-linear hardening model, but if a failure/longevity study of the MFC is performed, the Ramberg-Osgood model would be preferable due to its fidelity in the critical yielding region. As for the shear stress-shear strain behavior, both the Ramberg-Osgood model and the quadratic regression fit the data extremely well. While the former approach has the aforementioned drawbacks, it is derived from observed mechanical behavior, while the latter is simply a mathematical formula with no physical basis. Also, the lines in Figure 7, the slopes of which represent the major Poisson's ratio, fit well through the data over the entire applicable strain range. Overall, this project gives researchers in the field of intelligent structures an in-depth view of the short-circuit mechanical behavior of the MFC actuator, including both values for essential elastic engineering constants as well as equations designed to handle even complex plastic deformation.

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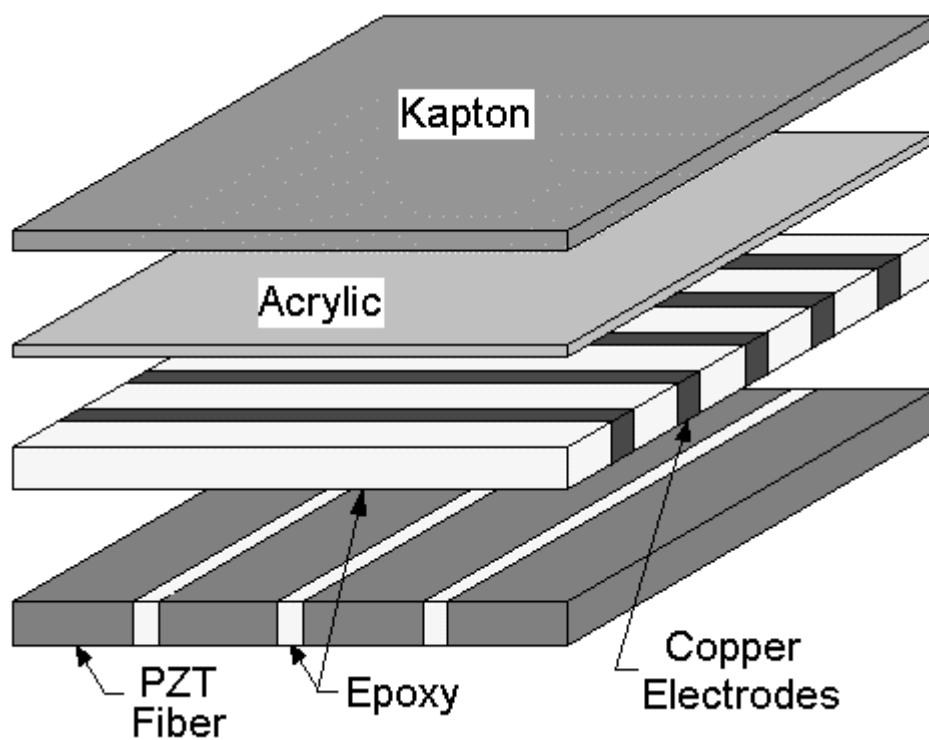


Figure 1. Top Layers of MFC Specimen, Symmetrically Laminated About Middle of PZT Layer.

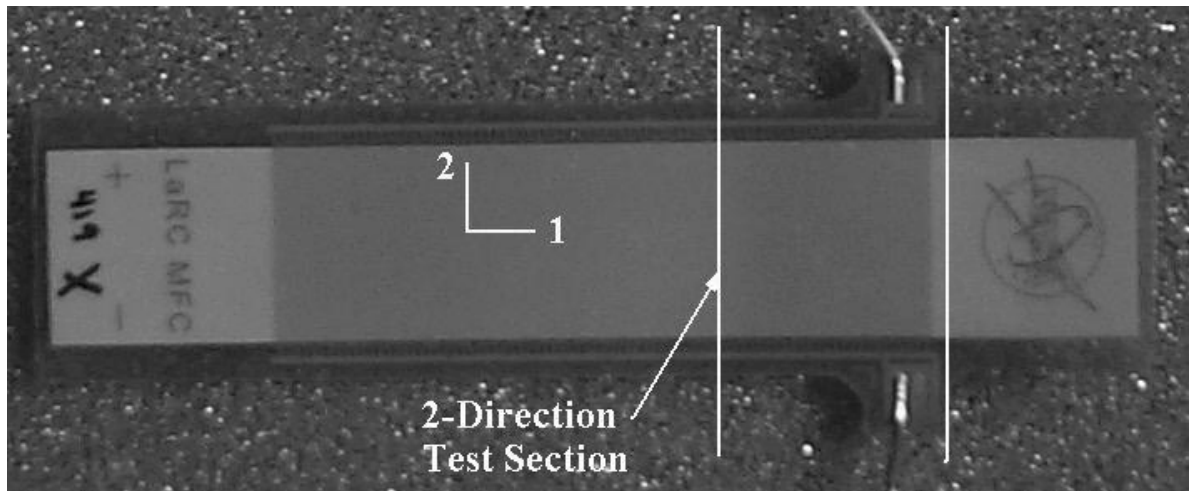


Figure 2. MFC Tensile Test Specimen With 1-2 Coordinate System.

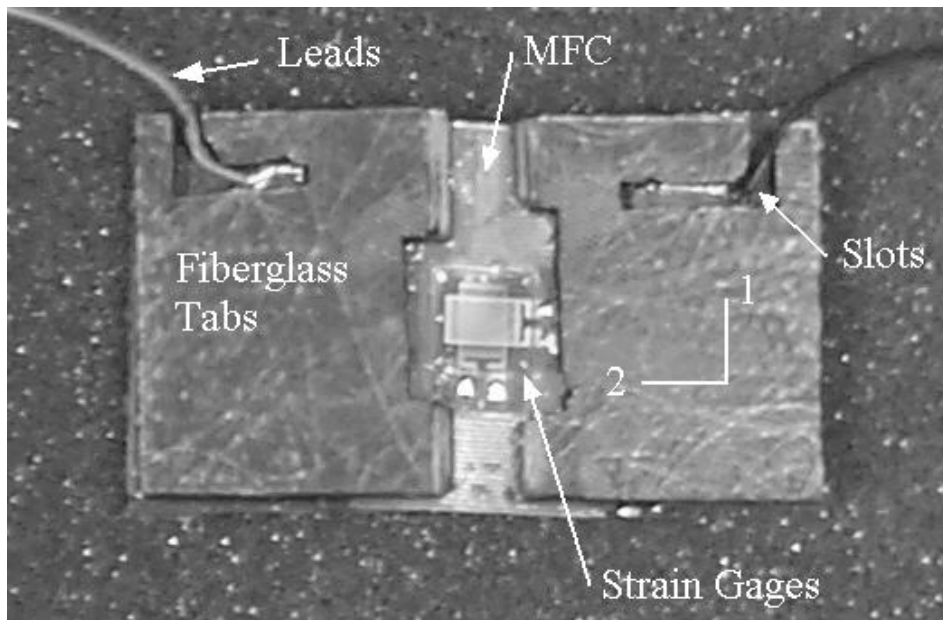


Figure 3. 2-Direction Test Specimen, with Fiberglass Tabs.

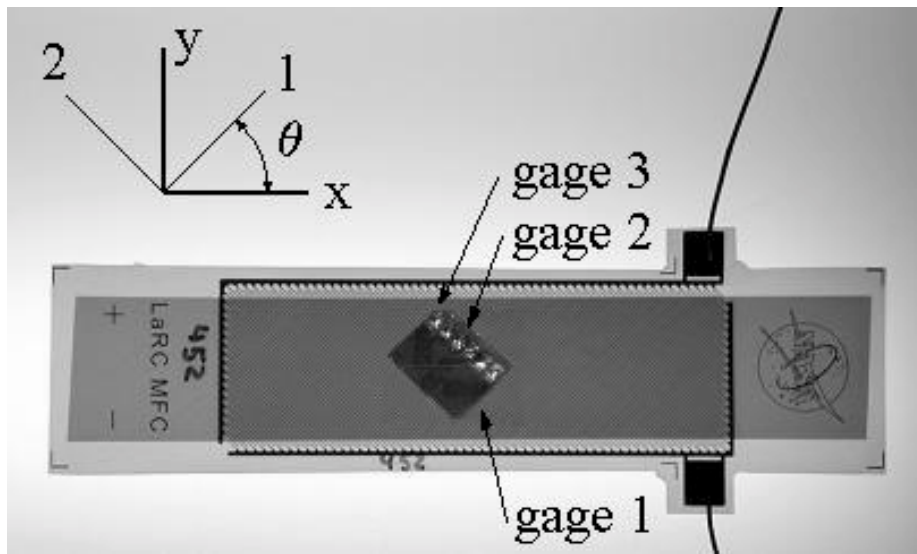


Figure 4. Off-Axis Tensile Specimen with PZT Fibers at $\theta = 45^\circ$.

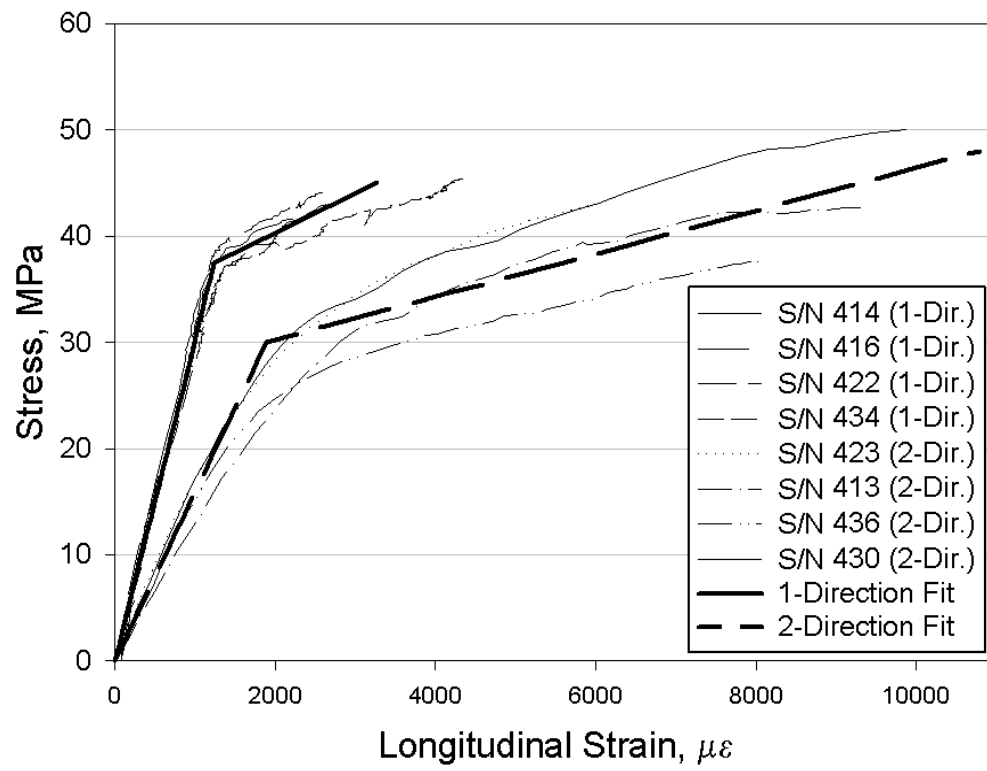


Figure 5. Elastic, Linear-Hardening Model and Experimental Data.

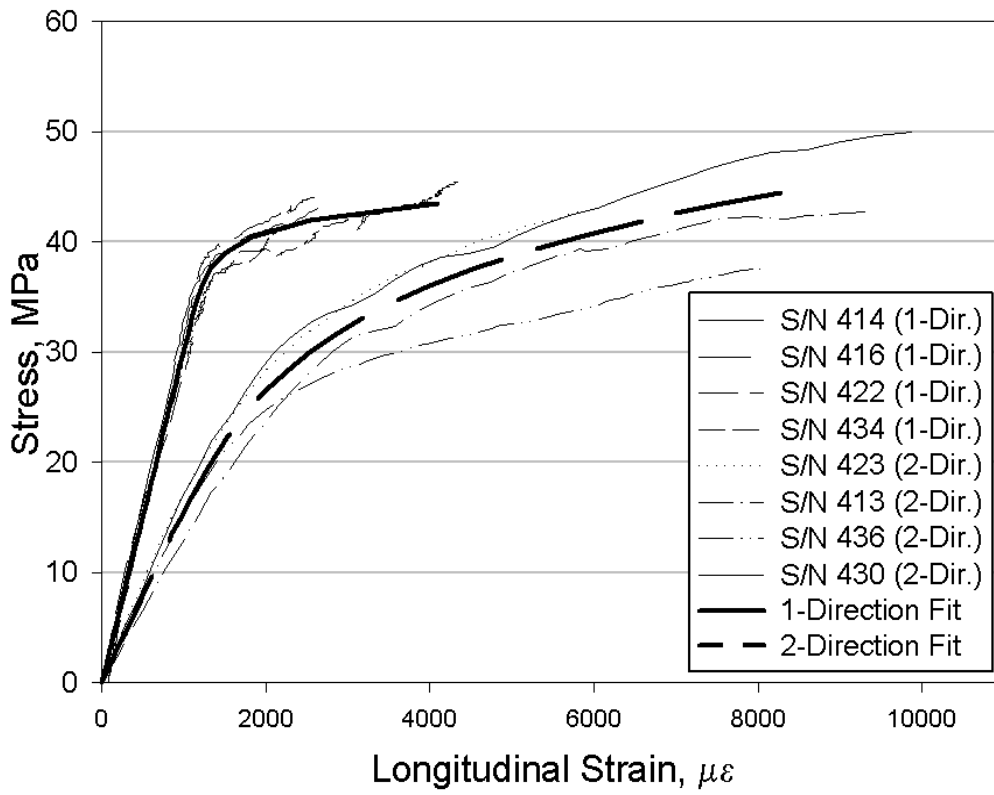


Figure 6. Ramberg-Osgood Model and Experimental Data for 1 and 2-Direction Stress-Strain Curves.

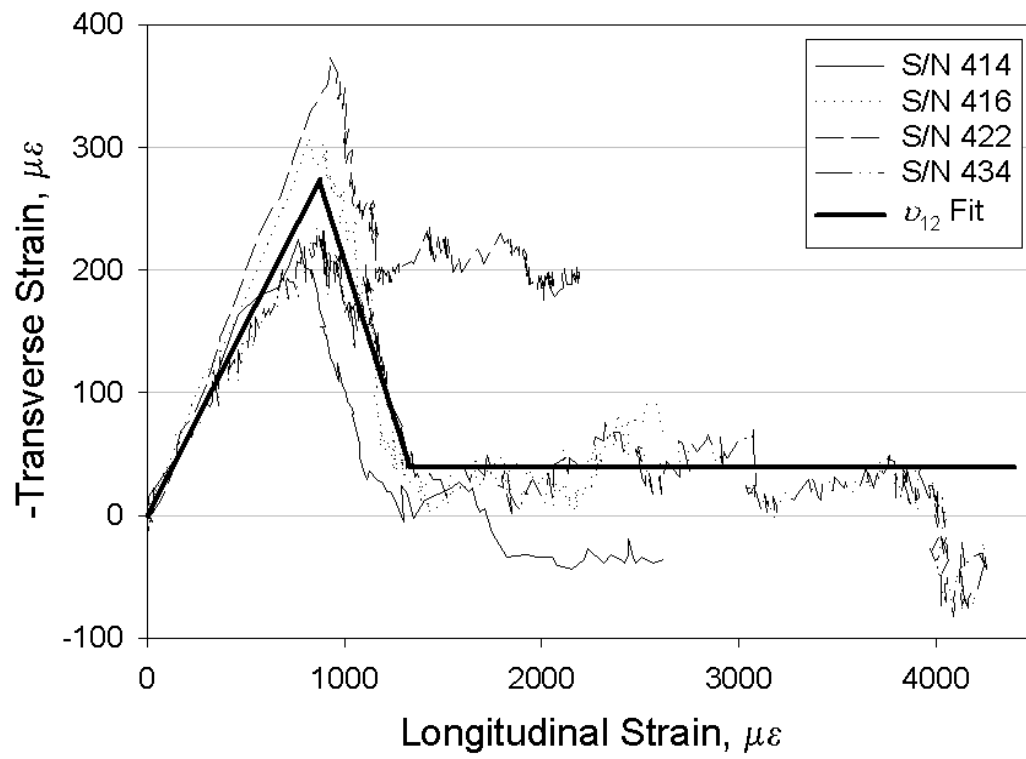


Figure 7. Minus Transverse Versus Longitudinal Strain for 1-Direction Test.

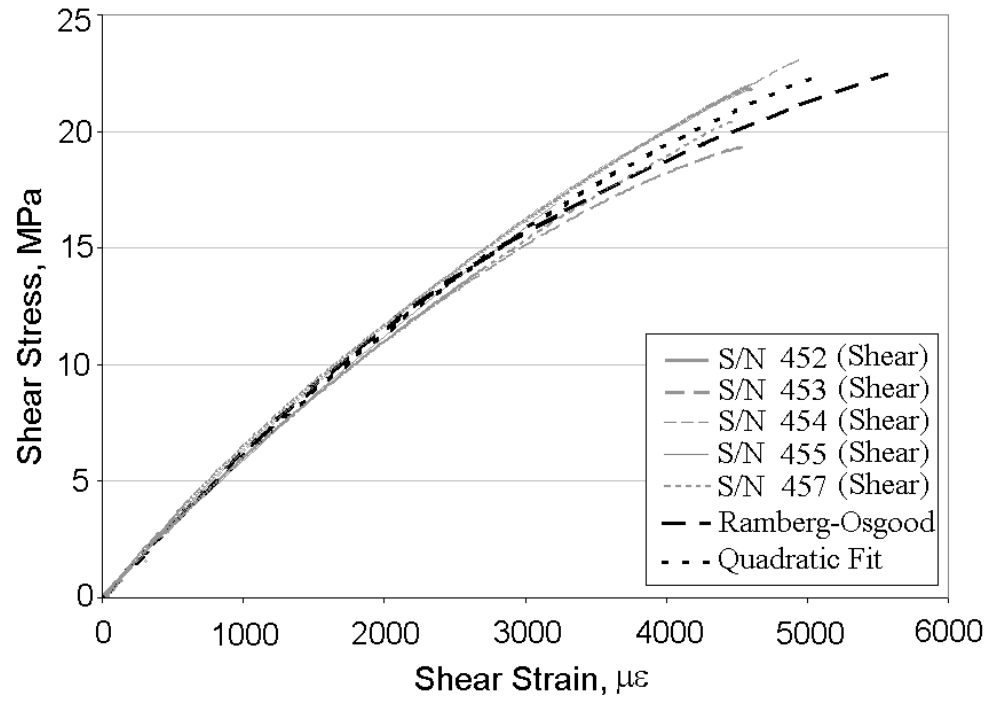


Figure 8. Ramberg-Osgood Model, Quadratic Least-Squares Fit and Experimental Data for Shear Stress-Shear Strain Curves.

Table 1. Elastic-Linear Hardening and Ramberg-Osgood Parameters for 1 and 2-Direction Behavior.

Test Type	E_i , GPa	$\delta_i E_i$, GPa	δ_i	σ_{oi} , Gpa	n_i	H_i , GPa	ν_{12}	Plastic ν_{12}	ν_{21}
1-Direction Test ($i = 1$)	30.21	3.689	0.1221	0.0363	0.0422	0.0559	0.3115	-0.5063	N/A
2-Direction Test ($i = 2$)	15.88	2.023	0.1273	0.0096	0.1849	0.1167	N/A	N/A	0.1638

Table 2. Ramberg-Osgood and Quadratic Regression Parameters for Shear Behavior.

G_{12} , GPa	n_{12}	H_{12} , GPa	C_1 , GPa	C_2 , GPa
6.2880	0.2681	0.1193	6.4899	-408.8660

Table 3. Predicted Orthotropic Elastic Constants From Classical Lamination Theory.

Test Type	E_i , GPa	E_i % Error	ν_{12}	ν_{12} % Error	ν_{21}	ν_{21} % Error	G_{12} , GPa	G_{12} % Error
1-Direction Test ($i = 1$)	28.75	-4.82%	0.2640	-15.25%	N/A	N/A	5.55	-11.74%
2-Direction Test ($i = 2$)	18.60	17.09%	N/A	N/A	0.1708	4.25%		