A numerical and experimental comparison of test methods for the shear strength in hybrid metal/thermoplastic-compounds

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Abstract. The lap shear test is a common method for determining the interlaminar shear strength of material compounds due to its simplicity with regard to specimen production and experimental realization. However, the obtained results strongly depend on the material pairing and the specimen geometry, which have a significant influence to the stress state within the interface during the experiment. A torsion test method using butt-bonded hollow cylinders seems more appropriate due to the expected more homogeneous shear stress distribution. The aim of this work is to compare both testing methods by experimental and numerical investigations. A comparative study with thermally joined aluminum-polyamide hybrid compounds was performed. The corresponding simulations were carried out with the commercial FE-software Abaqus™, using a user defined material model for the thermoplastic joining partner and the built-in cohesive behavior contact property for modelling interfacial damage.

1. Introduction

Current developments in lightweight applications confirm the trend towards multi-material design. The combination of different material classes offers an efficient link of beneficial properties in hybrid material compounds. Especially in the automotive industry, hybrid material compounds of polymer and metal provide an economical way for lightweight design [1]. In addition to that the interest in thermoplastic polymers like polyamide is increasing because of advances like short cycle times and better recyclability compared with thermosetting materials. Furthermore, the application of thermoplastic polymers offers a direct thermal joining without adhesives or joining elements. A suitable way for mechanical interlocking of both components under thermal joining conditions is the surface pretreatment of the metal by abrasive or additive processes prior to the joining process [2].

The mechanical behavior of the compound depends on the properties of each single component as well as on the interface between them. Also, the assisted design by finite element (FE)-simulations in all stages of development requires input data that describes single material properties of the compound and in addition to that the interface between the single materials. The properties of the metallic joining partner can be determined by tensile tests, whereas the thermoplastic joining partner has to be tested under various load conditions and speeds due to strongly strain rate dependent and viscoelastic-viscoplastic behavior. The properties of the interface play a key role under the combination of different material classes in hybrid material compounds. The interface behavior has to be replicated by data determined in test methods of linear elastic fracture methods (LEFM) and values of interface strength at different load directions [3].
In particular, the lap shear test based on DIN EN 1465 is one of the most used methods for determining the shear strength in multi material compounds reasoned for example by the simple production of test specimens. One disadvantage of the lap shear test is the non-uniform stress distribution in the interface [4], particularly dependent on the difference in stiffness between the joined materials. Especially non-reinforced thermoplastic polymers in combination with metallic layers exhibit an early crack initiation at the interface in lap shear tests. A consequence of this is a premature failure and overlap length-dependent values for the obtained shear strength [4, 5, 6]. In this study, a torsion test method using butt-bonded hollow cylinders based on DIN EN 14869-1 is investigated as an experiment for the determination of interface shear strength in hybrid material compounds and compared to the lap shear test. The advantage of a more homogeneous shear stress distribution and the resulting, more reliable determination of the shear strength value should be shown by FE-simulations and validated by experimental data on a polyamide 6 (PA6)/aluminum compound.

2. Experimental

2.1 Lap shear test
In the lap shear test, two flat plates, each consisting of a partner of the compound to be examined, are bonded together. The specimen geometry used here is shown in Figure 1. The specimen is loaded with a tensile force perpendicular to the joining zone, causing a shear stress within the interface. Assuming an ideal homogeneous stress distribution, the shear strength $\tau_{\text{max}}$ is calculated from the fracture force $F_{\text{max}}$ divided by the overlap area $A_o$.

$$\tau_{\text{max}} = \frac{F_{\text{max}}}{A_o} = \frac{F_{\text{max}}}{w \cdot l_o} \quad (1)$$

![Figure 1. Geometry of lap shear specimen based on[7].](image)

Width: $w = 25$ mm  
Height plate 1: $h_1 = 3$ mm  
Height plate 2: $h_2 = 3$ mm  
Length plate 1: $l_1 = 100$ mm  
Length plate 2: $l_2 = 100$ mm  
Overlap length: $l_o = 12.5$ mm

2.2 Butt-bonded hollow cylinder torsion test
In the hollow cylinder torsion test, two thin-walled pipes, each consisting of a partner of the compound to be examined, are butt-bonded together. The specimen geometry used in this work is shown in Figure 2. The compound is loaded with a torsional moment, causing a shear stress parallel to the interface. The specimen is twisted until fracture and the shear strength $\tau_{\text{max}}$ then calculated from the maximum torque $T_{\text{max}}$ divided by the polar section modulus $W_p$. 

![Figure 1. Geometry of lap shear specimen based on[7].](image)
\[ \tau_{\text{max}} = \frac{T_{\text{max}}}{W_p} = \frac{T_{\text{max}} \cdot 16 \cdot d_o}{\pi \cdot (d_o^4 - d_i^4)} \] (2)

Outer diameter: \( d_o = 24.5 \text{ mm} \)
Inner diameter: \( d_i = 20.5 \text{ mm} \)
Height cylinder 1: \( h_1 = 30 \text{ mm} \)
Height cylinder 2: \( h_2 = 30 \text{ mm} \)

\textbf{Figure 2.} Geometry of butt-bonded hollow cylinder specimen based on[8].

2.3 Specimen production

The tests were carried out at an EN AW 6082/PA6 hybrid material compound. To create a reproducible and well adhering surface, undercuts were produced by coating with thermal spraying. After grit blasting, the aluminum substrate was coated by a high-velocity electric wire arc spraying system developed at Chemnitz University of Technology (CUT) [9]. As feedstock material, a cored wire NiAl95/5 alloy with a diameter of 1.6 mm was used. This is a typical material for bonding layers which provides excellent adhesion to the substrate. The parameters used for producing the rough surface structure are shown in Table 1. According to the production parameters determined in [5], the joining process was done by hot pressing with a joining pressure of 0.3 MPa and a joining temperature of 230 °C, which is approximately 10 °C above the melting temperature of the polyamide. The lap shear specimens shown in Figure 3a have to be reworked after pressing by removing expelled melt around the interface with a metal saw. The rework on the hollow cylinder specimens however causes much more effort because they need to be turned on the inside as well as on the outside to ensure the necessary centricity for testing. The polyamide has been conditioned before manufacturing and testing according to [10] at 70 °C and 62% humidity. 3-6 specimens have been produced and tested per geometry.

\textbf{Table 1.} Parameters for thermal spraying.

| Parameter                  | Value                        |
|----------------------------|------------------------------|
| Spraying system            | High-velocity electric wire arc (modified Visu Arc 350) |
| Substrate activation       | Grit blasting Al₂O₃, 2 bar, 70°, 20 cm, type WFA F 16 |
| Spraying material          | Ni - 95% / Al - 95% (DURUM DURMAT AS-756 1.6 mm) |
| Current                    | 120 A                        |
| Voltage                    | 28 V                         |
| Process gas                | Air, 5.5 bar                 |
| Spraying Distance          | 200 mm                       |
| Row spacing                | 6 mm                         |
| Feed speed                 | 1 m/s                        |
3. Simulation

To investigate the obtained results depending on the used test method, FEM simulations offer a detailed view into the relevant mechanical quantities. Therefore, a sufficiently exact simulation model is needed. This includes the material data as well as the parameters of the interface. The simulations were carried out with the commercial FE-software Abaqus™ 6.14.

3.1 Material model

The metallic joining partner of the compound can be depicted with a built-in elastic-plastic material model, whereas a user defined material is needed for the thermoplastic partner due to its time- and strain rate-dependent behavior. This user defined material is modelled by a viscoplastic material model by Kießling et al. [3, 11]. This material model is formulated based on a concept of material modeling at large strains by directly connected rheological elements, which has been introduced by Kießling et al. [12]. The parameters for the user defined material have to be determined by solving an optimization problem, which is based on minimizing the deviation between the load-displacement curves produced in material tests (tensile tests, relaxation tests, cyclic tests) and the numerically reproduced load-displacement curves using the material model.

3.2 Interface

The interfacial damage is modeled by the built-in cohesive behavior contact property. The principle of the failure mechanism is the same for the normal (subscript I) as well as for both shear (subscripts II and III) directions. For reasons of comprehensibility, the following explanations do not distinguish between the load directions. The interfacial adhesion can be described as a spring braced between the two contacting surfaces (see Figure 4). When a load is applied, the spring keeps its penalty stiffness $K$ till the damage initiation point at the separation $\delta_1$ is reached. Further increasing separation causes damage evolution and corresponding softening of the spring till complete failure at $\delta_2$. 

**Figure 3.** (a) Lap shear specimen (b) Butt-bonded hollow cylinders specimen.
Figure 4. Cohesive behavior contact property, principle and stress-separation curve.

The parametrization is done by the critical stress $\sigma_{\text{lim}}$, where the damage is initiated, and by the interlaminar fracture toughness $G_C$, which is the energy per area needed to destroy the interface. Both of these values can be obtained experimentally. The separation at the damage initiation point $\delta_1$ can be calculated from the chosen penalty stiffness of the spring $K$ and the critical stress $\sigma_{\text{lim}}$ (Equation (3)). The separation at the point of complete failure $\delta_2$ is calculated from the interlaminar fracture toughness $G_C$ and the critical stress $\sigma_{\text{lim}}$ (Equation (4)).

\[
\delta_1 = \frac{\sigma_{\text{lim}}}{K} \tag{3}
\]

\[
\delta_2 = 2 \cdot \frac{G_C}{\sigma_{\text{lim}}} \tag{4}
\]

The damage variable $d$ describes how the penalty stiffness is reduced once the initiation criterion is reached. For linear softening, it can be calculated by Equation (5). It has a value of 0 at the damage initiation point $\delta_1$ and reaches 1 at the point of complete delamination $\delta_2$.

\[
d = \frac{\delta_2 \cdot (\delta - \delta_1)}{\delta \cdot (\delta_2 - \delta_1)} \tag{5}
\]

The resulting stress $\sigma$ can then be calculated by Equation (6).

\[
\sigma = \begin{cases} 
K \cdot \delta, & \delta < \delta_1 \\
(1 - d) \cdot K \cdot \delta, & \delta_1 \leq \delta \leq \delta_2 \\
0, & \delta > \delta_2 
\end{cases} \tag{6}
\]

The critical tension as well as the fracture toughness vary depending on the load direction. In the investigated case, the adhesion in shear direction is much higher than the adhesion in normal direction. For this reason, at least 4 parameters have to be obtained experimentally to describe the interface sufficiently. These are the critical stresses in normal and shear direction $\sigma_{I,\text{lim}}$ and $\tau_{II,\text{lim}}$ as well as the corresponding fracture toughness $G_{IC}$ and $G_{IIIC}$. The critical stresses can be determined with the hollow cylinder specimens described in this article. For the critical tension in normal direction, the hollow cylinders are loaded with a tensile force instead of a torque. For the fracture toughness, modified versions of the Single cantilever beam (SCB) test based on [13] for $G_{IC}$ and End Notched Flexure (ENF) test based on [14] for $G_{IIIC}$ are suitable. The behavior under mixed mode loading (combined
normal and shear) can be specified by using analytical forms. For the damage initiation, the quadratic stress criterion is used. The damage is initiated, when the quadratic interaction function involving the contact stress ratios reaches the value of 1 (Equation (7)) [15]. For the normal direction, only tensile stress is taken in to consideration, whereas compressive stress does not cause damage.

\[
\left( \frac{\langle \sigma_I \rangle}{\sigma_{I,\text{lim}}} \right)^2 + \left( \frac{\tau_{II}}{\tau_{II,\text{lim}}} \right)^2 + \left( \frac{\tau_{III}}{\tau_{III,\text{lim}}} \right)^2 = 1
\]  

(7)

The damage evolution law describes how the damage evolves under mixed mode loading. The Benzeggagh-Kenane criterion is used in this work because it seems to produce the most reasonable results over a large range of mode ratios [16]. The corresponding fracture toughness can be calculated by Equation (8), where \( G_I, G_{II} \) and \( G_{III} \) denote the strain energy release rates in normal and both shear directions. The cohesive property parameter \( \eta \) considers the influence of the mode mix to the resulting fracture toughness \( G_C \). The entire calculation of the resulting stiffness and damage under mixed mode loading can be seen in [16].

\[
G_C = G_{IC} + (G_{II} - G_{IC}) \cdot \left( \frac{G_{II} + G_{III}}{G_I + G_{II} + G_{III}} \right)^\eta
\]  

(8)

The values used for the simulation can be seen in Table 2. The critical stresses \( \sigma_{I,\text{lim}} \) and \( \tau_{II,\text{lim}} \) were obtained with the specimens shown in Figure 3b. Experiments for determining the fracture toughness \( G_{IC} \) and \( G_{II} \) as well as the corresponding exponent \( \eta \) are currently under investigation and did not produce reasonable results yet, so they have been chosen to fit the experimental data.

| \( \sigma_{I,\text{lim}} \) [N/mm²] | \( \tau_{II,\text{lim}} \) [N/mm²] | \( G_{IC} \) [mJ/mm²] | \( G_{II} \) [mJ/mm²] | \( K_I \) [N/mm³] | \( K_{II} \) [N/mm³] | \( \eta \) [-] |
|---|---|---|---|---|---|---|
| 8 | 17 | 0.7 | 3.5 | 1000 | 1000 | 0.6 |

### 4. Results and discussion

#### 4.1 Interface microstructure

The bonding mechanism between substrate and coating system is mainly mechanical interlocking. For NiAl95/5, also micro welding is proven. The coating as an additional element results in the formation of two interfaces in the hybrid compound. Due to the bad polar matching of metal and PA6, the main bonding mechanism between coating and PA6 is also mechanical interlocking. As seen in the cross section of the joined specimens (Figure 5), the coating forms a relatively thin layer which fully covers the aluminum sheet. A good adhesion is achieved by broadly excluded void parts at the interface. The rough surface of the coating with undercuts as well as the porous structure are offering a better potential for mechanical interlocking. Nevertheless, PA6 shows partially delamination along straight tracks of the interface. A reason for this is the mismatch in chemical compatibility as well as the difference in thermal expansion. The shrinkage tension during the cooling in the joining process favors the void formation at straight tracks. Nevertheless, mechanical interlocking and clamping can be provided by the coating roughness, which offers a good adhesion.
4.2 Grey scale correlation
For visualizing the specimen deformation and crack propagation during the lap shear experiment, grey scale correlation data has been recorded. Figure 6 shows the side view of the overlapping area of a specimen with the standard geometry ($l_o = 12.5$ mm) with aluminum on the top side and polyamide 6 on the bottom side. The evaluated quantity shown is the deformation due it makes the crack visible. Figure 6a shows that the crack is already emerging at a small cross head displacement of 11% of the total displacement where fracture occurs. The delamination propagates continuously along the interface (Figure 6b). Just before failure at 99% of the fracture displacement, an additional crack emerges at the end of the overlapping area (Figure 6c). Immediately afterwards the specimen breaks.

4.3 Experimental and numerical results
To investigate the overlap length-dependent results for the shear strength $\tau_{max}$ obtained by the lap shear test, specimens with an overlap length of $l_o = 5$ mm respectively $l_o = 25$ mm were produced in addition to the standard geometry shown in Figure 1 with $l_o = 12.5$ mm. The tests were carried out with a cross head displacement of 1 mm/min. The experimental as well as the corresponding numerical results can be seen in Table 3.
Table 3. Experimental and numerical results of lap shear tests.

| \( l_0 \) [mm] | \( F_{\text{max}} \) [N] | \( \tau_{\text{max}} \) [N/mm²] | \( \tau_{\text{max,Sim}} \) [N/mm²] | \( \Delta_{\text{Sim}} \) [%] | \( \tau_{\text{max,Sim}}/\tau_{\text{II,lim}} \) [%] |
|----------------|----------------|----------------|----------------|----------------|----------------|
| 5              | 1425           | 10.59          | 10.61          | 0.2            | 62.4           |
| 12.5           | 2529           | 7.99           | 8.27           | 3.5            | 48.6           |
| 25             | 2763           | 4.47           | 4.29           | -4             | 25.2           |

It can be seen, that there is indeed a strong influence of the overlap length on the resulting shear strength. An increase of 400% in \( l_0 \) only causes an increase of 94% in the maximum tensile force \( F_{\text{max}} \) and a decrease of 58% in the corresponding shear strength \( \tau_{\text{max}} \). Multiple overlap lengths were also considered in the carried-out simulations to investigate this effect by post processing the numerical results. Figure 7a and Table 3 show the same effect as the experiments, the resulting shear strength \( \tau_{\text{max,Sim}} \) declines with increasing overlap length. Moreover, the determined tensile force converges to a maximum (Figure 7b). The crack front evolves with nearly constant shape (see Figure 8b) without significantly increasing force (see Figure 10a) if the overlap length is long enough. The stresses concentrate around the crack front, whereas the still adhering interface areas experience only small stresses (see Figure 9a). The partial delamination causes an elongation of the unconstrained polyamide, which reduces the system stiffness. This reduction in stiffness reduces the increase in tensile force caused by further cross head displacement. From a certain delaminated length, both effects compensate and the delamination continues without increasing tensile force.

![Figure 7](image-url)

**Figure 7.** Overlap length-dependent results obtained by simulated lap shear test (a) shear strength (b) maximum tensile force.

Figure 8 shows the interfacial damage variable CSDMG in the joining zone during the simulated lap shear test. It can be seen that the crack already emerges at small loads (a). The crack front evolves continuously (b) until a tensile force causes additional delamination at the end of the overlapping area (c). Immediately afterwards total failure occurs and the polyamide is separated completely from the aluminum (d). The way of crack propagation that can be seen here fits well to the grey scale correlation data shown in Figure 6.
Figure 8. Damage variable CSDMG in the interface during lap shear test with $l_0 = 12.5$ mm (a) 2.5 mm cross head displacement (b) 7.6 mm cross head displacement (c) 9.3 mm cross head displacement (d) 9.5 mm cross head displacement.

The hollow cylinder torsion tests were carried out with an angular velocity of 5 °/min. The experimental and numerical results are shown in Table 4.

Table 4. Experimental and numerical results of hollow cylinder torsion test.

| $T_{\text{max}}$ [Nm] | $\bar{\tau}_{\text{max}}$ [N/mm²] | $\tau_{\text{max,Sim}}$ [N/mm²] | $\Delta_{\text{Sim}}$ [%] | $\tau_{\text{max,Sim}}/\tau_{\text{II,lim}}$ [%] |
|-----------------------|-----------------|--------------------------|----------------|--------------------------------|
| 25.35                 | 17.91           | 17.76                    | -0.8           | 104.5                         |

The shear strength $\tau_{\text{max}}$ determined by hollow cylinder torsion is, with a value of 17.91 N/mm², about 124 % higher than the 7.99 N/mm² determined by the lap shear test with standard geometry. The corresponding simulation result for the determined shear strength $\tau_{\text{max,Sim}}$ is very close to chosen simulation parameter $\tau_{\text{II,lim}}$ with an error of only 4.5%, which shows that the shear strength value obtained by hollow cylinder torsion is much more reliable than the value obtained by the lap shear test with $l_0 = 12.5$ mm with an error of 51.4%. This significant difference can be explained with the much more homogenous shear stress distribution in the interface. Figure 9 shows the shear stress distribution just before total failure for both testing methods. In the lap shear test, a significant share of the interface is already delaminated, whereas the shear stress in the still connected spots varies between very low values and the critical stress $\tau_{\text{II,lim}}$. In contrast, the shear stress is almost homogenous all over the interface in the hollow cylinder torsion test. The failure occurs nearly instantly after reaching the maximum torsional load without premature crack propagation. The load-displacement curves for both tests can be seen in Figure 10. During the lap shear test, the force increases close to its maximum value and then stays nearly constant despite further increasing displacement due to the crack propagation. During the hollow cylinder torsion test, however the torque increases fitting to the increase of displacement. The declining gradient of the torque curve is caused by the viscoelasticity and -plasticity of the polyamide.
Figure 9. Shear stress distribution in the interface just before failure (a) lap shear test with \( l_o = 12.5 \) mm (b) hollow cylinder torsion test.

Figure 10. Load-displacement curve (a) Lap shear test with \( l_o = 25 \) mm (b) hollow cylinder torsion test.

Figure 11 shows the obtained shear strength values from the experiments as well as from the simulations for all examined specimen geometries. It can be seen that there is a good correlation for the obtained shear strength as well as for the load-displacement curves (see Figure 10), which leads to the conclusion that the numerical model based on the viscoelastic-viscoplastic material model from Kießling in connection with the cohesive behavior contact property is a useful approach for simulating hybrid metal/thermoplastic-compounds.
5. Summary and conclusions
The lap shear test is a commonly used experiment for determining the shear strength of a material compound. In context of other investigations at various metal-thermoplastic compounds within the cluster of excellence MERGE, it showed that the obtained results for the shear strength are varying widely depending on the specimen geometry and the material pairing. Another test, where two hollow cylinders, butt-bonded with the same procedure, are twisted until failure seems more appropriate for determining the shear strength because of a more homogenous shear stress distribution within the interface. To investigate the quality of both testing methods in comparison to each other, a numerical study, validated by experimental results, has been carried out in this work. The numerical models were built up in Abaqus™ 6.14. For the metallic joining partner, a built-in elastic-plastic material behavior could be used, whereas a user defined material model was necessary for the viscoelastic-viscoplastic thermoplastic part. The interfacial damage was modeled with the built-in cohesive behavior contact property. Experiments have been carried out for three lap shear specimen geometries and one hollow cylinder torsion specimen geometry.

The numerical simulations as well as the experiments showed that the results determined by the hollow cylinder torsion test are much more reliable than the results from the lap shear test. The reasons could be seen best in the post processing of the numerical results, where interfacial damage and stresses during the crack propagation were evaluated. During the lap shear test, the crack evolves early before failure with a very inhomogeneous shear stress distribution. In contrast, the failure during the hollow cylinder torsion test occurs nearly instantaneous after the crack evolves. As a result, the critical shear stress obtained from the hollow cylinder torsion test differs only 4.5% from the corresponding simulation parameter, whereas the value obtained from the lap shear test with the standard geometry is 51.4% too low. The results from the lap shear test can be improved by a shorter overlap length, but because of the occurring inhomogeneous stress state, there will always be a significant error.

Consequently, the lap shear test should only be used to qualitatively compare the shear strength of specimens with the same geometry and the same material pairing. It is not appropriate to determine the real shear strength, whereas the hollow cylinder torsion test is well suited. Furthermore, a good correlation between simulation and experiment with regard to crack propagation, the resulting load-displacement curves and the obtained shear strengths shows that the numerical model based on the viscoelastic-viscoplastic material model from Kießling in connection with the cohesive behavior contact property for modeling the interfacial damage is a useful approach for simulating hybrid metal/thermoplastic-compounds.
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