Comparison of Creep Behavior of UD and Woven CFRP in Bending

RUI MIRANDA GUEDES
MÁRIO A. VAZ

Department of Mechanical Engineering and Industrial Management (DEMEGI), Faculty of Engineering of University of Porto (FEUP), Porto, Portugal

ABSTRACT

Experimental results for creep bending tests of tape and twill woven carbon fiber-reinforced polymer (CFRP) laminates are presented and discussed. The aim of the research program was to characterize and compare the long-term behavior of both laminates. These materials will be included as structural elements in the construction of supports for delicate and precise radiation detection elements, so they need to be highly stable under any environmental conditions. The time dependency of the fiber-dominated properties of the CFRP laminates is negligibly small. Therefore the initial analysis pointed to better creep behavior of the twill-woven CFRP laminate and also predicted null creep strains. The experimental results show a different picture: the tape laminates performed better than the woven laminates, which exhibit larger creep strains than the initial guess.

The European Laboratory for Particle Physics (CERN) is building a new particle accelerator called the Large Hadron Collider (LHC). The LHC is an accelerator that brings protons and ions into head-on collisions at higher energies than ever achieved before. This will allow scientists to penetrate still further into the structure of matter and re-create the conditions prevailing in the early universe, just after the “Big Bang.” This is a big project with new challenges in many engineer fields, especially in the application of new materials. The DEMEGI-INEGI group is involved in the characterization of advanced composite materials that will be used to build the support structures of particle detectors. These structures should present high dimensional stability because small deviations from the initial position will lead to large errors in signal detection of the particles. A research program was established to determine the long-term behavior of two different composite materials. Environment-controlled bending creep tests of cantilever plates were used in order to compare the behavior of two different carbon fiber-reinforced polymers (CFRPs). Although bending tests do not produce uniform stresses and strains in the material and introduce, simultaneously, creep in bending and in plane stress relaxation, they are easier to perform than tensile tests. Nevertheless, the research team has acquired some experience in this type of test [1], with satisfactory results in recent years. On the other hand,
Table 1
Geometry of test specimens from plate
UC125 RNA $[0^\circ/45^\circ/90^\circ/\!-45^\circ]_2s$

| Specimen | A1  | A2  | A3  | A4  |
|----------|-----|-----|-----|-----|
| $h$ (mm) | 2.32| 2.36| 2.35| 2.32|
| $B$ (mm) | 48.87| 50.08| 50.42| 49.51|

bending loads are present in almost every real structure and are difficult, if not impossible, to avoid.

In design, when creep is considered, it is usual that the item should remain in service for an extended time, usually longer than it is practical to run creep experiments on the material to be employed. Thus, it is necessary to extrapolate the information obtained from relatively short-time laboratory creep tests, applying the time–temperature superposition principle (TTSP) to predict in-service behavior. Therefore this work is also a contribution to assess the application of the TTSP as a basis for a long-term behavior prediction.

MATERIALS
A brief presentation of the experimental test specimens is given. A set of 50 $\times$ 200 mm$^2$ specimens was obtained from composite plates fabricated at INEGI using the unidirectional UC125 RNA and the twill-woven CC194 RNA. The resin epoxy system (RNA) was the same for both laminates, having a 125°C cure temperature. The stacking sequence of the laminates and the geometry of test specimens are presented in Tables 1, 2, and 3, where $B$ represents the width and $h$ the thickness of each one.

MECHANICAL CHARACTERIZATION
The layer elastic properties had been already determined at the INEGI by conventional tests. Table 4 summarizes the results obtained for the two laminates.

The three-point bending (3PB) flexural test, as shown in Figure 1, was used to determine the bending modulus ($E_b$) and the bending stress strength ($\sigma_r$).

The geometry of the test follows ASTM Standard D790M except for the rate definition. In these tests a rate of 5 mm/min was used instead of the 12 mm/min recommended by the standard. The stiffness-to-strength ratio and the geometry of the laminates was chosen in order to obtain large deflections in 3PB tests. The ASTM standard recommends the

Table 2
Geometry of test specimens from plate UC125 RNA $[0^\circ/90^\circ]_4s$

| Specimen | B1  | B2  | B3  |
|----------|-----|-----|-----|
| $h$ (mm) | 2.32| 2.36| 2.35|
| $B$ (mm) | 48.87| 50.08| 50.42|
following formula to determine the rupture stress:

$$\sigma_r = \frac{3P \cdot L}{2B \cdot h^2 \left[ 1 + 6 \left( \frac{w_{\text{max}}}{L} \right) - 4 \left( \frac{h \cdot w_{\text{max}}}{L^2} \right) \right]} \quad (1)$$

In this formula $\sigma_r$ = rupture stress in outer fibers at midspan; $P$ = load at break; $L$ = load span; $B$ = width of the beam; $h$ = thickness of the beam; $w_{\text{max}}$ = deflection at the center of the span.

There is an exact solution for the large deflection of a simply supported beam with a central load [2], although an iterative numerical method is needed to obtain the solution, due to the nonlinear nature of the exact solution. On the other hand, an approximated solution was obtained based on the classical beam theory, denominated from now on as corrected linear theory (CLT).

The corrected bending moment at any cross section of the beam, using the referential system given in Figure 2, is

$$M = -\frac{P}{2} x - N \cdot y \quad (2)$$

with

$$\left\{ \begin{array}{l}
N = R \sin(\alpha) \\
R \cos(\alpha) = \frac{P}{2}
\end{array} \right\} \quad (3)$$

The equation for the deflection curve of the beam is

$$y'' = \gamma [-x - \tan(\alpha) \cdot y] \quad (4)$$

where

$$\gamma = \frac{1}{EI} \frac{P}{2}$$

Table 3
Geometry of test specimens from plate
CC194 RNA $[0^\circ]_{4s}$

| Specimen | C4 (mm) | C5 (mm) | C6 (mm) | C7 (mm) |
|----------|---------|---------|---------|---------|
| $h$      | 2.00    | 2.02    | 2.01    | 2.02    |
| $B$      | 48.91   | 49.22   | 48.28   | 47.26   |

Table 4
Layer elastic properties of the laminates

| Specimen   | $E_1$ (GPa) | $E_2$ (GPa) | $v_{12}$ | $G_{12}$ (GPa) |
|------------|-------------|-------------|----------|----------------|
| UC125 RNA  | 92.05       | 11.69       | 0.25     | 3.74           |
| CC194 RNA  | 53.57       | 53.57       | 0.058    | 3.82           |
with the following boundary conditions:

\[ y(0) = 0 \quad y\left( \frac{L}{2} \right) = 0 \]  \hspace{1cm} (5)

Then the solution of Eq. (4), satisfying the boundary conditions given by Eq. (5), is

\[ y = \frac{\sqrt{2}}{\sqrt{\gamma \cdot \tan(\alpha) \cdot \tan(\alpha)}} \cdot \sin[\sqrt{\gamma \cdot \tan(\alpha)}] - \frac{x}{\tan(\alpha)} \]  \hspace{1cm} (6)

The central deflection can be calculated using Eq. 6 for \( x = L/2 \):

\[ y \left( \frac{L}{2} \right) = \frac{\sqrt{2} \tan[\sqrt{\gamma \cdot \tan(\alpha)} \cdot L/2]}{\sqrt{\gamma \cdot \tan(\alpha) \cdot \tan(\alpha)}} - \frac{L}{2 \tan(\alpha)} \]  \hspace{1cm} (7)

Finally, the formula for the stress in the midspan outer fibers is obtained:

\[ \sigma_{r} = \frac{3P}{B \cdot h^2} \left( \frac{L}{2} + \tan(\alpha) \left[ \frac{\sqrt{2} \tan[\sqrt{\gamma \cdot \tan(\alpha)} \cdot L/2]}{\sqrt{\gamma \cdot \tan(\alpha) \cdot \tan(\alpha)}} - \frac{L}{2 \tan(\alpha)} \right] \right) \]  \hspace{1cm} (8)

The solution depends on the value of \( \tan(\alpha) \), which is obtained by solving the following nonlinear equation:

\[ y'(0) = \frac{1}{\tan(\alpha) \cdot \cos[\sqrt{\gamma \cdot \tan(\alpha)} \cdot L/2]} - \frac{1}{\tan(\alpha)} = \tan(\alpha) \]  \hspace{1cm} (9)

Large deflections of the beam are related to changes in the span length and to slippage of the specimens at the supports [3]. Change in the span length takes place as a consequence...
of rotation of the beam on the supports when large deformation is induced, as is described in Figure 3 for the case studied.

The changes in the distance $L$ can be estimated as follows:

$$ L \rightarrow L - 2a, \quad a = \frac{d + h}{2} \sin(\alpha) $$

(10)

where $d =$ diameter of pin support; $h =$ thickness of the specimen.

The comparison of exact theory (ET) and the CLT with the experimental results lead to a correction of the length change for both theories:

$$ L \rightarrow L - 2a, \quad \begin{cases} a = \frac{d + 2h}{2} \sin(\alpha) \quad \text{(ET)} \\ a = \frac{d - 2h}{2} \sin(\alpha) \quad \text{(CLT)} \end{cases} $$

(11)

These corrections are empirical, without geometric justification, and only proved to be valid for this particular case. The justification for the need of these empirical corrections can be given by two arguments. The first one is the change of the area where the central load is applied, which increases with the load. The second one is the accumulation of different types of internal damage in the laminates during the loading, which causes a decrease of the stiffness. In fact, the decrease of the stiffness has been used as a measure of the damage by many researchers, as referenced by Steif [4]. Obviously, the empirical corrections may compensate for the consequences of these phenomena.

In Figure 4 the experimental maximum deflections in function of the load are plotted and compared with the exact and corrected linear solutions.

In Figure 5 the calculated maximum stresses in function of the load are plotted for the exact theory (ET), correct linear theory (CLT), the ASTM equation, and the linear theory (LT). All cases match very well except, of course, the linear theory.

Only the exact solution can predict an instability condition; this results in the load reaching a maximum and falling thereafter [2] for

$$ P_{\text{crit}} \cdot \frac{L^2}{EI} = 6.72 $$

(12)

Using the previous analysis on the 3PB test results, the flexural properties were calculated and are presented in Tables 5, 6, and 7.

Compared to tape laminates UC125 $[0^\circ/90^\circ]_6$, quasi-laminar textile composites CC194 $[0^\circ]_6$ with equal volume fractions of in-plane fibers had slightly lower in-plane stiffness. This is explained by the tow waviness in the textile composites [5]. The rupture load for the UC125 test specimens was always lower than the critical load given by Eq. (12),
as shown in Figure 6. In the case of the CC194 test specimens the rupture load was very close to the critical load, pointing to a rupture due to geometric instability. As a consequence, the measured rupture stresses for the CC194 laminates are much lower than the value indicated by the supplier.

Alternatively, this methodology can be used to measure the damage by recalculating the modulus each time, instead of using the empirical correction given by Eq. (11). The modulus decay given by the ratio $E/E_b$, where $E$ is the instantaneous modulus, was calculated and is presented in Tables 5, 6, and 7.

**CREEP TEST APPARATUS**

Due to limitations on the temperature-control device inside the laboratory, an environmental chamber was used. Inside the chamber the temperature and the normalized humidity were maintained steady.
In Figure 7 the test apparatus is shown inside the environmental chamber. The maximum applied stresses were always lower than 10% of the rupture stress. A close view of the test apparatus with the respective geometry is also shown in Figure 8.

The creep extensions were measured using electrical strain gauges, HBM 10/350LY41. For the bending tests, the tensile and compression extensions were recorded close to the section of maximum bending moment. A personal computer-controlled data acquisition board, SOLARTRON 3595, was used to automatically record the experimental data.

The conventional foil electrical resistance strain gauges used in these tests displayed the required sensitivity characteristics but were unstable over the long term. In order to smooth out extraneous noise without altering the data characteristics and compact data into a smaller set, the following procedure [6] was used:

1. For each decade, in logarithmic time scale, the data were divided into 10 equal time intervals.
2. The data in each time interval were fitted into a single-term exponential,

$$\varepsilon = A \cdot e^{B \cdot t}$$  \hspace{1cm} (13)

where $\varepsilon$ = the strain response, $t$ = time, $A$ and $B$ = best-fit constants by the least-squares method.
3. The entire $n$ data points in each time interval were replaced by $\bar{\varepsilon}$ and $\bar{t}$:

$$\bar{t} = \frac{1}{n} \sum_{i=1}^{n} t_i$$  \hspace{1cm} (14)

$$\bar{\varepsilon} = A \cdot e^{B \cdot \bar{t}}$$

| Specimen | B1 | B2 | B3 | Average |
|----------|----|----|----|---------|
| $E_b$ (GPa) | 46.76 | 46.72 | 48.72 | 47.40 |
| $\sigma_r$ (MPa) | 850 | 850 | 865 | 855 |
| $E/E_b$ (%) | 93.5 | 91.2 | 92.4 | 92.4 |
Table 7

| Specimen | C1   | C2   | C3   | Average | Supplier |
|----------|------|------|------|---------|----------|
| $E_b$ (GPa) | 41.94 | 42.50 | 42.16 | 42.20   | 50       |
| $\sigma_r$ (MPa) | 717    | 727   | 731   | 725     | 900      |
| $E/E_b$ (%)  | 92.5   | 91.4  | 91.6  | 91.8    |          |

**DYNAMIC-MECHANICAL-THERMAL-ANALYZER (DMTA) TEST RESULT**

The characterization of the mechanical properties of polymeric materials on a DMTA analyzer gives the complex compliance ($S^*$) as a function of angular frequency $\omega$. In order to characterize the viscoelastic material behavior in a very broad range of frequencies (or times), it is necessary to combine measurements at several temperatures applying the time-temperature superposition principle (TTSP). According to this principle, a given property measured for short times must be identical with one measured for longer times at a lower temperature, so the curves are shifted parallel to the horizontal axis, matching to form a master curve.

The dynamic test specimens were cut out from plates with three different stacking sequences. The dynamic tests were carried out in a cantilever beam apparatus. The specimens had a thickness of 2.2 mm, a width of 10.0 mm, and a distance between the support and the load application point of 22.0 mm.

A brief description of the methodology used to obtain the creep master curves [7] is given. For each temperature level, the frequencies varied from 0.1 to 100 Hz. The maximum imposed deflection was 64 $\mu$m, and the temperature levels were within the range 20–135°C. Following the frequency–time transformation procedure, the short-term compliance curves in time domain were obtained. Then the data were shifted for each temperature to a reference temperature, $T_{ref}$, using the time–temperature superposition principle (TTSP) to build up the master creep compliance curve as shown in Figure 9.

![Figure 6](image.png)

**Figure 6.** Comparison of rupture and critical load for the 3PB rupture tests.
Figure 7. Test apparatus inside the environmental chamber.

Figure 8. Test geometry and position of the strain measuring point.

Figure 9. Normalized master creep compliance curves.
Many authors have already discussed the inaccuracy of the simple power law to model the whole master curve. The simple power law is defined as follows:

$$S(t) = S_0 + S_1 \left( \frac{t}{\kappa_0} \right)^n$$

where $S_0$ = time-independent initial compliance, $S_1$ = coefficient of the time-dependent compliance, $n$ = material constant, and $\kappa_0$ = unit reference time.

Many polymers, such as cross-linked polymers, deform in an asymptotic form, from glassy to rubber-like behavior. In the literature there are many expressions of the time-dependent compliance that can describe the viscoelastic phenomena over a broad range. In this study the response function [8] chosen to fit the master curve was given by

$$\frac{S(t)}{S_0} = 1 + \frac{S_\infty - S_0}{S_0} w(t)$$

where $w(t)$ is the Cole-Cole function,

$$w(t) = \frac{1}{1 + (\kappa_0/S_0)^n}$$

where $S_0$ = time-independent initial compliance, $S_\infty$ = time-independent compliance at infinite time, $n$ = material constant, and $\kappa_0$ = unit reference time.

The master curves were fitted to the Cole-Cole function, but it was found that the curve fitting did not have good agreement in the longer time range. The curve fitting and the fitting results are shown in Figure 9 and in Table 8.

The discrepancy in the longer time range was related to the shape of the master compliance curves. Although the master curves show a decrease of creep at longer times, it was clear that it did not reach a limit value as the Cole-Cole function predicts. Nevertheless, if the working temperatures are lower than 50°C, then the longer time range, 106–1014 h, represents very long times, i.e., 114 and 11.4 billions of years, respectively. Hence, in this case, it is perfectly reasonable to use the simple power law to predict long-term behavior in a human time scale, i.e., 50 years.

### Creep test results

The creep compliance is defined as

$$S(t) = \frac{\epsilon(t)}{\sigma_0}$$

where $\epsilon(t)$ = strain measured, $\sigma_0$ = constant stress imposed, and $S(t)$ = creep compliance.
Figure 10. Normalized creep compliance for the UC125 RNA \([0^\circ/90^\circ]_{4s}\) test specimens.

For a better comparison of all test specimens, the normalized creep compliance was used instead of the creep compliance. The normalized creep compliance is defined as

$$S_R(t) = \frac{S(t)}{S_0}$$

where \(S(t)\) = creep compliance, \(S_0\) = instantaneous compliance, and \(S_R(t)\) = normalized creep compliance.

After almost 1,800 h of bending creep test of specimens made of UC 125 RNA at 23°C and 50% RH, no significant creep strains were detected within the strain gauges’ accuracy, i.e., \(\pm 20\) μm.

These results motivated the decision to increase the temperature to 50°C instead of increasing the loading stress, with two purposes: accelerate the viscoelastic behavior and avoid the nonlinear behavior.

In Figures 10 and 11, the creep test results are plotted with the simple power law predictions (Findley) and the master creep curve (DMTA). All experimental results are in tension except B2(C) and C5(C), which are in compression. The Findley or simple power law was already defined by Eq. (15).

Figure 11. Normalized creep compliance for the CC 194 RNA \([0^\circ]_{4s}\) test specimens.
These experimental results, representing more than 2,500 h, revealed an increase of the creep compliance around 6.7% on average for the CC194 and around 2.5% on average for the UC125. Surprisingly, the creep tests pointed out that the CC194 has a larger increase of creep compliance than the UC125. For continuous carbon fiber composites, the time dependency exhibited by the fiber-dominated mechanical properties $E_{11}$, $v_{12}$ is negligibly small, when compared to the highly time-dependent matrix-dominated properties. Then, for these stacking sequences no significant creep was expected due to the existence of fibers in the loading direction.

At this stage some conclusions can be addressed. The results showed a large discrepancy between the DMTA and the bending creep, which apparently have distinct behaviors. In fact, each test involved specimens with different dimensions, but the scale factor alone could not explain such large deviations. On the other hand, the Findley equation prediction for the CC194 specimens, based on the first 100 h, show good agreement with the experimental data until 1,000 h were reached. After this time the creep data show a large decrease of the creep rate, apparently diverging from the power law. As for the UC125 specimens, the predictions of the Findley are in good agreement with the experimental data, certainly due to the very low creep level presented. Having this in mind, a new model was developed.

### Analysis of Experimental Results Using a New Model

The new model was developed after a careful analysis of the DMTA test apparatus. It was not difficult to conclude that the cantilever beam should be considered a short beam. Therefore it becomes obvious that the DMTA results were related not only to the compliance but also to the shear compliance of the laminate. For that very reason the properties of the viscoelastic matrix could only be determined approximately.

The Poisson ratio $\nu^m$ of the viscoelastic matrix was considered constant and equal to 0.25. The creep compliance $S^m = S^m_{11} = S^m_{22}$ and the shear creep compliance $S^m_{66}$ of the matrix were calculated using some approximations. In this case it was considered that the DMTA test measured, directly, the shear creep compliance of the laminate, admitting that the influence of the creep compliance of the laminate could be ignored, i.e., $S_{11}(t) \approx 1/E_b$. Equation (20) gives the relation between the creep compliance, obtained directly from DMTA tests, and the shear creep compliance of the laminate. Equation (20) was obtained by comparison of the cantilever beam solution with and without the shear correction.

$$S_{66}(t) = \frac{1}{6} \left( \frac{L}{h} \right)^2 \left[ \tilde{S}(t) - \frac{1}{E_b} \right]$$  \hspace{1cm} (20)

where $L =$ length of the beam, $h =$ thickness of the laminate, $\tilde{S}(t) =$ creep compliance obtained from the DMTA, and $E_b =$ bending modulus of the laminate considered linear elastic.

The micromechanics formulas based on the Halpin-Tsai equations have been adapted and used for viscoelastic analysis [9, 10]. Based on such analysis, assuming the fibers linear elastic and the woven composite as a multidirectional laminate consisting of the fiber angles, the relationship between shear creep compliance of the matrix, $S_{66}^m(t)$, and laminate shear creep compliance, $S_{66}(t)$, was obtained as follows:

$$S_{66}^m(t) = \frac{1}{2(1 - v_f)} \left[ [S_{66}(t) - S_{66}^f] \cdot (1 + v_f) + \{[S_{66}(t)]^2 \cdot (1 + v_f)^2 \right. \nonumber$$

$$+ S_{66}(t) \cdot S_{66}^f \cdot (2 - 12v_f + 2v_f^2) + \left( S_{66}^f \cdot (1 + v_f)^2 \right)^{1/2} \right)$$  \hspace{1cm} (21)
where $v_f$ = the volume fraction of the fiber, and $S_{66}^f$ = fiber shear compliance considered linear elastic and equal to 22 GPa [5].

Finally, using a relation applied successively elsewhere [10], the creep compliance of the matrix was determined using Eq. (22):

$$S^m(t) = \frac{S^m_{66}(t)}{2(1 + v^m)}$$

A nonlinear viscoelastic analysis of the carbon fiber matrix interphase has already been presented by another researcher [11]. The present approach considers a viscoelastic interlayer between successive plies of the laminate, i.e., a resin-rich interlayer zone with the same properties as the resin, with a thickness to be determined, as shown in Figure 12.

Introducing the geometry and properties of the laminates into the LAMFLU program [12], it was possible to determine approximately the resin interlayer thickness, unknown at this stage, for the UC125 and CC194 laminates using the first 100 h of experimental results. The results for the interlayer thickness, see Figure 12, are presented in Table 9.

The values determined for the interlayer thickness reveal that the twill-woven laminate (CC194) has a larger resin interlayer, i.e., 44% of the ply thickness, than the tape laminate (UC125).

Compared to tape laminates, woven composites with the same volume fractions of in-plane fibers usually have slightly lower in-plane stiffness because of the tow waviness. Therefore, axial shear stresses are higher under nominally aligned loads [5]. The tow waviness and the higher shear stress could justify the larger resin interlayer thickness of the CC194 in the present model.

The analysis of delamination in angle-ply composites led some researchers [13] to model the composite laminate as an assembly of anisotropic homogeneous plies bonded by thin resin interlayers. The interlaminar resin layer was considered an isotropic material with a uniform thickness of one-tenth of the individual ply thickness as observed under a microscope [14].

In the present case no microscopic observations were made, but it was clear that the present model was a crude simplification of the real resin interlayer of the CC194 laminate.

### Table 9

| Dimensions (mm) | $h_p$ | $h_i$ | $h_p/h_i$ (%) |
|-----------------|-------|-------|---------------|
| UC125           | 0.1438| 0.0296| 21            |
| CC194           | 0.2500| 0.1086| 44            |
Probably the interlayer thickness that was determined for the present model reflected the influence rather the real geometry of the viscoelastic interlayer.

In Figures 13 and 14 the predictions of the present model (Model) and of the simple power law (Findley) are plotted with the averaged experimental data (Exper.). The predictions made by LAMFLU (Model) compared reasonably well with the experimental results. The model predicts a limit to the creep deformation and the experimental data appear to follow the same trend.

The laminate plies were considered linear elastic for the CC194 and the UC125. In fact the UC125 is not really unidirectional, i.e., it contains glass fibers in the weft direction, about 20% by weight. Therefore the viscoelastic interlayers suffer creep and stress relaxation simultaneously and as a consequence originate load transfer to the adjacent layers. After a certain period of time an equilibrium state is reached and the creep deformation is arrested.
CONCLUSIONS

The methodology to design highly stable structures followed by CERN led us to choose CFRP laminates as structural elements. The creep behavior of a quasi-laminar textile and an equivalent tape laminate was compared.

The initial analysis pointed to the twill-woven laminate as a better choice on a long-term-behavior basis. The experimental creep tests showed a different picture: the tape laminates exhibited lower creep strains than the twill-woven laminates and proved to be more stable. The simple power law was adequate to fit the UC125 and CC194 experimental data for the first 100 h. The model extrapolations for the UC125 laminates showed good agreement with the experimental results. For the CC194 laminates the model extrapolation showed good agreement until 1,000 h were reached; after this the model diverged from the experimental results. The experimental data for the CC194 laminates showed an evolution of the creep strain to a limit value near 2,000 h.

One simple explanation for CC194 behavior, which came out initially, was found on the reinforcement tissue itself. The overlap of the transverse and longitudinal fibers (twill-woven) could produce plies with fibers not stretched. The tensile stresses and the viscoelastic matrix then promoted the stretching of the fibers. This phenomenon should not happen in unidirectional reinforcements. Nevertheless, this paradigm did not seem satisfactory to explain the experimental results.

The master creep curves obtained from dynamic mechanical and thermal analysis (DMTA) were very similar for both laminates UC125 RNA $[0^\circ/90^\circ]_{4s}$ and CC194 RNA $[0^\circ]_{4s}$. The master curves showed an increase of the creep compliance (to a limit value?) between 16 and 19 times the initial value. Further, the master curves indicated, as shown in Figure 9, that it takes an absurd time to reach this limit at 50°C, i.e., millions of years!

On the other hand, the comparison of master creep curves and creep test results showed a large discrepancy. A new interpretation of the DMTA results was done. This allowed the determination of the matrix viscoelastic properties using micromechanic analysis. The introduction of a viscoelastic interlayer between plies into the laminate model resulted in a new approach. The model developed explained the experimental results and permitted a long-term prediction of the creep bending response of the UC125 RNA $[0^\circ/90^\circ]_{4s}$ and CC194 RNA $[0^\circ]_{4s}$ laminates based on time-temperature superposition principle.

REFERENCES

[1] R. M. Guedes, A. T. Marques, and A. H. Cardon, Creep/Creep-Recovery Response of Fibredux 920C-TS-5-42 Composite under Flexural Loading, *Appl. Composite Mater.*, vol. 6, pp. 71–86, 1999.
[2] J. Williams, *Stress Analysis of Polymers*, pp. 160–173, Ellis Horwood, England 1980.
[3] Y. M. Tarnopols'skii and T. Kincis, *Static Test Methods for Composites*, chap. 5, pp. 220–263, Van Nostrand Reinhold, New York, 1985.
[4] P. S. Steif, Stiffness Reduction due to Fiber Breakage, *J. Composite Mater.*, vol. 17, no. 2, pp. 152–172, 1983.
[5] B. Cox and G. Flanagan, Handbook of Analytical Methods for Textile Composites, Tech. Rep. 4750, NASA, 1997.
[6] E. M. Wu, N. Q. Nguyen, and R. L. Moore, Matrix-Dominated Time-Dependent Deformation and Damage of Graphite/Epoxy Composite Experimental Data under Ramp Loading, Tech. Rep. AFWAL-TR-82-3076, Lawrence Livermore Natl. Lab., Livermore, CA, 1982.
[7] R. M. Guedes, A. T. Marques, and A. H. Cardon, Creep or Relaxation Master Curves Calculated from Experimental Dynamic Viscoelastic Function, *Sci. Eng. Composite Mater.*, vol. 7, no. 3, pp. 259–267, 1998.
[8] Q. Yang, Nonlinear Viscoelastic-Viscoplastic Characterization of a Polymer Matrix Composite, Ph.D. thesis, Free University of Brussels (V.U.B.), 1996.
[9] S. W. Beckwith, Viscoelastic Characterization of a Nonlinear, Glass/Epoxy Composite Using Micromechanics Theory, in JANNAF, Structures and Mechanical Behavior Working Group Meeting, San Francisco, CA, February 1975.
[10] A. Horoschenkoff, Characterization of the Creep Compliances J22 and J66 of Orthotropic Composites with PEEK and Epoxy Matrices Using the Nonlinear Viscoelastic Response of the Neat Resins, *J. Composite Mater.*, vol. 24, pp. 879–891, August 1990.
[11] E. Sancaktar and P. Zhang, Nonlinear Viscoelastic Modelling of the Fiber-Matrix Interphase in Composite Materials, *J. Mech. Design*, vol. 112, pp. 605–619, 1990.
[12] R. M. Guedes, A. T. Marques, and A. H. Cardon, Analytical and Experimental Evaluation of Nonlinear Viscoelastic-Viscoplastic Composite Laminates under Creep, Creep-Recovery, Relaxation and Ramp Loading, *Mech. Time-Dependent Mater.*, vol. 2, pp. 113–128, 1998.
[13] S. Wang, An Analysis of Delamination in Angle-Ply Fiber-Reinforced Composites, *Trans. ASME*, vol. 47, pp. 64–70, 1980.
[14] C. Wu, Nonlinear Analysis of Edge Effects in Angle-Ply Laminates, *Comput. Struct.*, vol. 25, no. 5, pp. 787–798, 1987.