On the simulation of panel distortions due to hot curing adhesives

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Abstract

Hot curing one-part adhesives are often used to bond car body shells. The cure process of the adhesive, however, can lead to distortions, i.e. unwanted, visible deformations of the adherends. In case of outer car panels, these distortions are considered as visual defects, even though the structural integrity might not be affected. In order to avoid distortions by a proper control of the bonding process, a thorough understanding of the development of distortions is necessary. Finite element simulations can help to gain insight into this development. In this work, a simulation model is proposed and used to study the appearance of distortions in a steel sheet over different temperature cycles. The model takes chemical shrinkage and thermal deformation as well as gelation and stress relaxation into account. It was found that the heating rate can affect distortions. Lowering the cure temperature only lowers distortions for high temperature rates. Low heating rates can reduce distortions.

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1. Introduction

Adhesive bonding is a widely-used joining technology in automotive industry. Hot-curing one-part adhesives are well suited for car body manufacturing because of their good oil absorption and flexible applicability. Bonding processes with this type of adhesive are not restricted by pot life or assurance of a correct mixture ratio. Moreover, the necessary heat treatments can be combined with heat treatments in the baking process after electroplastic coating. By that, production steps can be reduced. The cure process of the adhesive at elevated temperatures, however, can lead to distortions, i.e. unwanted, visible deformations of the structure, especially when thin-walled structures as outer car panels are involved. These distortions can occur along the bond line (local distortions) or affect the entire geometry (global distortions). Adapting the production process or repair work can be costly and time-consuming, once these defects occur. Therefore, it is desirable to predict distortions before they arise and modify the manufacturing accordingly to prevent them. Several constitutive models of curing polymers have been successfully used to predict residual stresses in composite manufacturing and other processes (Kiasat, 2000; De Vreugd et al., 2010; Jansen et al., 2012). With regard to car panel distortion, however, models mainly focused on the cooling down phase (Hahn and Jendrny, 2003; Fuchs et al., 2010). These models do not allow for following the development of distortions over the entire cure cycle. They assume a stress-free state when the cooling down sets in. But investigations on residual stresses in laminates show that the structure is not warpage-free if heated up, again, to cure temperature after manufacturing (Gigliotti et al., 2003). The objective of this work is to build up a simulation model for local panel distortions, which is capable of predicting the development of distortions over different cure cycles. It should take known phenomena of curing polymers into account. By that, it will be a basis for analyzing bonding processes with regard to panel distortions.

2. Background

A main cause for panel distortions is the combination of changing properties of the adhesive. During the bonding process the adhesive develops from a viscoelastic liquid to a viscoelastic solid (Ferry, 1980; te Nijenhuis, 1997). At the same time the adhesive's specific volume changes. Due to the cross-linking of polymer chains the density of the adhesive increases. This process is referred to as chemical or reactive shrinkage. The changing temperature leads to additional thermal deformation of the adhesive. The evolving properties of the adhesive are considered the main cause for local distortions, i.e. deformations close to the bond line. Therefore, they are the main focus of this work. But it should be emphasized that further phenomena play a role in panel distortions. The temperature cycle can cause relative movements of the adherends...
3. The model

In this work, a simulation model for developing local distortions due to hot curing adhesives is proposed. The cure process is assumed to be quasi-static. In that case, the balance of linear momentum can be expressed by the equation

$$\frac{\partial \sigma_{ij}}{\partial t} = 0,$$

where $\sigma_{ij}$ is the Cauchy stress tensor. In addition, it is assumed that $\sigma_{ij}$ is split in a mechanical, a thermal and a chemical part:

$$\sigma_{ij} = \sigma_{ij}^{me} + \sigma_{ij}^{th} + \sigma_{ij}^{ch}.$$  

(2)

While several experimental investigations focus on a final state of distortions after the cure process (Andersson, 2009; Bouten, 1990; Fernholz et al., 2007; Patankar et al., 2013), in some works their development over the temperature cycle is examined (Priesnitz et al., 2014; Chudaska and Hahn, 1993, 1994a,b; Hahn et al., 1999). These publications show that significant deformation develops in the cooling phase. But even in the heating and holding phase of a temperature cycle deformation can arise. Changes to the temperature cycle (such as cure temperature, dwelling periods, heating and cooling rates) affect significantly residual stresses and the properties of the adhesive bond (Mathias, 1989; White and Hahn, 1993; Hahn et al., 1993). This knowledge is already applied in cure cycle optimization. White and Hahn (1993) found that at elevated temperatures before the final cure temperature is reached can reduce residual stresses in composites.

Besides experimental research there have also been several different approaches to predict residual stresses in composites (Dano and Hyer, 1998, 2002; Lopes et al., 2007) or adhesive bonds (Hahn and Jendrny, 2003; Fuchs et al., 2010; Patankar et al., 2013). Prediction models based on elastic material behavior (Hahn and Pagano, 1975; Plepyss et al., 1994; Yu et al., 2006; Basu and Kia, 2008; Jumbo et al., 2010) are available as well as models based on viscoelasticity (Hahn and Jendrny, 2003; Fuchs et al., 2010; Patankar et al., 2013). These approaches take only the cooling down phase into account. Stresses in the heating and holding phase are neglected. Other constitutive models of curing polymers (Adolf and Martin, 1996; Adolf and Chambers, 2007; De Vreugd et al., 2010; Liebl et al., 2012a,b; Jansen et al., 2012; Kiasat, 2000) allow to capture the full cure cycle. They indicate that stresses can occur before the cooling down sets in. These models have been successfully applied to coating processes (Jansen et al., 2012), electronic packaging (De Vreugd et al., 2010) and composites manufacturing (Kiasat, 2000). An application to local panel distortions due to adhesive bonding is missing.

In some prediction models the chemical shrinkage is not accounted for (Hahn and Jendrny, 2003; Basu and Kia, 2008). Some authors argue that the contribution of chemical shrinkage to residual stresses is small (Hahn and Ort, 1997; White and Hahn, 1992). Genidy et al. (2000) state that the low influence of chemical shrinkage on residual stresses in composites predicted in reference White and Hahn (1992) is due to an underestimation as further experimental work by the same authors show (White and Hahn, 1993). Other investigations on the development of panel distortions (Priesnitz et al., 2014; Chudaska and Hahn, 1994a) indicate a significant deformation before the cooling down sets in. Chudaska and Hahn (1994a) reason that this displacement is caused by chemical shrinkage of the adhesive. De Vreugd (2011, p. 70) points out that standard tests for measuring chemical shrinkage are performed by measuring volumes at ambient temperature before and after curing. But the shrinking process takes place at elevated temperatures. Therefore, he measures changes in the specific volume over a full temperature cycle. A three-dimensional simulation model on distortion development would give insight in how chemical shrinkage and other changing material properties mentioned above contribute to the development of local panel distortions.

![Fig. 1. Three different stages of the temperature cycle.](image-url)
the final glass transition temperature of the adhesive. Under these conditions, the rubbery (fully relaxed) modulus dominates the mechanical behavior of the adhesive (Adolf and Martin, 1996; Jansen et al., 2012), and relaxation phenomena are negligible. Thus, the material can be described with temperature (T)- and q-dependent elastic stress–strain relations. For that, the stress tensor \( \sigma \) is split in its deviatoric part \( \sigma_{ij}^{\text{dev}} = \sigma_{ij} - \frac{1}{3} \sigma_{kk} \delta_{ij} \) and the hydrostatic pressure \( p \):

\[
\sigma_{ij} = \sigma_{ij}^{\text{dev}} - p \delta_{ij},
\]

where \( \delta_{ij} \) is the Kronecker delta. The volume deformation is described by

\[
- \dot{p} = K(T) \varepsilon_{kk}^{\text{me}},
\]

with \( \varepsilon_{kk}^{\text{me}} \) being the time derivative of the trace of the mechanical strain tensor. Changes in shape are described by

\[
\varepsilon_{ij}^{\text{dev}} = \frac{2}{G(q)} \varepsilon_{ij}^{\text{me} dev},
\]

where \( G(q) \) is the cure dependent rubbery shear modulus. Eqs. (5) and (6) describe a hypoelastic material. Similar rate equations have been used before to describe evolving properties of polymers (Martin and Adolf, 1990; Hossain et al., 2009; Lion and Johlitz, 2012). Hossain et al. (2009) point out, that these equations reflect a physical observation of cure: the stress state will remain unaltered if the applied deformation does not change, even though the elastic properties of the material evolve.

As in de Vreugd (2011), the bulk modulus \( K \) is conveniently approximated based on a modified Tait equation:

\[
K = \frac{1}{B(T)},
\]

\[
B(T) = b_1 \exp(-b_2 T),
\]

\[
\beta(T) = k_1 S_0 + \frac{1}{2} k_2 S_0 (1 + \tanh(C_1 (T - T_g))) + \frac{C}{B(T)},
\]

\[
C = \frac{1}{C_0}.
\]

where \( C_0 \) and \( C \) are material constants. \( \beta(T) \) is usually chosen to be 0.0894 (Zoller and Fakhredinne, 1994). \( T_g \) is the glass transition temperature of the fully cured adhesive. A plot of the bulk modulus versus the temperature with the material parameters used (Appendix A) can be found in De Vreugd et al. (2010) and de Vreugd (2011).

A function for a degree-of-cure-dependent shear modulus is proposed by Adolf and Martin (1996) and used in Jansen et al. (2012) among others. This function is used to describe the elastic shear modulus of stage II:

\[
G(q) = G_{\text{final}} \left( \frac{q^2 - q_{\text{gel}}^2}{1 - q_{\text{gel}}^2} \right)^{\frac{1}{2}},
\]

where \( G_{\text{final}} \) is the shear modulus of the fully cured adhesive and \( q_{\text{gel}} \) is the degree of cure at the gel point.

Besides elastic properties, functions for the thermal and chemical strain need to be determined. The CTE \( \alpha \) is obtained in a similar way as the bulk modulus (De Vreugd et al., 2010; de Vreugd, 2011), resulting in:

\[
\alpha(T) = \frac{1}{3} \left[ k_1 + \frac{1}{2} k_2 (1 + \tanh(C_1 (T - T_g))) \right].
\]

The parameter \( C_1 \) in Eqs. (8) and (10) was introduced by the authors to smooth the transition between a constant CTE below and above \( T_0 \). Here, we only take thermal strain into account that occurs beyond the gel point:

\[
\varepsilon_{ij}^{\text{th}}(T) = \varepsilon_{ij}^{\text{th}}(T_g) + \int_{T_g}^{T} \alpha(T) dT,
\]

with \( T_g \) being the temperature at which the adhesive reaches the gel point in the cure cycle. De Vreugd et al. (2010) found a linear correlation between chemical shrinkage and the degree of cure. In this model we only consider strain beyond the gel point:

\[
\varepsilon_{ij}^{\text{ch}}(q) = \varepsilon_{ij}^{\text{ch}}(T_g),
\]

\[
\varepsilon_{ij}^{\text{ch}}(q) = (q - q_{\text{gel}}) \varepsilon_{ij}^{\text{ch}}(T_g),
\]

where \( \varepsilon_{ij}^{\text{ch}} \) is the total linear chemical strain at the cure temperature (~1/3 of the volumetric chemical strain) after full cure.

3.3. Stage III

In stage III chemical and thermal strain are calculated as before by Eqs. (10)–(12).

Also, as in stage II, volume changes are assumed to be elastic with a temperature-dependent bulk modulus (Eqs. (5), (7), (8) and (9)). The deviatoric part is defined by a linear viscoelastic relation. The stress rate reads

\[
\dot{\sigma}_{ij}^{\text{dev}} = 2G_m \dot{\varepsilon}_{ij}^{\text{me} dev} + 2 \sum_{m=1}^{M} G_m \dot{\varepsilon}_{ij}^{m dev},
\]

\[
\dot{\varepsilon}_{ij}^{m dev} = \varepsilon_{ij}^{m dev},
\]

\[
\varepsilon_{ij}^{m} = \varepsilon_{ij}^{m} - \frac{t_{m-1} t_{m}}{t_{m}} \varepsilon_{ij}^{m dev},
\]

\[
G_{\infty} = \frac{M}{t_{m}} G_{m} \exp(-t/t_m),
\]

with the according relaxation times \( t_m \) (cf. Zienkiewicz and Taylor, 2000).

In general, a viscoelastic model with temperature and degree of cure dependent relaxation times can be used to describe all three stages of the cure cycle. However, for cure at temperatures high above the final glass transition temperature, such a model would not improve the prediction of distortions. Relaxation effects over different extent of cure are small and difficult to measure in that temperature range. They hardly affect stress calculations. Cure cycles with these high temperatures are common for bonding car body shells. Therefore, switching the constitutive model does not come with any drawbacks for that specific application.

3.4. ABAQUS implementation

The equations mentioned above were implemented in ABAQUS subroutines. This allows to apply the material law to arbitrary geometries. ABAQUS solves Eq. (1). The degree of cure is treated as an additional field variable and calculated in the subroutine USDFLD. An explicit Euler scheme is used to calculate q-increments from Eq. (2). Thermal and chemical strain are calculated in the subroutine UEXPAN. Eq. (11) is approximated with the trapezoidal integration scheme.

In ABAQUS it is not possible to switch between the elastic and viscoelastic material model. Therefore, both were implemented as subroutines and the switch is done in ABAQUS’ UMAT subroutine. To obtain an incremental scheme, the time derivative of Eqs. (4)–(6) are approximated by applying the simple but stable increment scheme

\[
\int_{T_{g+1}}^{T_{g+2}} = \frac{\Delta T}{2},
\]

\[
\int_{T_{g+1}}^{T_{g+2}} = f_{\text{th}} + \frac{\Delta T}{2},
\]

where \( f \) is replaced by the corresponding stress or strain component. The same scheme is applied to the Eqs. (13) and (14). The tangent moduli \( \frac{\Delta \sigma_{ij}^{\text{dev}}}{\Delta \varepsilon_{ij}^{\text{me} dev}} \) are needed for the solving algorithm of ABAQUS are calculated in the same manner.
3.5. Numerical issues

The adhesive is a liquid in stage I. Strain that occurs in that stage is assumed to not cause any static stresses other than hydrostatic ones; neither in this stage, nor in later stages. Therefore, in this approach, the simulation of stage I is done without the adhesive. At the transition to stage II, the adhesive is inserted in the model with the *MODEL CHANGE statement in ABAQUS. This function allows to insert the adhesive elements at the gel point in a strain-free state.

A sudden switch between the material models of stage II and III can cause oscillations in the stress and strain functions. Therefore, the stress tensor is calculated by linear interpolation between both models in a transition region between $T_g$ and $T_g + T_{\text{trans}}$:

$$\sigma_y = (1-w)\sigma_y^{\text{el}} + w\sigma_y^{\text{vis}}.$$  

where $\sigma_y^{\text{el}}$ is the elastic stress tensor, calculated with the Eqs. (4)–(6), and $\sigma_y^{\text{vis}}$ the viscoelastic one, using Eqs. (13) and (14) instead of (6). $w$ is defined as

$$w = \begin{cases} 1 + \frac{T - T_g}{T_{\text{trans}}} & \text{for } T_g \leq T \leq T_g + T_{\text{trans}} \\ 0 & \text{for } T > T_g + T_{\text{trans}} \\ 1 & \text{else} \end{cases}.$$  

The tangent moduli are calculated accordingly:

$$\frac{\partial \Delta \sigma_{ij}}{\partial \Delta e_{ij}} = (1-w)\frac{\partial \Delta \sigma_{ij}^{\text{el}}}{\partial \Delta e_{ij}^{\text{el}}} + w\frac{\partial \Delta \sigma_{ij}^{\text{vis}}}{\partial \Delta e_{ij}^{\text{vis}}}.$$  

3.6. Simulation of local distortions

The bonding between a 0.7 × 50 × 20 mm steel sheet and a 8 × 50 × 20 mm steel bulk part with a 10 mm broad and 2 mm thick bond line is simulated (see Fig. 2). The material parameters for the steel are $E = 210 \text{ GPa}$, $\nu = 0.3$, $\alpha = 1.3 \times 10^{-5} / \text{K}$. Parameters for the adhesive are taken from De Vreugd et al. (2010) (material C) and can be found in Appendix A. The material used there is an epoxy molding compound for electronic packaging.

Different cure processes are investigated, in which the structure is exposed to the temperature cycles listed in Table 1. The cycles share the same cooling rate of 5 K/min. The cycles start and end at 20 °C.

4. Results

For better comparison, all curves are shifted on the time scale so that the cooling down phase starts at $t = 0$.

4.1. Cure cycle A

Fig. 3 shows the temperature curve and the evolution of cure in cycle A. In this cycle, the chemical and thermal deformation “compete” in the heating phase (Fig. 4). When the cure temperature is reached, the sum of both components is above zero, i.e. the sum of chemical and thermal deformation leads to an increased volume of the adhesive (mechanical strain neglected, see Eq. (3)). The difference in $y$-displacement of node $N_1$ and $N_2$, $\Delta u_y = u_y(N_2) - u_y(N_1)$, is monitored over time (see Fig. 2). As can be seen in Fig. 5, the steel sheet shows a first downwards and then upwards movement in the heating phase. At cure temperature, however, the steel sheet stays in a downwards bent state before it gets pulled down further in the cooling phase.

Fig. 6 illustrates the stress and strain development in the adhesive layer over cycle A. The curves show $\sigma_{xy}$ and $\varepsilon_{xy}$ at an integration point positioned at $x = (2.211, -1.542, 5.789)$ mm in the reference configuration (see Fig. 2).

4.2. Other cure cycles

Figs. 7 and 8 show the displacement of the steel sheet for the different cure cycles. The temperature at which the gel point is reached in the temperature cycle, $T_{\text{gel}}$, changes with the temperature rate in the heating phase, see Fig. 9. For the cycles H155 and H170, $T_{\text{gel}}$ is equal to the cure temperature and would not increase with higher temperature rates.

![Fig. 2. Structure for simulating local distortions. The $y$-displacement is inhibited at the edges marked in blue. The symmetry allows to reduce the simulation model to the colored quarter piece.](image)
The low rates show a first downwards movement followed by an upwards movement of the steel sheet before the cure temperature is reached. This behavior can also be seen in the curves for the cycles A and M170. In all other cycles the sheet moves downwards only.

Fig. 10 shows the displacement after different cycles. The model predicts the same displacement for both cure temperatures with low and medium heating rates.

The subroutine switched to the transition region between stage II and III at \( t = 820 \text{ s} \) for cure at 170 °C and at \( t = 620 \text{ s} \) for cure at 155 °C, respectively. The displacement curves in Figs. 5, 7 and 8 start to gradually flatten from that point on.

### 4.3. Long-term behavior

The Prony series for the shear modulus of the material contains large relaxation times. This allows to investigate the long-term behavior of the material. For that reason, another time step after the cure cycle was calculated, where the material stays at 20 °C and has time to reduce distortions by relaxation. The step time is two years. For all cure cycles the displacement changes less than 0.13% in these two years after the cure cycle.

### 5. Discussion

#### 5.1. Cure cycle A

Fig. 4 shows that chemical and thermal strain are in the same order of magnitude for the material combination and temperature cycle investigated. Therefore, neglecting chemical deformation is not justifiable. The downwards and upwards movement of the steel sheet in the heating phase can be explained by thermal and chemical strain in that phase, which leads first to a decrease and later to an increase in the volume of the adhesive (mechanical strain neglected). In the holding phase the sheet remains in a downwards bent state. Experimental investigations on similar geometries (Priesnitz et al., 2014; Chudaska and Hahn, 1993, 1994a,b; Hahn et al., 1999) show the same behavior. In the case at hand, however, the displacement cannot be explained with a volume reduction that is caused by reactive shrinkage, since the volume increases (mechanical strain neglected), see Fig. 4. Hence, the mechanical part of the strain tensor must cause the downwards displacement. Further study revealed that in this specific material combination the adherends expands even more than the adhesive. This leads to a Poisson effect. The adhesive is stretched...
in-plane direction (the x-z-plane, see Fig. 2) and contracts in out-of-plane direction. The Poisson effect is caused by a low CTE of the polymer. A high CTE of the adhesive would cause an inverse effect and an upwards displacement. Due to the temperature dependent CTE of the adhesive, both effects can occur in one temperature cycle. In the cooling phase these effects are partly reversed since the thermal deformation of the adherends decreases.

The increase of \( \varepsilon_{xy} \) from the gel point on (see Fig. 6) does not immediately lead to shear stress \( \sigma_{xy} \). This is due to the shear modulus that is still zero at the gel point. With the shear modulus
increasing, changes in strain lead to changes in stress. When the strain stays constant in the holding phase, the stress does not alter, either. At the transition to stage III, the stress curve raises slightly steeper. This is due to the increased instantaneous stiffness in the viscoelastic model.

5.2. Other cure cycles

The low heating rates show the same downwards–upwards movement as in cycle A. The development of the chemical and thermal strain explains this behavior. For the medium and high...
rates the gel point is at higher temperatures. Therefore, the thermal strain causing displacements in the heating phase is less dominant. The curves in Figs. 7 and 8 show already significant differences at the end of the holding phase (t = 0). These differences do not change much in the cooling down phase. Models focusing on the cooling down only, would not be able to show this difference.

The model predicts the same displacement for both cure temperatures with low and medium heating rates. That implies that lowering the cure temperature from 170 to 155 °C would not decrease the distortions for these rates. Increasing the temperature rate in the heating phase leads to more absolute final displacements. Lowering the cure temperature reduces distortions for these rates. Increasing the temperature lowering the cure temperature from 170 to 155 °C at a rate of 21 K/min, respectively. Therefore, only between these two rates the displacement curves afterwards.

The steepening of the displacement curves during the transition to stage III in Figs. 5, 7 and 8 can be explained by the increase in the stiffness due to the switch to the viscoelastic material law. The gradually decreasing CTE (see Fig. 4) at \( T_g \) leads to a flattening of the displacement curves afterwards.

6. Conclusions

A model for local panel distortions due to hot-curing adhesives is proposed and used to study the development of local panel distortions. Displacements occurring during the cure process can not only be caused by chemical shrinkage but also by a Poisson effect where the adhesive is stretched in in-plane direction of the steel sheet. The temperature curve, especially the heating rate, affects final distortions. Lowering the cure temperature reduces distortions only for high temperature rates. Lower temperature rates in the heating phase lead to a gelation at lower temperature and to less distortions.

In future research, the material properties of a commercial automotive adhesive will be investigated. A comparison between distortion predictions based on the model presented here and experimental studies (Priesnitz et al., 2014) is planned.

Acknowledgments

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Appendix A. Material parameters of the adhesive

In the appendix of de Vreugd’s thesis (de Vreugd, 2011) the Prony terms of the tensile modulus \( E(t) \) can be found. From that function the shear relaxation modulus is approximated by

\[
G(t) = \frac{3K_0E(t)}{9K_0 - E(t)}
\]

with \( K_0 = 17.136 \text{ MPa} \) being the bulk modulus at 60 °C, see Eqs. (7)-(9). By fitting Eq. (15) to that function the parameters below are obtained with \( G_{\infty} = G_{\text{final}} = 590 \text{ MPa} \).

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