Numerical simulation of the activation behavior of thermal shape memory alloys

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ABSTRACT

Problems in using shape memory alloys (SMA) in industrial applications are often caused by the fragmentary knowledge of the complex activation behavior. To solve this problem, Fraunhofer IWU developed a Matlab®-based simulation tool to emulate the properties of a SMA wire based on the energy balance. The contained terms result of the characteristic material behavior combined with thermal, electrical, and mechanical conditions. Model validation is performed by laboratory tests. It is shown that there is almost no difference between the measured and the simulated actuator movement. Due to the good quality of the model it is possible to use it in a control loop. Knowing current and voltage enables the computation of the electrical resistance of the actuator and can therefore be used for feedback control. Implementation of the results into industrial applications is exemplified by integration of an actuator in a flap as used in air condition systems of cars. Furthermore, the SMA-based drive will be compared to an electromechanical drive.

Keywords: Shape-Memory-Alloy, modeling, control, self-sensing

1. INTRODUCTION

Due to intense material research in recent years the performance of thermal shape-memory-alloys (SMA) increased considerably, which resulted in numerous new application fields for this kind of intelligent material. The most promising fields of application are determined by the specific actuation characteristics of the material. In addition to thermal activation a very important property is the very high specific workload of actuators made from SMA. A comparison of the volume specific workload of different actuator types is shown in Figure 1 [1]. From the graph it is obvious that SMA-actuators reach very high work load levels which are nearly five orders of magnitude higher than those obtained for small DC-drives. Research at the Fraunhofer IWU is therefore focused on the development of small SMA-drives. The major benefits are a significantly reduction in scale, weight, and costs of such drives.

![Graph showing specific work load comparison](image)

Figure 1: Comparison of different actuator types and application of SMA-wires in switching drives.

Thermal shape memory alloys have the special ability to “remember”and re-assume their original shape following permanent plastic distortion below a specific critical temperature by means of heating to above this temperature. A reversible austenite-martensite phase transformation is requirement for the development of the shape memory effect. Analogous to steel the high temperature phase $\beta$ of the material is also described as austenite and low temperature phase $\alpha$ as martensite. In an ideal situation the austenite $\beta$ phase is converted into the martensite $\alpha$ phase as a result of shear. Due to diffusion-free rearrangement processes in relation to the atoms this generates a change in the stacking sequence of the crystal lattice levels and therefore a change in the structure of the crystal lattice. Consequently, two different stress-

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strain-curves exist as shown in Figure 2. In the low-temperature phase a small Hook region is followed from a so-called plateau-stress where the wire can easily be deflected almost without increasing the applied external stress. After setting the stress to zero, a plastic deflection remains at the wire. Heating the wire causes the described phase transition and results in a completely different stress-strain-behavior. The Hook region is significantly wider, the Young-modulus is two to three times higher and applying a high amount of stress causes a so called super-elastic-behavior. During the phase transition from martensite to austenite (heating) the wire is able to produce mechanical work (see Figure 3 a). The amount of work depends on the mechanical boundary conditions of the wire. In case of a free wire the amount of work would be zero, but the actuator deflection would be maximal. In contrast, blocking the wire causes a very high actuation force but no deflection. The work output is also zero. Using a spring with a defined stiffness as boundary element instead causes a deflection as well as a reaction force and therefore a usable workload. The amount of that workload depends on the stress-strain-curves of the used material and the design of the spring. The mechanical design of such actuators has been described previously and details can be found in [2].

Figure 2: Energy balance as base of the modeling.

The estimated benefits of SMA wire actuators, i.e. reduced mass, scale, and cost meet the current demands of the automotive industry. Many of the drives in modern cars need to perform a linear stroke of several millimeters with a force level ranging from several Newton up to several 10 Newton. A part of them, like locking devices exclusively operate in switching mode. A drive for locking a petrol cap is shown as example in Figure 1 b. Instead of using a DC-drive with gear head and steering rod the same function can be performed by using a simple SMA wire possessing a significantly reduced number of parts, mass, size and cost. The design process of a switching drive is focused on mechanical function. The thermal design only has to prevent an unwanted activation by environmental heat and consequently no detailed thermal model is needed.

Besides switching drives there is an increasing demand of position controlled drives such as headlight adjustment systems or flaps for the air condition control unit. To realize these drives using SMA-actuators a position control of the wire is necessary. To design an adequate controller the limitation to mechanical aspects is not sufficient. In fact the thermal behavior of the actuator has to be rather focused. Therefore, an overall model to simulate the controlled SMA-drive and above all the control design is necessary. In order to promote the industrial application of SMA-actuators the model should not only be used to simulate the actuator system, it also has to clear the essential aspects of control design, concerning a clearly arranged design of a simple control algorithm.

2. NUMERICAL MODEL OF THE SMA-WIRE-ACTUATOR

For modeling SMA-actuators different approaches are known. Micro structural approaches are used to describe physical processes, but are not suitable for describing the macroscopic behavior of an actuator. Previously described models [3] utilize ideas from statistics and thermodynamics and describe the evolution of two martensite fractions based on the theory of thermally activated processes by using transition probabilities. However, such models are usually suitable for simulating the macroscopic behavior [4], but due to extensive mathematical demands not usable for linearization purposes or common system analysis. Another way for modeling SMA-actuators is to use phenomenological models. The problem in using such approaches is not to loose the physical dependencies by using the mathematical approaches. For example the given model in [5] describes the stationary hysteretic transfer behavior from electrical current to actuator displacement using a Preisach Model. The implemented parameters are derived by mathematical approaches and do not carry physical information about the actuator. In contrast, the modeling approach reported in [6] constrains the mathematical approach to the inner behavior of the material. This yields physically defined model parameters, which are measured or are given in the material specification.
To solve the problem of understanding the complex activation behavior and to determine different control algorithms, Fraunhofer IWU developed a Matlab®-based simulation tool, which is based on the approach given in [6], to emulate the properties of an SMA wire [7]. The tool is based on the power balance shown in Figure 2. The contained terms result of the characteristic material behavior combined with the thermal and mechanical boundary conditions and can be described briefly by

\[ P_{\text{el}} = P_{\text{local}} + P_{\text{therm}} + P_{\text{mech}}. \]  

(1)

The energy input is represented by a defined electrical current which is applied to the SMA-wire. As a result of the electrical resistance of the wire thermal energy is induced and an activation power is delivered to the SMA-material. This power meets some negative feedback mechanisms such as thermal convection and thermal radiation. The residual thermal power applied to the SMA-actuator causes a temperature variation. A specific characteristic in the system behavior of the SMA-actuator is the discrete occurrence of the phase transition process, which is modeled by a finite state machine. That enables to change the phase transition process depending on rising or falling temperature.

\[ \xi(\theta) = -\frac{1}{\pi} \xi_{\text{start}} \cdot \text{atan} \left[ a \cdot \left( \theta - \frac{1}{2} (A_s + A_f) \right) \right] + \frac{1}{2} \xi_{\text{start}} \]  

(2)

Herein \( \xi \) is the volume percentage of martensite in the material, \( A \) is the phase transition temperature of the material and \( a \) is a coefficient describing the curvature of the function. In fact the stress dependency of the transition temperatures has to be considered. According to the literature a linear approach is suitable. In [6] the stress-dependent phase transition temperatures are calculated by:

\[ A_s = A_{s,\text{start}} + \frac{\sigma}{C_A} \quad \text{and} \quad A_f = A_{f,\text{start}} + \frac{\sigma}{C_A} \]  

(3)

for the transition into the austenite structure and by:

\[ M_s = M_{s,\text{start}} + \frac{\sigma}{C_M} \quad \text{and} \quad M_f = M_{f,\text{start}} + \frac{\sigma}{C_M} \]  

(4)

for the re-transition into the martensite structure. The temperatures \( A_{}\text{start} \) and \( M_{}\text{start} \) and the coefficients \( C_A \) and \( C_M \) have to be obtained by measurements on the used material since most suppliers do not provide them in the material specification. The wire temperature \( \theta \), which is responsible for the amount of martensite in the wire, depends on the input energy and the output energy. The input energy for an electrical heated wire is given by:
\[ P_{el} = U \cdot I = I^2 \cdot R(\xi) \]  

were \( R \) is a function of the amount of martensite existing in the wire. The systems output energy is defined by thermal losses like the convection power, the radiation power, the conductive power, and by the achieved mechanical power. The mechanical power depends on the boundary conditions of the actuator.

2.2 Modeling mechanical power

In case of using the actuator in connection to an elastic spring (see Figure 3 a) the stress in the wire is given by:

\[ \sigma = \frac{F_{\text{start}} + \Delta L \cdot c_{\text{spring}}}{A_q}. \]  

The resulting mechanical power is described by:

\[ P_{me} = \sigma \cdot A_q \cdot \left( -\frac{d\Delta L}{dt} \right) = \sigma \cdot A_q \cdot L_{\text{Start}} \left( -\frac{d\varepsilon}{dt} \right) = \sigma \cdot A_q \cdot L_{\text{Start}} \left( -\frac{d}{dt} \left( \varepsilon_{\text{el}} + \varepsilon_{\text{plast}} + \varepsilon_{\text{th}} \right) \right). \]

The actuator stroke \( \Delta L \) directly results from the actuators strain. It consists of a plastic deflection \( \varepsilon_{\text{plast}} \) recovered during the phase transition, an elastic part \( \varepsilon_{\text{el}} \) influenced by the changing Young’s Modulus and a thermal deflection \( \varepsilon_{\text{th}} \). The plastic deflection can be described by:

\[ \varepsilon_{\text{plast}} = \varepsilon_{\text{plast, start}} \cdot \xi. \]

The elastic deflection of passive wires can be described as:

\[ \varepsilon_{\text{el}} = \frac{\sigma(\Delta L)}{E(\xi)}. \]

In contrast to passive materials the Young’s Modulus changes with the ongoing phase transition and has to be considered in the model by:

\[ \frac{1}{E} = \frac{\xi}{E_M} + \frac{1 - \xi}{E_A}. \]

For describing the thermal deflection \( \varepsilon_{\text{th}} \), the temperature difference between the wire and the environment:

\[ \Delta \vartheta = \vartheta - \vartheta_E \]

is defined as system variable. The thermal deflection can be calculated by the equation suitable for passive materials:

\[ \varepsilon_{\text{th}} = \beta(\xi) \cdot \Delta \vartheta, \]

but also have to be enlarged by a term describing the dependency of the thermal expansion coefficient \( \beta \) on the phase transition state:

\[ \beta = \xi \cdot \beta_M + (1 - \xi) \cdot \beta_A. \]

Considering all these relations Eq. (7) can be written in complete form:

\[ P_{me} = \sigma \cdot A_q \cdot L_{\text{Start}} \left[ -\frac{d}{dt} \sigma(\Delta L) \left( \frac{\xi}{E_M} + \frac{1 - \xi}{E_A} \right) + \varepsilon_{\text{plast, start}} \cdot \xi + \left( \xi \cdot \beta_M + (1 - \xi) \cdot \beta_A \cdot \Delta \vartheta \right) \right]. \]

2.3 Modeling internal heating power

The internal thermal power transformed in the actuator consists of a part resulting in a temperature change of the actuator \( P_{\text{temp}} \). This amount of power is equal to passive materials and can be calculated by:

\[ P_{\text{temp}} = m \cdot c_p \cdot \Delta \dot{\vartheta} \]
referring to the materials heat capacity \( c_p \) and its mass \( m \). Due to the specific behavior of the SMA material an additionally heat power has to be considered. The energy needed for the phase transition have to applied during the transition from martensite state to austenite state (increase wire temperature). This energy is released whilst cooling the wire. According to [6] this power can be described by:

\[
P_{\text{trans}} = H \cdot m \cdot \dot{\xi}.
\] (16)

The coefficient \( H \) is called specific transition enthalpy and can be obtained from supplier information or by performing a differential scanning calorimetry on the used SMA-material. Combining Eq. (15) and (16) yields the description for the internal heat power:

\[
P_{\text{heat}} = m \cdot \left( c_p \cdot \Delta \vartheta + H \cdot \dot{\xi} \right)
\] (17)

2.4 Modeling thermal losses
As shown in [7], [8] thermal losses add the biggest part to the energy conversion in the system. Because of the low operation temperature the radiation part of thermal losses is much smaller than the convection part. Neglecting the radiation part will not cause any significant inaccuracies in the model. The conduction part can also be neglected, justified with the small clamping and the small diameter of the wire.

![Figure 4: Convection losses.](image)

The convection power in general form then is described as:

\[
P_{\text{th}} = A_{\vartheta} \cdot \alpha \cdot \Delta \vartheta.
\] (18)

In Eq. (18) \( \alpha \) is called the heat transmission coefficient and contains the heat conductivity \( \lambda \), the Nusselt-Number \( \text{Nu} \) and the characteristic flow length for the horizontal cylinder body \( d \cdot \pi/2 \):

\[
\alpha = \lambda \cdot \frac{1}{d \cdot \pi/2} \cdot \text{Nu}
\] (19)

The non-dimensional Nusselt-number represents the geometric influence of the heat transmission. The value results from numerical solutions of the heat transmission relations. It is independent of geometric dimensions, and is described by approximating functions. The scaling to real geometry is done by implementing the characteristic flow length. For simple geometries, like for instance horizontal cylinder bodys, the Nusselt-number is given by [9]:

\[
Nu = \left\{ \begin{array}{ll}
0.752 + 0.387 \left\{ \frac{Pr \cdot \left( d \cdot \pi/2 \right)^3}{\nu^2} \cdot \frac{\Delta \vartheta}{\vartheta_E + 273.15 \vartheta} \left( 1 + \left( \frac{0.559}{Pr} \right)^{9/6} \cdot \nu^{1/6} \cdot \vartheta^{1/6} \right)^2 \right\} & \text{if } Pr \leq 65 \left( \frac{97}{\nu} \right)^{1/3} \\
0.752 + 0.387 \left\{ \frac{Pr \cdot \left( d \cdot \pi/2 \right)^3}{\nu^2} \cdot \frac{\Delta \vartheta}{\vartheta_E + 273.15 \vartheta} \left( 1 + \left( \frac{0.559}{Pr} \right)^{9/6} \cdot \nu^{1/6} \cdot \vartheta^{1/6} \right)^2 \right\} & \text{if } Pr > 65 \left( \frac{97}{\nu} \right)^{1/3}
\end{array} \right.
\] (20)

As consequence of the temperature variance of the heat conductivity \( \lambda \) and the Nusselt-number \( \text{Nu} \), the heat transmission coefficient \( \alpha \) is also strongly non-linear.
The detailed numerical model can be developed by combining the several terms of energies. Inserting Eq. (5), (14), (17) and (18) in Eq. (1) results in the detailed power balance of the SMA-wire actuator. In combination with the Eq. (2) and Eq. (3), (4) describing the material behavior the overall model can be completed. The developed equation can not be solved in general closed form but simulated in a numerical simulation tool. Matlab®/Simulink® is a graphical oriented tool for numerical simulations of dynamic systems and suitable for the model of the SMA-wire actuator. The equations mentioned here are arranged like it is shown in Figure 5 a. It is obvious, that there are many feedback loops concerning the thermal and the mechanical behavior of the actuator.

The validation of the model was done by several laboratory measurements. For instance an SMA-wire was deflected, by applying a mass at the end of it. The input power was supplied by a laboratory power amplifier driven in current control mode. Figure 5b shows the resulting actuator stroke that was measured by a triangulation sensor. The simulated actuator stroke fits with the measured one what implies high model accuracy. This accuracy is reached by representing all existing power types in an extending form with mostly non-linear terms. Due to that the model gets complex and is suitable for optimization of defined arrangements but it is rather not suitable for the analytical design of a simple control algorithm.

3. MODEL REDUCTION FOR CONTROL DESIGN

3.1 Model reduction concerning material behavior and mechanical workload

For an effective control design a linear description of the control plant is essential. Due to the thermal hysteresis a linearization of the model described (see point 2) at a defined work-point is not possible. Hence an other approach using piece-wise straight lines for $\xi(\theta)$ can be developed. The principle idea is shown in Figure 6 a. The transition between the straight lines effects discrete and is triggered by over- or under running a defined temperature. The description of the event-discrete behavior is realized by defining the states and transitions shown in Figure 6 b. A complete pass through the hysteresis is preconditioned. In state $Z_2$ the amount of martensite $\xi$ is given by:

$$\xi = 1 - \frac{\theta - A_s}{A_f - A_s}.$$  \hfill (21)

and in state $Z_4$ it is defined by:

$$\xi = 1 - \frac{\theta - M_f}{M_s - M_f}.$$  \hfill (22)

In Eq. (21) and (22) the terms $M_s$, $M_f$, $A_s$, $A_f$ are the phase transition temperatures (PTT) of the material under prevailing stress conditions. In states $Z_1$ and $Z_5$ the material has a complete martensitic or austenitic structure and behaves as passive material without actuating characteristics.
Regarding to Eq. (7) the mechanical power of the actuator can be written as:

\[ P_{mech} = \sigma \cdot A_y \cdot L_0 \left( -\frac{d\varepsilon}{dt} \right). \]  

(23)

Considering only the plastic part of the actuator deflection (see Eq. (8)) the time derivation of the strain \( \varepsilon \) can also be described in dependency of \( \xi \):

\[ \dot{\varepsilon}_{plast} = \dot{\varepsilon}_{plast, start} \cdot \xi. \]

(24)

The insertion of Eq. (24) into Eq. (23) and the relation to the enthalpy power \( P_{trans} \) given in Eq. (16) leads to the proportion of enthalpy power to mechanical power:

\[ \frac{P_{max}}{P_{mech}} = \frac{H \cdot m \cdot \dot{\xi}}{\sigma \cdot A_y \cdot L_0 \cdot \varepsilon_{plast, start} \cdot \xi} = \frac{H \cdot \rho_{SMA}}{\sigma \cdot \varepsilon_{plast, start}}. \]

(25)

Considering typical values for a NiTi-based SMA-materials as for instance 6450 kg/m\(^3\) density \( \rho_{SMA} \), 20000 J/kg transition enthalpy \( H \), 100 MPa applicable stress \( \sigma \) and 3% durable strain \( \varepsilon \), the enthalpy power is more than 40 times higher than the achievable mechanical power. Due to this the mechanical power can be neglected, in spite of being the only usable power. This clarifies the low efficiency of SMA-wire actuators.

### 3.2 Model reduction concerning thermal losses

To obtain a linear system description, the convection losses \( P_{th} \) also have to be described in a linear term. In [10] a linear approach is presented but the parameters were defined by measurements of the thermal behavior. Rather an approach based on analytical model as given in Eq. (18) can be developed. The problem in Eq. (18) is the strong nonlinear curvature of the thermal convection coefficient \( \alpha \) (see Figure 4 a). The dependency on the temperature is especially strong at the lower temperature region \( \Delta \vartheta \) from in 0 K to 20 K. A linearization of the thermal convection coefficient is not possible. The multiplication of \( \alpha \) by the temperature \( \Delta \vartheta \) rather gives a more suitable curvature of the convection power \( P_{th} \). The problem resulting by the linearization is that the power loss then is dependent of the length and the diameter of the wire. Due to the two-dimensional heat transfer process, the wire length only correlates linear with the wire surface \( A_w \). Relating the power to the wire length

\[ p_{th} = \frac{P_{th}}{L_0} = 2 \cdot \lambda \cdot Nu \cdot \Delta \vartheta, \]

(26)

this dependency can be eliminated. Due to the diameter dependency of the Nusselt-number(see Eq. (20)), the convection power now only depends on the diameter of the wire. Wires are only available in few different diameters so that dealing with that constraint does not bring complications. Figure 4 b shows the slightly quadratic curvature of the length related convection power. Using a regression line in form:

\[ p_{th} = k_1 \cdot \Delta \vartheta \]

(27)
results in a suitable approximation of the length related convection power. The gradient of the function can be determined by using well known regression algorithms. The maximum deviation in a region up to 80 K is 5%. For analyzing actuators with different diameters the coefficient \( k_1 \) can be determined relatively easy. The overall convection power \( P_{th} \) can be calculated by multiplying Eq. (27) with the wire length \( L_0 \).

3.3 Linear system description

According to the simplification of the model, the material behavior presented in chapter 3.2 and the linear description of the convection power shown in chapter 3.3, the model of the SMA-wire actuator can now be described in linear form. Considering the different transfer behavior in the states \( Z_1/Z_3 \) and in \( Z_2/Z_4 \), two different models based on Eq. (1) have to be created.

To describe the transfer behavior in states, \( Z_1/Z_3 \) Eq. (1) can be simplified to:

\[
P_{el} = m \cdot c_p \cdot \Delta \dot{\vartheta} + k_1 \cdot L_0 \cdot \Delta \vartheta.
\]

Transforming into Laplace-domain, neglecting the initial value and solving for \( \Delta \vartheta \) results in the dynamic transfer behavior from an electrical input power to the wire temperature:

\[
\Delta \vartheta(s) = \frac{1}{k_1 \cdot L_0} \frac{1}{1 + s \cdot \frac{m \cdot c_p}{k_1 \cdot L_0}} \cdot P_{el}(s).
\]

This behavior corresponds with the one of a first order leg-element. The response time:

\[
T_{13} = \frac{m \cdot c_p}{k_1 \cdot L_0} = \frac{\pi \cdot c_p \cdot \rho \cdot d^2}{4 \cdot k_1}
\]

does not depend on the length but on the diameter of the wire like the convection constant \( k_1 \).

To calculate the transfer behavior of the actuator in states \( Z_2 \) and \( Z_4 \) Eq. (28) has to be extended by the enthalpy power which occurs during the phase transition \( P_{\text{trans}} \). Due to its dependency on the derivation of the amount of martensite \( \xi \), this term firstly has to be transformed to \( \Delta \dot{\vartheta} \). This results in:

\[
\dot{\xi} = \frac{-\Delta \dot{\vartheta}}{A_f,\text{start} - A_s,\text{start} + \frac{c \cdot \Delta L}{A_f, c_A}} = \frac{-\Delta \dot{\vartheta}}{A_f,\text{start} - A_s,\text{start} + \Delta A_{s,f}}
\]

in state \( Z_2 \) and

\[
\dot{\xi} = \frac{-\Delta \dot{\vartheta}}{M_s,\text{start} - M_f,\text{start} + \frac{c \cdot \Delta L}{A_f, c_M}} = \frac{-\Delta \dot{\vartheta}}{M_s,\text{start} - M_f,\text{start} + \Delta M_{s,f}}
\]

in state \( Z_4 \). The last summand in the denominator extends the temperature interval of the phase transition caused by the changing stress in the wire during actuation. The power balance whilst the phase transition can be written as:

\[
P_{el} = k_1 \cdot L_0 \cdot \Delta \dot{\vartheta} + \left[ c_p + \frac{H}{A_f,\text{start} - A_s,\text{start} + \Delta A_{s,f}} \right] \cdot m \cdot \Delta \vartheta = k_1 \cdot L_0 \cdot \Delta \dot{\vartheta} + \left[ c_p + \frac{H}{A_f, c_A} + \Delta c_{ph} \right] \cdot m \cdot \Delta \vartheta
\]

in state \( Z_2 \) and as:

\[
P_{el} = k_1 \cdot L_0 \cdot \Delta \dot{\vartheta} + \left[ c_p + \frac{H}{M_s,\text{start} - M_f,\text{start} + \Delta M_{s,f}} \right] \cdot m \cdot \Delta \dot{\vartheta} = k_1 \cdot L_0 \cdot \Delta \dot{\vartheta} + \left[ c_p + \frac{H}{A_f, c_M} + \Delta c_{pl} \right] \cdot m \cdot \Delta \dot{\vartheta}
\]
in state $Z_d$. The transition enthalpy acts as additional heat capacitance, whereby the first order leg-element-behavior is also valid during the phase transition. In contrast to Eq. (30) the time-constants of the system are different, depending on heating or cooling the wire. They are significantly bigger compared to Eq. (30). That means that the transfer behavior is significantly slower whilst the phase transition.

Figure 5c shows the scheme of the developed linear model. It consists of a first order leg element with a variable time constant and a line based hysteresis element. The hysteresis element defines the state of the actuator in dependency of the temperature. The time constant then is chosen by the actual state of the wire. The model is now suitable for designing a position control loop.

4. CONTROL DESIGN AND IMPLEMENTATION

4.1 Realization of the internal resistance feedback

Development of applications with continuous positioning demands always requires a closed loop control of the actuator stroke. These control loops usually necessitate an external position sensor, which means a huge effort. SMA control loops rather can be developed without an external position sensor, because the material behavior possesses to get information about the actuator stroke only by measuring the resistance [11]. During phase transition from the martensitic to the austenitic lattice and the so involved changes in the structure of the SMA-Material, the status of transition correlates with the electrical resistance. In fact the lattice structure in the austenite state is more regular than in the martensite state, therefore the specific electrical resistance of austenite is significantly smaller than the one of martensite. The information of the actual wire stroke can be determined by measuring the electrical resistance during positioning operations. Compared to the implementation of an external position sensor this can be achieved by significantly less effort, because an electrical interface is needed anyway to control the power input of the actuator.

As shown in the literatures, for instance [6], [8], there are different possibilities to model the length-resistance-correlation of SMA-wires. That varies from elementary linear approaches to complex approaches considering the temperature variance of the specific resistance and the changing geometry during the deflection of the wire. According to planned applications an implementation of a microcontroller is necessary. In combination with the requirements regarding the positioning accuracy of flaps using a linear approach seems to be adequate. In [1] a linear interpolation of the specific resistance from martensite $\rho_A$ to austenite $\rho_M$ in the form:

$$R = \left[ \xi \cdot \rho_A + (1 - \xi) \cdot \rho_M \right] \frac{L}{A_t}$$

is published. Elimination of the martensite amount $\xi$ results in a linear correlation of wire length and resistance:

$$L = L_m - \frac{\Delta L_{\text{rev}}}{\Delta R_{\text{rev}}} \cdot (R_m - R).$$

In this equation $\Delta L_{\text{rev}}$ and $\Delta R_{\text{rev}}$ are the maximal differences of the wire resistance and the achievable stroke during the phase transition (see Figure 7 a). Implementing the correlation to transform a given reference position $L_{\text{ref}}$ into a reference resistance $R_{\text{ref}}$ as described in the following chapter requires a solution of the reference resistance:

$$R_{\text{ref}} = R_m - \frac{\Delta R_{\text{rev}}}{\Delta L_{\text{rev}}} \cdot L_{\text{ref}}.$$ 

It has to be remarked, that the reference position $L_{\text{ref}}$ always represents a contraction of the wire and decreases from the maximum wire length in full martensite state. The required resistance and stroke values can be determined by measurements or calculations regarding the geometrical dimensions and the material parameters of the wire.

4.2 Control design

The model developed in chapter 3.3 includes a hysteresis element, which makes the realization of the position control relatively ambitious. Nevertheless linear control approaches are a suitable choice. This is caused by the simplicity of the algorithms and the possibility to optimize the controller only by adjusting a few parameters. Furthermore such control algorithms are relatively robust concerning possible variations in actuator parameters. Overall linear controllers offer the possibility to realize adequate closed loop controls without being an expert in control design.
The structure of the control loop is displayed in Figure 7 (b). The plant is represented by the simplified model developed in this paper. The reference value of the control loop is calculated by transforming the desired position $L_{\text{ref}}$ with the length-resistance-correlation according to Eq. (37) into a reference resistance value. The control loop in fact is actually a resistance control loop rather than a position control loop. Using this arrangement provides the benefit to change the used length-resistance-correlation without changing the whole control loop. The structure of the control loops considers the negative feedback of the wire resistance. That means an increasing of electrical power $P_{el}$ causes a decreasing of the resistance $R$. Considering the real resistance-length-correlation, as shown in Figure 7 (a), working near the end of the phase-transition-region this coherence changes into a positive feedback causing instability of the control loop. To avoid this suitable sanctions have to be established but they are not focused in this paper. The control loop furthermore consists of the linear controller $G_R$ and a saturation element resulting by the limited performance of the power amplifier and the passive cooling approach.

The hysterisis element and the continuous changes in time constant of the plants avoid the design of one controller for all operation ranges. The controller rather has to be adaptive or an operation range has to be defined. Considering the operation mode of a position control loop, the controller will mostly operate in the actuator states $Z_2/Z_4$ (during phase transition). Designing a non-adaptive controller adjusted to the time constant $T_2$ seems to be adequate because the difference between $T_2$ and $T_4$ is relatively small. Choosing an operating point enables to simplify the whole control structure (see Figure 7 (b)) to a completely linear control loop as shown in Fig. 9 (c). The plant only consists of a simple first order lag element and can be controlled by a simple PI-controller that is written in Laplace-domain as:

$$G_c(s) = k_c \frac{(T_c s + 1)}{s}. \quad (38)$$

Choosing the controller time constant $T_c$ in a way to compensate the plants time constant $T_2$ results for the closed loop in a first order lag-behavior without overshoot as described by:

$$\frac{R(s)}{R_{\text{ref}}(s)} = \frac{1}{k_c \cdot k_p \cdot s + 1}. \quad (39)$$

The time constant of the closed loop is given by:

$$T_{\text{CL}} = k_c \cdot k_p = k_c \cdot \frac{\Delta R_{\text{ref}}}{A_{f,\text{start}} - A_{c,\text{start}} + \Delta A_{c,f}} \cdot \frac{1}{k_1 \cdot L_0} \quad (40)$$

and can be adjusted in a wide range by changing $k_c$. Whereas the theoretical minimum of the time constant $T_{\text{CL}}$ is unlimited in the practical realization the value is limited by the output power of the amplifier.

### 4.3 Realization and validation

The approach discussed in this paper was realized in the testing application shown in Figure 8 (b). This application represents a climate control flap assembled in cars. Such flaps are responsible for guiding the cool or warm air in the passenger room of the car. Regarding the driver’s demands the control unit generates a reference value for the opening angle of the flaps. The opening angle varies continuously from $0^\circ$ (closed) to approximately $45^\circ$ (open). The
requirements concerning actuation forces and the system dynamics are moderate. Actually such flaps are operated by small DC-drives as shown in Figure 8 (a). The DC-drive consists of a stepping motor combined with a multi-staged gear.

![Image of DC-drive](a) gear-headed DC-drive  (b) SMA-wire drive  (c) measured control performance

**Figure 8: Implementation and validation**

The realized SMA-wire drive (see Figure 8 (b)) rather consists of an SMA-wire, a spring to generate the pull-back force, two clamps for wire mounting and a steering rod. The control algorithm described above, was implemented in a microcontroller-based hardware. The controller parameters were determined directly using the simplified plant model, given in chapter 3.3. No additional adjustments during implementation were necessary. The performance of the control loop was determined by laboratory measurements and is shown in Figure 9 (c). The maximal deviation occurs at the opening position of the flap at almost 45° and is about 5% or 2.25°. This is caused by the linear approximation of the stroke-resistance-correlation. Regarding Figure 7 (a) it is obvious that near \( R_A \) the deviation of the approximation is maximal. However, due to the slightly sensitivity of the air flow concerning the fully opened flap this is tolerable.

The benefit using gear-headed drives is the well-known technology of them. Due to the wide-range application of DC-drives in many fields and the ongoing development activities, very well-engineered systems are available at the market. Further benefits are arising with the compact design and the reusability due to the possibility of applying the same drive at different applications. The main attributes of a typical DC-drive for automotive applications are given in Table 1. Establishing SMA-wire drives into automotive applications is only possible if significant benefits can be delivered in comparison to conventional DC-drives. That has to consider first of all technical aspects but above all financial aspects. The estimated benefits have to reach a level where the risks whilst introducing a new technology are tolerable. Benefits in functional attributes are not appropriate because the actually used DC-drives fulfill the requirements in every aspect. The benefits rather have to address financial aspects. That especially becomes important considering the huge quantity needed in automotive industry.

The realized SMA-wire drive meets the functional requirements, concerning actuation force, actuator stroke and operation temperature. The generated benefits in comparison to the DC-drive (see Table 1) are located in the complexity of the drive, concerning a reduced mass, a reduced number of parts and a reduced cross-section. There is furthermore a significant reduction of the production costs noticeable, but it can not be numbered this time.

**Table 1: Parameter comparison of the actual DC-drive and the developed SMA-wired drive**

<table>
<thead>
<tr>
<th>Attribute</th>
<th>DC-Drive</th>
<th>SMA-wire Drive</th>
</tr>
</thead>
<tbody>
<tr>
<td>Reaction time (complete open-close cycle)</td>
<td>3 sec</td>
<td>2…3 sec</td>
</tr>
<tr>
<td>installation space</td>
<td>compact</td>
<td>stretched along the air duct</td>
</tr>
<tr>
<td>acoustic emissions</td>
<td>slightly noise emission</td>
<td>no noise emission</td>
</tr>
<tr>
<td>mechanical complexity</td>
<td>high</td>
<td>low</td>
</tr>
<tr>
<td>mass</td>
<td>approx. 65g</td>
<td>approx. 20g</td>
</tr>
<tr>
<td>number of parts</td>
<td>&gt;20</td>
<td>&lt;10</td>
</tr>
<tr>
<td>positioning accuracy</td>
<td>±1.5°</td>
<td>±2.25°</td>
</tr>
<tr>
<td>energy consumption</td>
<td>1 W during flap movement</td>
<td>1 W permanent</td>
</tr>
</tbody>
</table>
5. SUMMARY AND OUTLOOK

Shape-memory-alloys (SMA) are easy to integrate into mechanical structures and capable of handling high specific workloads. Therefore, SMAs possess an outstanding potential to serve as positioning devices in various applications. In modern cars a multiplicity of electrical drives are used. The applications reach from locking devices over adjusting systems to flap drives. In the air conditioning systems for example exclusively electrical DC-drives are used. Due to its high specific workload, Shape-Memory-Alloys are a promising alternative. The estimated benefit is the possibility to design significantly smaller and cheaper drives in comparison to conventional DC-drives. However, due to the non-linear material behaviour it is often difficult to develop suitable controllers for SMA positioning applications. We presented here the multi-domain modelling of an electrically heated SMA wire which includes changes of electrical parameters in conjunction to mechanical parameters. Due to the correlation between electrical resistance and mechanical stroke it was possible to implement a resistance-based position control without the necessity of an external positioning sensor. In order to design a linear position controller by common rules the highly complex and nonlinear model was simplified to an easy manageable model.

Controller development yielded a PI-algorithm that was implemented on a micro-controller based system as part of a car air condition flap. The performance of the controller was validated during laboratory measurements. Compared to the actually used DC-drives the system fulfills all requirements, with little constraints in position accuracy and power consumption. The main benefit of the SMA-drive is the significant reduction of complexity resulting in a perspicuous reduction in production costs.

Further work will focus on increasing the accuracy of the position control by applying more exact approximations of the length-resistance-correlation. Other points to clarify regard effects of the controlled operation mode. First of all there is the permanent power consumption (see Table 1) caused by the continuous heating of the material. One possible solution is the usage of mechanical detents to allow switching off the heating current. Another one is using a second SMA-wire in an antagonistic operation mode. The second problem to be solved is related to the durability of the SMA-actuator. Whereas durability aspects are known for actuators used in cyclic mode for position controlled actuators this effect is rather unexplored. The major aspect to clarify is the influence of small but often occurring fluctuations in the amount of martensite, caused by small changes in the reference position and fluctuations of environmental parameters.

REFERENCES