**Competition between rate-dependency of bulk laminates and interlaminar interface on the responses of thermopLastic composites**

Ditho Pulungan1, Ping Hu1, Arief Yudhanto1, Gilles Lubineau1, Recep Yaldiz2

1King Abdullah University of Science and Technology (KAUST), Physical Science and Engineering (PSE) Division, COHMAS Laboratory, Thuwal, 23955-6900, Saudi Arabia

Email: [dithoardiansyah.pulungan@kaust.edu.sa](mailto:dithoardiansyah.pulungan@kaust.edu.sa), Web Page: <http://cohmas.kaust.edu.sa>

2SABIC, T&I Composites, P.O. Box 319, 6160 AH Geleen, The Netherlands

**Keywords:** DCB, delamination, viscoelasticity, viscoplasticity, mesoscale

**Abstract**

Thermoplastic composites can be an alternative solution for automotive light-weighting and hence, the reduction of carbon emissions. Double cantilever beam (DCB) test is one of the primary standards tests used to evaluate the Mode I interlaminar fracture toughness of composite laminates. DCB tests at various pulling rates are usually performed to get information about the rate-dependent fracture toughness of a given composite. Usually, the observed rate-dependent response has attributed solely due to the influence of the interlaminar interface although the bulk-ply in laminate itself is rate-dependent. In this paper, we use a viscoelastic viscoplastic ply damage model and viscoelastic cohesive element model to study the response of DCB test at various pulling rates. The objective is to break down the contribution of each constituent (bulk-ply and interlaminar interface) by turning on and off the rate-dependent model in the ply and the interface. Hence, we can isolate the contribution of each constituent to the increase of apparent load-displacement curves.

1. Introduction

Thermoplastic composites can be an alternative solution for automotive light-weighting with the final aim of reducing the carbon emissions [1]. In addition to their affordable cost, they also exhibit fast processing capability and excellent impact resistance. Yet, their optimum use for primary automotive structures has not been achieved due to the lack of understanding of their damage and failure behaviors. Unlike thermosets, significant time/rate-dependent behaviors of the thermoplastic matrices may play a significant role in determining the damage mechanisms and global response of thermoplastic composites. It is thus imperative to have a predictive virtual testing tool that can provide an insight into the most influencing factors guiding the damage behaviors [2].

In this work, we studied the continuous glass fiber-reinforced polypropylene (GF/PP) thermoplastic composites. Our recent works showed their rate-dependent responses, which are due to the viscoelasticity and viscoplasticity of the thermoplastics [3]. One of the challenges in interpreting the DCB test or other interfacial characterization test results is the source of the apparent rate-dependent responses. This may come solely from the interface rate-dependency or the bulk laminates itself. Therefore, this could affect the parameter calibration results of the rate-dependent interface model.

In this work, we developed an associated finite element (FE) model to study the competition between the rate-dependent property of the bulk laminate and that of the interlaminar interface. The intralaminar damage of the composite ply was modeled based on the classical mesoscale ply damage model LMT-Cachan [4], [5], which was then extended to include viscoelasticity, viscoplasticity and pressure-dependent plasticity of thermoplastic matrix. The interlaminar delamination was modeled by cohesive interface elements with a viscoelastic version of the bilinear traction-separation law [6]. The ply damage and the cohesive interface models were implemented on a user-defined subroutine (UMAT) and user-defined element (UEL), respectively, in ABAQUS/Standard platform. By activating or deactivating the rate-dependency of each constituent, i.e., either the bulk laminate or the interface, the study has successfully revealed the source of the apparent rate-dependent responses of DCB tests at various strain rates.

2. Experimental Works

Several tests have been performed to calibrate the anisotropic viscoelastic and viscoplastic behavior of the elementary ply [7]. Tensile-relaxation test on was done to obtain the relaxation response of GF/PP under shear loading. This provides us with the shear relaxation moduli of the elementary ply at various time scales (0.01 – 1000 seconds). The test was done by pulling the ply up to a predefined strain and then the cross-head displacement was kept constant to impose a constant strain. During this constant strain, the stress-relaxation occurs due to its viscoelastic behavior. The stress-relaxation was then measured with the load cell of the Instron machine. The constant strain was kept up to approximately 1 hour. Similar test was also performed on GF/PP laminates. This gives us the information of tensile-relaxation moduli of the ply. These two tests were necessary to perform since the orthotropy of the ply prevents the direct relationship between shear and tensile moduli as usually found in isotropic materials.

Monotonic tensile tests on laminates was conducted at various tensile strain rates ranging from 0.001/s to 0.1/s. The test has been done at various rates to get the stress-strain response of the elementary ply to later be fitted by a viscoplastic yield function. Note that the viscoplasticity here is fitted based on GF/PP shear behavior. However, under DCB test, the viscoplastic effect may be insignificant due to the low strain imposed at the bulk laminates.

The viscoelastic behavior of the interface is calibrated based on the rate-dependency of the thermoplastic matrix itself. The interlaminar interface of the investigated laminate is dominated by thick matrix rich-region in which Mode-I delamination will dominate the DCB load-displacement response. Here, we assumed that the maximum stress of the traction-separation law of the GF/PP interlaminar interface increases around 10% of static value with per decade increase of separation rate. This assumption is in line with several experimental results reported in the literature [8].

3. Finite Element Model

3.1. Ply modeling

We modeled the elementary ply of the composite using the classical LMT-Cachan mesoscale damage model. Since the focus of the research is to understand the effect of rate-dependency on the bulk laminates, we neglect the damage development in the ply, and hence, the damage model of the ply is not included. The model only consists of the viscoelasticity and viscoplasticity of the ply. The viscoelasticity follows the anisotropic generalized Maxwell viscoelastic as follow.

|  |  |
| --- | --- |
|  | (1) |

|  |  |
| --- | --- |
|  | (2) |

where , and are the shear relaxation moduli, tensile relaxation moduli and relaxation time of the -th Maxwell branch in the viscoelastic model of the bulk laminates, respectively. The viscoelastic stress-strain relationship is then expressed as follows.

|  |  |
| --- | --- |
|  | (3) |

|  |  |
| --- | --- |
|  | (4) |

|  |  |
| --- | --- |
|  | (5) |

Meanwhile, the viscoplasticity formulation adopts the power-law viscoplasticity as follow.

|  |  |
| --- | --- |
|  | (6) |

where the  is the effective stress components;  and are material parameters for pressure-dependency. Note that the damage here is neglected and therefore . In addition, due to limited plasticity, the pressure-dependency can be neglected in this DCB case, and therefore we set and . In order to include the viscoplasticity, the yield strength is expressed as a function of equivalent viscoplastic strain and viscoplastic strain rate as shown below.

|  |  |
| --- | --- |
|  | (7) |

where is the shear yield hardening at quasi-static loading. , , and are fitting parameters for strain hardening, while D and E are rate-dependent hardening parameters that scale the quasi-static strain hardening towards the intended strain rates. Table 1 list all material parameters required for modeling the rate-dependent ply behaviors. Interested reader can refer to [7] for more details.

The viscoelasticity and the viscoplasticity model have been implemented in a predictor-corrector scheme. In this scheme, we initially assume that the viscoelastic strain increment is the total strain increment, i.e., and then, the updated stress is predicted based on this assumption. Such a predicted stress is also known as the trial viscoelastic stress. Based on the trial stress, the yield function is then evaluated. If the yield function threshold is exceeded then a correction on the assumed viscoelastic strain increment need to be done such that, which means of the total strain increment, some portion of it is related to the viscoplastic strain increment.

3.2. Interface modeling

The interlaminar interface of GF/PP is modeled with the rate-dependent viscoelastic traction-separation law. The classical bilinear traction-separation law is extended to the viscoelastic theory as explained by Kaliske and coworkers [6]. Since the deformation occurs in the polymer matrix, thus it is reasonable that the rate-dependent behavior of the interface to assumed as rate-dependency of the polymer matrix itself. Hence, the rate-dependency of the interlaminar interface follows the similar rate-dependency of the polymers.

|  |  |
| --- | --- |
|  | (8) |

where is the traction in experienced by the interface that contain , and i.e. she,ar, tangential and normal tractions, respectively. is the tractions contribution from elastic branch and is the traction contribution from -th Maxwell branch.

|  |  |
| --- | --- |
|  | (9) |

is the stress contribution from -th Maxwell branch. is the elastic stiffness tensor for the spring in the -th Maxwell branch.

|  |  |
| --- | --- |
|  | (10) |

is the elastic stress contribution from the damageable elastic spring.

|  |  |
| --- | --- |
| and | (11) |

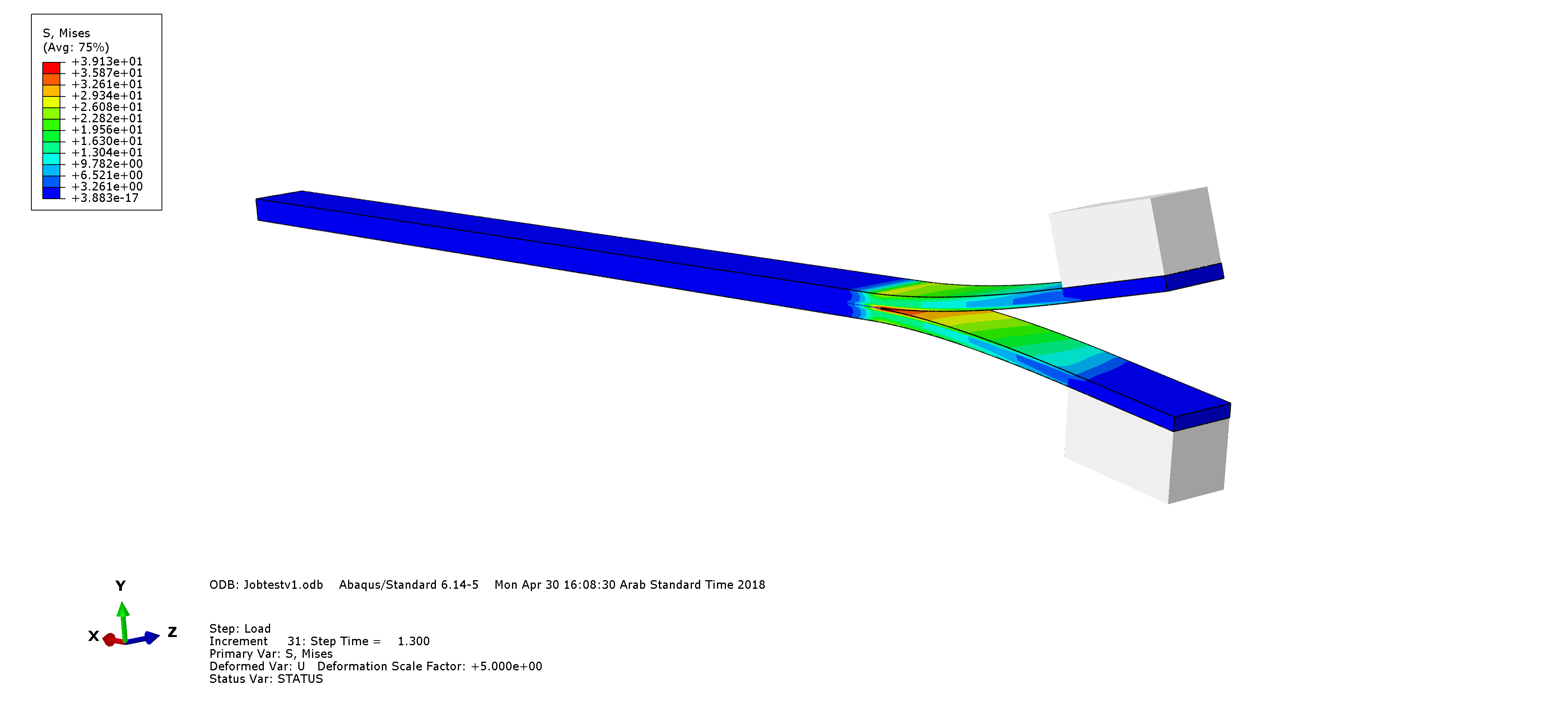
where bold symbols represent a vector or a matrix. Here, , and are the shear, tangential and normal separation of the cohesive element, respectively. Table 2 lists all material parameters required for the viscoelastic cohesive elements. Fig. 1 showed the viscoelastic traction separation curves at various separation rates.



**Figure 1.** Traction separation law of the viscoelastic bilinear model for the cohesive elements.

3.3. FE discretization and boundary conditions

We developed 3D finite element model of double cantilever beam (DCB) specimen as shown in Fig. 2. The dimension of the specimen is 250 mm long, 20 mm width and total 4 mm thick. The lever was consist of laminate where the fibers span along the longitudinal direction of the DCB sample. The lever is discretized with 3D hexahedral elements with reduced integration points (C3D8R) with a characteristic length of mesh around 0.5 mm. The constitutive of the lever can describe the anisotropic viscoelastic viscoplastic damage behavior of the GF/PP laminates. Meanwhile, the interlaminar interface is modeled with zero-thickness cohesive interface element with the viscoelastic bilinear traction-separation law. The displacement-controlled loading is applied at the two blocks with the total opening of 100 mm within 1200 seconds (pulling rate of 5 mm/min). Here, we repeat the same simulation at higher pulling rates of 500 mm/min.



**Figure 2.** FE model of DCB test to measure Mode-I interlaminar fracture toughness.



**Figure 3.** Validation of the finite element model of DCB test at 5 mm/min.

4. Results and Discussions

First, we validated the DCB FE model to simulate DCB test at low speed (referred here as Sim-A). Fig. 3 showed the comparison between simulation and experimental results at 5 mm/min. The simulation results with the provided viscoelastic parameters fit nicely the experimental curves.

Next, we made two FE analyses on DCB samples pulled at a high rate (500 mm/min): (1) only the interface that is rate-dependent (Sim-B) and (2) both interface and laminate are rate-dependent (Sim-C). Fig. 4 showed the force-displacement curve of simulated DCB test result at high and low rates. Fig. 4 showed that as the pulling rate increases, the force-displacement curve increases (see Sim-A and Sim-B). However, by comparing Sim-B and Sim-C results, it is clearly shown that there is a negligible difference in the load-displacement curves. We can see that the rate-dependency comes mainly due to the interlaminar interface. There is negliible contribution of the viscoelastic behavior of bulk laminate on the load-displacement curve. This is because the flexural response of the DCB lever is governed by the stiffness of the laminate along the fiber direction. Note that further studies on stacking sequence of DCB are required since the contribution of the viscoelasticity of the bulk laminate may on the stacking sequence.



**Figure 4.** Effect of rate-dependency of each constituent (interface and ply) to the global increase of load-displacement curves. Static loading speed is 5 mm/min, while high loading speed is 500 mm/min.

5. Conclusions

We proposed a rate-dependent model for both bulk laminate and interlaminar interface. The bulk laminate was modeled as anisotropic generalized Maxwell viscoelastic with Johnson-Cook viscoplasticity. Meanwhile, the interface was modeled using the viscoelastic version of the bilinear traction-separation law. We developed a 3D finite element model of DCB test to study the source of rate-dependent behavior during the DCB test. We found that the effect of the bulk laminate on the apparent rate-dependency is negligible for this particular stacking sequence. We have successfully shown that the rate-dependent load-displacement curves from DCB test are caused by the rate-dependency of the interlaminar interface. Therefore, accounting for rate-dependent interface elements is very important to predict the nonlinear damage behavior of composite at various strain rates.

Acknowledgments

The research reported in this publication was supported by SABIC and King Abdullah University of Science and Technology (KAUST).

Appendix

**Table 1.** Material properties for elementary ply.

|  |  |  |  |
| --- | --- | --- | --- |
| **No** | **Parameters** | **Value** | **Unit** |
| 1 |  | 3300 ± 400 | MPa |
| 2 |  | 1600 ± 1200 | MPa |
| 3 |  | 1400 ± 1200 | MPa |
| 4 |  | 1300 ± 1200 | MPa |
| 5 |  | 1300 ± 1000 | MPa |
| 6 |  | 1300 ± 240 | MPa |
| 7 |  | 1300 ± 500 | MPa |
| 8 |  | 1170 ± 170 | MPa |
| 9 |  | 100 ± 10 | MPa |
| 10 |  | 400 ± 400 | MPa |
| 11 |  | 460 ± 290 | MPa |
| 12 |  | 430 ± 150 | MPa |
| 13 |  | 290 ± 20 | MPa |
| 14 |  | 410 ± 120 | MPa |
| 15 |  | 0.01 | Second |
| 16 |  | 0.1 | Second |
| 17 |  | 1.0 | Second |
| 18 |  | 10.0 | Second |
| 19 |  | 100.0 | Second |
| 20 |  | 1000.0 | Second |
| 21 |  | 4.9 | MPa |
| 22 |  | 38.0 | MPa |
| 23 |  | 0.3 | - |
| 24 |  | 190 | - |
| 25 |  | 0.11 | - |
| 26 |  | 1 | - |
| 27 |  | 0 | - |

**Table 2.** Material parameters for cohesive element model.

|  |  |  |  |
| --- | --- | --- | --- |
| **No** | **Parameters** | **Value** | **Unit** |
| 1 |  | 25926.0 | MPa |
| 2 |  | 5556.35 | MPa |
| 3 |  | 1544.97 | MPa |
| 4 |  | 4817.27 | MPa |
| 5 |  | 2310.00 | MPa |
| 6 |  | 4197.99 | MPa |
| 7 |  | 3458.00 | MPa |
| 8 |  | 10.0 | MPa |
| 9 |  | 1.0 | MPa |
| 10 |  | 0.0001 | Second |
| 11 |  | 0.001 | Second |
| 12 |  | 0.01 | Second |
| 13 |  | 0.1 | Second |
| 14 |  | 1.0 | Second |
| 15 |  | 10.0 | Second |
| 16 |  | 100.0 | Second |
| 17 |  | 1000.0 | Second |
| 18 |  | 0.25 | kJ/m2 |
| 19 |  | 0.9 | kJ/m2 |

References

[1] W. Schijve and G. Francato, “New thermoplastic composite solutions for automotive lightweighting,” *JEC Compos. Mag.*, no. 103, pp. 96–98, 2016.

[2] D. Pulungan, G. Lubineau, A. Yudhanto, R. Yaldiz, and W. Schijve, “Identifying design parameters controlling damage behaviors of continuous fiber-reinforced thermoplastic composites using micromechanics as a virtual testing tool,” *Int. J. Solids Struct.*, vol. 117, pp. 177–190, 2017.

[3] A. Yudhanto *et al.*, “Monotonic and cyclic responses of impact polypropylene and continuous glass fiber-reinforced impact polypropylene composites at different strain rates,” *Polym. Test.*, vol. 51, pp. 93–100, May 2016.

[4] P. Ladevèze and E. Le Dantec, “Damage modelling of the elementary ply for laminated composites,” *Compos. Sci. Technol.*, vol. 43, no. 3, pp. 257–267, 1992.

[5] G. Lubineau and P. Ladevèze, “Construction of a micromechanics-based intralaminar mesomodel, and illustrations in ABAQUS/Standard,” *Comput. Mater. Sci.*, vol. 43, no. 1, pp. 137–145, Jul. 2008.

[6] G. Geißler and M. Kaliske, “Time-dependent cohesive zone modelling for discrete fracture simulation,” *Eng. Fract. Mech.*, vol. 77, no. 1, pp. 153–169, Jan. 2010.

[7] D. Pulungan, G. Lubineau, A. Yudhanto, H. Wafai, R. Yaldiz, and W. Schijve, “A mesoscale viscoelastic pressure-dependent viscoplastic damage model for fiber-reinforced thermoplastic composites,” *InPreparation*, 2018.

[8] D. Pulungan, A. Yudhanto, S. Goutham, G. Lubineau, R. Yaldiz, and W. Schijve, “Characterizing and modeling the pressure- and rate-dependent elastic-plastic-damage behaviors of polypropylene-based polymers,” *Polym. Test.*, Feb. 2018.