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Robust Linear Control of a Bending Molecular Artificial Muscle Based on Spin Crossover Molecules

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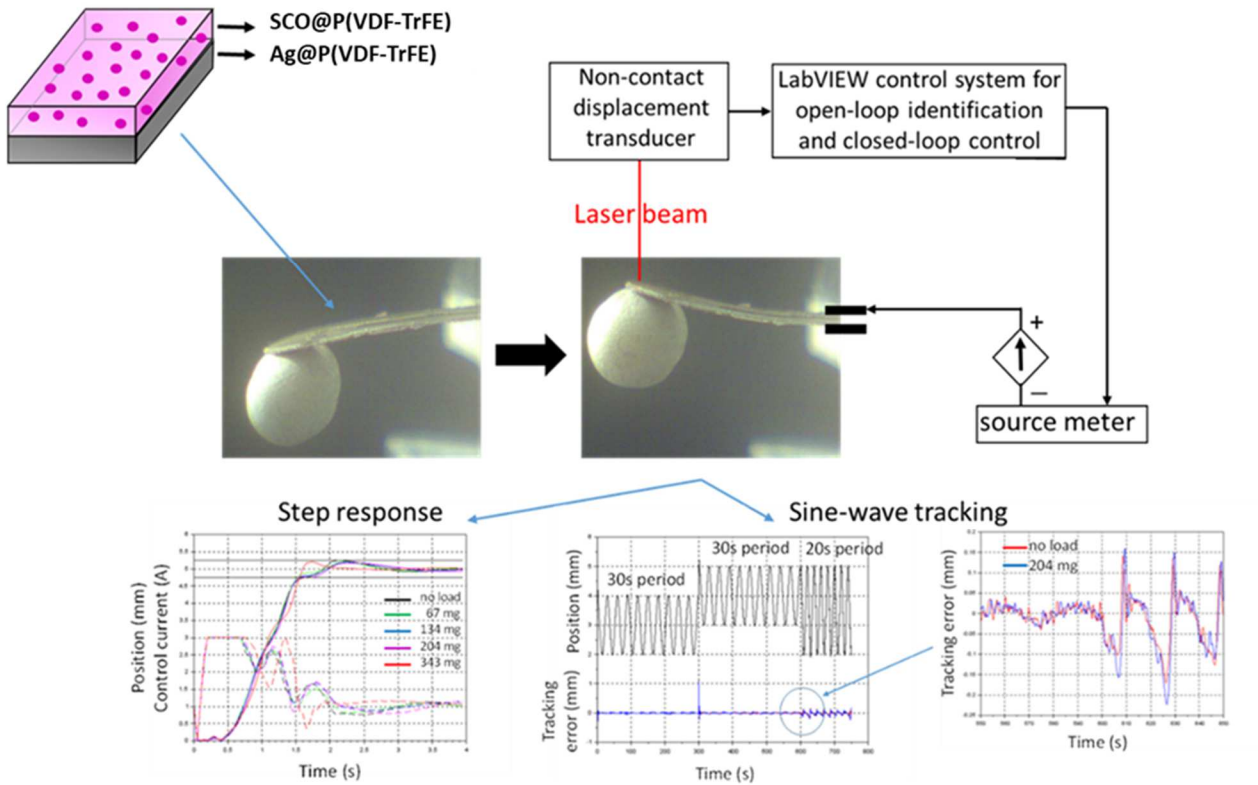
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Abstract. Bending actuators represent a popular class of artificial muscles for which efficient closed-loop control is a remarkable challenge, due to the complexity of physico-chemical phenomena occurring during dynamic contraction. In this study, we investigate an electro-thermally actuated, bilayer bending actuator based on spin crossover molecules. While these artificial muscles can open-loop contract in several tens of seconds, we show that a simple closed-loop PID-control is able to reduce the response time to ca.1.5 s, even with loads up to 5 times the actuator weight (i.e. a maximal load of about 343 mg). We demonstrate also satisfactory sine wave tracking performance. The relevance of this linear control approach applied to a nonlinear actuator is the consequence of the high sensibility of the actuator to the current, responsible for the Joule effect. Notably, the actuator roughly behaves like a second-order linear system, whose time “constants” decrease with current. Consequently, the natural speed improvement peculiar to the PID-controller is further amplified by this nonlinear effect, without any loss in stability of the closed-loop system.

Graphical abstract



Highlights

- Closed-loop control by means of a simple PID makes possible to diminish the open-loop response time by a factor of 10, for reaching a mean response time in closed-loop equal to about 1.5 second, when no load is embedded,
- Load robustness is emphasized with similar response time for loads up to 350 mg, which corresponds to ca. 5 times the own weight of the actuator,
- Very satisfying sine wave tracking is observed for a typical sine wave period of 20 seconds.

1. Introduction

Multilayer bending actuators are a class of actuating devices, whose bending movement results from a difference of strain between the constitutive layers. They can be controlled by various external stimuli, but electrical current or tension are the most relevant due to their ease of use. Such actuator devices can be considered as artificial muscles if, by analogy with skeletal muscle, their change of dimension can be controlled in open-loop [1]. In particular, the development of Ionic Polymer-Metal Composite (IPMC) artificial muscles opened the way towards practical applications especially in biomedical devices [2]-[4] and micro-robotics [5]-[11]. However, whatever the chemical composition of multilayer bending actuators, their control remains a considerable problem due to the complex nature of physical phenomena occurring during the bending movements of their multilayer structure. Attempts have been made for proposing physical models of the multilayer bending of conductive polymers [12]-[14]. Such models are particularly interesting for understanding the bending phenomenon, but their direct use for control is not obvious. Moreover, although open-loop control is feasible, closed-loop control is essential for using such artificial muscle as an actuator: trajectory to be tracked can be directly specified, time-response of the actuator can be quicker, and physical phenomena peculiar to the materials used can be mastered, such as the so-called “drift”-phenomenon, which is a slow convergence towards the steady-state position [15]. In this context, open-loop positioning stability offers the advantage of making possible an open-loop identification of the dynamic contraction in order to use the identified model as a feedforward element of the closed-loop controller [16], [17]. The nonlinear nature of multilayer bending actuators often calls for sophisticated nonlinear approaches such as sliding mode control [18], adaptive control [19], [20], self-tuning control [21], [22], fuzzy or other intelligent-like control [23]. These sophisticated control approaches have for disadvantage their low efficiency. They can be very accurate, but the complex model has to be re-verified if a new sample is used and a high number of parameters (or rules) must be tuned. When the actuator is considered alone, this long process of control development can be admissible. It is, however, less obvious if we consider applications involving several actuators acting together as it is the case in robotics. For this reason, although we have a long experience in nonlinear control of pneumatic artificial muscles [24], [25], we also tried to promote simple ways for controlling artificial muscles. Linear PID (proportional-integral-derivative) control is particularly easy to put into work, whatever the linear or nonlinear character of the system to be controlled. This approach was naturally applied to bending actuators with parameters chosen using customary approaches, such as the Ziegler-Nichols rules [26]. Here we discuss an alternative, phenomenological approach of the PID-control applied to artificial muscles: an open-loop linear identification stage is used for highlighting peculiar properties of the dynamic contraction of the artificial muscle, from which simple, linear closed-loop control schemes can be derived. For example, in the case of pneumatic McKibben muscles, we

recently proposed an unusual linear I-controller based on the fact that any artificial muscle has its own stiffness and damping, which can respectively play the role of the P and D-actions of the PID [27].

In the present work, we propose to put into work a similar phenomenological control approach in the case of a promising, new bilayer bending actuator, based on spin crossover (SCO) particles [28], which are loaded into a P(VDF-TrFE), poly(vinylidene fluoride-trifluoroethylene) polymer matrix. To form a bilayer, the SCO@P(VDF-TrFE) layer is topped with a Ag@P(VDF-TrFE) (Ag = silver flakes) conductive layer [29]. The actuating strain arises from the significant molecular volume change that accompanies the electro-thermally induced spin crossover phenomenon in these materials [30]. This bending actuator has two key characteristics. First, it is fabricated as a monolithic object with a common polymer matrix backbone for the two layers, with consequently no delamination process occurring during the bending phenomenon. Second, the resulting monolithic material can be easily cut for different shapes and sizes [29]. In our previous work, several samples were developed by modifying the composition and microstructure of the SCO@P(VDF-TrFE) active layer – allowing us to adjust the actuator properties (strain, transition temperature, response time, etc.) [29]. Despite their differences, we have identified several common properties of these samples: They display a high sensibility to the control current, their rising time is rather long (several tens of seconds) and exhibit a pure delay before the movement begins. The latter phenomenon is a direct consequence of the time required to reach the spin transition temperature (see [29]). From the consideration of these properties, it is clear that one must privilege a low transition temperature to reduce both the initial pure delay and the rising time. For this reason, in the present work, we used a device incorporating 25 wt% of the sample $[\text{Fe}(\text{4-pentyl-1,2,4-triazole})_3](\text{tosylate})_2$ (actuator **1**) with spin transition occurring at 67 °C and 42 °C on heating and cooling, respectively. Although the pure delay was reduced in this sample to about 0.5 s, the rising time still largely exceeds 10 s. As a consequence, the 95% response time – the time beyond which the response keeps between 0.95 and 1.05 times the steady-state position – varies between ~25 s (1.2 A-step) and ~40 s (0.9 A-step). From this rough analysis, one might wonder whether our approach is relevant in terms of coming up with a new technology of bending actuation.

In the present paper, therefore we will extend and deepen the preliminary analysis of the control of **1** conducted in ref. [29] so as to grasp how a simple PID closed-loop control approach can enable this actuator to contract in closed-loop in less than 2 seconds. The originality of our approach consists in assuming that a simple linear PID-control can derive benefit from certain nonlinear properties of the artificial muscle for largely improving its contraction performances. After a first open-loop identification step proposed in Section 3.1, we will justify the choice of a PID-control in Section 3.2,

and finally we will emphasize the robustness of the considered actuator when carrying loads and highlight its performance during sine-wave tracking in Sections 3.3 and 3.4, respectively.

2. Experimental

The bilayer actuators were fabricated as described in ref. [29]. They were cut in a ‘U-shape’ and clamped at one end (see Fig. 1). The moving part of the actuator has a lateral dimension of ca. $3 \times 1 \text{ cm}^2$, with a 0.2 cm gap in the middle to create a conductive circuit. The thickness of the bilayer is roughly $150 \text{ }\mu\text{m}$ and the weight of the moving part is ca. 70 mg. We note that this choice of shape makes easy to embed a glued load. A computer-controlled current source (Keithley 2420) is used for heating the actuator via Joule effect, whereas the resulting bending movement is controlled by means of a laser triangulation position sensor (Micro Epsilon OptoNCDT 1750), pointing on the tip of the actuator. Open- and closed-loop control is achieved by means of the LabView-software (see Fig. 1).

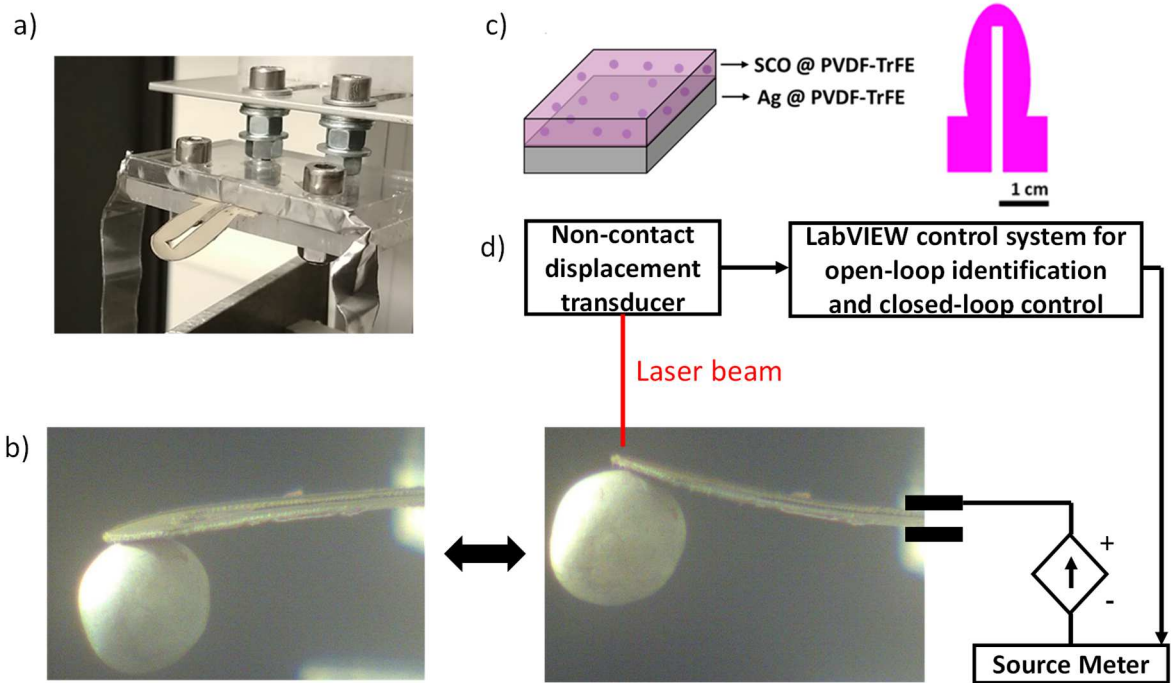


Fig. 1. Experimental setup: (a) photograph of a sample clamped in its support, (b) typical bending of an actuator with a payload, (c) bilayer structure and actuator geometry, (d) control scheme.

In its actual form, we have not tried to include a force sensor in the experimental set-up. This is especially difficult to do in the case of a bending actuator; this is why we preferred to estimate the force ability of the actuator by considering known embedded masses at its tip.

Results and discussion

2.1. Linear identification of the artificial muscle

The stable open-loop positioning character of the artificial muscle points the way towards open-loop identification, especially from recording its positioning in response to various step-wise excitations. The identification problem can be tackled using various methods such as the ANFIS-NARX

paradigm, recently applied to IPMC (Ionic Polymer Metal Composite) [14] to achieve an advanced non-linear model of the full bending actuator. In a more conventional way, classical techniques of linear system identification applied to the time variation of the sample can be used. In ref. [15], the authors propose a sixth-order transfer function with a constant gain to identify their polypyrrole tri-layer actuator. Our approach differs in the sense that it is based on a preliminary observation of the open-loop step-response of the bending actuator. From Figs. 2a and 2b, it appears that the initial slope is close to zero and, whatever the control current, no large oscillation can be observed during the rising phase towards the steady-state. We remark also that no overshoot of the steady-state value occurs. However, a pure delay exists whatever the current value. Since it appears to be limited to less than 0.5 second, it will be neglected in our linear identification. As a consequence, a simple overdamped second-order model with two real poles will be considered according to the following transfer function: $x_M(s)/I(s) = K/(1 + T_1s)(1 + T_2s)$, where x_M denotes the tip position of the bending artificial muscle, I the control current, K the gain of the system, T_1 and T_2 , the time “constants” of the system, supposed to be overdamped, and s designates the variable of Laplace formalism.

In our open-loop identification experiments, the control current value is varied in a limited range between 0.7 and 1.2 A. We have chosen these values because it makes possible to control the tip movement of the sample between ca. 0 and 7 mm, which is just beyond the positioning range we considered in the closed-loop experiments. In practice, the sample can be controlled in a much larger current range of 0 – 3 A. However, beyond a current value of ca. 1.2 A, the temperature of the sample becomes too high (ca. 140 °C) leading to the modification and ultimately to the destruction of the sample. The result of the identification of the open-loop step response is shown in Fig. 2. The result of the identification of the open-loop step response is shown in Fig. 2. One can depict in Fig. 2a the response of the actuator for a positive current step of 1.2A and then its reverse movement when a step back to 0 A is applied. The same parameters have been used for the ascending and descending model (dashed line). The good agreement between real response and the model emphasizes a similar behavior of the actuator bending and unbending. This is a very positive attribute for tracking performances requiring movements in each direction. Fig. 2.b shows responses to different positive steps whereas Fig. 2.c shows the current dependence of the damping factor $z = (T_1 + T_2)\sqrt{T_1T_2}/2$, and the natural frequency $\omega_n = 1/\sqrt{T_1T_2}$ deduced from the identified values of T_1 and T_2 [31]. The identification accuracy was simply measured by $Mean_{0 \leq t \leq t_{horizon}} |(x_L(t) - x(t))/x_L(t)|$, where $t_{horizon}$ is a ‘time horizon’ we have taken equal to 70 s. Similar accuracy was obtained in the whole positioning range (0.11 mm for 0.7 A, 0.07 mm for 0.8 A, 0.08 mm for 0.9 A, 0.06 mm for 1 A, 0.08 mm for 1.1 A and 0.10 mm for 1.2 A), which suggests a satisfactorily identification.

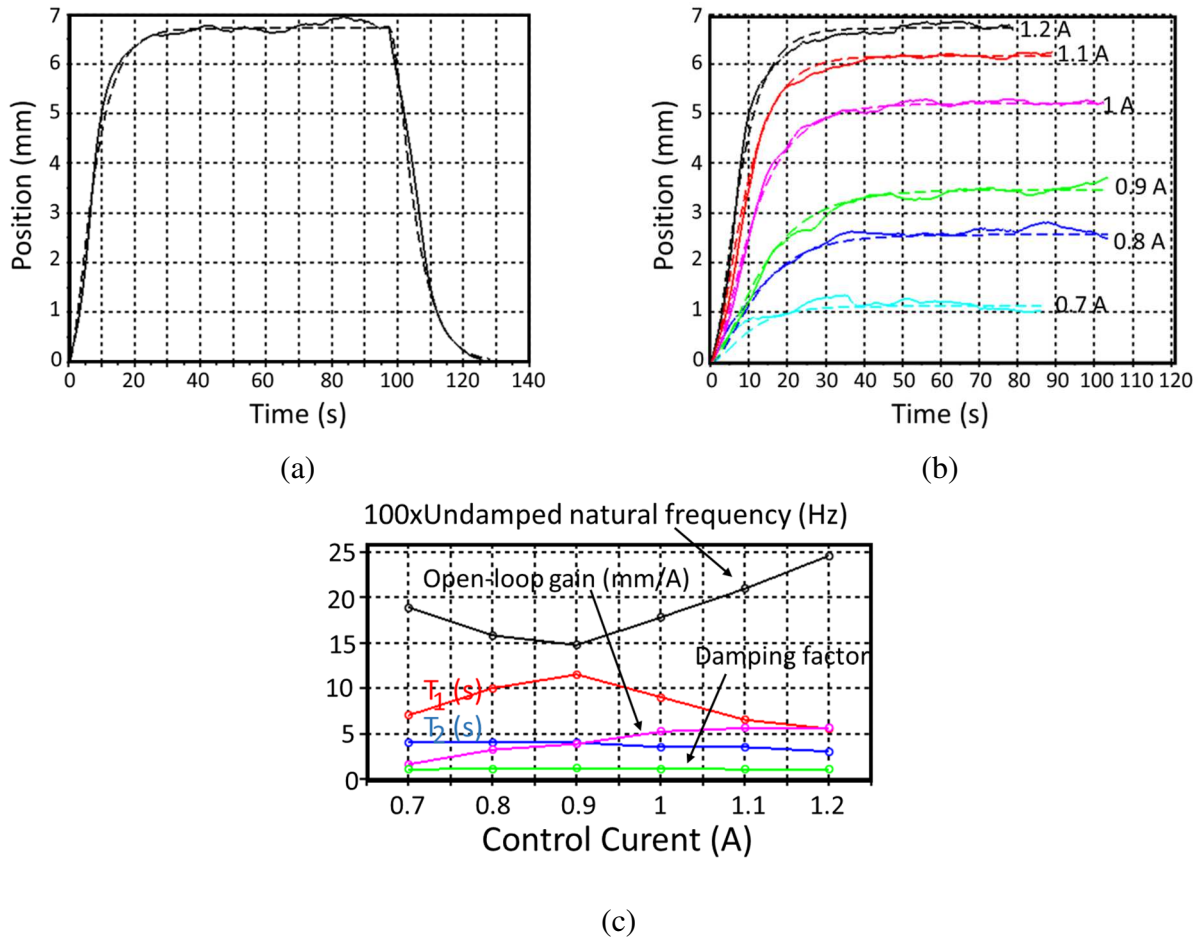


Fig. 2. Open-loop identification: (a) Typical response (full line) and corresponding linear model (dashed line) for a 1.2 A step and its return to zero, (b) Step responses for a given input current (full line) with corresponding linear identification (dashed line) and (c) the extracted parameters of the second-order model.

We must underline the absence of ‘drift’, which often makes the convergence to the steady-state slower than expected for a linear system. This phenomenon is frequently observed in multilayer bending actuation devices especially for the higher step control values [15]. The fact that T_1 , T_2 and the gain K are changing with control current emphasizes the nonlinear character of the actuator. However, two remarks can be made:

- The damping factor z is almost constant, approximatively equal to 1. In open-loop, the actuator behaves like a critically damped linear system, which means that the system returns to the equilibrium as fast as possible without overshooting. This naturally optimal damped character of the actuator should make the tuning of a PID closed-loop control easier;
- The ω_n -parameter appears to increase with rising current, from the 0.9 A-step value. This means that, above this threshold-value, the higher is the control current, the faster is the bending. This is another very positive point for a linear closed-loop control: when the actuator

will be far away from its goal, the effect of the PID will be amplified by the natural actuator behavior, and this effect will be moderated at the goal's approach. Moreover, the fact that the gain K appears to increase slowly with increasing current intensity also participates in this amplification effect of the PID controller. In the next section, we show how we can derive benefit of this identification for a simple, but relevant, linear PID-control of this nonlinear system.

2.2. PID control

The pseudo second-order dynamic behavior of the considered actuator, with its two real poles, suggests a simple closed-loop control by means of a PI-controller, which we can write as:

$$I(t) = G[\varepsilon(t) + (\frac{1}{T_I}) \int_0^t \varepsilon(\tau) d\tau] \quad (1)$$

where I is now the closed-loop control current, $\varepsilon(t)$ is the desired position minus the real position, and G, T_I are the corresponding proportional and integral parameters of the PI. The corresponding transfer function of the open-loop system can be written as:

$$x_M(s) / \varepsilon(s) = KG(1 + T_I s) / [(1 + T_1 s)(1 + T_2 s)T_I s] \quad (2)$$

It is clear that the choice $T_I = \text{Max}(T_1, T_2)$ leads to eliminate the dominant pole of the system to be controlled, which is apparently always T_1 in our case. By doing $T_I = T_1$ in Equ. (2), the following function transfer of the closed-loop system results - where x_d is the desired position:

$$x_d(s) / x_M(s) = KG / [KG + (1 + T_2 s)T_1 s] \quad (3)$$

It is then possible to choose the G -gain by means of the following relationship:

$$G = \frac{T_1}{4z^2KT_2} \quad (4)$$

where z is the damping factor of the closed-loop system. Typically, $z = 0.7$ realizes the best compromise between low rising time and overshooting. Fig. 3a shows the simulation of a closed-loop step-response for a target position of 5 mm, which is compared to the experimentally measured open-loop response with a similar target position (5.2 mm). We can observe here the classical speed improvement obtained by the PI-controller: the response time is diminished from ~31 s to ~14 s.

Let us consider now a PID written in the form:

$$I(t) = G[\varepsilon(t) + T_d \dot{\varepsilon}(t) + (\frac{1}{T_I}) \int_0^t \varepsilon(\tau) d\tau] \quad (5)$$

where T_d corresponds to the derivative parameter, in complement with the already defined gain and integral parameters, applied to the real sample. As shown in Fig. 3a, a simple manual tuning of parameters has led to an experimental step-response with a response time of about 1.6 s, which is an order of magnitude smaller than the value predicted by the simulated PI-controller. To explain this huge difference, we shall examine the electrical energy transmitted to the actuator during its

contraction in the case of the real open-loop response and the real closed-loop response (Fig. 3b). This energy (Joule heating) has the form $R I^2$, where I is the control current and R is the electrical resistance of the actuator, measured by a four-wire measurement setup, simultaneously with the actuation of the device. From these measurements, it appears clearly that the real PID-control can boost the system response by increasing the current intensity to reach, temporally, its maximum 3 A-value, which is significantly higher than the one required for maintaining the sample in its steady-state. This suggests that when the current intensity is high, but limited by the 3 A-bound, the actuator response accelerates. In other words, its equivalent natural frequency, which was shown to increase between 0.9 A and 1.2 A (Fig. 2b), must continue to increase beyond 1.2 A. To justify this assumption, we developed an original identification of the actuator system from the knowledge of its real closed-loop step response.

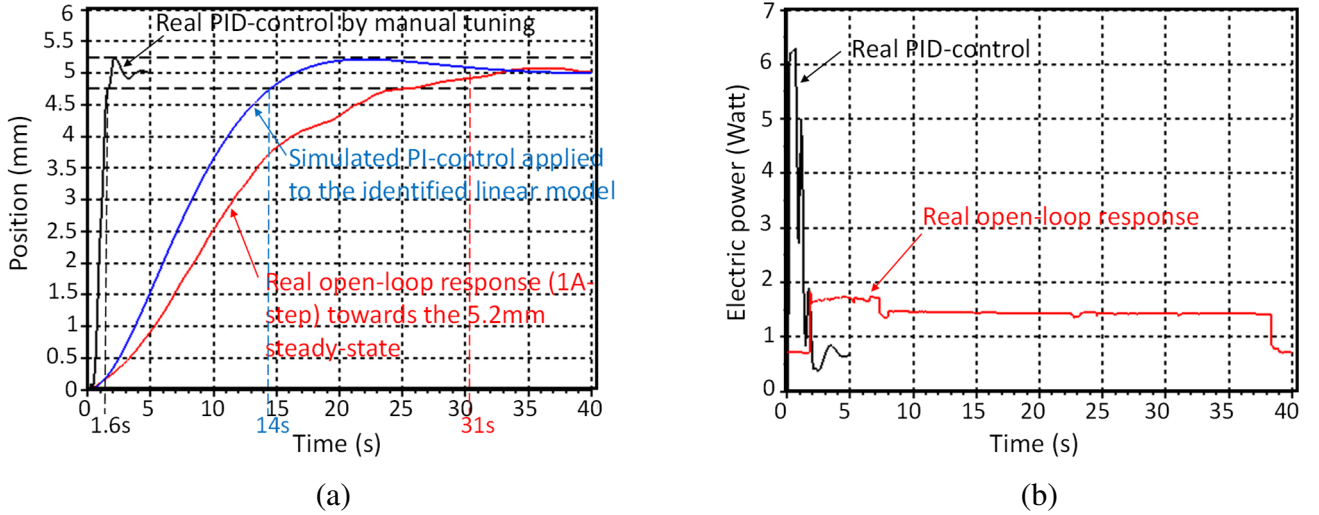


Fig. 3. (a) Simulation of an optimal PI-control for a 5 mm target position (blue line) compared to the experimental open-loop response (green) as well as to the best manually-tuned, experimental PID step response (red). (b) Measured electrical power supplied to the actuator in the open-loop and closed-loop cases.

While the second-order open-loop identification suggested the use of a PI-controller for the closed-loop control, it appeared in practice that a PID with a non-zero value for the derivative term gives a better result than a pure PI-controller with, however, a relatively low value for the ‘D’-term. Fig. 4a displays the real closed-loop step responses obtained for four target positions (2, 3, 4 and 5 mm). The three parameters of the PID were manually tuned in order to get the best achievable 95% response time. For each target position, the optimized PID values (G, T_d, T_I) are very similar: (2.5, 0.1, 0.7), (2.2, 0.15, 0.4), (1.8, 0.1, 0.5) and (1.8, 0.1, 0.4) for $x_d = 2, 3, 4$ and 5 mm, respectively. We can notice that the value ‘ $T_d T_I$ ’, representing the ratio between the derivative component ‘ $G T_d$ ’ and the integral

component ' G/T_I ' varies between 0.04 and 0.07 (s^2), indicating much greater influence of the integral component over the derivative component. In practice, a first real PI-controller was tuned in order to get the best response time of the closed-loop system, before adding a slight derivative component in order to reduce the observed oscillating behavior of the actuator during its rising movement. For reasons of clarity, the presentation was limited to the sole best PID results. In these conditions, it appears that the overshooting can be kept under control in such a way that the step-response does not leave the range $[0.95x_d, 1.05x_d]$ as soon as it is inside. On the other hand, the 95% response times are very similar, with values between 1.3 and 1.6 s. These two findings suggest that, despite the pure delay, which now cannot be neglected, the whole closed-loop system globally behaves like a linear system. Consequently, since the PID is a linear controller, the actuator system alone behaves as a linear system, but with a much faster response than the open-loop system. This is this “equivalent” linear system of the actuator, brought about by the PID-controller, which we decided to identify. For doing that, we consider the following transfer function:

$$g(s) = K_e(1 - \tau s)/(1 + \tau s)(1 + T_{1e}s)(1 + T_{2e}s) \quad (6)$$

This model now considers four parameters: $K_e, T_{1e}, T_{2e}, \tau$. The first three parameters have the same meaning that the initial parameters K, T_1, T_2 of the previously considered linear model of the actuator. On the other hand, the τ -parameter is supposed to take into account the pure delay of the “equivalent” actuator in the form of the sub-system, inspired by the first-order Padé-approximation [31]: $(1 - \tau s)/(1 + \tau s)$. It is however important to note that the τ -parameter cannot be considered as an estimation of the pure delay of the global system due to the too close values for τ, T_{1e} and T_{2e} . This linear identification was deduced from the comparison between the simulated step-response of the transfer function $g(s)PID(s)/(1 + g(s)PID(s))$, where $PID(s)$ is the transfer function of the PID, whose parameters are the real parameters considered for each desired position. The resulting simulated PID-control of the identified, “equivalent” actuator linear model is drawn in dashed lines in Fig. 4a and compared to the real PID-response. Fig. 4b shows the corresponding model parameters compared to the parameters of the original model of the actuator that we used for open-loop identification. Two main points result from this comparison. First of all, the time “constants” (T_{1e}, T_{2e}) of the equivalent actuator linear model are more than one order of magnitude lower than the initial ones (T_1, T_2). Furthermore, the parameters K_e, T_{1e}, T_{2e} do not significantly differ for different target positions (between 2 – 5 mm). These findings lead us to the surprising conclusion that the PID control is able to derive benefit from the nonlinear character of the actuator for considerably improving its response time. When going from the open-loop to the closed-loop, this response time can be diminished by a factor of 10. This phenomenon seems to be the consequence of the improvement in the responsiveness of the actuator itself when the control current increases. Remarkably, to some extent, the PID gives rise to the linearization of the actuator, denoted by the fact

that the response time becomes roughly independent of the desired position, albeit in a limited range between 2 - 5 mm. It is however to be noted that the about 0.5 s muscle delay still exists. Most artificial muscles exhibit a pure delay, which can be reduced by the use of nonlinear control methods [32], but at the price of greater complexity in the number and the tuning of controller parameters. In the case of this study, we voluntarily limit our closed-loop controller to a sole linear PID. Neglecting the muscle delay in the closed-loop controller is also motivated by the fact that, in the case of an actuation device, tracking performances are often more important than step response performances, and that initial delay has less effect in tracking performances than in step response performances.

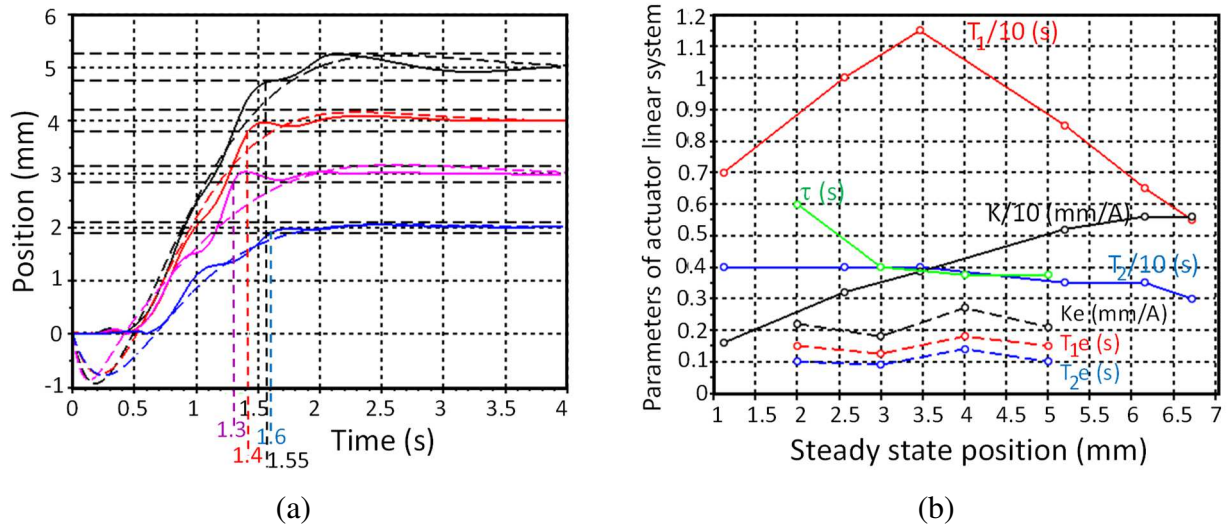
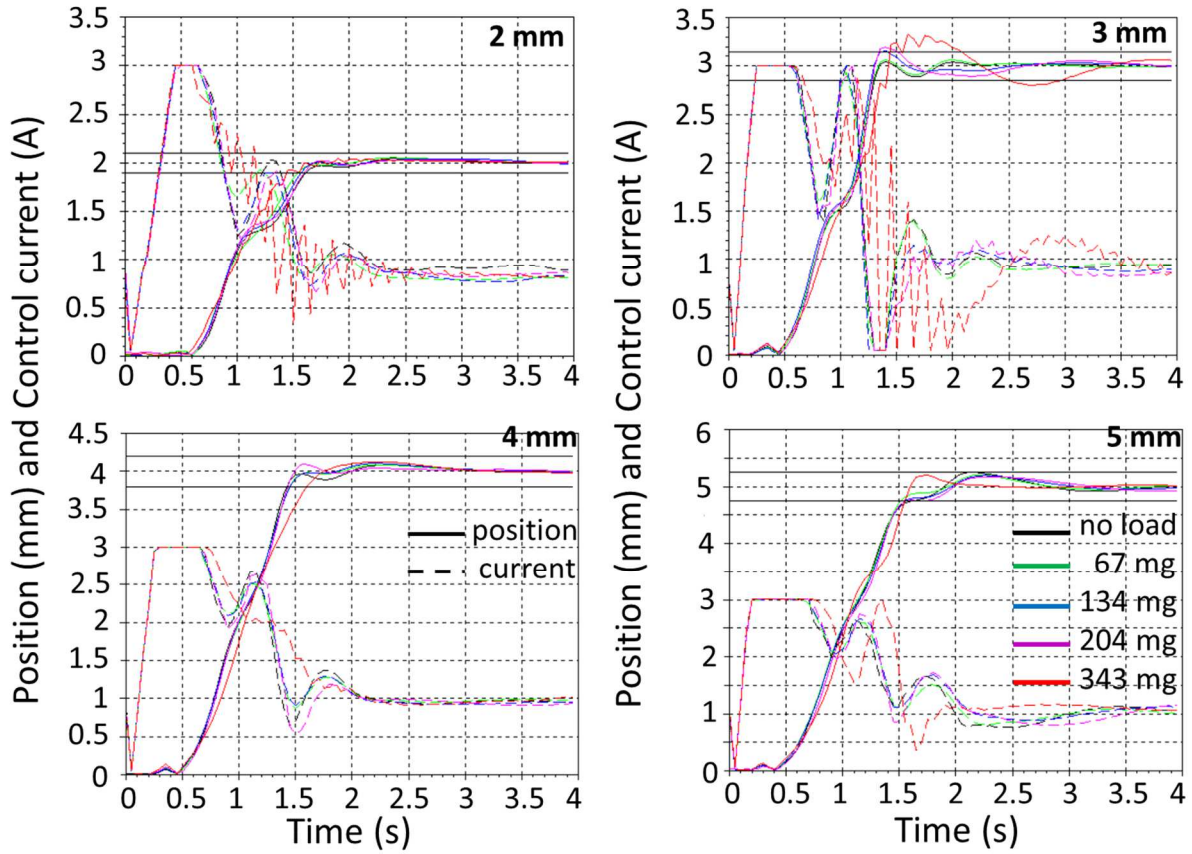


Fig. 4. Step-responses in closed-loop control and derived identification of the actuator: (a) Time response for desired positions 2, 3, 4 and 5 mm (in full line) and corresponding simulated PID-control applied to the equivalent identified actuator linear model (in dashed line), (b) Comparison between parameters of the initial identified open-loop linear model and those of the equivalent linear model.

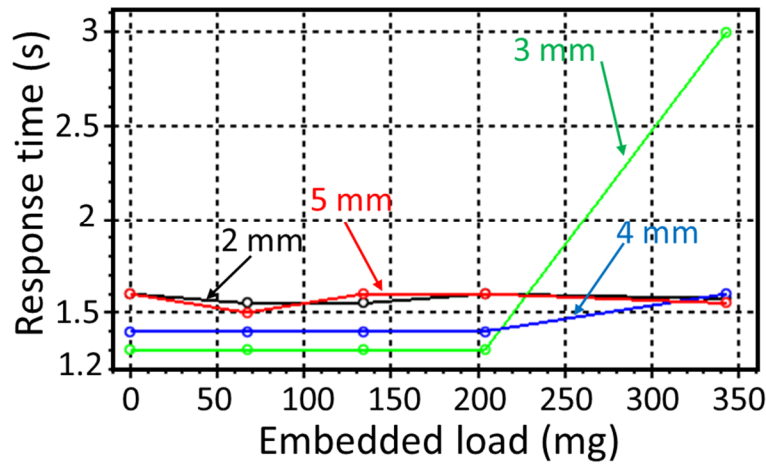
Crucially, the tuning of the PID for a rapid step-response does not deteriorate the stability. A simple way for evaluating the stability and the robustness in the case of our closed-loop system can consist in determining the corresponding couple (phase margin, gain margin). We propose to derive these two parameters from the knowledge of the “equivalent” actuator linear model. We obtained, respectively, for the target positions 2, 3, 4 and 5 mm the following couples: (55°, 7 dB), (57°, 10 dB), (58°, 9 dB) and (58°, 10 dB). One must note that a satisfactory phase margin is supposed to be greater than 45° and the gain margin is supposed to be equal to ca. 10 dB [31]. In particular, the phase margin of 55°-58° in the case of the equivalent actuator model, largely beyond the usual “comfort value” of 45°, suggests a robustness of the proposed control. In the following, this point will be verified for movements with embedded loads.

2.3. Performance with embedded loads

From a practical point of view, any actuator must be able to embed a load [16], [33]. Yet, most studies dealing with the control of multilayer bending actuators do not report experiments with loads. In the case of a bending actuator, the load can be placed at the tip of the cantilever, as shown in Fig. 1d. It is important to note that the ability of lifting a load is made possible by the relative stiffness of the proposed actuator. We have analyzed this ability both in step response and trajectory tracking. The relevance of our control approach is the most evident when we consider the same step responses with embedded load by keeping, in the full range of loads, the same values for the PID-parameters than those tuned for the controller without any load (see Fig. 5). As it can be seen, in all reported experimental step responses, the maximum value of 3 A for the control current is used during a short time, leading to heating of the sample at about 120 °C. Under open-loop conditions, it is impossible to use the system to perform rapid motion as overheating occurs very quickly above 140 °C, and leads to melting of the material after some seconds. For this reason, as already mentioned, currents above 1.3 A are never used in open-loop conditions, but in closed-loop, it is possible to use such temporary overheating. Moreover, it is important to note that current peaks essentially occur in the case of a step response while they are not present during tracking responses, as considered further, due the continuous character of the trajectory to be tracked. We have found the actuator to be able to operate continuously over 2 weeks and 35000 thermal cycles with no observed change in the actuators performance [29] as the PID controller effectively prevents the material from going above 120 °C while still allowing access to the fast motion granted by brief use of 3A currents.



(a)



(b)

Fig. 5. Closed-loop step response with embedded loads: (a) Position and current versus time for the four considered target positions: 2 mm, 3 mm, 4 mm and 5 mm – in each case for 5 different loads. (b) Corresponding 95% response time versus load.

2.4. Sine wave tracking performance

The closed-loop step experiments discussed above are favored by the high sensibility of the actuation device to react quickly to temperature changes when it has reached the spin transition temperature

[28]. However, a reverse movement requires cooling, which takes more time. In order to determine the practical consequences of this non-symmetrical behavior between warming and cooling, peculiar to all Joule-effect actuation systems, we have analyzed the dynamic behavior of our artificial muscle in response to multiple sine waves. Fig. 6 shows selected sine wave tracking experiments. Two cases have been considered. First, a sequence of three sine waves with successive 30s-30s-20s periods and, respectively, 2-4 mm, 3-5 mm and 2-5 mm target position changes was programmed. These periods are sufficiently long to make possible the cooling of the sample during the reverse movement imposed by the sine wave. It is important to note that, in all reported tracking experiments, the same set of PID-parameters ($G = 1.8, T_d = 0.1, T_I = 0.5$) was used. Through this choice of a constant set of PID-parameters, we aimed for analyzing the possibility of an efficient trajectory tracking with a pure linear control, whatever the frequencies and amplitudes of considered sine waves. As it can be seen, the tracking error, defined as the difference between the target and real position, remains between -0.2 and $+0.15$ mm when no load is embedded, whereas this range slightly increases $[-0.25$ mm, $+0.2$ mm] for a 204 mg-load. One shall note that the actuator reacts very well when the direction of the sine wave is changing, which is remarkable in comparison with other types of artificial muscles exhibiting frictional-like phenomena or other adverse effects when the actuator has to move back. This phenomenon is consistent with the symmetrical behavior already noticed for ascending and descending steps (Fig. 2.a). Moreover, during tracking, control current, not shown in Fig. 6 for reasons of clarity, stays at a value close to 1A, which is still in accordance with closed-loop step results shown in Fig. 5, where the steady-state current is inside a $[0.8$ A- 1.2 A]-range for all considered target positions.

In a second type of experiment, we looked for the limits of the actuator when it is submitted to a higher frequency. We programmed a sequence of three sine waves, with the same displacements as in the previous experiment, but the periods are now reduced to 20 and 15 s. As it can be seen in Fig. 6, the descending phase in the 15s-period, which requires the fastest cooling of the sample, generates a large deviation from the desired sine wave with a maximum tracking error of ~ 0.55 mm. We believe this last result stems from the characteristics of the actuation device itself rather than from the limitations of our control approach.

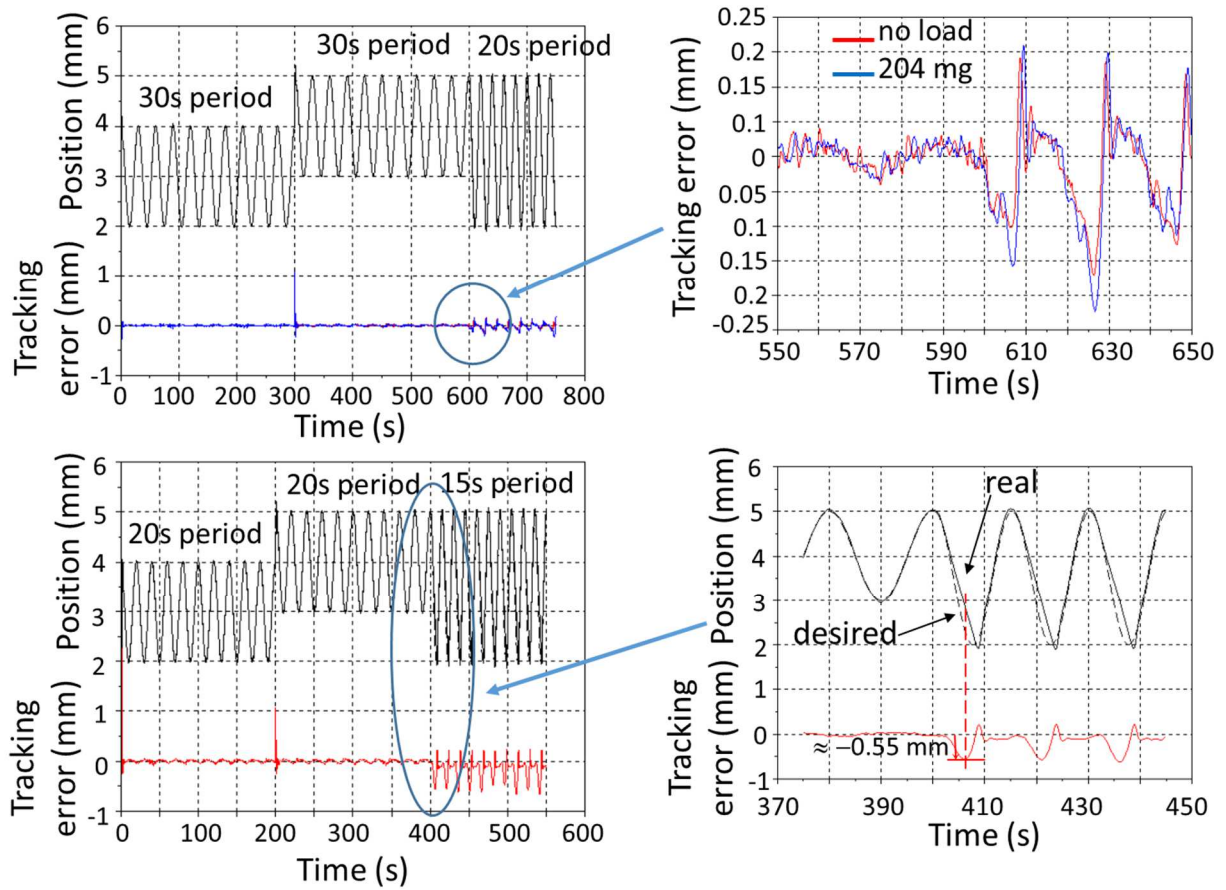


Fig. 6. Sine wave tracking experiments in closed-loop for different target periods and displacements – either with or without payload. The target (resp. real) positions are plotted using dashed (resp. straight) lines. The tracking errors are also shown.

Conclusions

We investigated the PID control of a new type of bending actuator, based on spin crossover molecules. The justification of using a so common linear controller for a highly nonlinear actuator is based on a two-step process. We started by an open-loop identification, which led us to propose a second-order linear model, whose parameters were shown to vary with the control current. This open-loop identification step suggests that the time “constants” of the second-order model decrease monotonously from a threshold value, leading to an increasing actuator responsiveness when the control current increases. This natural decrease of the actuator response time with increasing current intensity was effectively demonstrated in closed-loop PID control experiments. To verify this amplification effect, in a second step, we identified an “equivalent actuator” linear model derived from the “best-tuned” PID. Time “constants” of this equivalent linear model appear diminished by a factor of ~ 10 in comparison with the original actuator linear model. Overall, the proposed PID-control is able to generate, in a current range of 0 - 3 A, step responses of the actuator tip with displacements

in the range of 2 – 5 mm associated with a 95% response time between 1.3 and 1.6 s. Remarkably, the latter remains virtually unchanged when loads are embedded (up to 350 mg, which corresponds to ca. 5 times the own weight of the actuator). The sine wave tracking performance of the actuator is also very satisfying with respect to the physical cooling possibilities of the actuator. In a more general manner, we believe that such a linear control method proves that a simple PID-control can be adapted for nonlinear actuators if the PID can derive benefit of these nonlinear effects.

Further work will aim to include a self-sensing process inside the actuator as discussed, for example, in [34]. Practical applications of the actuator require a good control of the temperature; this can easily be done in closed-loop by a direct control of the current value, and the time during which higher values of the current can be imposed. A self-sensing of the actuator position, combined with a simple PID closed-loop control, as defended in this article, should make the actuator autonomous and safe for future human-friendly applications. Further improvements in this direction could be also obtained using an active material with lower spin transition temperature.

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