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Nam N. Huynh

University of Wollongong, nhuynh@uow.edu.au

Gursel Alici

University of Wollongong, gursel@uow.edu.au

Geoffrey M. Spinks

University of Wollongong, gspinks@uow.edu.au

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Force Analysis and Characterization of Polymer Actuators

Nam N. Huynh¹, Gursel Alici^{1,2}, and Geoff M. Spinks^{1,2}

²ARC Center of Excellence for Electromaterials Science

¹School of Mechanical, Material and Mechatronics Engineering
Faculty of Engineering, University of Wollongong
Wollongong, NSW, Australia

Abstract - There has been an increasing interest in conducting polymers, especially polypyrrole (PPy), as potential actuators for many cutting edge applications including micromanipulation and biomedical devices. Their performance needs to be assessed in terms of force and displacement outputs before they can be employed in practical devices. As part of an ongoing project to develop a robotic gripper for micromanipulation/fabrication applications, this study focuses on (i) deriving a mathematical model to predict the force produced at the tip of a trilayer bending type PPy-based actuator under input voltages, and (ii) experimentally verifying the model. The model has been used to estimate the force produced by a robotic finger consisting of a PPy actuator and a rigid link. The results presented show a good agreement between the experimental and predicted forces for two actuators with the dimensions of (10mm x 1mm x 0.17mm) and (5mm x 1mm x 0.17mm) with driving voltages up to 0.8 V.

Index Terms – *Electroactive polymer actuators, modelling, system identification*

I. INTRODUCTION

Conducting polymer actuators are potential actuators for many emerging applications including micromachining, microassembly, microswitches, micromanipulation and biomedical devices. PPy based conducting polymer actuators have been reported [1-4] to possess many useful properties such as (i) they can be stimulated by typically 1 V or less, (ii) they have large and reasonably stable strain (3% in-plane to more than 30% out of plane) as well as high strength (Young's modulus varies from 0.05 GPa to 100 GPa and tensile strength from 1 MPa to 1 GPa), (iii) they can generate considerably high force and moment when employed as either linear, volumetric or bending actuators, (iv) they can be positioned continuously between two extremes, and (v) they are lightweight and biocompatible, and can operate in and out of liquid media. Of course, they have some disadvantages. Firstly, their speed of response is slow due to their actuation mechanism which relies on ion/mass transfer [5], [6]. Another drawback is the loss of positioning precision due to nonlinear hysteresis and creep effects. The later hampers efforts of developing fairly accurate mathematical models to predict their force and bending displacement outputs. Such models are of significant importance to design new actuators satisfying force and/or displacement requirements [7, 12-15].

Many of the modeling approaches reported in the literature which are mainly for bi-layer polymer strips are based on the classical bending beam theorem. The modeling approaches are very similar to the bending modeling of the bi-metal thermostat reported by Timoshenko [8]. For example, Berry and Pritchett [9] were reported to be pioneers in applying bending beam method to study the moisture swelling in polymers. Their work focused onto analyzing the kinetics of diffusion controlled bending. The method was therefore applicable for sorption-related studies. Such a bending model for bilayer strips was also readily employed for investigation of the heat of solution and activation energy for diffusion. The study of Berry and Pritchett was then further developed by Tong et al. [10] whose work demonstrated the effects of film thickness and heat treatment to the water sorption by a thin poly(methyl methacrylate) (PMMA) film. This revised bending-beam technique was claimed to be adaptable for transport studies of penetrant into ultrathin polymer films. Pei and Inganas [11] reported a mathematical model simulating the bending of a PPy-based bipolymer strip. The model was then used as a tool to study the volume changes of PPy layers during reduction/oxidation processes, the accompanying charge compensation (mass-transport) and the phase transition of polypyrrole. Later on, Madden [7] suggested a bending model for trilayer PPy based actuator, but without its experimental verification.

This study focuses on deriving a mathematical model to predict the blocking force at the tip of a trilayer bending type PPy-based actuator under driving voltages. As opposed to the previous version of these actuators, the actuator operates in air. The experimental and theoretical results presented show that the model is valid enough to predict the force output of bending type PPy actuators. The model has been extended to predict the force output of a robotic finger consisting of a polymer actuator and rigid link made of carbon fiber.

II. CONDUCTING POLYMER ACTUATOR

The PPy-based conducting polymer is provided by the Intelligent Polymer Research Institute (IPRI) at our university. It has five layers. The outmost two layers are polypyrrole with thicknesses of 30 μm . The middle layer is polyvinylidene fluoride (PVDF), an inert, non conductive, porous polymer serving as the separator of two electroactive PPy layers and reservoir for electrolyte TBA.PF₆ (tetrabutylammonium

hexafluorophosphate) 0.05 M in solvent propylene carbonate. Thin layers of platinum of 10 to 100 Å are sputtered on top of PVDF to enhance the conductivity between PPy layers and electrolyte. The cross section of the actuator considered in this study is shown in Fig. 1.

Fig. 1 Cross section of the PPy-based actuator (not to scale).

The structure of the actuator driving a rigid link which is similar to a finger of a robotic gripper is shown in Fig. 2.

Fig. 2 Structure and dimensions of the robotic finger (front view and top view)

The finger is basically a strip of PPy-based conducting polymer loaded by a thin layer of rigid carbon fibre, in which the conducting polymer works as an *actuator and a joint* while the carbon fibre attached on the top of the polymer serves as a rigid link for the robotic finger. The finger is typically 1 mm wide, 10 mm long (5 mm for actuator part and 5 mm for rigid part).

Fabrication process of a robotic finger is outlined as follows.

- A sheet of conducting polymer is trimmed into strips of 1x15 mm² and carbon fibre is trimmed into pieces of 1x5 mm².
- Carbon fibre pieces are cured in an oven for about 10 minutes at 100°C. Carbon fibre should be very rigid after taken out of the oven.
- Double-sided sticky tape is put onto rigid carbon fibre pieces.
- The rigid carbon fibre piece with the sticky tape on one side is then attached on the polymer strip.

The samples are replenished in tetrabutylammonium hexafluorophosphate (TBA.PF₆) 0.05 M electrolyte for five minutes before each test.

III. FORCE MODEL FOR TRILAYER CONDUCTING POLYMER ACTUATOR

Fig. 3 Actuator geometry and demonstration of parameters used in the model.

The model is developed based on the following assumptions

- Cross sections are plane at any position along the actuator (pure bending).
- PPy and PVDF are elastic and isotropic. The Young moduli of PPy and PVDF remain unchanged throughout the range of bending being considered.
- The thickness change of PPy layers when ions get in and out during oxidation-reduction process is negligible compared to the overall thickness of the strip.
- The rate of ions entering or leaving PPy layers is considered constant along the actuator length.
- Based on the previous assumption, the strain in PPy layers at distance y from the neutral axis due to the thickness change of PPy layer during redox process is constant throughout PPy layers and is denoted by α . It is reported in the literature [7, 12] that α is a function of the strain to charge ratio and charge density in the PPy layers.
- Strain at any cross section of the actuator is symmetric about the neutral axis.

Strains in the upper PPy layer, in PVDF layer and in the lower PPy layer at distance y from neutral axis are, respectively.

$$\epsilon_{1u} = \frac{y}{R} + \alpha \quad \left(\frac{h_2}{2} \leq y \leq h_1 + \frac{h_2}{2} \right) \quad (1)$$

$$\epsilon_2 = \frac{y}{R} \quad \left(-\frac{h_2}{2} \leq y \leq \frac{h_2}{2} \right) \quad (2)$$

$$\epsilon_{1l} = \frac{y}{R} - \alpha \quad \left(-h_1 - \frac{h_2}{2} \leq y \leq -\frac{h_2}{2} \right) \quad (3)$$

R is the radius of curvature of the neutral axis. In above equations, we assume that the bending actuator has a constant curvature along its length. Using Hooke's law, the stress in each layer is

$$\sigma_{1u} = \left(\frac{y}{R} + \alpha \right) E_1 \quad \left(\frac{h_2}{2} \leq y \leq h_1 + \frac{h_2}{2} \right) \quad (4)$$

$$\sigma_2 = \frac{y}{R} E_2 \quad \left(-\frac{h_2}{2} \leq y \leq \frac{h_2}{2} \right) \quad (5)$$

$$\sigma_{1l} = \left(\frac{y}{R} - \alpha \right) E_1 \quad \left(-h_1 - \frac{h_2}{2} \leq y \leq -\frac{h_2}{2} \right) \quad (6)$$

The total moment due to the induced internal bending moment and external force must always be zero at any cross section along the actuator when it is at a stable position

$$\int_{\frac{h_2}{2}}^{\frac{h_2}{2} + h_1} \sigma_{1u} b y dy + \int_{-\frac{h_2}{2}}^{\frac{h_2}{2}} \sigma_2 b y dy + \int_{-\frac{h_2}{2} - h_1}^{-\frac{h_2}{2}} \sigma_{1l} b y dy - FL = 0 \quad (7)$$

F is an external force acting at the actuator tip. E_1 and E_2 are Young moduli of PPy and PVDF respectively, b is width

of the actuator, L is the actuator length. Substituting (4), (5), (6) into (7) produces

$$\begin{aligned} & \int_{\frac{h_2}{2}}^{\frac{h_2}{2}+h_1} \left(\frac{y}{R} + \alpha \right) E_1 b y dy + \int_{\frac{h_2}{2}}^{\frac{y}{R}} E_2 b y dy + \int_{\frac{h_2}{2}-h_1}^{\frac{h_2}{2}} \left(\frac{y}{R} - \alpha \right) E_1 b y dy - FL = 0 \\ \Leftrightarrow & \frac{2E_1 b}{R} \left[\frac{\left(\frac{h_2}{2} + h_1 \right)^3}{3} - \frac{\left(\frac{h_2}{2} \right)^3}{3} \right] + \alpha E_1 b h_1 (h_1 + h_2) \\ & + \frac{2E_2 b}{R} \frac{\left(\frac{h_2}{2} \right)^3}{3} - FL = 0 \end{aligned} \quad (8)$$

According to the actuator geometry shown in Fig. 3, the area moments of inertia of the PVDF layer and PPy layers are given by

$$I_{PVDF} = \frac{b h_2^3}{12} = \frac{8b \left(\frac{h_2}{2} \right)^3}{12} = \frac{2b \left(\frac{h_2}{2} \right)^3}{3} \quad (9)$$

$$I_{PPy} = \frac{b (h_2 + 2h_1)^3}{12} - \frac{b h_2^3}{12} = \frac{2b \left(\frac{h_2}{2} + h_1 \right)^3}{3} - \frac{2b \left(\frac{h_2}{2} \right)^3}{3} \quad (10)$$

Substituting (9) and (10) into (8) results in

$$\frac{1}{R} [E_1 I_1 + E_2 I_2] + \alpha E_1 b h_1 (h_1 + h_2) - FL = 0 \quad (11)$$

$$\Rightarrow \frac{1}{R} EI + \alpha E_1 b h_1 (h_1 + h_2) - FL = 0, \quad (12)$$

where $EI = E_1 I_1 + E_2 I_2$ expresses the flexural rigidity for the whole actuator.

Equation (12) describes two special cases [7, 15];

- (i) Free deflection: no force is applied at the actuator tip ($F = 0$). This case refers to the maximum deflection of the actuator under an input voltage.
- (ii) Zero deflection: Force is applied at the actuator tip so that vertical displacement of the tip is zero. This case identifies the maximum force that can be exerted at the tip of the actuator.

The free deflection case is applied to identify the value of α with the reciprocal of the curvature $1/R$ being measured from experiments. Values of α corresponding to different input voltages are then applied in the zero deflection case to calculate the expected force produced at the actuator tip.

In free deflection case, $F = 0$. Rewrite (12), we have

$$\alpha = - \frac{EI}{RE_1 b h_1 (h_1 + h_2)} \quad (13)$$

In zero deflection case, as actuators being considered are relatively small, the $1/R$ can be approximated by zero. From (12), we have

$$F = \frac{E_1 b \alpha h_1 (h_1 + h_2)}{L} \quad (14)$$

It can be inferred from (14) that the force at tip of an actuator decreases when its length increases.

The above discussion is now applied to calculate force created at the tip of a robotic finger. For the robotic finger structure shown in Fig. 2, the rigid part apparently does not contribute to the bending of the finger. The force model described by (14) is therefore only applied to the 5 mm actuator. Bending moment created by the actuator is then divided by the whole actuator length, which is typically 10 mm, to approximate the force created at the finger tip.

If the actuator length in the finger structure is denoted by a , the total length of the finger is denoted by L' and the approximated force at finger tip is denoted by F_{finger} , the force created by a robotic finger can be described mathematically by (15). These parameters are depicted in Fig. 4.

Fig. 4 Demonstration of parameters in the force model for the robotic finger.

$$F_{\text{finger}} = \frac{Fa}{L'}, \quad (15)$$

where F is calculated from (14).

IV. EXPERIMENTAL EVALUATION

The experimental setup is shown schematically in Fig. 5.

Fig. 5 Schematic representation of the equipment setup.

eDAQ e-corder recorder unit was used to record, amplify, filter and, together with *eDAQ Chart* softwares, analyse data. *eDAQ Potentiostat* is a three-electrodes preamplifier. *Aurora Scientific Inc. Dual-mode lever arm system, model 300B* was used to measure the force.

As shown by the arrows in Fig. 5, input voltage from *eDAQ Potentiostat* was applied to the actuator sample via electrode clamps. The actuator constrained by the force sensor created a contact force which was recorded by the e-corder

recorder unit. The processed output signal from recorder unit was consecutively sent to a computer where it is displayed and analyzed using eDAQ Chart version 5.1 for Windows. The current and voltage were sent directly from potentiostat to the e-corder recorder unit to be displayed on the computer monitor. The force measurement lever was set at neutral position of the sample. The tip force was recorded and saved in a file together with input voltage and current. The maximum tip displacements were identified by recording movement of the strip on grid paper using a video camera.

Square wave input voltages with amplitudes of 1 V, 0.8 V, 0.6 V, 0.4 V and 0.2V were applied. The frequency of the applied voltages was 3 pulses per minute or 0.05 Hz. After each test, samples were neutralized by applying input voltage of 0 V until current passing through electrodes was zero. A typical recorded current, voltage and force data are shown in Fig. 6, as extract from eDAQ Chart.

The force experiments were conducted on two actuator samples ((10mm x 1mm x 0.17mm) and (5mm x 1mm x 0.17mm)) and a robotic finger with the dimensions of 5+5mm x 1mm x 0.17mm.

Fig. 6 A typical current, voltage and force data recorded for a 5mm x 1mm x 0.17mm actuator under 1V.

V. EXPERIMENTAL RESULTS AND DISCUSSION

The actuator parameters are as follows

- PPy thickness $h_1 = 0.03$ mm.
- PVDF thickness $h_2 = 0.11$ mm.
- Width of the finger $b = 1$ mm.
- Young modulus of PPy $E_1 = 80$ N/mm².
- Young modulus of PVDF $E_2 = 440$ N/mm².
- Poisson's ratio of PPy and of PVDF $\nu_{PPY} = \nu_{PVDF} = 0$.

The theoretical and experimental force results for the 5 mm and 10 mm actuators are shown in Fig. 7 and Fig. 8, respectively. The force results for the robotic finger are shown in Fig 9.

(a) Sample 1

(b) Sample 2

Fig. 7 Theoretical and experimental force results for two 5 mm actuators. The inverse of the corresponding radii of curvatures are shown below the force results.

(a) Sample 1

(b) Sample 2

Fig. 8 Theoretical and experimental force results for two 10 mm actuators. The inverse of the corresponding radii of curvatures are shown below the force results.

Fig. 9 Theoretical and experimental force results for the robotic finger (the upper plot). The inverse of the corresponding radii of curvatures are shown in the lower plot.

Step response experiments were conducted to evaluate the speed of response of the 5 mm and 10 mm actuators and the robotic finger. The results are provided in Fig. 10.

(a)

(b)

(c)

Fig. 10 Force step responses of (a) 5 mm actuator, (b) 10 mm actuator and (c) the robotic finger under a range of input step voltages.

A close-up of the force responses of the 5 mm actuator is shown in Fig. 11, where it can be seen that there is almost no time delay in the response. Further, the higher is the input voltage, the higher is the speed of response.

Fig. 11 Force responses of the 5 mm actuator for the first 10 seconds under a range of input voltages.

The results depicted in Fig. 7 to Fig. 9 show good agreement between experimental data and modeling values with input voltage up to 0.8 V for whole-actuator samples and up to 0.4 V for the robotic finger. This is mainly due to the assumption of a small and a constant curvature while the force model was developed. It is, therefore, not accurate enough to describe the force at higher voltages. As seen in the results presented in Fig. 7 to Fig. 9, the force model can produce accurate results for actuator curvature up to 0.05 mm^{-1} .

Another explanation to the inaccuracy of the force model at high input voltages is that (14) was derived from (12) with the approximation of zero bending – infinite radius of curvature ($1/R = 0$) of the actuator. However, the experiments showed that bending does exist when the actuators are in contact with the force measurement lever. Such bending is very obvious at higher input voltages and with longer actuators. As shown in Fig. 12, the 10 mm and 5 mm actuator samples bend under an input voltage of 1.0 V. While curvature of such bending can be negligibly small for a 5 mm actuator, it cannot be for a 10 mm actuator. The mathematical model reported in this paper needs to be refined to include bending effect in predicting the output force.

Fig. 12 (a) 5 mm actuator and (b) 10 mm whole-actuator samples bending against force measurement lever.

VI. CONCLUSION

We have established an analytical model to predict the blocking force at the tip of a trilayer polymer actuator. The same model is extended to estimate the force reflected at the tip of a robotic finger consisting of the polymer actuator and a rigid link. The results calculated using the model showed a good agreement with the experimental results. However, the

large deflection of the actuator, which contradicts with our fundamental modeling approach, adversely affects the model accuracy. The accuracy was shown to decrease when bending curvature of the actuator exceeds 0.05 mm^{-1} . This bending is negligible for short actuators, but quite obvious for long actuators especially at high driving voltages. Future work involves (i) formulating the blocking force and the maximum deflection of the actuator as if the actuator is a one end-cantilevered beam under a distributed load, and (ii) estimating the effective modulus of elasticity of the actuator and the distributed load, which represents the actuation ability of the polymer actuator, as a function of the input voltage. We propose such a force model as an alternative to the analytical model reported in this paper.

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