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OPTIMIZED CONTROL STRATEGIES FOR FAST SWITCHING SOLENOID VALVES

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ABSTRACT

In this paper, the effects of different strategies for energizing solenoid valves are studied. These strategies are chosen subject to obtain soft-landing and concurrently minimization of power dissipation. A lumped parameter reluctance model is used to reflect the electrical and magnetic properties of a dual coil high-speed solenoid digital valve. This model is validated by stationary and transient experiments. The data from the model are in good agreement with the measurements. It is shown that retarded magnetic flux increase and spatial field diffusion phenomena are the major limiting factors for the feasible switching frequency of the spool motion. Thus, it is proposed to match the control strategy to these effects in order to reduce power dissipation in the coil as well as in the magnetic core. A control strategy that minimizes the power losses is obtained within a trajectory generating framework where the differential flatness property is used as a key enabler for efficient optimization schemes. The validity of the proposed hypothesis is demonstrated in several simulations, where the method using voltage profiles is compared against state of the art boost and hold energizing schemes.

Keywords: solenoid valve control, eddy current, current-shaping, soft-landing

1 Introduction

Fast switching solenoid digital valves with switching times below one millisecond are an emerging technology, enabling a variety of new fluid power applications. The fields that can benefit from these kinds of devices range from anti blocking system (ABS) control valves (Lolenko and Fehn, 2007), camless engines for increased fuel efficiency and emission reduction (Turner, 2006), to diesel engine and aircraft turbine injection control for resonance damping and combustion stabilization (Banaszuk, 2006). Another emerging field that requires this type of device is digital fluid power.

Concepts like those presented by Linjama (2008) for digital flow control units or switching power converters suggested by Scheidl (2005) and Kogler and Scheidl (2008) require high speed valves.

Furthermore, there is a continuously growing demand of electromagnetic drives with high dynamics.

Due to cost, durability, and stiffness, the use of eddy current mitigating bundled lamination is rarely possible in digital valves. The choice of material with more suitable properties might be restricted as well, depending on the application. Particularly in cylindrically shaped spool valves where the use of laminated steel is not practically feasible. Therefore, induced eddy currents in the core play a prevailing role in regard to the dynamics and cannot be neglected.

In this paper, the focus is put on control strategies only, without considering mechanical design aspects. It is understood that for designing excellent high-speed valve systems, both fields have to be coupled.

In recent years, a number of papers and dissertations have been published that are dealing with various aspects of the magnetic portion of the valve.

To a great extent, the papers are confined to the aspect of modelling the magnetic properties, in particular with FEM or FD methods (Aldefeld, 1982; Lequesne, 1990; Klesen and Nordmann, 1999 and Uusitalo, 2008). While these methods provide accurate stationary and nowadays also transient results, the computational requirements are yet too high to use these types of models in an optimization framework, where hundreds or even thousands of optimization runs are needed. Thus, a more simplified approach, based on magnetic inductance and flux channels has been proposed by

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Piron et al. (1999), Bottauscio et al. (2004), and others.

The simplest approach to approximate the effects caused by eddy currents is a resistor in parallel to the inductance that mimics a short circuit ring around the coil. This has been proposed by Abrahamsen et al. (1994), and lately being revisited by Reinertz and Murrenhoff (2009).

While a retarded flux is reflected by these models with sufficient accuracy, the actual current through the coil is not and therefore needs some additional treatment like filtering.

An extension that has been used by Abrahamsen et al. (1994) is to include a static flux / current map with the aforementioned approach. However, a filter to reflect delay in the increase of eddy current is still needed.

In a more accurate reluctance model, an iterative approach would be needed to consider the mutual interaction of current and flux (Ströhla, 2002).

Nevertheless, the magnetic inductance model is widely used in literature.

Fewer papers deal with modelling and energizing strategies (Reuter, 2006). Kajima (1993) and Kajima and Kawamura (1995) suggest boost and hold strategies and some electrical circuit topologies to control a high speed solenoid valve.

In recent years, questions in regard to simplifying and optimizing hydraulic digital valve performance have been addressed. Particularly, pulsed voltages and event based feed-forward control strategies for switching valves have been investigated by Lolenko and Fehn (2007) and Gunselmann (2005).

However, a systematic optimization of spool motion subject to soft-landing and power dissipation still seems to be an unsolved problem.

From a controls perspective, when the task is to operate a high-speed solenoid valve, a major challenge is to create the very high spool accelerations required to achieve the desired switching frequency. Boost and hold driver topologies are most often used, with possible modifications to enable energy recuperation, while de-energizing the coils. However, such a topology limits the ability to influence the spool motion, since it usually does not allow shaping or profiling the electrical current or voltage that energize the solenoid arbitrarily. The latter is actually desired to match and optimize the applied current to a desired spool motion and the magnetic flux evolvement, as shown in this paper.

This research analyzes if current profile shaping can be beneficial for reducing coil and eddy current losses. That is done by analyzing the effect of various current waveforms on the spool motion and energy losses. To this end, quantities are studied that create and affect the magnetic force, such as current shape, saturation effects and eddy currents.

A second reason why advanced control schemes are desirable for fast switching actuators is the problem of soft-landing and robustness. With the demand on high spool acceleration, the impact velocity of the spool might be high, even if the mechanical design is chosen in such a way that the fluid provides a certain amount of damping. Actively controlling the spool motion enables soft-landing and consistent actuator performance despite changes in fluid properties, e.g. viscosity and pressure of the system. In this research, dual coil solenoid valves, as depicted in Fig. 1 and 3, have been considered. For such a device, an advanced control scheme based on differential flatness has been proposed in (Reuter, 2006) and is used as a starting point. However, in order to obtain more viable results, this model had to be enhanced in such a way that eddy current effects are also considered.

In the next section, some basic magnetic facts are reflected, in order to familiarize the reader with the problems that arise when dealing with high speed magnetic actuators. Then, a formulation of the proposed hypothesis is given and a description how to overcome the previously mentioned drawbacks. Furthermore, the experimental setup is described that has been used for model validation. After that, details of the optimization approach are provided which have been found to be suitable. Various comparisons between a current state of the art energizing scheme and the proposed method are shown as well. The paper is finished by a discussion of the results and some conclusions and open questions for future work.

2 Problem Formulation

In magnetic actuators, Lorenz force and/or Maxwell force is used to move a magnetic plunger, spool or armature. Here the focus is put on spool valves, where the Maxwell force is used to move the spool. This force is proportional to the square of magnetic flux through the cross-sectional area of the spool. The flux depends on the magnetic induction or flux density, which is a function of the current, the change in current respectively. The total amount of current flowing through a closed magnetic field line is called the magneto-motive force.

For slow magnetic actuators, the use of these relations is simple and leads to good results, both for valve operation as well as for modelling.

However, when dealing with high-speed magnetic actuators, rapid flux changes occur. According to Maxwell's 2nd equation, a changing flux density induces an electrical field in the magnetic material, which is proportional to the rate of change. When the conductivity of the magnetic material is high, this field leads to a significant eddy current in the material, with opposite sign, compared to the driving current. This eddy current leads to two major effects influencing the evolvement of the flux. First of all, the electromagnetic force is diminished, which results in a retarded increase in flux and therefore delays the increase of the force that attracts the spool.

The second effect caused by the eddy current is that the spatial penetration of the spool by the magnetic induction is governed by a diffusion equation. The flux penetrates the spool from the outer radius towards the centre. This means, the effective cross-sectional area available to produce the force is subject to a retarded increase, Fig. 2.

Both, the time delay and the spatial diffusion are limiting the time, required to build up the Maxwell force.

From the previous paragraph a fundamental property for any fast switching electro magnetic actuator became obvious. This is the fact that the force is not a function of the driving current alone, but also depends on the eddy current that is a result of the rapid change in flux per unit time when a quick change in accelerating force is required. Since both, driving current through the coil and eddy current in the iron core are determining the magneto-motive force, it is suggested that, given a certain limitation in the maximum voltage which can be applied to the solenoid, there might be an optimal way to energize the coil in order to bring up the force as fast as possible, concurrently however limiting the flux change to keep the eddy currents and therefore the losses in the iron core low.

According to Kallenbach et al. (2008) and Aldefeld (1982) the diffusion velocity depends on the variable permeability of the iron. A typical strategy for controlling a solenoid valve is to overdrive the coil in order to bring the permeability down and thus decrease the time required for the field to penetrate the core. However, by doing so the eddy current losses are inevitably increasing. Again, there might be a trade-off and a strategy for energizing the coil in a way that the speed of flux change is tuned to the field diffusion effects. This means, the change in flux is limited to the amount that matches the speed of field diffusion.

3 Experimental Setup and Model

For the study, a simple mock-up as shown in Fig. 1 is used.

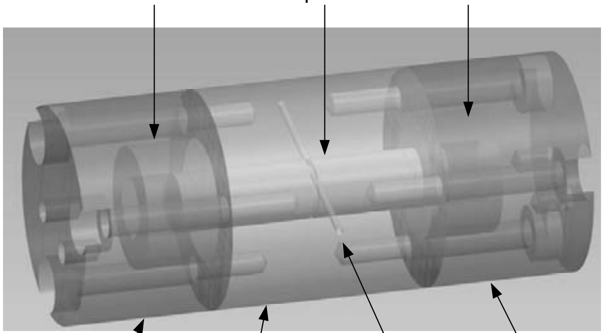

Fig. 1: *Valve Mock-Up*

The spool moves between two driving coils, each located at a respective endcap. The stroke of the spool is 1 mm. In order to be able to measure the spool motion, a simple position sensor is integrated. This sensor comprises of an infrared LED and a photo transistor. A ring shaped notch of one millimetre is milled into the spool. The spool displacement causes a variation of the amount of light reaching the transistor, thus allows a fast and accurate position measurement. The valve mock-up is built to be vertically symmetric. For simplicity of modelling, the influence of screws and drill holes on the magnetic circuit is neglected and axial symmetry is assumed.

Figure 2 shows the diffusion process of the magnetic flux density into the iron material calculated by a finite element simulation. Here only one half of the total valve is shown. It can be seen that the major part of the flux density is concentrated around the coil. It is clearly indicated that the flux density practically amounts to zero at the centre of the valve (that is where the channel for the position sensor is located). Thus, modelling is simplified and both valve parts can be treated separately. Due to symmetry it is sufficient to model one side of the valve only. Afterwards, the model can be replicated for the other side.

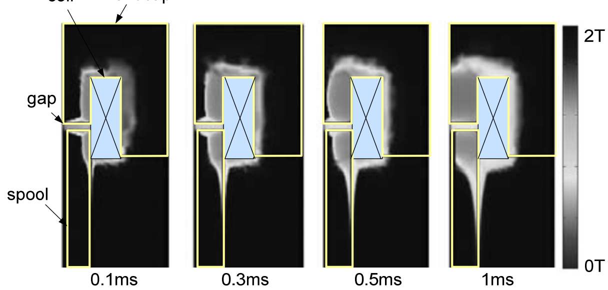

Fig. 2: *Diffusion process of the magnetic flux density*

In the upper part of Fig. 3 a network model for the magnetic part of the valve model incorporating eddy current effects is depicted. The magnetic network consists of magnetic reluctances $R_m = l/\mu A$ and magnetic inductances $L_m = 1/R_{el}$ as proposed by Kallenbach et al. (2008) where l is the length of the respective flux path, A is the cross-sectional area and μ is the magnetic permeability. L_m is defined as the reciprocal value of the electrical resistance R_{el} of the eddy current path. λ is the magnetic flux. R_{mFe1} and L_{mFe1} represent the outer hollow cylindrical part of the valve. R_{mFe2} and L_{mFe2} stand for the inner cylindrical part of the endcap. R_{mFe3} and L_{mFe3} characterize the spool part of the valve. R_{mgap} is the reluctance of the air gap that carries the flux from which a magnetic force, F_{mag}, results on the spool. $R_{m\sigma 1,\dots,4}$ are magnetic reluctances representing the leakage flux paths around the air gap. R_{menc} is the reluctance of the air gap between spool and enclosure part of the valve.

The magnetic reluctances and inductances can be summed up as follows:

$$R_{mFe} = R_{mFe1} + R_{mFe2} + R_{mFe3} \tag{1}$$

$$L_m = L_{mFe1} + L_{mFe2} + L_{mFe3} \tag{2}$$

$$R_{m\sigma} = \left(R_{m\sigma 1}{}^{-1} + \dots + R_{m\sigma 4}{}^{-1}\right)^{-1} \tag{3}$$

$$R_{mTotal} = R_{mFe} + R_{enc} + \frac{R_{gap} R_\sigma}{R_{gap} + R_\sigma} \tag{4}$$

Hence, the magnetic network can be modelled as shown in the lower part of Fig. 3. The magnetic flux is driven by the magneto-motive force iN where i is the coil current, N the number of coil windings. i results from the driving voltage v_{drv} as well as from the induced voltage $v_i = d\psi/dt = N\, d\lambda/dt$ and depends on the copper resistance of the coil windings R_{Cu}. F_{mag1} is the accelerating magnetic force, F_{mag2} the decelerating force, m_{Sp} is the mass of the spool. Indices $_{1,2}$ relate to the respective side of the valve.

Up to now, the model is simplified by not taking into account any mechanical properties besides viscous friction and is given here without derivation:

$$v_{\mathrm{drv}1,2} = R_{\mathrm{Cu}} i_{1,2} + \frac{d\psi_{1,2}}{dt} \tag{5}$$

$$\dot{\lambda}_{1,2} = k_1 \left(\frac{N}{R_{\mathrm{Cu}}} v_{\mathrm{drv}1,2} - \lambda_{1,2} R_{\mathrm{mTotal}} \right) \tag{6}$$

$$F_{\mathrm{mag}1,2} = \frac{\lambda^2_{\mathrm{gap}1,2}}{2\mu_0 A_{\mathrm{gap}}} \tag{7}$$

$$\ddot{z} = \tfrac{1}{m_{\mathrm{Sp}}} \left(F_{\mathrm{mag1}} - F_{\mathrm{mag2}} \right) - c_{\mu} \dot{z} \;\; sign(\ddot{z}) \tag{8}$$

Here A_{gap} is the surface area of the spool, λ_{gap} the magnetic flux flowing through A_{gap}. z is the position of the spool and c_{μ} is the viscous friction coefficient. The coefficient k_1 is given in the appendix.

4 Model Validation

Validation test results are shown for the static case. Afterwards transient behaviour is looked at.

For static validation, magnetic forces attracting the spool are measured for fixed air gaps and impressed coil currents. This data is compared to the results obtained from the proposed lumped parameter model and a finite element simulation.

In order to be able to measure forces at fix air gap lengths, the valve is embedded in a measurement setup which together with a position sensor enables precise positioning of the spool. A force sensor supplies the relevant data.

The measurement setup is configured in such a way, that one of the endcaps (see Fig. 1) is left off and a force sensor is mounted at the spool and fixed on a rigid structure. This configuration is mounted on a linear stage and the air gap can be adjusted by moving the valve housing.

The left part of Fig. 4 shows force curves for different air gap lengths.

As it can be seen, there is good agreement between the data obtained by finite element simulation and the lumped parameter model. For low currents, the measured forces almost agree with the simulation results while higher currents lead to a significant deviation. Enquiry has shown that the deviation is caused by deformation of the force sensor and measurement setup due to inadequate stiffness. During the tests, this deformation leads to a reduction of the air gap length by up to 0.05 mm, which results in higher magnetic forces. This can be confirmed by the plot in the right part of Fig. 4. Here the same simulation is run but each air gap length is reduced by 0.05 mm. Then, the obtained force data is compared with the original forces measured again. There is good accordance between measurement and simulation for higher currents. This clearly indicates the deformation of the test setup.

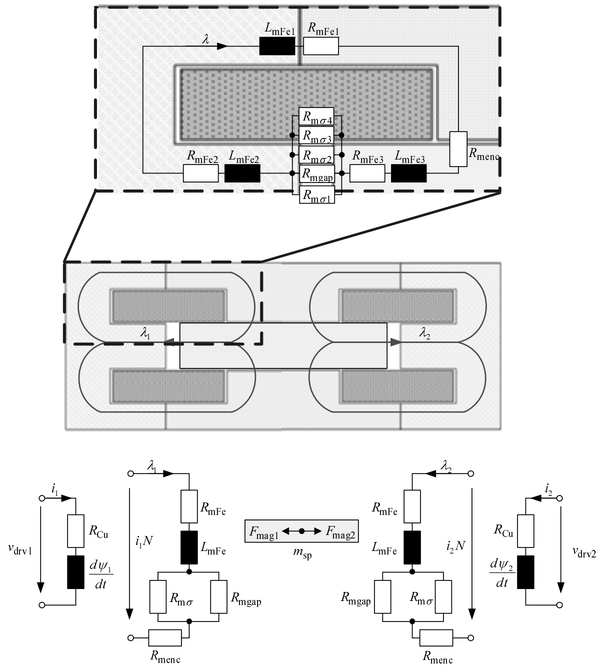

Fig. 3: *Lumped Parameter Model*

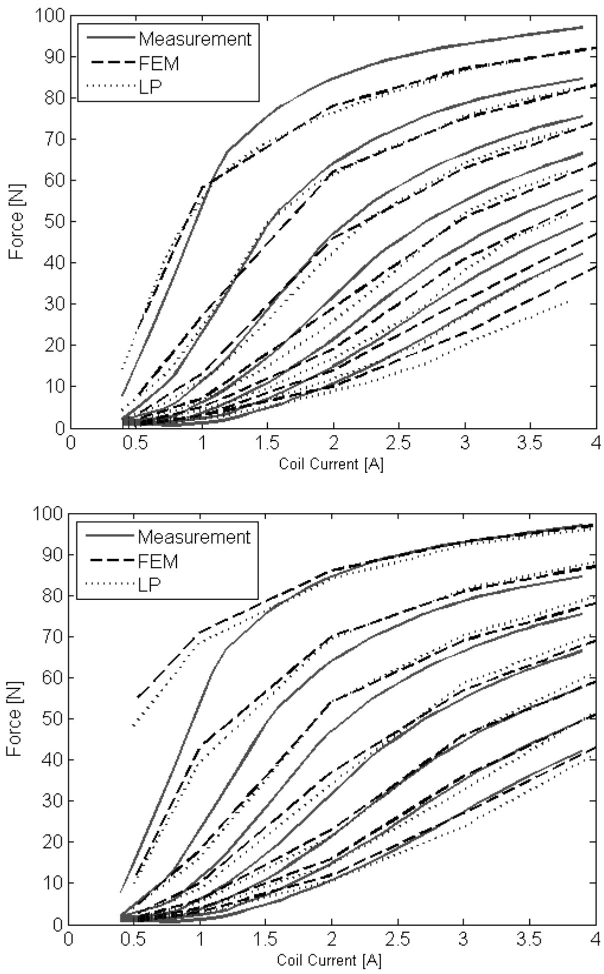

Fig. 4: *Upper: Static analysis of forces, assumed airgaps for measurement: 0.1,0.2,…,0.7 mm, airgap for simulation: 0.1,0.2,…,0.7 mm, Down: Static analysis of forces, assumed airgaps for measurement: 0.1,0.2,…,0.7 mm, airgap for simulation: 0.05,0.15,…,0.65 mm*

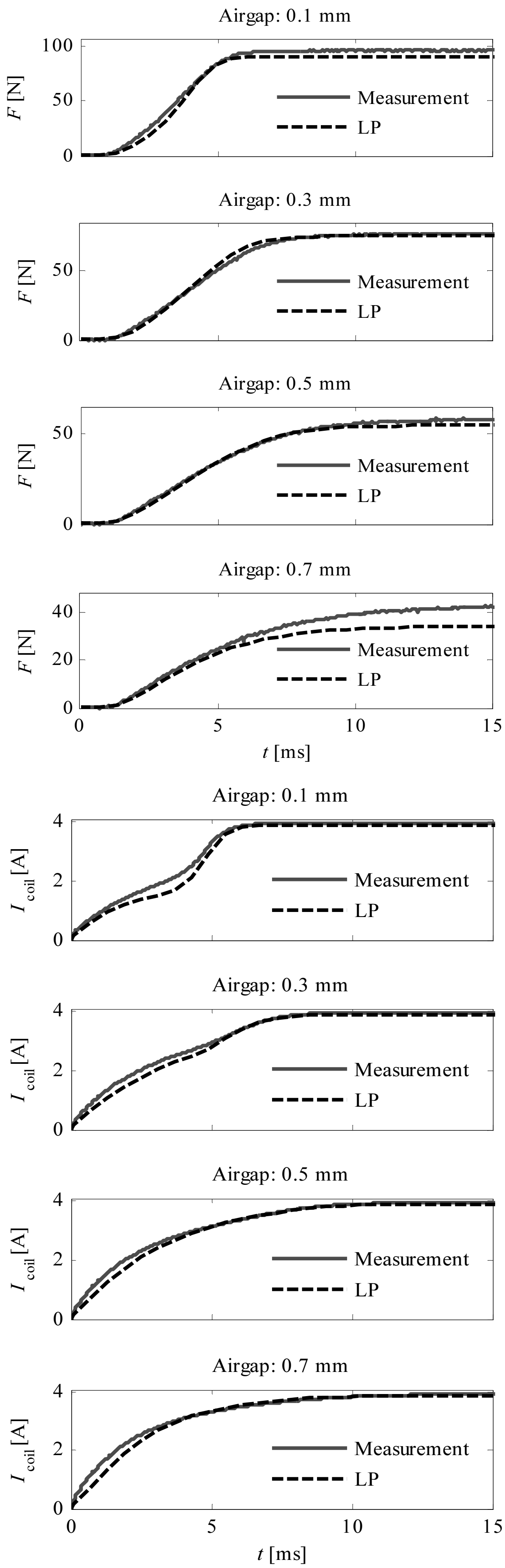

Fig. 5: *Resulting transient force (upper) and current (down) trajectories when applying a voltage pulse of 10 Volts*

Nevertheless, regarding the accordance of the lumped parameter model with the finite element simulation results as well as with the measurements incorporating deformation effects, a proof for the applicability of the proposed lumped parameter model is given for the static case.

For the validation of the transient behaviour, force and coil current trajectories resulting from voltage pulses are compared over time. Measurement data is gathered using the same test setup as for static testing. In Fig. 5 results of the lumped parameter model simulation are plotted against measurement data. The plots show exemplary test results for a voltage pulse of 10V and air gap lengths of 0.1, 0.3, 0.5 and 0.7 mm. It can be seen that for transient simulation there is good agreement between modelling and measurement results. The fact that there is an aberration of forces at stationary states, again is a result of the deformation of the force sensor and the test setup.

Results of static and transient simulations compared with measurement data as depicted in this section provide a satisfactory proof for the applicability of the proposed lumped parameter model.

5 Spool Motion Optimization

In general, there are several methods to find voltage profiles that achieve the desired spool motion while concurrently minimizing the power dissipation.

A brute force method would be to parameterize the voltage signal by spline coefficients, repeatedly integrate the system and change the coefficients using a suitable optimization software until the desired result is achieved. In this case, the system is considered from input to output and the desired boundary conditions (end position, end velocity of the spool) need to be enforced by penalty functions.

Another opportunity would be to use an optimal control framework (Reuter, 1998). However, the change in parameters, in particular the magnetic permeability would cause difficulties.

A more straightforward and elegant approach for trajectory generation is possible within the framework of differential flatness (Löwis and Rudolph, 2003; Koch et al., 2004 and Reuter, 2006).

The system is considered backwards, from the output towards the input. A sufficiently smooth spool motion trajectory $z_{ref}(t)$ is defined and the flatness property is used to algebraically find the associated driving voltage that is required to achieve the spool motion. The method is briefly summarized here: Due to the fact that the spool position $z(t)$ is a flat output of the system, all other states and the input can be computed from $z(t)$ and a finite number of its time derivatives. Thus, given a sufficiently smooth spool reference motion $z_{ref}(t)$, all quantities, such as the required forces, the required flux, the resulting eddy and coil currents as well as the required driving voltage can be calculated without the need of integrating the model. This inversion provides a fast means to numerically optimize the reference trajectory $z_{ref}(t)$ to achieve the desired properties. This approach has been discussed in more detail by Reuter

(2006). It is worth mentioning, that nonlinear saturation effects are taken into account by a flux density dependent relative permeability function $\mu_r(B)$. The inverted model is given here without detailed derivation. Since each coil can provide an attracting force only, one has to distinguish positive and negative acceleration. A monotonic motion in positive z direction is considered. Therefore the sign of velocity $\dot{z}$ is always positive. Since two control inputs v_{drv1} and v_{drv2} are available, but just one control variable z, the assumption is made that at any given time only one coil out of the two is energized. This is required to make a necessary condition for differential flatness hold. Otherwise, the inversion of the model would not be a one-to-one mapping. Since the forces created by the two coils have opposite signs, this assumption does not restrict the feasibility of any trajectory, however, it might be interesting to investigate the effect on the results, if this restriction is relaxed.

The required magnetic force for spool acceleration $\ddot{z}(t)$ along a given trajectory is given by

$$F_{\mathrm{mag1}} = \ddot{z} + c_{\mu}\dot{z}, \text{ if } \dot{z} > 0 \text{ and } \ddot{z} > 0 \tag{9}$$

$$F_{\mathrm{mag2}} = \ddot{z} - c_{\mu}\dot{z}, \text{ if } \dot{z} > 0 \text{ and } \ddot{z} < 0 \tag{10}$$

The necessary reference flux is given by:

$$\lambda_1 = \sqrt{\frac{F_{\mathrm{mag1}}}{c_1}}(c_2 - z) \tag{11}$$

$$\lambda_2 = \sqrt{\frac{F_{\mathrm{mag2}}}{c_2}}(c_2 - (z_0 - z)) \tag{12}$$

See (Lolenko and Fehn, 2007) for the derivation of the magnetic force.

The coefficients c_1 and c_2 are given in the appendix. Differentiating Eq. 11 and 12, and using the results to replace $d\lambda_1/dt$ and $d\lambda_2/dt$ on the left hand side of Eq. 6, and finally solving for v_{drv1} and v_{drv2} , yields the driving voltages, required to force the spool along the trajectory $z_{\mathrm{ref}}(t)$.

$$\begin{aligned} v_{\mathrm{drv1}} = \frac{1}{k_1 k_2}\left(\frac{(c_2 - z)^2}{2\lambda_1}(m\dddot{z} + c_{\mu}\ddot{z}) - \dot{z}\frac{\lambda_1}{(c_2 - z)}\right) \\ + \left(k_1 k_3 + k_1 \frac{(k_4 - z)k_5}{(k_4 + k_5) - z}\right)\lambda_1 \end{aligned} \tag{13}$$

$$\begin{aligned} v_{drv2} = \frac{1}{k_1 k_2}\left(\frac{(c_2 - (z_0 - z))^2}{2\lambda_1}(m\dddot{z} - c_{\mu}\ddot{z}) \right. \\ \left. + \dot{z}\frac{\lambda_1}{(c_2 - z)}\right) \\ + \left(k_1 k_3 + k_1 \frac{(k_4 - (z_0 - z))k_5}{(k_4 + k_5) - (z_0 - z)}\right)\lambda_1 \end{aligned} \tag{14}$$

The coefficients k_i are given in the appendix.

Remark:

A simplified model, without flux channels is used. Up to now, it remains an open question, how the flatness based approach could be extended to a model using flux channels: Rotfuss et al. (2000) suggests a distributed parameter approach. However, in their work the question of how the necessary boundary magnetic field can be calculated from a given force profile, remains open. The same holds for calculating the required flux in a model with spatially distributed flux channels.

As it has been shown, however, the model presented in this publication is accurate enough to allow significant confidence in the results.

Technically, for the optimization, the motion profile is parameterized with ten quartic spline functions. In addition, two more splines are added to the right and left of the trajectory in order to have sufficient parameters available to force the profile to the desired boundary conditions, i.e. zero velocity at start and end, as well as zero acceleration. From Eq. 13 and 14 it can be seen that the derivatives of $z(t)$ up to the third order are required to calculate the voltages. Therefore, quartic splines are used to ensure sufficient smoothness.

As a cost function, the integrated square of the voltage signal of each coil is used.

$$J = \int_0^{t_f} v_{\mathrm{drv1}}^2(\tau) + v_{\mathrm{drv2}}^2(\tau)\, d\tau \tag{15}$$

This is directly related to the minimization of the overall flux change and therefore minimizing the eddy currents.

6 Simulation Results

In order to prove the positive effects of using voltage and current profiles, as proposed in this paper, comparative simulations have been conducted. In a previous paper (Reuter and Maerkl, 2009) the comparison was carried out between the optimized voltage profiles and simple voltage pulses. In this paper, the comparison is done subject to a more practically relevant energizing strategy with multiple voltage levels. This strategy is known as Boost and Hold or multi level strategy. In applications such as diesel common rail fuel injectors and fast switching valves, this can currently be regarded as state of the art.

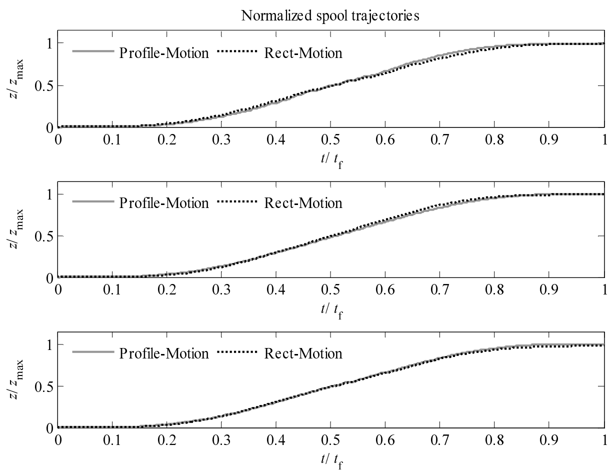

Fig. 6: *Comparison of the spool motion subject to pulse and profile for the 3 experiments*

To make the comparison fair, the voltage levels for the boost and hold driving circuit have been selected, according to the voltage used by the profiles, to enable a decent match between the voltages. Further, the adjustment of the duration of the boost and hold phases is done in such a way that the spool motion profile, which results from the energizing pattern, closely matches the optimal spool motion, found by the optimization procedure described in the previous section (Fig. 6).

In all experiments, the constraint on spool motion was to obtain zero velocity at the start and end positions, thus achieving soft-landing.

In the work presented here, the investigation is restricted to the spool motion only, without considering additional hydraulic forces acting on the spool. Taking these into account would be possible; this however would not change the results qualitatively. Switching losses due to pulse width modulation have not been taken into account, either. These losses, although significant, would add to both energizing schemes in a similar way and are neglected in order to focus on the energizing strategy.

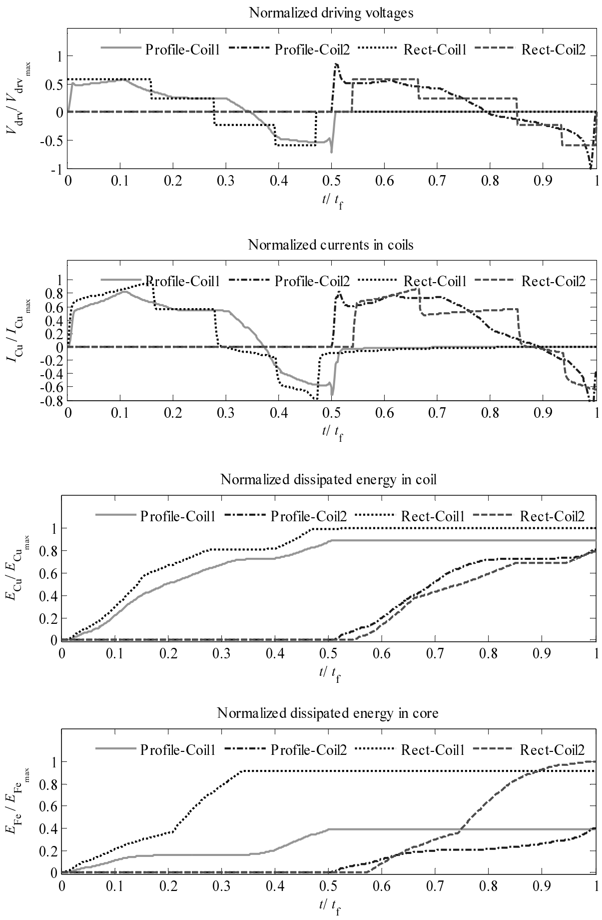

Fig. 7: *Normalized voltages and coil currents (upper) and power dissipation (down) for the 5ms case*

To demonstrate the benefits of the suggested approach, three different experiments are shown, with valve switching times of five, two and one milliseconds.

The spool motion trajectories achieved by the various strategies are shown in Fig. 6. It can be seen that the motion patterns coincide to a great extent. Therefore, the influence of different motion profiles is eliminated and the comparison is solely subject to the effect of the energizing method.

For model parameters, the values from the valve mock-up have been used. This has not been optimized for high speed applications, though.

The relevant results are shown in Fig. 7 to 9.

The first two subplots show the driving voltages and resulting currents. The third and fourth subplots show the evolution of the energy losses, i.e. the dissipated energy while the solenoid is energized. The third subplot reflects the losses in the coil and the fourth the losses due to eddy current. For comparison, all signals have been normalized to the maximum value. In first two subplots, the energizing voltages and currents for the accelerating coil are active from 0 to about half of the travel time, while the decelerating coil is active from the time, the first coil becomes passive to the end of the travel time.

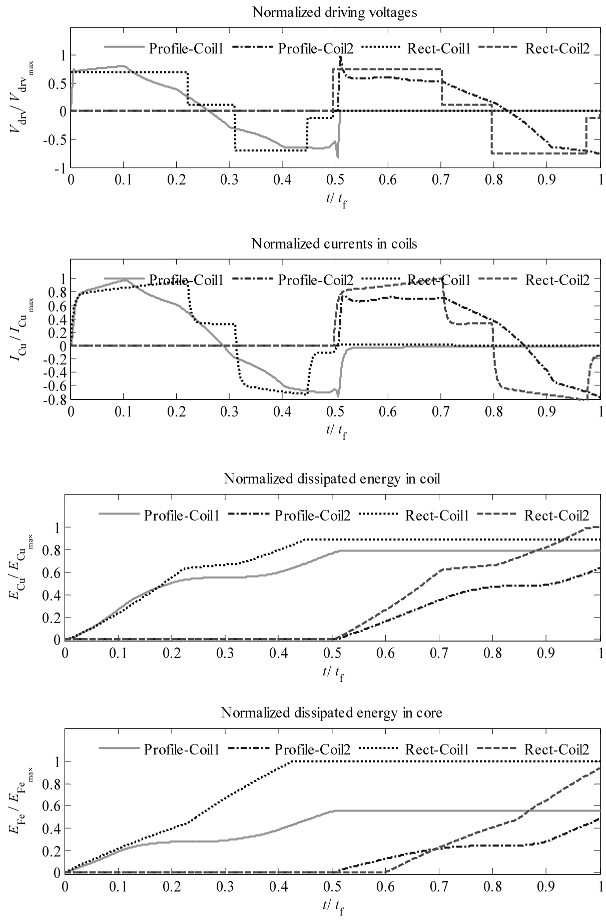

Fig. 8: *Normalized voltages and coil currents (upper) and power dissipation (down) for the 2ms case*

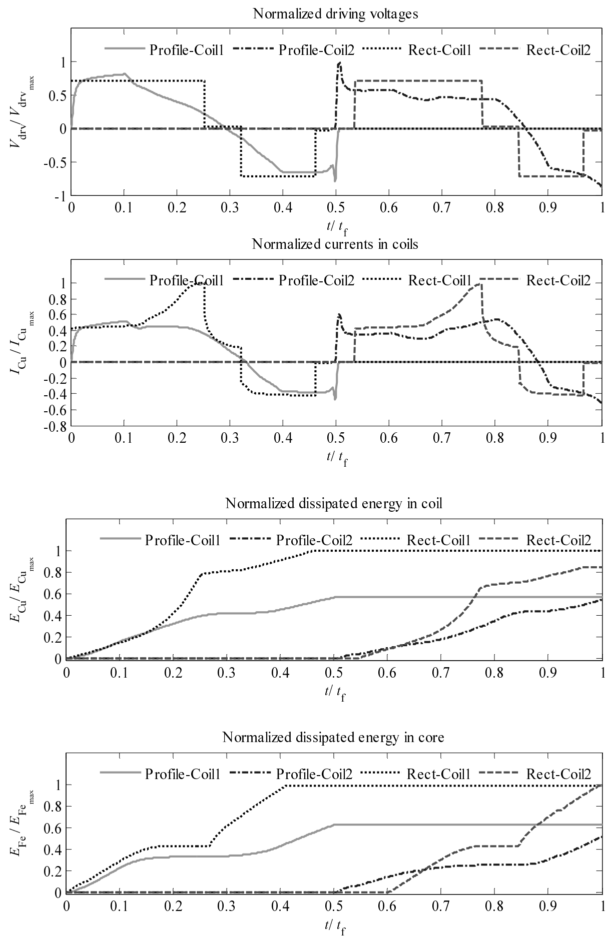

Fig. 9: *Normalized voltages and coil currents (upper) and power dissipation (down) for the 1ms case*

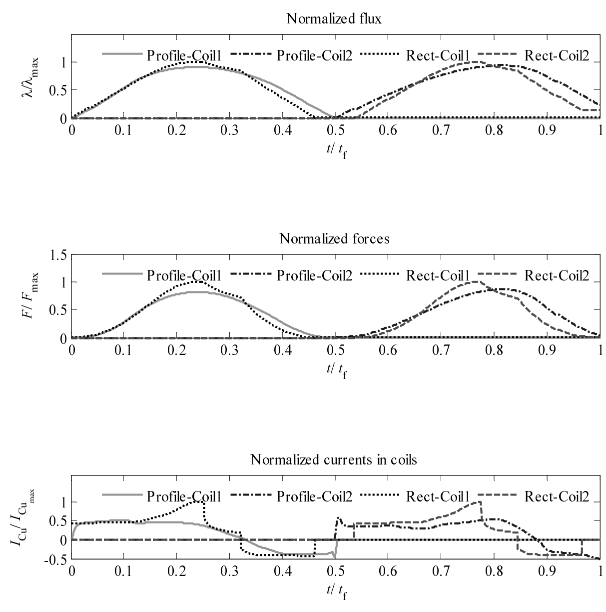

Fig. 10: *Flux, force and current for the 1ms case*

7 Discussion

Figure 7 to 9 illustrate that for all experiments the power dissipation is lower when using the continuous voltage profile instead of only four voltage levels. This feature becomes more significant, the shorter the switching time gets. While for the five and two millisecond experiment the reduction of power dissipation is particularly prevailing in the eddy current losses, for the 1 ms case it is very significant in the coil losses as well. The reason for this behaviour is found in the flux saturation due to the high force that is required to accelerate the spool according to the commanded motion. The time instant where saturation takes place, is clearly marked by the huge increase in current after 0.1 ms in Fig. 10 lower graph, dotted curve. Due to saturation, the change in flux becomes neglectable and therefore the induced voltage is reduced to zero, allowing the current to rise rapidly. However, this does not increase the force and therefore produces significant power losses. On the other hand, it would not be possible to switch to the "hold" voltage level, since a maximum of force is required to achieve the desired spool motion. In contrast, the optimized voltage profile has been found to be adjusted in such a way that the maximum force is achieved closely at the saturation boundary. Continuous reduction of the voltage enables the system to maintain the force along the required trajectory without a useless increase in current.

This clearly shows that the approach of over exciting magnetic circuits, as it is typically found in Diesel fuel injection applications, has drawbacks, which can be overcome by using a voltage profile approach. This also supports the hypothesis that a match of driving voltage to eddy current and field diffusion phenomena results in a significant reduction of power loss.

In particular from Fig. 7 it can be seen that with a carefully designed multilevel approach similar results can be obtained in applications that do not require operating the magnetic circuit at the saturation boundary. However, in fast switching applications this means a constraint in spacing and material.

From the coil current plots (Fig. 7 to 9) it can be seen that in order to bring the force down within in the required time it is even necessary to provide a negative current to the coil. This surprising phenomenon can be explained by keeping in mind that when the flux is decreasing, the sign of the eddy current changes, which retards the depletion of the magnetic field. Since the flux is a function of the magneto-motive force, a change in the sign of the coil current supports the field decrease. This explanation is supported by the results published in (Reuter and Maerkl, 2009), where the conductivity has been assumed to be several orders of magnitude lower, than the actual electric conductivity of the material. There, the current always remains positive.

Conclusion

The results of this study give rise to the assumption that in the future, when a variety of extremely fast switching valve applications might be in the field, the way how the solenoids are energized needs to be changed subject to the way it is done today.

In digital hydraulics, there might be a variety of systems with a high number of on/off valves of different sizes that need to be switched very fast. Given the increased number of valves, reducing power consumption is highly desirable. In this paper a way has been proposed, that this can be achieved.

It turns out that for operating fast switching valves a close match of voltage levels and switching times can provide significant reduction in power dissipation. This can be achieved by carefully designing control strategies. Given a certain configuration of the electric and magnetic properties, the voltage profiling approach can be considered as a lower bound, and therefore optimal.

With this knowledge, the approach can be used to design voltage shapes that match the optimal shape as closely as possible.

For a practical implementation a multi level inverter topology can be used. The number of voltage levels can be scaled according to the required quality of the voltage profile approximation. Such a power electronic device has been set up in the controls lab of HTWG Konstanz, where experimental studies with a valve, especially designed for fast switching applications, will be conducted. The feasibility to create the required voltage and current profiles has been proven.

Practical verification of the results presented in this paper within an actual hydraulic setup is planned for future work. Moreover, there are further opportunities for the application of advanced control strategies given the fact, that the spool motion can be arbitrarily shaped. For instance, it would be an interesting question, if the spool motion can be designed and controlled in such a way, that pressure waves induced by the rapid motion can be mitigated.

Open questions from the controls point of view would be in particular a robust observer based control strategy that is suitable for outside lab applications.

Moreover, from the magnetic modelling and trajectory generation point of view, it remains an open question how the flatness property can be extended towards the flux channel model. A more accurate modelling approach for the interaction between flux and current is desired as well, to further improve the prediction of actual system performance.

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Nomenclature

A	cross-sectional area	[m^2]
A_{gap}	surface area of the spool	[m^2]
c_μ	viscous friction coefficient	[kg/s]
$F_{\text{mag}1,2}$	magnetic force	[N]
$i_{1,2}$	coil current	[A]
l	flux path length	[m]
L_{m}	sum of the magnetic inductances	[1/Ω]
L_{mFe1}	magnetic inductance outer hollow cylindrical part of the valve	[1/Ω]
L_{mFe2}	magnetic inductance inner cylindrical part of the endcap	[1/Ω]
L_{mFe3}	magnetic inductance spool part of the valve	[1/Ω]
m_{SP}	mass of the spool	[kg]
N	number of coil windings	-
R_{Cu}	copper resistance of the coil windings	[Ω]
R_{el}	electrical resistance	[Ω]
R_{menc}	reluctance of the air gap between spool and enclosure part of the valve	[A/Vs]
R_{mFe}	sum of the magnetic reluctances	[A/Vs]
R_{mFe1}	magnetic reluctance outer hollow cylindrical part of the valve	[A/Vs]
R_{mFe2}	magnetic reluctance inner cylindrical part of the endcap	[A/Vs]
R_{mFe3}	magnetic reluctance spool part of the valve	[A/Vs]
R_{mgap}	air gap reluctance	[A/Vs]
R_{mTotal}	sum of all magnetic reluctances	[A/Vs]
$R_{\text{m}\sigma}$	sum of the magnetic reluctances representing the leakage flux paths	[A/Vs]
$R_{\text{m}\sigma\ i}$	magnetic reluctances representing the leakage flux paths	[A/Vs]
$v_{\text{drv}1,2}$	driving voltage	[V]
v_{i}	induced voltage	[V]
z	actual Position of the spool	[m]
z_0	starting position of the spool	[m]
$z_{\text{ref}}(t)$	desired trajectory for spool motion	[m(t)]
$\lambda_{1,2}$	magnetic flux	[Vs]
$\lambda_{\text{gap}1,2}$	magnetic flux flowing through A_{gap}	[Vs]
μ_0	vacuum permeability	[Vs/Am]
μ_{r}	relative permeability	-
μ	permeability	[Vs/Am]
$\Psi_{1,2}$	flux linkage	[Vs]

Indices $_{1,2}$ relate to the respective side of the valve.

Appendix

$$c_1 = \frac{\mu_0 A_{\text{gap}} R_\sigma^2}{2}$$

$$c_2 = \mu_0 A_{\text{gap}} (R_{\text{gap}}^{\max} + R_\sigma^2)$$

$$k_1 = \frac{R_{\text{cu}}}{L_{\text{mFe}} R_{\text{cu}} + N^2}$$

$$k_2 = \frac{N}{R_{\text{cu}}}$$

$$k_3 = \frac{R_{\text{mFe}}}{\mu}$$

$$k_4 = \mu_0 R_{\text{mgap}}^{\max} A_{\text{gap}}$$

$$k_5 = R_{\text{m}\sigma}$$

$$k_6 = \mu_0 R_\sigma A_{\text{gap}}$$

References

Abrahamsen, J. G., **Ennemark, P.** and **Jensen, F.** 1994. A Novel Electromagnetic Model of a Linear Reluctance Actuator. *Proceeding of the ICEM 94*, Paris, pp. 414 - 419.

Aldefeld, A. 1982. Felddiffusion in Elektromagneten. *Feinwerk & Messtechnik*, 90(5), pp. 222 - 226.

Banaszuk, A., **Metha, P. G.**, **Jacobson, C. A.** and **Khibnik, A. I.** 2006. Limits of Achievable Performance of Controlled Combustion Processes. *IEEE Transaction on Control Systems Technology*, 14(5), 2006.

Bottauscio, O., **Manzin, A.**, **Conova, A.**, **Chiampi, M.**, **Gruosso, G.** and **Repetto, M.** 2004. Field and Circuit Approches for Diffusion Phenomena in Magnetic Cores. *IEEE Transactions on Magnetics*, Vol. 40, pp. 1322 - 1325.

Gunselmann, C. 2005. *Regelungsverfahren fuer Sensorlose Elektromagnetische Umschwing-Aktuatoren.* PhD thesis. Ruhr-Universität Bochum.

Kallenbach, E., **Eick, R.**, **Quendt, P.**, **Stroehla, T.**, **Feindt, K.** and **Kallenbach, M.** 2008. *Elektromagnete*. Edition 3, Wiesbaden: Vieweg Teubner.

Kajima, T. and **Kawamura, Y.** 1995. Development of a High–Speed Solenoid Valve: Investigation of Solenoids. *IEEE Transactions on Industrial Electronics*, 42(1), pp. 1 - 8.

Kajima, T. 1993. Development of a High-speed Solenoid - Valve-Investigation of the Energizing Circuits. *IEEE Transactions on Industrial Electronics*, 40(4), pp. 328-435.

Klesen, C. and **Nordmann, R.** 1999. Dynamic Forces of Electromagnetic and Electrodynamic Actuators in Mechatronics Systems under Consideration of Eddy Currents. *Kolloquium Aktoren in Mechatronischen Systeme*, Duesseldorf, pp. 143 - 152.

Koch, C., **Lynch, A.** and **Chung, S.** 2004. Flatness-based Automotive Solenoid Valve Control. *Proc. 6th Symposium on Nonlinear Control Systems (IFAC)*, Stuttgart, Germany, pp. 1091 - 1096.

Kogler, H. and **Scheidl, R**. 2008. Two Basic Concepts of Hydraulic Switching Converters. *The First Workshop on Digital Fluid Power*, 3 October, Tampere, Finland, pp. 7 - 30.

Lequesne, B. 1990. Dynamic Model of Solenoids Under Impact Excitation, Including Motion and Eddy Currents. *IEEE Transactions on Magnetics*, 26(2), pp. 1107 - 1116.

Linjama, M. 2008. Digital Hydraulics Research at IHA. *The First Workshop on Digital Fluid Power*, 3 October, Tampere, Finland, pp. 7 - 30.

Loewis, V. J. and **Rudolph, J.** 2003. Real-time Trajectory Generation for Flat Systems with Constraints, *Nonlinear and Adaptive Control, LNCIS*, 281, pp. 385 - 394.

Lolenko, K. and **Fehn, A.** 2007. Model-based Open-loop Control Design for a Hydraulic Brake System with Switching Solenoid Valves. *Automatisierungstechnik*, Vol. 55, Wissenschaftsverlag Oldenburg.

Piron, M., **Sangha, P.**, **Reid, G.**, **Miller, E.**, **Ionel, D.M.** and **Coles, J. R.** 1999. Rapid Computer–Aided Design Method for Fast–Acting Solenoid Actuators. *IEEE Transactions on industry applications*, 35(5).

Reinertz, O. and **Murrenhoff, H.** 2009. Dynamic Modelling of switching valves. *O + P : Zeitschrift für Fluidtechnik. - Mainz : Vereinigte Fachverlage. - Nebent.: Ölhydraulik und Pneumatik,* 53(4), pp. 152 - 155.

Reuter, J. 1998. Mobile Robots Trajectories with Continuously Differentiable Curvature: An Optimal Control Approach. *IEEE Int. Conf. on Intelligent Robots and Systems*, Victoria, B.C.

Reuter, J. 2006. Flatness-based Control of a Dual Coil Electro-hydraulic Actuator. *Proc. of the 4th Symposium on Mechatronics (IFAC)*, Heidelberg, Germany, pp. 48 - 54.

Reuter, J. and **Maerkl, S.** 2009. Aspects on Controlling Dual Coil Solenoid Digital Valves. *Second Workshop on Digital Fluid Power*, Linz, Austria.

Rothfuss, R., **Becker, U.** and **Rudolph, J.** 2000. Controlling a Solenoid Valve: A Distributed Parameter Approach. *Proc. of the Mathematical Theory of Networks and Systems (MTNS)*, Perpignan, France.

Scheidl, R., **Steiner, B.**, **Winkler, B.** and **Mikota, G.** 2005. Basic problems in fast hydraulic switching valve technology. *Proc. of the Sixth International Conference on Fluid Power Transmission and Control (ICFP)*.

Stroehla, T. 2002. *A Contribution to Simulation and Design of Electromagnetic System by Magnetic Networks*. PhD thesis. TU Ilmenau.

Turner, J. W. G. and **Stretch, D. A.** 2006. Production AVT Development: Lotus and Eaton's Electrohydraulic Closed-Loop Fully Variable Valve Train System. *Green Car Congress*.

Uusitalo, J. P. 2008. Bistable Valve Actuator. *First Workshop on Digital Fluid Power*, Tampere, Finland.

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