

Inverse procedure for mechanical characterization of multi-layered non-rigid composites parts with applications to the assembly process

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Abstract

In assembly process, non-rigid parts in free-state may have different shapes compared to the designed model due to geometric variations, gravity load and residual stresses. For non-rigid parts made by multi layered fiber-reinforced thermoplastic composites, the assembly process becomes much more complex due to the nonlinear behavior of the material. This paper presented an inverse procedure for characterizing large anisotropic deformation behavior of four-layered, carbon fiber reinforced polyphenylene sulphide, non-rigid composites parts. Mechanical responses were measured from the standard three points bending test and the surface displacements of composite plates under flexural loading test. An orthotropic hyperelastic material model was implemented in the user-defined material subroutine of Abaqus for finite strain shell elements to analyze the behavior of flexible fiber-reinforced thermoplastic composites. Error functions were defined by subtracting the experimental data from the numerical mechanical responses. Minimizing the error functions helps to identify the material parameters. These material parameters were validated for the case of an eight-layered composite material.

Keywords: Finite element method; Finite strain shell element; Anisotropic material model; Fiber-reinforced composites; Assembly process.

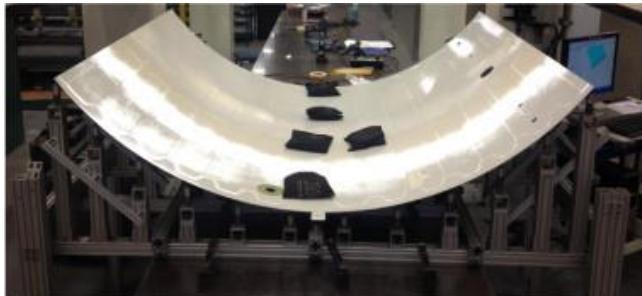
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23 **1. Introduction**

24 One important element in the quality control of products is to analyze whether the post-assembly
 25 shape fulfills the designer's geometric specifications. This task is performed by evaluating the
 26 component's shape after having mounted it into the final assembled configuration. Large non-rigid
 27 parts in *free-state*, such as aerospace panels may have deviations from their nominal (CAD) shape
 28 caused by geometric variations, gravity load and residual stresses. It makes the assembly process
 29 difficult, even impossible when the deviation is out of tolerance. To solve this problem, a non-rigid
 30 part must be mounted on special fixtures to simulate the *assembly-state*. Then, the mismatches
 31 between its real geometry and its target nominal geometry are evaluated. This process is usually
 32 carried out by using coordinate measuring systems or laser scanners. Figure 1 shows an example of
 33 an aerospace panel restrained by known forces (weight) on its inspection fixture before the
 34 measurement process. This inspection task is generally laborious and time-consuming. Therefore,
 35 there is a great interest in the industry towards developing virtual inspection methods, which can
 36 significantly reduce inspection time and cost.



38 *Figure 1. An aerospace panel restrained by known forces on its inspection fixture [1]*

39 Some researchers [2-5] proposed virtual inspection methods based on numerical approaches by
 40 building a finite element (FE) model of the non-rigid part considered in free-state. Boundary
 41 conditions were then imposed on this FE model to constrain the part to its working shape. The virtual
 42 restrained part was compared to the nominal CAD model to evaluate profile deviation. Obviously,
 43 the accuracy of numerical simulation is one of the most important aspects of a virtual inspection
 44 process. The deformation of non-rigid parts must be simulated accurately by finite element analysis
 45 which requires good material models and an appropriate characterization method for assessing
 46 material parameters associated with these models. For a non-rigid composite part, this work becomes

47 highly challenging because of its complicated anisotropic nonlinear behavior. Thin orthotropic
48 materials, such as fiber reinforced composites, can be described by a lamina constitution with four
49 independent in-plane elastic parameters (longitudinal Young's modulus E_1 , transverse Young's
50 modulus E_2 , in-plane shear modulus G_{12} and Poisson's ratio λ_{12}) [6-8]. However, during its
51 assembly process, a non-rigid part undergoes large deformation, which makes that material
52 constitutive properties change considerably. As a consequence, this model is no longer valid for
53 assembly processes. Instead, hyperelasticity provides a framework for modelling large anisotropic
54 deformation. According to the Lagrangian description, the constitutive properties of the material
55 (stiffness) varies with the gradient of deformation and the anisotropic effect is characterized by the
56 fiber's reorientation. This framework was successfully used in characterizing the behavior of fiber-
57 reinforced composites. Pham et al. [9], Aimene et al. [10], Peng et al. [11] and Gong et al. [12]
58 proposed hyperelastic constitutive material models and demonstrated their suitability for modelling
59 large anisotropic deformation of fiber reinforced composites in manufacturing processes. Therefore,
60 a hyperelastic constitutive model in the form of a strain energy function could be an appropriate
61 approach to characterize the anisotropic behavior of flexible fiber-reinforced thermoplastic
62 composites (FRTPC) during assembly processes.

63 Even if a very well-suited constitutive model is chosen, determining accurate material parameters
64 is also one of the most important prerequisites in order to obtain reliable results from an assembly
65 process simulation. Therefore, parameters of the constitutive model considered must be estimated by
66 the most appropriate method. Simulation-based inverse characterization is a powerful and efficient
67 tool to characterize the mechanical behavior of materials. This procedure is based on an optimization
68 process that minimizes a multi-objective function that expresses discrepancies between experimental
69 data of characterization tests and computed responses for these tests. Here, the computed responses
70 are used as "function evaluation" and the material parameters employed in the numerical model are
71 the variables to be determined in this optimization process. Over the past decades, a number of
72 researchers used the inverse procedure to get the constitutive material behaviors from standard tests
73 such as tensile tests, compression tests, bending tests, torsion tests, etc. [13-18]. However, the
74 deformation fields generated from the standard tests in many cases cannot represent the complex
75 deformation fields of some particular applications. In a previous study [19], the bending properties

of multi-layered carbon fiber reinforced polyphenylene sulphide (CF/PPS) were obtained from three-point bending tests, but results showed that this approach needs to be improved to better characterize the behavior of non-rigid composite parts during the assembly process. To overcome this problem, non-standard experiments were developed by some researchers to be able to more accurately capture real deformation behaviors. Wang et al. [20] proposed an inverse method to determine elastic constants using a circular aluminum disk. Pagnotta [21] identified the elastic properties of materials from displacements of a thin, simply supported isotropic square plate. Bruno et al. [6] presented a method for identifying the elastic properties of aluminum and unidirectional Graphite/PEEK laminate from measurements of the displacements of plates under loading configurations. It can be seen that the selection of a test type greatly affects the accuracy of characterization. During assembly, the complexity of a non-rigid composite part behavior cannot be well characterized using a single experimental test. Obviously, the combination of results obtained from both standard and non-standard tests could lead to a more realistic description of material behavior.

In this study, in order to characterize the large bending behavior of multi-layered CF/PPS during the virtual assembly process, an inverse multi-objective optimization process combining standard and specific non-standard tests was developed. Four-layer CF/PPS sheet specimens were used for characterization. Three-point bending tests with two different stacking sequences $[0, 90]_4$ and $[\pm 45]_4$ were performed as the standard method. Flexural loading tests with a large multi-layer composite sheet in different support configurations were carried out as non-standard tests. These non-standard tests were chosen because the deformation state in these tests is close to that in the assembly process. Therefore, the characterized material properties obtained from the combination of experimental data from the three-point bending test and flexural loading tests is more appropriate to represent the behavior of non-rigid composite parts during the assembly process. For the optimization procedure, numerical simulations were performed using an orthotropic hyperelastic shell formulation, which is available in ABAQUS. In this work, the anisotropic hyperelastic material model developed by Vu et al. [19] was used and implemented in ABAQUS as a user-defined material model (UMAT). The material parameters obtained from the inverse characterization procedure were validated for the case of an eight-layered CF/PPS material.

104 **2. Experimental work**

105 In this study, the thermoplastic composite used for the experimental works was a pre-consolidated
 106 plate of 4 layers of CF/PPS commercialized by Royal Tencate Corp. In each layer, a polyphenylene
 107 sulphide (PPS) matrix was reinforced by two orthogonal families of carbon fiber (CF). The fiber
 108 volume fraction (V_f) was of 50%. The total thickness of the four-layer laminate was approximately
 109 1.24 mm (0.31 mm / layer).

110 **2.1 Three-point bending test**

111 Specimens with two different stacking sequences $[0, 90]_4$ and $[\pm 45]_4$ with dimensions 300 mm \times
 112 34 mm \times 1.24 mm were used for the three-point bending test. The tests were performed on the MTS
 113 testing machine. Table 1 shows the test parameters. Each specimen (5 pieces for each stacking
 114 sequence) was supported on two rollers and loaded in its center with displacement control (Figure
 115 2). The applied force and the displacement at the center of the specimen were then recorded.



116 (a)

117 (b)

118 *Figure 2. (a) Test specimens and (b) three point bending test using the MTS machine.*

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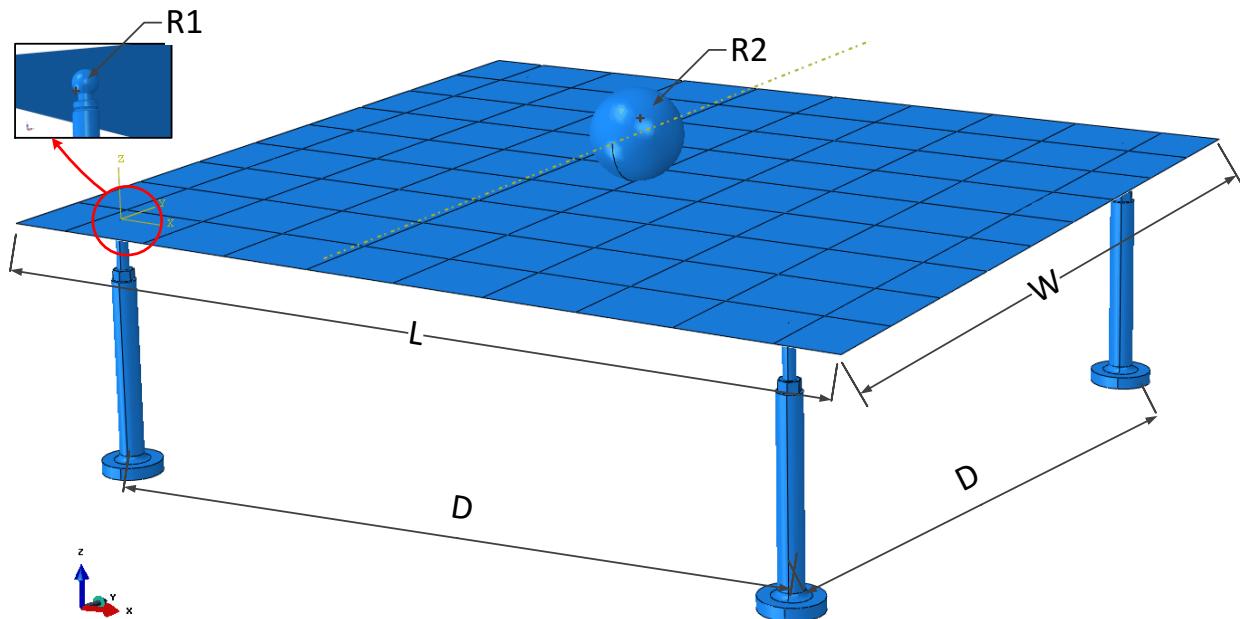
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125 *Table 1. Test parameters*

Parameters	Value
Specimen dimensions	300 mm × 34 mm × 1.24 mm
Support span	140 mm
Velocity	4 mm/min
Max displacement at center of support span	20 mm
Radius of loading noses and supports	25 mm

126 2.2 Flexural loading test

127 Figure 3 shows a schematic of the flexural loading test used. A plate with dimensions 930 mm ×
 128 890 mm was supported by a system of four rigid spherical-head supports. Two different
 129 configurations of support systems were used in this test. This plate then underwent bending
 130 deformation imposed by a 3.63 kg ball applied at its center point. Table 2 summarizes experimental
 131 parameters.



132
 133 *Figure 3. Flexural loading test schematic*
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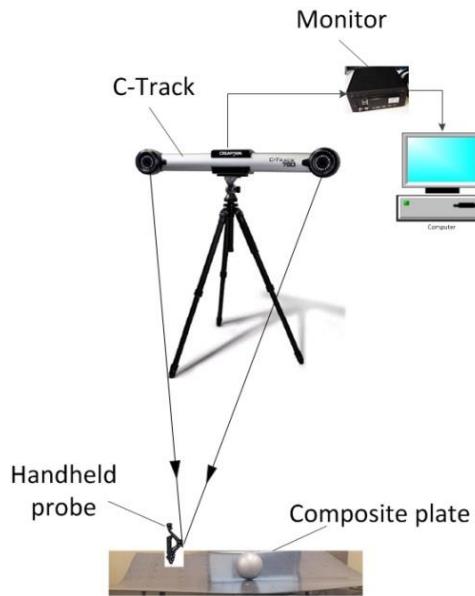
135 *Table 2. Experimental parameters*

Composite plate	
Material	CF/PPS
Stacking sequence	$[0, 90]_4$
Plate dimensions	L = 930 mm, W = 890 mm
Support system	
Radius of sphere-head supports	$R_1 = 6.35$ mm
Distance between supports	Configuration 1: D = 762 mm Configuration 2: D = 660.4 mm
Steel ball	
Radius of steel ball	$R_2 = 49.5$ mm
Weight of steel ball	3.63 kg

136 By using a Creaform HandyPROBE device, the displacement of the plate was measured at 110 points
 137 on the plate surface. Three measurements were performed for each support configuration to get
 138 average values. The HandyPROBE device consists of a tracking system equipped with a C-track and
 139 a handheld probe as shown in Figure 4. The triangulation obtained from two video cameras on the
 140 C-track device and the retroreflective target of the handheld probe were used to calculate the
 141 coordinates of each point.

142 The accuracy of the measurement is limited by uncertainty of the support system as well as the
 143 measurement process. In this work, uncertainty of the support system is approximately ± 0.03 mm
 144 and ± 0.02 mm for Configuration 1 and Configuration 2, while uncertainty of the measurement
 145 process is up to ± 0.2 mm.

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147

148 *Figure 4. Experimental apparatus*149 **3. Modelling**150 **3.1 Material model**

151 The hyperelasticity modeling concept is based on the existence of a strain energy function using
 152 the Lagrangian variables, which are appropriate for the description of large deformations. The
 153 mechanical behavior of a thermoplastic reinforced by two families of fiber can be represented by a
 154 strain energy function of the right Cauchy-Green deformation tensor $\mathbf{C} = \mathbf{F}^T \mathbf{F}$ and the initial fiber
 155 directional unit vectors \mathbf{a}_0 and \mathbf{g}_0 :

$$156 \quad \Psi = \Psi(\mathbf{C}, \mathbf{a}_0, \mathbf{g}_0) \quad (1)$$

157

158 where $\mathbf{F} = \partial \mathbf{x} / \partial \mathbf{X}$ is the deformation gradient. \mathbf{X} represents the position vector of each point of the
 159 solid body in the reference configuration, and \mathbf{x} represents the position vector of the corresponding
 160 point in the current configuration. For an orthotropic hyperelastic model, the initial directions of the
 161 two fibers are orthogonal, i.e. $\mathbf{a}_0 \perp \mathbf{g}_0$ and the strain energy function in Equation (1) can be written
 162 in terms of invariants of \mathbf{C} as:

$$163 \quad \Psi = \Psi(I_1, I_2, I_3, I_4, I_6) \quad (2)$$

164 where $I_1 = \text{tr}(\mathbf{C})$, $I_2 = \frac{1}{2} [\text{tr}(\mathbf{C})^2 - \text{tr}(\mathbf{C}^2)]$, $I_3 = \det(\mathbf{C})$, $I_4 = \mathbf{a}_0 \cdot \mathbf{C} \mathbf{a}_0 = \lambda_a^2$, $I_6 = \mathbf{g}_0 \cdot \mathbf{C} \mathbf{g}_0 = \lambda_g^2$. Here,

165 λ_a^2 and λ_g^2 are the square of the stretching of fibers along their initial directions \mathbf{a}_0 and \mathbf{g}_0 .

166 The second Piola-Kirchhoff stress tensor is derived directly from the hyperelastic strain energy
167 function and given by:

$$168 \quad \mathbf{S} = 2 \frac{\partial \Psi}{\partial \mathbf{C}} = 2 \left(\omega_1 \frac{\partial I_1}{\partial \mathbf{C}} + \omega_2 \frac{\partial I_2}{\partial \mathbf{C}} + \omega_4 \frac{\partial I_4}{\partial \mathbf{C}} + \omega_6 \frac{\partial I_6}{\partial \mathbf{C}} \right) \quad (3)$$

169 where $\omega_1 = \frac{\partial \Psi}{\partial I_1}$, $\omega_2 = \frac{\partial \Psi}{\partial I_2}$, $\omega_4 = \frac{\partial \Psi}{\partial I_4}$, $\omega_6 = \frac{\partial \Psi}{\partial I_6}$.

170 The Cauchy stress tensor can be simply obtained by:

$$171 \quad \boldsymbol{\sigma} = \frac{1}{\sqrt{I_3}} \mathbf{F} \mathbf{S} \mathbf{F}^T. \quad (4)$$

172 The mechanical response of CF/PPS material used in this study is represented by an orthotropic
173 incompressible hyperelastic model proposed by Vu et al. [19]. Its strain energy function has the
174 following form:

$$175 \quad \Psi = M_1(I_1 - 3) + M_2(I_2 - 3) + M_3(I_1 - 3)(I_2 - 3) + k_1 \left[e^{k_2(I_4 - 1)^2} - 1 \right] + k_3 \left[e^{k_4(I_6 - 1)^2} - 1 \right] - \frac{1}{2} p(I_3 - 1) \quad (5)$$

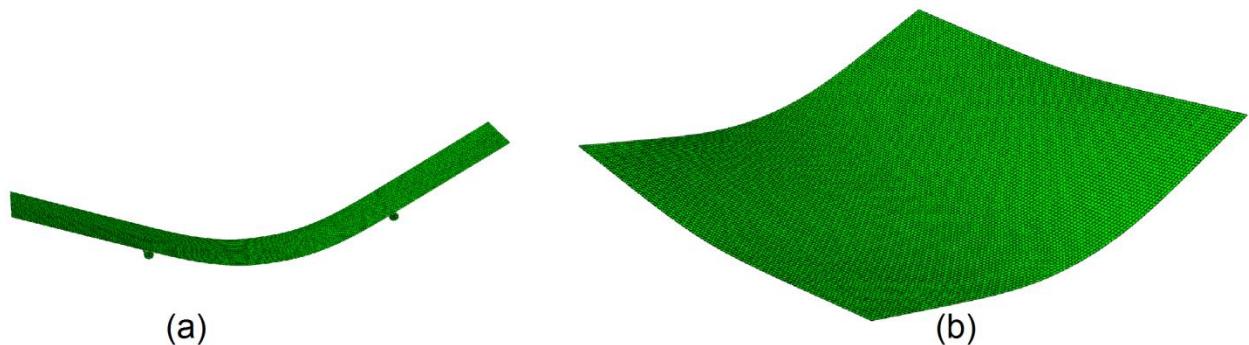
176 where M_1 , M_2 , M_3 , k_1 , k_2 , k_3 , k_4 are the material parameters. Please refer to reference [19] for
177 more details.

178 3.2 Computational experiment

179 The numerical simulations corresponding to the three-point bending test and flexural loading test
180 presented in the previous section were performed using the commercial FE package
181 Abaqus/Standard. Four-node shell elements (S4R) with a four layer composite section were used to
182 model the specimens. Each layer behaves like an orthotropic material characterized by the
183 constitutive model of Equation (5). A frictionless contact between the support system and the
184 specimen was defined for both tests. For the flexural loading test, a 35.59 N concentrated force,
185 which is equivalent to the weight of the steel ball, is set on the center point of the plate. The

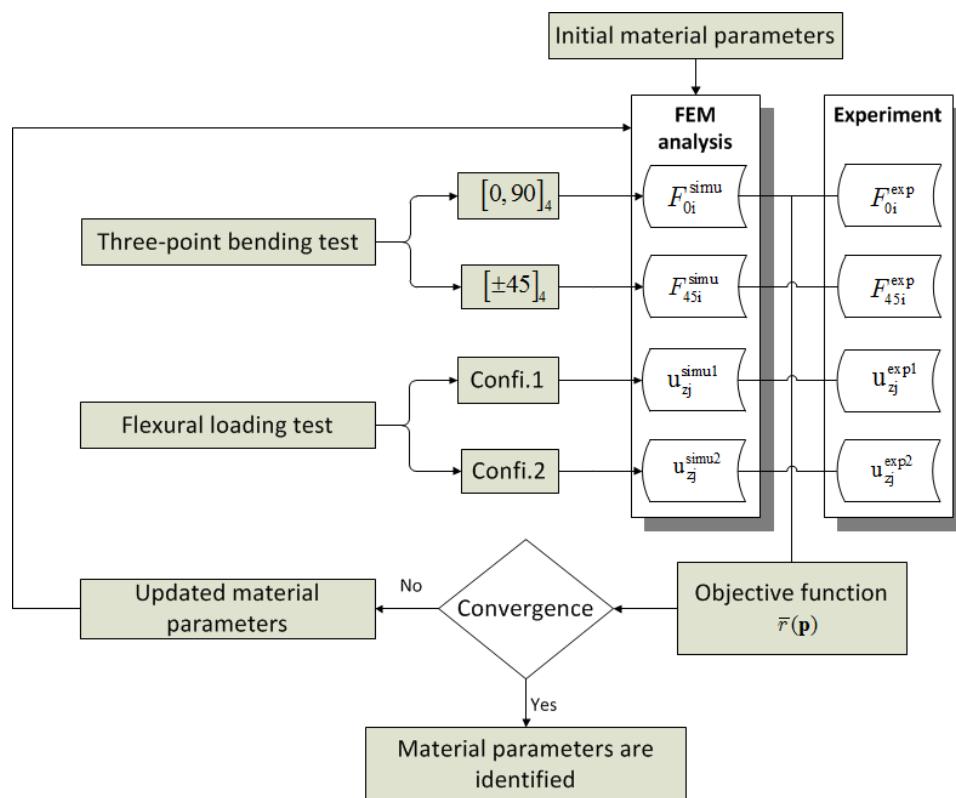
186 computational models for the three-point bending test and flexural loading test are depicted in Figure
 187 5.

188



189
 190 *Figure 5. Computational models: (a) Three-point bending test, (b) Flexural loading test*

191 4. Identification of constant material parameters



192

193 *Figure 6. Inverse characterization flowchart*

194 The material parameters $M_1, M_2, M_3, k_1, k_2, k_3, k_4$ of the strain energy function in Equation (5
 195) for multi-layered carbon fiber reinforced material are identified by minimizing objective functions
 196 that represent discrepancy between experimental test data and numerical simulation results. The
 197 updated material parameters were performed with the Global Response Surface Method for multi-
 198 objective optimization. As presented in (equation 6?) Figure 6, the difference of loads between
 199 experimental data and numerical results was taken into account for building the objective function
 200 associated with the three-point bending test while vertical plate displacements were used to build the
 201 objective function associated with the flexural loading test. These objective functions are as follows:

$$202 \quad \bar{r}_b(\mathbf{p}) = \sqrt{\frac{1}{2N_b} \left\{ \sum_{i=1}^{N_b} \left(\frac{F_{0i}^{\text{exp}} - F_{0i}^{\text{simu}}(\mathbf{p})}{F_{0\max}^{\text{exp}}} \right)^2 + \sum_{i=1}^{N_b} \left(\frac{F_{45i}^{\text{exp}} - F_{45i}^{\text{simu}}(\mathbf{p})}{F_{45\max}^{\text{exp}}} \right)^2 \right\}} \quad (6)$$

$$203 \quad \bar{r}_f(\mathbf{p}) = \sqrt{\frac{1}{2N_f} \left\{ \sum_{j=1}^{N_f} \left(\frac{u_{zj}^{\text{exp1}} - u_{zj}^{\text{simu1}}(\mathbf{p})}{\max(u_{zj}^{\text{exp1}})} \right)^2 + \sum_{j=1}^{N_f} \left(\frac{u_{zj}^{\text{exp2}} - u_{zj}^{\text{simu2}}(\mathbf{p})}{\max(u_{zj}^{\text{exp2}})} \right)^2 \right\}} \quad (7)$$

204 Herein, \mathbf{p} is the list of unknown parameters. The relative error values $\bar{r}_b(\mathbf{p})$ and $\bar{r}_f(\mathbf{p})$ represent the
 205 objective functions of the three-point bending test and flexural loading test respectively. N_b is the
 206 number of displacement steps at which loads were measured in the three-point bending test. F_{0i}^{exp} and
 207 F_{0i}^{simu} represent the experimental and computed loads respectively at step i for the stacking sequence
 208 $[0,90]_4$. F_{45i}^{exp} and F_{45i}^{simu} represent the experimental and computed loads respectively at step i for the
 209 stacking sequence $[\pm 45]_4$. N_f is the number of measured points on the plate surface in the flexural
 210 loading test. u_{zj}^{exp1} and u_{zj}^{simu1} denote the experimental and numerical vertical displacements at point j
 211 for Configuration 1. u_{zj}^{exp2} and u_{zj}^{simu2} denote the experimental and numerical vertical displacements
 212 at point j for Configuration 2.

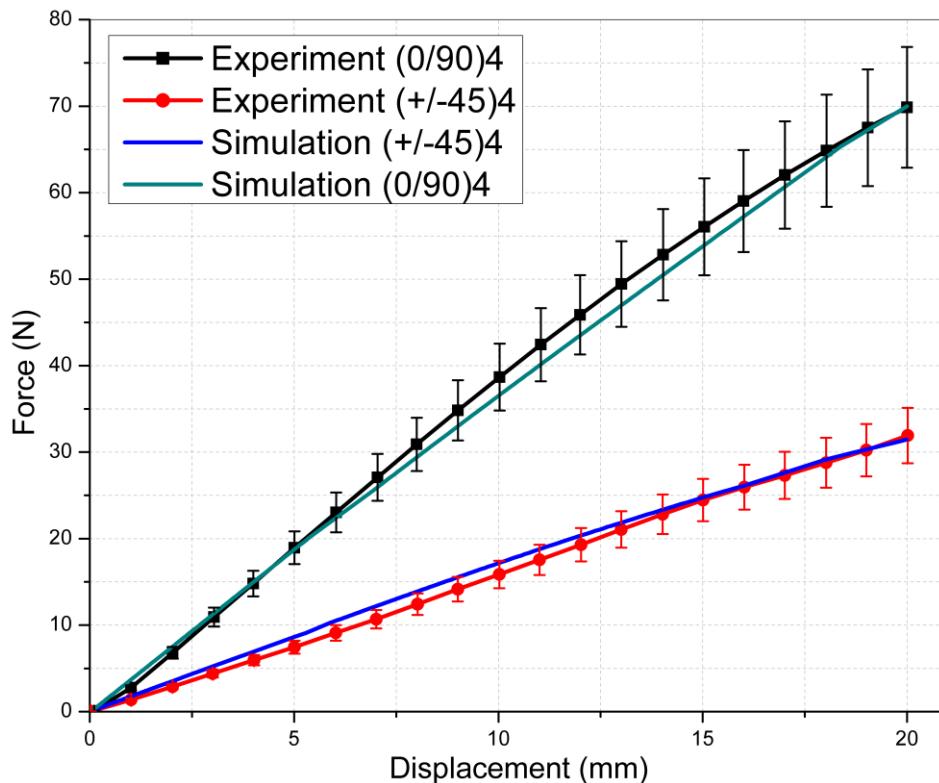
213 Convergence criterion is set to be reached when the updated parameter values are (difference ?)
 214 inferior to 0.5% of actual parameter values. The iterative process ended after 11 steps of the updating
 215 process. Table 3 shows the converged parameter values.

216 *Table 3. Optimized parameter values*

M_1 (MPa)	M_2 (MPa)	M_3 (MPa)	k_1 (MPa)	k_2	k_3 (MPa)	k_4
703.3	915.0	512.0	1131.0	46.2	1131.0	46.2

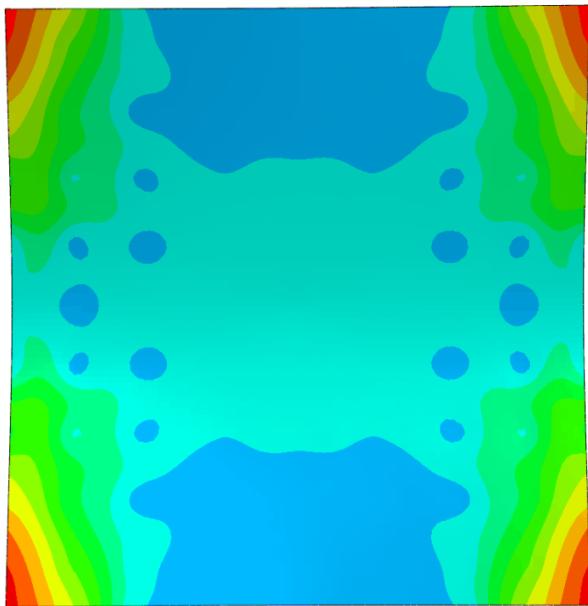
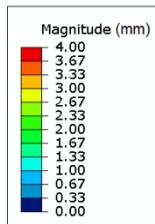
217 Good agreement was found between experimental and computed loads associated with the three-
 218 point bending test versus displacement for the optimized parameters set (Figure 7). With the small
 219 value of relative errors $\bar{r}_b(\mathbf{p}) = 0.0173$, it turned out that the inverse procedure leads to a good fit
 220 between experimental and numerical loads.

221 Figure 8 and Figure 9 show the residual difference between measured and calculated vertical
 222 displacement of composite plate for flexural loading tests. The average differences between
 223 experimental and numerical results were found to be 1.53 mm for Configuration 1, and 0.88 mm for
 224 Configuration 2. The relative error $\bar{r}_f(\mathbf{p})$ is 0.056. It demonstrated that a good match of the calculated
 225 and the measured vertical displacement was achieved as well for the flexural loading test.



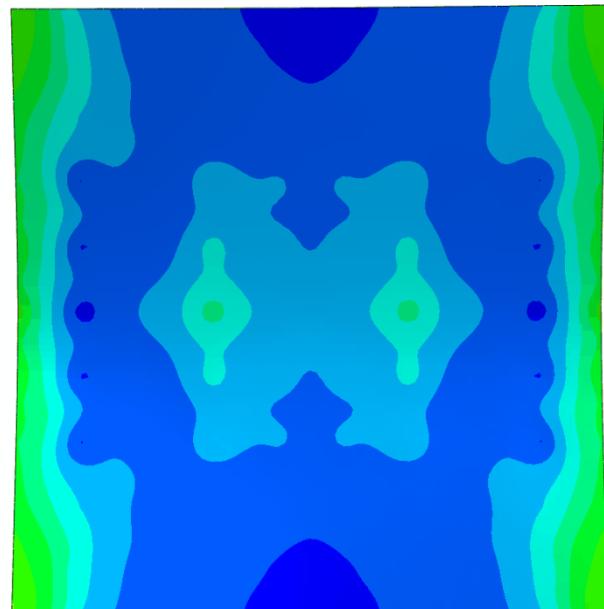
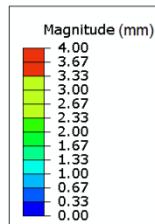
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227 *Figure 7 Comparison between numerical results and experimental data for three-point bending test*



228

229 *Figure 8. Residual difference between measured and calculated vertical displacement for*
230 *Configuration 1*

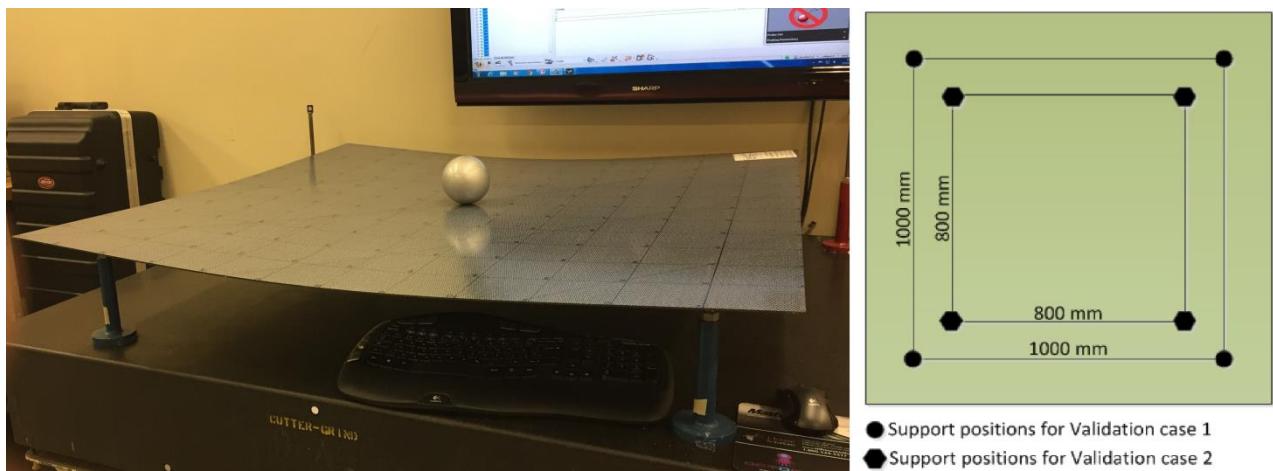


231

232 *Figure 9. Residual difference between measured and calculated vertical displacement for*
233 *Configuration 2*

234 **5. Validation of material model**

235 In this study, the material used for validating the material model is a consolidated plate of eight
 236 layers of CF/PPS with 2.48 mm thickness (0.31 mm/layer). The stacking sequence of plates is
 237 $[(0,90)/\pm45/\pm45/(0,90)]_s$. The flexural loading test for this plate of eight-layer laminate was
 238 performed using the same experimental set up as that used for the four-layer laminate in the previous
 239 section. Dimensions of the plate were 1200 mm x 1200 mm x 2.48 mm. Two validation cases with
 240 two different configurations were performed in this work (see Figure 10). Vertical displacement was
 241 measured at 169 points on the plate surface.



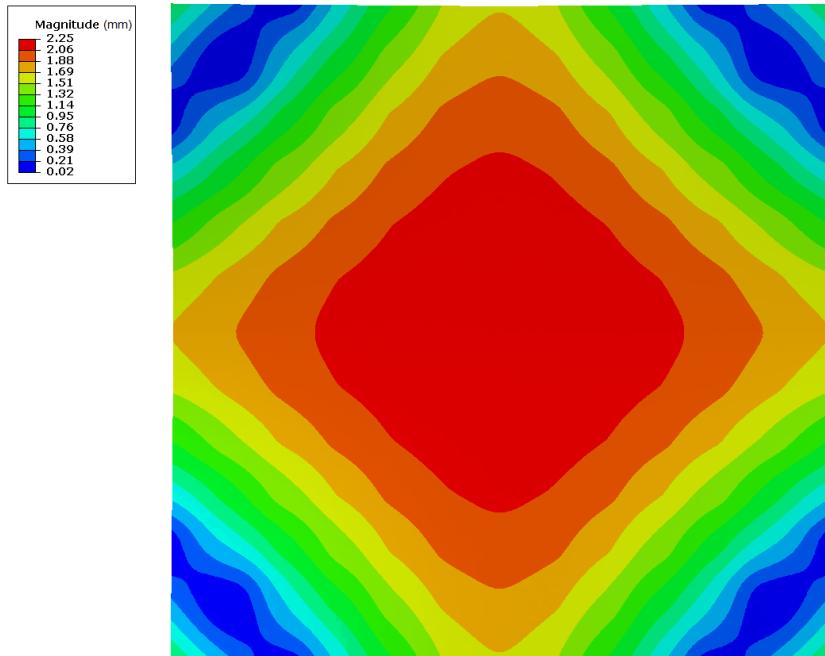
242 ● Support positions for Validation case 1
 ● Support positions for Validation case 2

243 *Figure 10. Validation test*

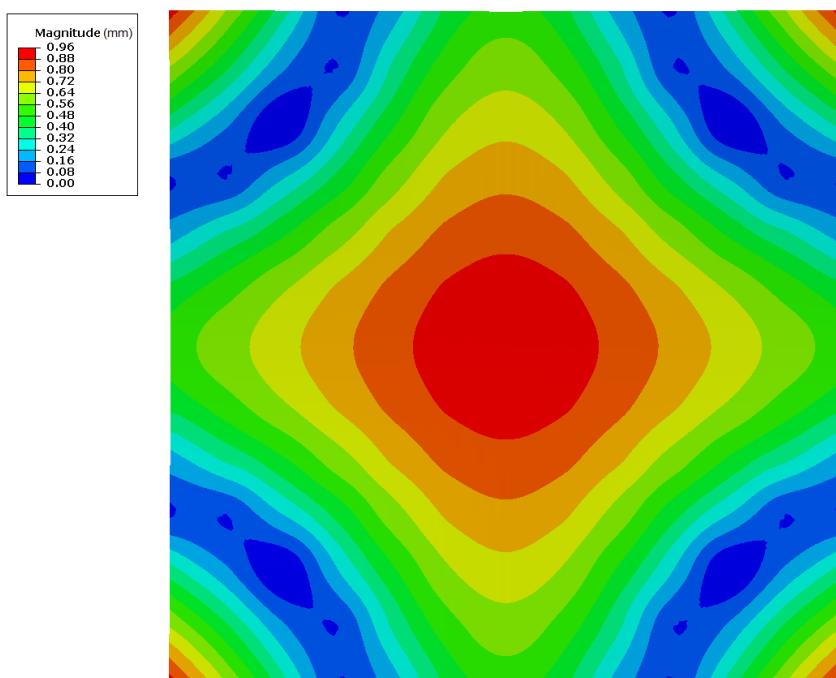
245 A FEA simulation procedure with Abaqus was applied to compute vertical plate displacements. The
 246 material parameters obtained from the inverse characterization procedure were used to simulate the
 247 deformation of this composite plate.

248 Figure 11 and Figure 12 show the residual difference between measured and calculated vertical
 249 displacement of the composite plate for these two validation cases. The average difference between
 250 experimental and numerical results was found to be 1.42 mm and 0.55 mm respectively for validation
 251 case 1 and case 2. Small relative errors obtained ($\bar{r}_1 = 0.0582$ for validation case 1 and $\bar{r}_2 = 0.0283$
 252 for validation case 2 demonstrated that the material model used here is appropriate for assessing the

253 mechanical behavior of this multilayered composite and that the identification procedure developed
254 in this paper is suitable for the characterization of this composite material.



255
256 *Figure 11. Residual difference between measured and calculated vertical displacement for*
257 *Validation case 1*



258

259 *Figure 12 Residual difference between measured and calculated vertical displacement for*
260 *Validation case 2*

261

262 **6. Conclusion**

263 In this paper, an inverse procedure was developed for characterizing the large bending
264 deformation behavior of four layers of CF/PPS material. A three-point bending test with two different
265 stacking sequences and a flexural loading test with two different configurations for boundary
266 conditions were performed to study the mechanical responses. FEA modelling was performed using
267 the Abaqus/Standard commercial FE package based on an orthotropic hyperelastic model for finite
268 strain shell elements. Material parameters associated with this hyperelastic model were identified by
269 minimizing discrepancy between experimental and numerical data. The material model parameters
270 obtained from the inverse characterization were validated for the case of an eight-layer CF/PPS
271 material. Results showed that the proposed method is appropriate for characterizing the behavior of
272 multi-layered composites in large deformation. The method presented in this paper can be applied
273 to characterize and simulate the large anisotropic deformation behavior of non-rigid composite parts
274 during the virtual assembly process.

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