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High-precision control of piezoelectric nanopositioning stages using hysteresis compensator and disturbance observer

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Abstract

This paper proposes a novel high-performance control scheme with hysteresis compensator and disturbance observer for high-precision motion control of a nanopositioning stage driven by a piezoelectric stack actuator (PSA). In the developed control scheme, a real-time inverse hysteresis compensator (IHC) with the modified Prandtl-Ishlinskii model is firstly designed to compensate for the asymmetric hysteresis nonlinearity of the PSA. Due to the imperfect compensation, the dynamics behaviors of the PSA-actuated stage with the IHC can be treated as a linear dynamic system plus a lumped disturbance term. Owing to the unknown nature of this lumped disturbance term, a disturbance observer (DOB) is used as a means for disturbance rejection. With the DOB, a tracking controller is finally designed and implemented to stabilize the position error. To verify the proposed control scheme, a real-time experimental platform with a PSA-actuated nanopositioning stage is built, and extensive experimental tests are performed. The comparative experimental results demonstrate the effectiveness and improved performance of the developed control approach in terms of the maximum-value errors, root-mean-square-value errors and hysteresis compensation.

Keywords: piezoelectric actuator, hysteresis, disturbance observer, motion control

(Some figures may appear in colour only in the online journal)

1. Introduction

In precision positioning applications such as micromanipulation [1, 2], atomic force microscopes [3] and ultraprecision machine tools [4], nanopositioning stages play a vital role in meeting the demand of the ultrahigh-precision motion. The key feature of nanopositioning lies in the fact that the positioning resolution, positioning accuracy and repeatability are within the nanoscale range. In this case, the traditional DC/servo motors are not up to this critical requirement. Owing to the excellent advantages of high displacement resolution, fast response time and large output force, piezoelectric actuators are usually utilized for nanopositioning stages [1]. However, the main challenge of the piezoelectric actuators for high-precision positioning is the inherent non-smooth hysteresis nonlinearity, which can lead to more than 15% positioning errors of the travel range. In particular, the positioning errors caused by the hysteresis nonlinearity may lead to undesirable inaccuracies or oscillations, and even instability [5, 6]. As a result, high-performance motion control of piezoelectric nanopositioning stages is becoming more and more important in nanopositioning applications.

To tackle this challenge, many efforts have been devoted to develop various control techniques for piezoelectric actuated systems involving with hysteresis. Feedforward control is the most common approach pioneered by Ge and Jouaneh [7] and extensive works have then been developed in [5, 8–13]. The main idea of the feedforward control is to construct an inverse hysteresis model to mitigate the hysteresis nonlinearity, and then the traditional linear control techniques can be applied for the dynamics system with hysteresis compensation [14, 15]. As an alternative, some attempts without modeling the inverse hysteresis have been made to directly apply the feedback control techniques such as H_{∞} control [16, 17], sliding model control [18, 19], and robust adaptive control [20, 21] to deal with the hysteresis nonlinearity. Due to non-smooth and nonlinear behaviors of the hysteresis, the main efforts are made in stability analysis for such feedback control techniques. On the other hand, neural network control [22], and fuzzy control [23] have also been developed to control of the piezoelectric actuated stages. It can be seen that, nowadays, development of control techniques for piezoelectric-actuated systems with hysteresis nonlinearity is an interesting topic. However, from the literature, the results are not completely satisfactory and new control approaches have still being sought. As a continuation, this paper aims to develop a new control method using the disturbance observer in tandem with the hysteresis inversion.

In the authors' previous work [24], a real-time inverse hysteresis compensator (IHC) with the modified Prandtl-Ishlinskii (MPI) model is designed to compensate for the asymmetric hysteresis nonlinearity of the piezoelectric stack actuator (PSA). However, dynamics of the PSA-actuated stage was not taken into account. This work is, therefore, motivated to extend the result in [24] to include the system dynamics, addressing the corresponding control issues. As a matter of fact, it is generally impossible to obtain the perfect hysteresis and system dynamics models due to the existence of the modeling uncertainties and disturbances. Errors are firstly introduced due to imperfect inverse compensation. Also, owing to modeling uncertainties and disturbances of the system dynamics, the dynamics of the PSA-actuated stage with the IHC can be treated as a linear dynamic system plus a lumped disturbance term. Due to the unknown nature of this lumped disturbance term, a disturbance observer (DOB) developed in [25, 26] is borrowed as a means for disturbance rejection. With the use of the DOB, a tracking controller is finally designed and implemented to stabilize the position error, which is verified by real-time experiments in a PSAactuated nanopositioning stage.

The remainder of this paper is organized as follows. In the next section, the developed controllers are designed. Then, in section 3, experimental setup and comparative experimental results are presented. After that, section 4 concludes this paper.

2. Controller design

In this section, the proposed control scheme with the IHC and DOB is detailed.

2.1. Hysteresis compensation

To facilitate the controller design, a dynamic model is required for description of the PSA-actuated system. In modeling the PSA-actuated system, the reasonable model



Figure 1. Block diagram of the dynamic model of a PSA-actuated system.

structure is a linear dynamic model G(s) preceded by a rateindependent hysteresis nonlinearity $\Gamma[\cdot][6, 17, 20, 27-29]$, which is shown in figure 1. The reader may refer to [6] for a detailed discussion and a short review of dynamic modeling approaches for PSA positioning systems. In this subsection, we focus on description and compensation of the hysteresis nonlinearity Γ .

The key of the inverse hysteresis feedforward controller is to cascade the inverse hysteresis model Γ^{-1} with the real hysteresis Γ to obtain an identity mapping between the desired output $\widehat{w}(t)$ and actuator response w(t), i.e.,

$$w(t) = \Gamma \Big[\Gamma^{-1} \Big[\widehat{w} \Big] \Big](t) = I \Big[\widehat{w} \Big](t) = \widehat{w}(t)$$
(1)

In this work, a modified Prandtl-Ishlinskii (MPI) model [24] is adopted to characterize and compensate for the asymmetric hysteresis of the PSA. In order to obtain the analytical inverse, the MPI model is expressed as follows

$$w(t) = \Gamma[v](t) = \Gamma_c[v](t) + \Gamma_p[v](t).$$
(2)

where v(t) is the input and w(t) is the output. In (2), $\Gamma_c[v](t) = g(v)(t)$ is a new model component generating the asymmetric hysteresis shape, and $\Gamma_p[v](t)$ is the classical P-I model defined as

$$\Gamma_p[v](t) = p_0 v(t) + \sum_{i=1}^n b(r_i) F_{or_i}[v](t).$$
(3)

where *n* is the number of the adopted OSP operators for modeling, and $b(r_i)$ is the weighted coefficient for the threshold r_i , $F_{or}[v](t)$ is the one-side play (OSP) operator [24], which is expressed as

$$F_{or}[v](0) = f_r(v(0), 0)$$

$$F_{or}[v](t) = f_r(v(t), F_r[v](t_i))$$
(4)

for $t_i < t \leq t_{i+1}$, $0 \leq i \leq N - 1$ with

$$f_r(v, u) = \max(v - r, \min(v, u))$$
 (5)

where $0 = t_0 < t_1 < ... < t_N = t_E$ is a partition of $[0, t_E]$, such that the function v(t) is monotone on each of the subintervals $[t_i, t_{i+1}]$. It should be noted that the OSP operator (4) has the rate-independent property, that is, the output of the OSP operator is only influenced by the current input value and the past extrema of input function v(t). The argument of the OSP operator is written in square brackets to indicate the functional dependence, since it maps a function to another function [8, 14, 24].

Therefore, the synthesis of efficient real-time control algorithms for the IHC of the asymmetric hysteresis



Figure 2. Block representation of the system with hysteresis compensation.

nonlinearity represented by the MPI model (2) is achieved by the following implicit equation

$$v(t) = \Gamma_p^{-1}[\varpi](t) \tag{6}$$

for the inverse hysteresis compensator

$$v(t) = \Gamma^{-1} \left[\widehat{w} \right](t) \tag{7}$$

where $\varpi = \widehat{w} - \Gamma_c[v]$, and Γ_p^{-1} is expressed as

$$\Gamma_p^{-1}[\boldsymbol{\varpi}](t) = \hat{p}_0 \boldsymbol{\varpi}(t) + \sum_{j=1}^n \hat{b}_j F_{o\hat{r}_j}[\boldsymbol{\varpi}](t)$$
(8)

with

$$\hat{r}_{j} = p_{0}r_{j} + \sum_{i=1}^{j-1} b_{i}(r_{j} - r_{i})$$

$$\hat{p}_{0} = \frac{1}{p_{0}}$$

$$\hat{b}_{j} = -\frac{b_{j}}{\left(p_{0} + \sum_{i=1}^{j-1} b_{i}\right) \left(p_{0} + \sum_{i=1}^{j} b_{i}\right)}$$
(9)

By this way, asymmetric hysteresis can be compensated. In such a case, the resulted system is approximated as a simple linear dynamics model $\hat{G}(s)$ as shown in figure 2. In fact, it is generally impossible to obtain the perfect hysteresis and system dynamics models due to the existence of the modeling uncertainties and disturbances. In the following, all the imperfect inverse compensation errors and modeling uncertainties as well as disturbances of the system dynamics are treated as a lumped disturbance term. Then, the DOB [25, 26] can be designed to reject the lumped disturbance.

2.2. DOB design

A typical DOB for a linear dynamic system is schematically shown in figure 3, where *P* is the dynamics model of the plant, *d* is the unknown term composed of un-modeled nonlinearities and disturbances, *P_n* is the nominal model of *P*, *u* is control input, *y* is the output of the plant, ξ represents the sensors noise, and *Q* is a low-pass filter usually called *Q*-filter. The nature of a DOB is to lump the external disturbances and model mismatch as an error term of the motion equation [30–33]. Since the inverse model of *P_n⁻¹* is generally noncausal, the *Q*-filter is introduced to make the feedback implementable. The filter's output \hat{d} is viewed as the estimate of the lumped disturbance *d*. With the DOB, the inner-loop system around the controlled plant is approximated as a



Figure 3. Structure of a DOB for a linear system.



Figure 4. Structure of a DOB-based controller with inverse hysteresis compensation.

simple nominal plant model P_n without the effect of model mismatch, which thus makes a simple controller to be designed. Therefore, the key issue of the DOB is reduced to modeling the plant and design the *Q*-filter. It can be seen that the DOB possesses the excellent advantages of the simple structure and transparent design.

As shown in figure 3, the DOB is designed based on the linear control theory using the transfer function method. In general, it cannot be applied to handle the asymmetric nonsmooth hysteresis nonlinearity in PSAs. To tackle this problem, the IHC with the MPI model is developed in this work as detained in section 2.1. In this way, the DOB can be applied to the linear dynamics model $\hat{G}(s)$ plus a lumped disturbance term *d* without suffering from the hysteresis nonlinearity. As a consequence, the structure of the DOBbased 2-DOF controller for PSA systems with the IHC is depicted in figure 4, where *C* is the tracking controller. In the following, we will analyze the robust stability of the DOBbased 2-DOF controller for the hysteresis compensated plant represented as $\hat{G}(s)$ that is defined as a collection of transfer functions.

As shown in figure 4, the transfer functions realized by the DOB are

$$G_{yu} = \frac{\widehat{G}\widehat{G}_n}{G_{DOB}}$$

$$G_{du} = \frac{\widehat{G}\widehat{G}_n(1-Q)}{G_{DOB}}$$

$$G_{\xi u} = \frac{\widehat{G}_n Q}{G_{DOB}}$$
(10)

where $G_{DOB} = \hat{G}_n + (\hat{G} - \hat{G}_n)Q$. As can be seen from (10), the DOB design comes down to the proper selection of the

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Figure 5. The experimental platform.



Figure 6. Block diagram of the experimental platform.

low-pass *Q*-filter to insure the robustness and disturbance rejection performance.

It should be noted the DOB can estimate disturbances precisely if they stay within the bandwidth of the DOBs lowpass filter Q(s) [25, 26, 31]. Therefore, in order to eliminate the disturbance as much as possible, it is desired that $Q \approx 1$ should be satisfied in a broad frequency range. However, the bandwidth of a DOB is limited by the robustness of a system and noise [31]. For the design of the *Q*-filter, the two basic requirements should be obeyed:

- (i) The relative degree of Q(s) should be larger than or equal to the relative degree of $\widehat{G}_n(s)$ in order to enable the practical implementation of the DOB.
- (ii) In the low frequency range, if $Q(s) \approx 1$, the disturbances can be rejected as much as possible from the transfer function G_{du} . However, to filter out the measurement noise, the Q(s) should go to zero in the high frequency range according to the transfer function $G_{\xi u}$. Therefore, the *Q*-filter should be a low-pass filter.

In general, the Q-filter is chosen with the following structure [25, 26]

$$Q(s) = \frac{1 + \sum_{m=1}^{n_q - p_q} f_m s^m}{1 + \sum_{m=1}^{n_q} f_m s^m}$$
(11)

where n_q is the order of Q(s), and p_q is the relative degree of Q(s).

With the developed *Q*-filter, major disturbances, especially low-frequency disturbances, are suppressed by the

Table 1. Identified parameters of the MPI model.

Number	r_i	b_i	a_i
1	0	0.2313	-0.1569
2	0.1	0.3059	0.4603
3	0.2	0.0155	
4	0.3	0.0752	
5	0.4	0.0683	
6	0.5	0.0252	
7	0.6	0.0035	
8	0.7	0.0094	
9	0.8	0.0264	
10	0.9	0.0032	



Figure 7. Comparison of the hysteresis loops generated by the PSA and the MPI model.

DOB. As a result, the transfer function of the inner loop from u to y can be approximated as the nominal dynamics model \hat{G}_n . Thus, any position-loop controllers C can be designed based on the nominal dynamics model \hat{G}_n as shown in figure 4. Without losing generality, we assume that C is a linear controller for the stability discussion as generally treated in the literature [25, 26, 28, 30, 34]. In this way, the input-output transfer function from y_d to y can be derived as

$$G_{y_d y} = \frac{C\hat{G}\hat{G}_n}{\hat{G}_n \left(1 + C\hat{G}\right) + \left(\hat{G} - \hat{G}_n\right)Q}$$
$$= \frac{C\hat{G}}{\left(1 + C\hat{G}_n\right) + \frac{\widetilde{G}_n}{\hat{G}_n} \left(Q + C\hat{G}_n\right)}$$
(12)

where $\widetilde{G}_n = \hat{G} - \hat{G}_n$ represents the model mismatch. Then, the robust stability of the DOB-based controller can be stated in the following theorem.

Theorem 1. Let the plant be described as $\hat{G} = \hat{G}_n + \tilde{G}_n$ with allowable multiplicative uncertainties \tilde{G}_n . Assume that the nominal model \hat{G}_n is a minimum phase system, and a linear controller *C* can stabilize \hat{G}_n . Then, the closed system has



Figure 8. Hysteresis loops with and without the IHC.



Figure 9. Comparison of frequency responses of experimental results and model simulation results.

robust stability if the model uncertainty satisfies

$$\left|\frac{\widetilde{G}_{n}(s)}{\widehat{G}_{n}(s)}\right|_{s=j\omega} < \left|\frac{1+C\widehat{G}_{n}}{Q+C\widehat{G}_{n}}\right|_{s=j\omega}, \quad \forall \ \omega.$$
(13)

Proof. The theorem can be proved in the same way as in [25, 26].





Figure 10. 15 μ m PTP positioning results using different controllers.

Table 2. 15 μ m PTP positioning performances of different controllers.

Performance	PIC	PIC+DOB	PIC+IHC	PIC+DOB+IHC
98% setting time (ms)	3.62	3.47	3.20	3.02
e_m (nm) e_{rms} (nm)	23.8 5	23.8 5	21.5 5	21.5 5





Figure 11. Experimental results of a triangular reference using PIC+DOB and PIC+DOB+IHC controllers.

systems with the non-smooth asymmetric hysteresis nonlinearity.

3. Comparative experiments

In this section, the effectiveness of the proposed control scheme will be verified by a series of comparative experimental studies on a PSA-actuated nanopositioning stage.

3.1. Experimental setup

A flexure hinge guided stage actuated by a PSA is used in this work to conduct the comparative study as shown in figure 5. A power amplifier with a gain of 15 is used to drive the PSA. A high-resolution strain gauge sensor integrated into the PSA is adopted to measure the real-time position. A signal conditioner is used to convert the measured position to analogue voltage in the range of 0–10 V. A dSPACE DS1103 control board equipped with the 16-bit DAC and 16-bit ADC is utilized to output the excitation for the power amplifier and capture the real-time displacement information from the signal conditioner. The sampling frequency of the dSPACE DS1103 control board is set as 20 kHz. Figure 6 shows the block diagram of the experimental platform.

Table 3. Tracking performances of different controllers under various references.

Performance	PIC+DOB	PIC+DOB+IHC
10 hz triangular		·
e_m (μ m)	0.1354	0.0899
e_{rms} (μ m)	0.0930	0.0278
Complex		
$e_m (\mu m)$	0.2167	0.0738
e_{rms} (μ m)	0.0859	0.0194

3.2. Controller implementation

To conduct tests in the experimental platform, the controllers developed in section 2 shall be implemented first in the dSPACE DS1103 control board.

3.2.1. *IHC.* To develop the IHC, the first step is to describe the hysteresis nonlinearity with the MPI model and identify the parameters of the MPI. Then, the IHC is designed through (6)-(9). In this work, the asymmetric part $\Gamma_c[v](t) = g(v)(t)$ of the MPI model is chosen as $g(v)(t) = a_1v^3(t)$. The effectiveness and benefit for such a selection can be referred to [24]. As shown in figure 1, the dynamics model of the PSA-actuated positioning stage is represented as a linear

dynamic model preceded by a rate-independent hysteresis nonlinearity that is described by the MPI model. In this work, the parameters for the linear dynamic part and the nonlinear hysteresis part are identified by two steps. To identify the nonlinear hysteresis part, the amplitude-varying input signals at low frequencies are used to excite the PSA since at low frequencies the linear dynamic model approaches its dc gain and the whole model could be represented well by the rateindependent MPI model. The input signal frequency is chosen to be 1 Hz in the experiment. Certainly, the frequency can also be chosen as another one such as 0.5 Hz or 0.1 Hz depending on the designers [6, 10, 29, 35, 36]. The identified parameters of the MPI model with ten OSP operators (i.e. n = 10) are listed in table 1. Figure 7 shows the comparison of experimental hysteresis loops of the PSA and associated prediction results with the MPI model. The maximal prediction error is about 1.4% of the full displacement range, which demonstrates the MPI model can well describe the asymmetric hysteresis nonlinearity of the tested PSA with both major and minor loops.

With the MPI model, the IHC can be implemented for hysteresis compensation. Figure 8 shows the hysteresis compensation experimental results with and without the IHC. It can be observed that the open-loop output versus input curve exhibits a hysteresis width of about 13.6% ($=\frac{h}{H} \times 100\%$). The presence of significant hysteresis non-linearity necessitates the development of IHC to compensate for the hysteresis. From figure 8, we can obtain that the hysteresis nonlinearity has been reduced by up to 75.7% with the IHC. Therefore, the hysteresis nonlinearity is greatly mitigated by using IHC and the resulted relationship between the desired position and the actual position is almost linear and symmetric, which makes it possible to develop the DOB.

3.2.2. DOB. As addressed in section 2.2, design of the DOB is to modeling \hat{G}_n and choose *Q*-filter. Since the IHC is developed to mitigate the hysteresis nonlinearity, a bandlimited white noise signal is used to identify the model \hat{G}_n with the the system identification toolbox of MATLAB. In this work, \hat{G}_n is identified as

$$\widehat{G}_n(s) = \frac{470.4068 \left(s^2 + 4.067e004s + 5.882e008\right)}{(s + 2726) \left(s^2 + 4030s + 1.067e008\right)}.$$
 (14)

Figure 9 shows the comparison of frequency responses of experimental results and model simulation results to demonstrate the effectiveness of the identified model.

Since the relative degree of \widehat{G}_n (14) is one, the *Q*-filter is chosen as

$$Q(s) = \frac{3\tau s + 1}{(\tau s)^3 + 3(\tau s)^2 + 3\tau s + 1}$$
(15)

where $\tau = 0.0005$.

3.2.3. Positioning controller. For the dynamics described by (14), a proportional integral controller (PIC) is sufficient for stabilization purposes. Therefore, the transfer function of the



Figure 12. Relation curves between referenced displacement and actual displacement with triangular reference.

PIC is given

$$C(s) = \frac{k_p s + k_i}{s}.$$
 (16)

where $k_p > 0$, and $k_i > 0$ are the proportional gain and integral gain respectively. The auto-tuning toolbox of Matlab is used to tune the control gains of the PIC as $k_p = 0.2$ and $k_i = 3365$.

3.3. Performance indexes

To quantify the performance of our developed controllers, the following two performance indexes calculated by the steady tracking data will be used for the comparative study.

(D1) $e_m = max (|x(t) - x_d(t)|)$: the maximum value of the error.

(D2) $e_{rms} = \sqrt{(1/T) \int_0^T |x(t) - x_d(t)|^2 dt}$: the root mean square value of the error with *T* representing the total running time.



Figure 13. Experimental results of a complex reference using PIC+DOB and PIC+DOB+IHC controllers.

3.4. Experimental results

Four sets of comparative experiments were conducted to illustrate the effectiveness of the proposed control scheme: (1) S1: Point-to-point positioning test, (2) S2: Triangular tracking test, (3) S3: Complex-reference tracking test. It should be noted that due to the non-smooth nature of the referenced signals in S1 and S2, the desired trajectories in S1 and S2 are generated by filtering the reference trajectories with a third-order stable system $\frac{1}{(\tau s + 1)^3}$ with $\tau = 0.0005$.

3.4.1. S1. Firstly, the point-to-point (PTP) positioning test was conducted. For comparisons, three kinds of controllers, i.e., traditional proportional integral controller (PIC), DOB-based PIC (PIC+DOB), PIC with inverse hysteresis compensator (PIC+IHC), and DOB-based PIC with inverse hysteresis compensator (PIC+DOB+IHC), are examined. Figure 10 shows the comparative results under 15 μ m PTP positioning tests. It should be noted that the PIC in the developed controllers has the same parameter values.

For a quantitative comparison, 98% settling time, and tracking errors of e_m and e_{rms} using different controllers are listed in table 2. From table 2, it can be seen that settling time of PIC+DOB+IHC is 5.63% less than PIC+IHC, 12.97% less than PIC+DOB, and 16.57% less than PIC. Although e_{rms} errors of all the controllers are the same, the e_m errors of PIC +IHC and PIC+DOB+IHC is 9.66% smaller than PIC+DOB

and PIC. It can be concluded that the tracking performances of the PIC are worst. The DOB is introduced to improve the settling time and the IHC is introduced to improve the tracking precision and settling time. Therefore, the PIC+DOB +IHC achieves the fastest response and best accurate positioning for PTP tests.

For the purpose of comparing the PIC+IHC and PIC +DOB+IHC to show the role of the DOB, we can see from table 2 that the tracking errors of e_m and e_{rms} with the PIC +IHC controller are in the same level with the ones of the PIC +DOB+IHC. However, we can observe that the setting times of the PIC+IHC and PIC+DOB+IHC are 3.2 ms and 3.02 ms respectively. Therefore, the PIC+DOB+IHC can at least improve the tracking speed compared with the PIC+IHC. As for the reason that the tracking errors of e_m and e_{rms} are in the same level, the improvement with the DOB depends on the experimental conditions, application environments, etc. The current experiments were conducted in an ideal lab and all the parameters of the involved models were carefully identified. The lumped disturbance term may not play a major effect. We can see that even at the ideal experimental environments, it still shows improvement on the settling time, which may present a significant improvement in non-ideal experimental environments. In short, the inclusion of the DOB design would add the benefits for the system tracking performance according to the design principle.



Figure 14. Relation curves between referenced displacement and actual displacement with complex reference.

3.4.2. S2. Triangular references are the standard signals for raster scanning of PSA-actuated stages in scanning probe microscopy [1, 3]. With the same control parameters in the PTP positioning test, the triangular tracking test was conducted to demonstrate the trajectory-tracking effectiveness of the developed control scheme with the DOB and IHC. Figure 11 shows 10-Hz triangular experimental results. Figure 11(a) depicts the comparisons of the referenced trajectory and the actual response, while figure 11(b) shows the comparisons of the tracking errors. It can be clearly seen that the response using the PIC+DOB +IHC controller well follows the reference. To quantify the tracking performance, error indexes i.e., e_m and e_{rms} are summarized in table 3. It can be seen from the table that e_m and e_{rms} errors of the PIC+DOB controller are 0.1354 and 0.0930 μ m, respectively. In contrast, the PIC+DOB+IHC controller results in e_m and e_{rms} errors of 0.0899 and 0.0278 μ m, respectively. Compared with PIC+DOB control, tracking errors of the proposed control scheme, in terms of e_m and e_{rms} , are reduced by up to 33.60% and 70.11%, respectively.

As addressed in the Introduction, the challenge for highperformance control of PSA lies in the existence of the hysteresis nonlinearity. Figure 12 shows the input-output relation curves compared with PIC+DOB control. From figure 12, we can see that using the proposed control scheme the hysteresis caused errors are reduced by about 92.59% compared with PIC+DOB control. Therefore, it can be concluded that the proposed PIC+DOB+IHC control performs better than PIC+DOB control owing to the development of the IHC unit.

3.4.3. S3. To further elucidate the advantages of the proposed control scheme, comparative experiments using PIC+DOB and PIC+DOB+IHC controllers are conducted under a complex reference. Figures 13 and 14 show the experimental results. Tracking of the referenced trajectory, tracking errors, and resulting input-output relation curves are given in figures 13(a), (b) and figure 14, respectively. The tracking performance indexes i.e., e_m and e_{rms} are summarized in table 3. As can be seen from the table, e_m and e_{rms} errors of the PIC+DOB controller are of 0.2167 and 0.0859 μ m, respectively. In contrast, the PIC+DOB+IHC controller results in e_m and e_{rms} errors of 0.0738 and 0.0194 μ m, respectively. As compared with PIC+DOB control, tracking errors of the proposed control scheme, in terms of e_m and e_{rms} , are reduced by up to 65.94% and 77.42%, respectively. From figure 14, it can be observed that with the proposed control scheme, the hysteresis caused errors are reduced by about 80% compared with PIC+DOB control. Again, the proposed PIC+DOB+IHC control performs better than PIC+DOB control.

4. Conclusion

In this paper, a novel high-performance motion control scheme for a PSA-actuated nanopositioning stage is developed with the IHC and DOB. The distinct features of this work are summarized as follows: (1) a real-time IHC with the MPI model is designed to compensate for the asymmetric hysteresis nonlinearity; (2) an inner-loop DOB is introduced as a means for disturbances rejection of the PSA-actuated stage with the IHC; (3) an outer-loop tracking controller is developed to improve the tracking performance of the PSA-actuated stage together with the IHC and DOB; (4) real-time comparative experiments on a PSA-actuated stage are finally performed to demonstrate the effectiveness and enhanced performance of the developed control scheme.

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