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PASSIVE FRICTION COMPENSATION USING A NONLINEAR DISTURBANCE OBSERVER FOR FLEXIBLE JOINT ROBOTS WITH JOINT TORQUE MEASUREMENTS

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Abstract. The friction and ripple effects from motor and drive cause a major problem for the robot position accuracy, especially for robots with high gear ratio and for high-speed applications. In this paper we introduce a simple, effective, and practical method to compensate for joint friction of flexible joint robots with joint torque sensing, which is based on a nonlinear disturbance observer. This friction observer can increase the performance of the controlled robot system both in terms of the position accuracy and the dynamic behavior. The friction observer needs no friction model and its output corresponds to the low-pass filtered friction torque. Due to the link torque feedback the friction observer can compensate for both friction moment and external moment effects acting on the link. So it can be used not only for position control but also for interaction control, e.g., torque control or impedance control which have low control bandwidth and therefore are sensitive to ripple effects from motor and drive. In addition, its parameter design and parameter optimization are independent of the controller design so that it can be used for friction compensation in conjunction with different controllers designed for flexible joint robots. Furthermore, a passivity analysis is done for this observer-based friction compensation in consideration of Coulomb, viscose and Stribeck friction effects, which is independent of the regulation controller. In combining this friction observer with the state feedback controller [1], global asymptotic stability of the controlled system can be shown by using Lyapunov based convergence analysis. Experimental results with robots of the German Aerospace Center (DLR) validate the practical efficiency of the approach.

Keywords. Friction compensation; Disturbance observer; Passivity control; Flexible joint robots.

1. INTRODUCTION

For some application fields, e.g. service robotics, medical robotics or space robotics, lightweight and a high load/weight ratio are essential, for which the design of the robot can be optimized by using Harmonic-Drive® gears with high gear ratio to reduce the robot weight and bring more torque after the gear [2, 3]. Hence, the accelerated masses are relatively low, which permits a safe robot interaction with the human and the environment. Simultaneously, a high gear ratio causes high motor friction and high joint elasticity. When the joint elasticity is high, the actuator friction can dominate the dynamic system behavior and therefore it is difficult to achieve the high position accuracy or the desired force at the robot end-effector. These challenging problems have to be taken into account in the control design and motivate to develop a model-free friction compensation method in this paper.

Several control methods have been proposed to compensate the friction effects. A simple method is model-based friction compensation that requires to know a precise friction model [4, 5, 6]. However, friction is a highly nonlinear, complex phenomenon and its parameters can vary with time, joint position, load or with temperature. So the model-based method can not achieve good position accuracy.

In order to overcome the problem of the model-based friction compensation method, adaptive techniques have been proposed in [7, 8] for flexible joint robots, which however take only static friction into account, without modeling dynamical effects. Furthermore, the adaptive friction compensation based on a LuGre dynamic friction model was treated in [9, 10, 11, 12]. However, the adaptive control is sensitive to unmodeled robot dynamics and its complexity can reduce system reliability.

In another concept, using direct joint torque measurements, the friction effect can be eliminated through an inner torque control loop [13, 14]. In [15] the joint torques can be indirectly estimated based on data from a 6DOF force/torque sensor at the robot base and then used in an inner torque control loop. In this case the unmodeled joint friction and actuator dynamics do not influence the estimation results, as in the direct measurement method.

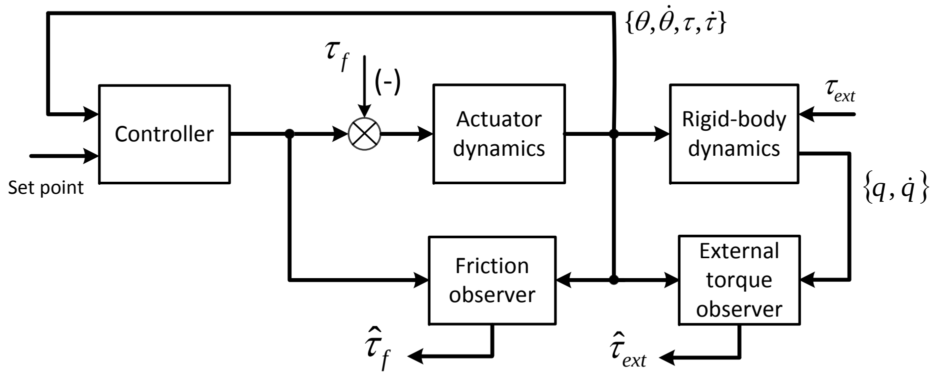

Figure 1. Observer concepts for friction torque and external torque estimation

Furthermore, a standard linear technique such as integrator is typically used in industrial robotics applications and show good practical performance [16]. Its analysis, however, is usually based on the linear technique and does not really fit to the strongly nonlinear robotic systems. In case of a regulation scheme only local convergence has been achieved in robotics [17]. For tracking control, a robust adaptive control scheme was proposed in [12] based on a cascaded structure with a full state feedback controller with integrator terms including adaptive friction compensation as inner control loop and computed torque as outer control loop. A global asymptotic tracking is achieved for the complete controlled robot system.

One of the most effective methods for friction compensation is a disturbance observer, e.g., [18, 19, 20, 21]. This method has the advantage of being model-free, and has been shown to be effective in practice to reject frictional effects. In case of flexible joint robots with joint torque measurements after the gearbox, one can distinguish between external loads acting on the link side of the robot and the internal friction disturbance acting mostly on the actuator. Hence, the same observer technique can be used to independently determine these two different disturbance torques (see figure 1). In [22] a linear disturbance observer for friction compensation was proposed for flexible joint robots. The observer is shown to provide a low pass filtered disturbance torque. The presented approach has the advantage to enable a passivity analysis when only viscose friction and Coulomb friction are considered, which allows in turn the treatment of a MIMO state feedback controller in a Lyapunov framework leading to global asymptotic results. To consider additionally the Stribeck friction effect, a PD controller was proposed in [23] based on nominal states (estimated motor position and motor velocity) from a disturbance observer for friction compensation. This method achieves global asymptotic stability. But the bandwidth is limited by using derivatives of the joint torque, whose gain depends on the convergence speed of the estimated motor position to the real motor position.

In this paper, motivated by considering all friction effects (viscose, Coulomb, Stribeck), a nonlinear approach based on the linear friction observer in [22] is proposed. This nonlinear observer allows a passive friction compensation in itself and therefore ensures system stability with any passive controller or passivity-based regulation controller. Because of its first order filter property this nonlinear observer is shown to be equivalent to integrator based controllers and therefore can achieve good control performance both in terms of the position accuracy and the dynamic behavior. Simulation and experimental results confirm our approach and indicate that this nonlinear observer-based friction compensation yields better performance in comparison with the linear observer-based friction compensation in [22] and the adaptive friction compensation in [12].

The paper is organized as follows. Section 2 introduces the robot model, whereas Section 3 summarizes the friction observer and the convergence analysis results from [22], obtained for Coulomb and viscose friction compensation. Section 4 introduces the new friction observer and passivity analysis for the simple case of one actuator, but the presentation and analysis are done so that the results can be directly applied to the whole multi-DOF robot. Using this result, Section 5 discusses the stability of the controlled systems with the state feedback controller combined with the new friction observer. Finally, the obtained performance is verified by experimental tests reported in Section 6.

2. MODELING THE FLEXIBLE JOINT ROBOT DYNAMICS

For a flexible joint robot with n rotary joints its simplified dynamics [24, 25] is described by

$$u = J\ddot{\theta} + \tau + DK^{-1}\dot{\tau} + \tau_f, \tag{1}$$

$$\tau + DK^{-1}\dot{\tau} = M(q)\ddot{q} + C(q,\dot{q})\dot{q} + g(q). \tag{2}$$

Therein, $q \in \mathbb{R}^n$ and $\theta \in \mathbb{R}^n$ are the link and motor angles, respectively. $\tau_f \in \mathbb{R}^n$ is the friction torque. The control input is the motor torque $u \in \mathbb{R}^n$. The motor inertia matrix $J \in \mathbb{R}^{n\text{x}n}$ is diagonal and positive definite. The transmission torque $\tau \in \mathbb{R}^n$ between motor and link dynamics is modeled as a linear function of the motor and the link position

$$\tau = K(\theta - q) \tag{3}$$

and is measured by strain gauge based torque sensors. The joint stiffness matrix $K \in \mathbb{R}^{n\text{x}n}$ and the joint damping matrix $D \in \mathbb{R}^{n\text{x}n}$ are diagonal and positive definite. Furthermore, $M(q) \in \mathbb{R}^{n\text{x}n}$ is the mass matrix, $C(q,\dot{q}) \in \mathbb{R}^{n\text{x}n}$ the centrifugal and Coriolis matrix, and $g(q) \in \mathbb{R}^n$ the gravity vector of the rigid body model.

Finally, in order to facilitate the controller design and the stability analysis, the following four properties are used

P.1: *The mass matrix $M(q)$ is symmetric and positive definite $M(q) = M^T(q)$ and satisfies*

$$\lambda_m \leq \| M(q) \| \leq \lambda_M \tag{4}$$

with λ_m, λ_M being the maximum and minimum eigenvalues respectively.

P.2: *The matrix $\dot{M}(q) - 2C(q, \dot{q})$ is skew symmetric and*

$$x^T(\dot{M}(q) - 2C(q, \dot{q}))x = 0, \quad \forall x, q, \dot{q} \in \mathbb{R}^n.$$

P.3: *The gravity torque $g(q)$ is given as the gradient of a potential function $U_g(q)$ so that $g(q) = \partial U_q(q)/\partial q$ and there exists a real number $\alpha > 0$, such that*

$$\begin{aligned} \|U_g(q_d) - &U_g(q) + (q - q_d)^T g(q_d)\| \\ &\leq \frac{1}{2}\alpha \|q - q_d\|^2 \ , \quad \forall \ q, q_d \in \mathbb{R}^n. \end{aligned} \tag{5}$$

P.4: *In consideration of all friction effects, the following friction model is used for stability analysis*

$$\tau_f = \tau_{f_{cs}} + \tau_{f_v} \tag{6}$$

$$\text{with} \quad \begin{cases} \tau_{f_{cs}} = (f_c + f_s e^{-\frac{\dot{\theta}^2}{v_c}}) sign(\dot{\theta}) \\ \tau_{f_v} = f_v \dot{\theta}. \end{cases}$$

Thereby, f_c, f_s and f_v represent the Coulomb, Stribeck, viscous coefficients, respectively. v_c is the Stribeck-constant velocity.

3. REVIEW OF THE STATE OF THE ART

Assume that one has a controller, which provides asymptotic stability for the system without friction. The question is whether an observer-based friction compensation ensures the stability and the convergence of the controlled system with friction. So, in this section the friction observer in [22] is reviewed and analyzed for the case of one joint.

This disturbance observer for friction compensation is shown in Figure 2. The observer has a very simple structure due to the measurement of both motor position (with numerically differentiated velocity) and elastic joint torque. By considering the friction torque as disturbance, this observer is designed based on the actuator dynamics without requiring the link dynamics and given by

$$u = J\ddot{\hat{\theta}} + \tau_a + \hat{\tau}_f \tag{7}$$

with

$$\begin{cases} \tau_a = \tau + DK^{-1}\dot{\tau} \\ \hat{\tau}_f = LJ(\dot{\hat{\theta}} - \dot{\theta}). \end{cases} \tag{8}$$

Thereby, the observer states $\hat{\theta}$ and $\hat{\tau}_f$ represent the estimation of the motor position and the friction torque, respectively. L is the control gain and positive definite.

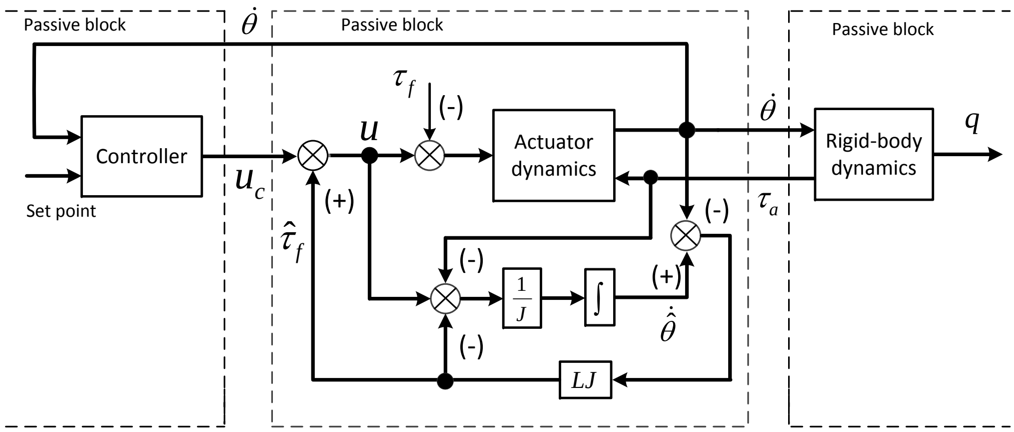

Figure 2. Overview of the system with the linear friction observer from [22] which can ensure the system passivity when considering Coulomb and viscose friction effects

By combining (1) with (7) and (8), one obtains the closed-loop dynamics of the controlled system with observer-based friction compensation

$$L^{-1}\dot{\hat{\tau}}_f + \hat{\tau}_f = \tau_f, \tag{9}$$

or

$$\hat{\tau}_f = \frac{1}{L^{-1}s + 1}\tau_f \tag{10}$$

where s is the Laplace operator. The estimated friction corresponds thus to the actual friction passed through a first order filter. From the property (6) and due to the linearity of the filtering operation, the friction estimation will contain a component corresponding to the Coulomb and Stribeck friction, and one corresponding to the viscous friction

$$\hat{\tau}_f = \hat{\tau}_{f_{cs}} + \hat{\tau}_{f_v}, \tag{11}$$

with

$$\begin{cases} \hat{\tau}_{f_{cs}} = \dfrac{1}{L^{-1}s + 1}\tau_{f_{cs}} \\ \hat{\tau}_{f_v} = \dfrac{1}{L^{-1}s + 1}\tau_{f_v}. \end{cases} \tag{12}$$

Furthermore, by definition of the filtered motor position as

$$\nu = \frac{s}{L^{-1}s + 1}\theta. \tag{13}$$

the estimated viscose friction torque is determined by

$$\hat{\tau}_{f_v} = f_v \nu. \tag{14}$$

Now, independent of the controller, the complete control law with the observer-based friction compensation is designed as

$$u = u_c + \hat{\tau}_f, \tag{15}$$

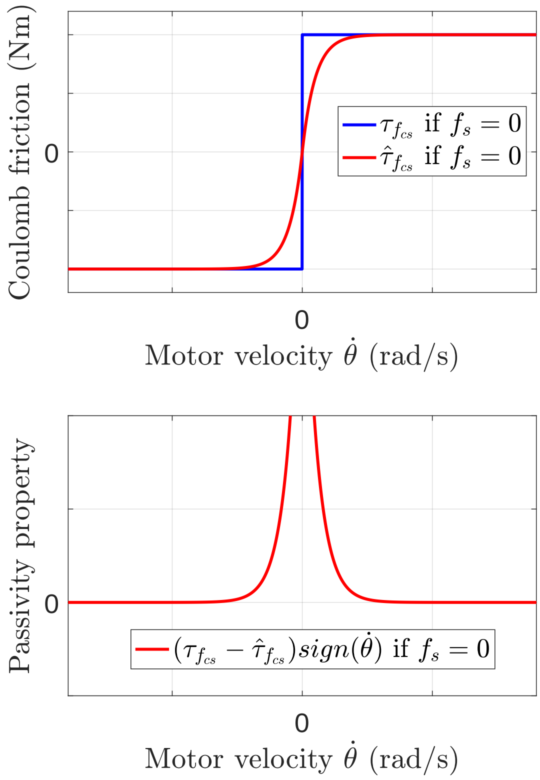

Figure 3. Stribeck friction is neglected and the linear friction observer in [22] can ensure that the energy of Coulomb friction compensation is dissipated or $\dot{\theta}(\tau_{f_{cs}} - \hat{\tau}_{f_{cs}})\big|_{f_s=0} \geq 0$

where u_c is the desired motor moment from the controller.

For the passivity analysis of the observer-based friction compensation, one considers the following storage function

$$S_\theta = \frac{1}{2}J\dot{\theta}^2 + \frac{1}{2}f_v L^{-1}\nu^2, \tag{16}$$

which contains in addition to the actuator kinetic energy also the kinetic energy related to the viscose friction compensation.

Taking derivative of this storage function for the considered friction model (6) and using (1), (15) one obtains

$$\dot{S}_\theta = \dot{\theta}u_c - \dot{\theta}\tau_a + P_f. \tag{17}$$

On the right hand side, the first term is the power supplied by the controller, the second term is the power transmitted to the links. The last term is the power dissipated due to friction and is obtained by

$$P_f = -\dot{\theta}(\tau_f - \hat{\tau}_f) + f_v L^{-1}\nu\dot{\nu}. \tag{18}$$

Inserting (6), (10) and (13) into (18) leads to

$$\begin{aligned} P_f &= -\dot{\theta}(\tau_{f_{cs}} - \hat{\tau}_{f_{cs}}) - \dot{\theta}(\tau_{f_v} - \hat{\tau}_{f_v}) + f_v L^{-1}\nu\dot{\nu} \\ &= -\dot{\theta}(\tau_{f_{cs}} - \hat{\tau}_{f_{cs}}) - \dot{\theta}(f_v\dot{\theta} - f_v\nu) + f_v\nu(\dot{\theta} - \nu) \\ &= -\dot{\theta}(\tau_{f_{cs}} - \hat{\tau}_{f_{cs}}) - f_v(\dot{\theta} - \nu)^2. \end{aligned} \tag{19}$$

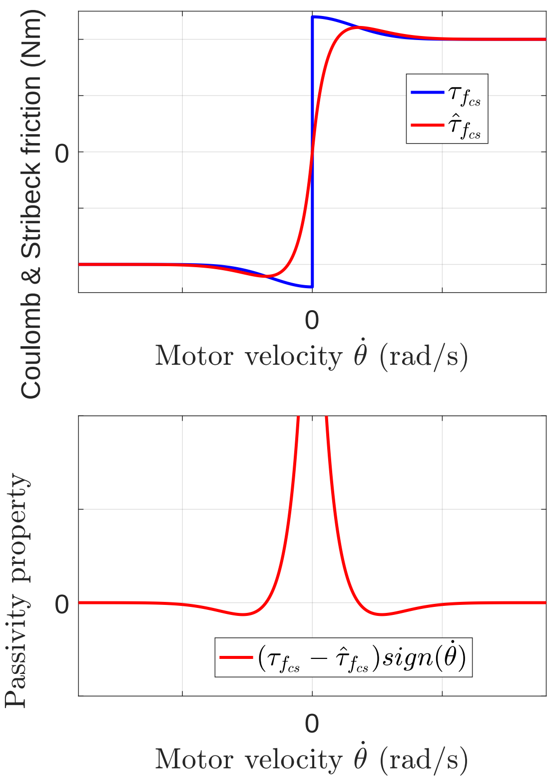

Figure 4. When considering the Stribeck friction, the friction observer in [22] causes overcompensation and does not ensure energy dissipation for Coulomb and Stribeck friction compensation or $\dot{\theta}(\tau_{f_{cs}} - \hat{\tau}_{f_{cs}}) < 0$

In case the Stribeck friction effect is neglected ($f_s = 0$), it can be easily recognized from Figure 3, that the Coulomb friction compensation has the property

$$\dot{\theta}(\tau_{f_{cs}} - \hat{\tau}_{f_{cs}})\big|_{f_s=0} \geq 0 \tag{20}$$

because from (10) the estimated Coulomb friction represents a first order filtered signal of a step input signal. Indeed, the absolute value of $\hat{\tau}_{f_{cs}}$ is always smaller (or equal) than the absolute value of $\tau_{f_{cs}}$ and the difference always has the opposed sign of $\dot{\theta}$. Therefore, (20) is true and hence (19) is always dissipated with $f_s = 0$.

Because the friction observer will always provide a filtered friction signal, the friction compensation will not be passive for any friction profile. This can be seen in Figure 4 for the case of the Stribeck effect. The filtered friction becomes temporarily higher than the real friction, leading therefore to an overcompensation of friction and thus to energy generation. This might result in limit cycles for the system. In the next section a nonlinear friction observer is going to be proposed which can ensure the passivity of the friction compensation including the Stribeck friction compensation.

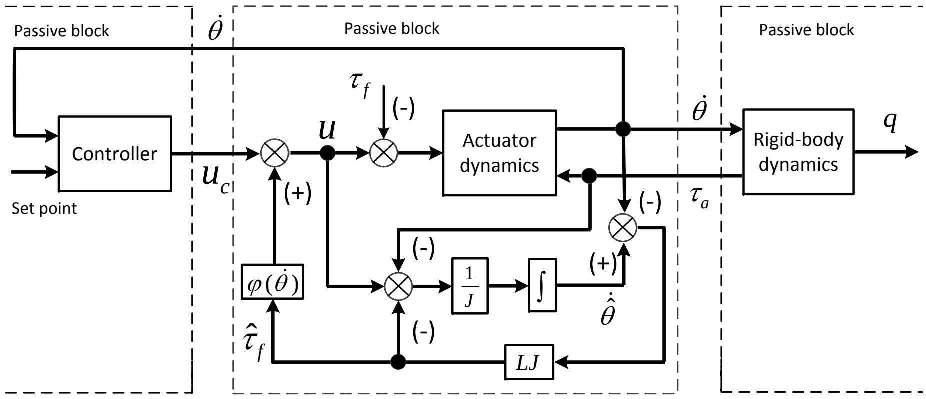

Figure 5. Overview of the system with the new proposed nonlinear friction observer

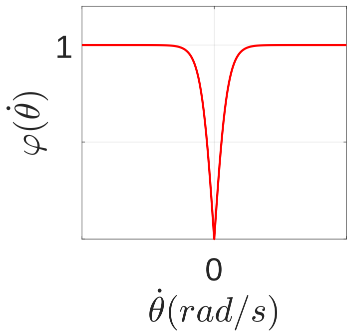

Figure 6. Function $\varphi(\dot\theta)$

4. NONLINEAR OBSERVER FOR PASSIVE FRICTION COMPENSATION

In order to consider the Stribeck friction effect for low motor velocity, the control scheme in Figure 2 is modified as sketched in Figure 5 by introducing an additional nonlinear function

$$\varphi(\dot\theta) = \tanh(\frac{\dot\theta^2}{\epsilon}), \tag{21}$$

which results in $\{0 \leq \varphi(\dot\theta) \leq 1 \ \forall \ \dot\theta\}$ with ϵ being a positive constant[1]. Figure 6 depicts the definition of the bounded function $\varphi(\dot\theta)$. When the motor velocity goes to infinity, this function is equal to one.

Now, the control law (15) is rewritten as

$$u = u_c + \varphi(\dot\theta)\hat\tau_f \tag{22}$$

and the power dissipated due to friction (18) is given by

$$P_f = -\dot\theta(\tau_f - \varphi(\dot\theta)\hat\tau_f) + f_v L^{-1}\nu\dot\nu. \tag{23}$$

[1]By increasing ϵ, the absolute value of the friction compensation torque can be kept smaller than the real friction torque and hence overcompensation of the Coulomb and the Stribeck friction effects is inhibited.

Inserting (6), (10) and (13) into (23) leads to

$$\begin{aligned} P_f = &- \dot{\theta}(\tau_{f_{cs}} - \varphi(\dot{\theta})\hat{\tau}_{f_{cs}}) \\ &- f_v\dot{\theta}^2 - f_v\nu^2 + f_v(\varphi(\dot{\theta}) + 1)\dot{\theta}\nu. \end{aligned} \tag{24}$$

From the properties of the bounded function $\varphi(\dot{\theta})$ in (21), (24) results in

$$\begin{aligned} P_f &\leq -\dot{\theta}(\tau_{f_{cs}} - \varphi(\dot{\theta})\hat{\tau}_{f_{cs}}) - f_v\dot{\theta}^2 - f_v\nu^2 + 2f_v|\dot{\theta}||\nu| \\ &\leq -\dot{\theta}(\tau_{f_{cs}} - \varphi(\dot{\theta})\hat{\tau}_{f_{cs}}) - f_v(|\dot{\theta}| - |\nu|)^2. \end{aligned} \tag{25}$$

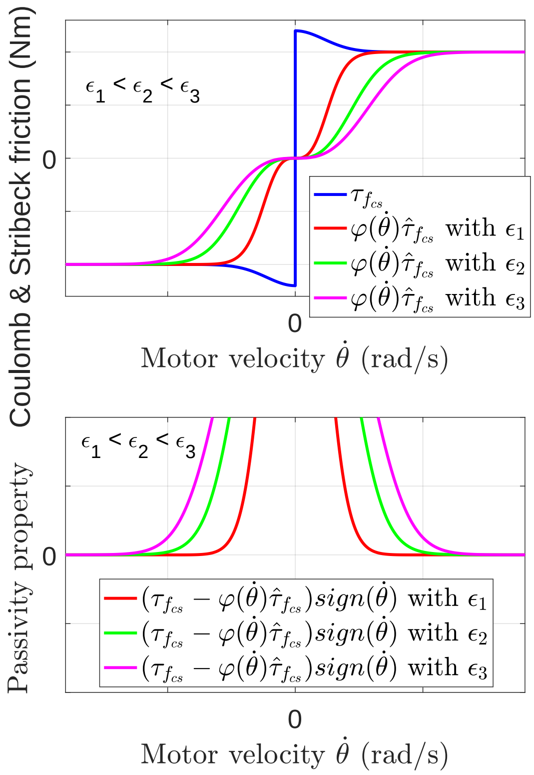

Figure 7. Bychoosing ϵbig enough ($\epsilon > v_c$),the nonlinear friction observercan ensure thatthe energy of Coulomb and Stribeck friction compensation is dissipated or $\dot{\theta}(\tau_{f_{cs}} - \varphi(\dot{\theta})\hat{\tau}_{f_{cs}}) \geq 0$

So, P_f is negative definite whenever $\dot{\theta}(\tau_{f_{cs}} - \varphi(\dot{\theta})\hat{\tau}_{f_{cs}}) \geq 0$. By choosing ϵ in (21) big enough ($\epsilon > v_c$), one can ensure that

$$\dot{\theta}(\tau_{f_{cs}} - \varphi(\dot{\theta})\hat{\tau}_{f_{cs}}) \geq 0 \tag{26}$$

and hence Coulomb and Stribeck friction compensation are dissipated as in Figure 7. Furthermore, the observer based friction compensation is passive with all the friction effects.

By choosing the function $\varphi(\dot{\theta})$ in Figure 6, $\varphi(\dot{\theta})$ is zero when $\dot{\theta} = 0$. Together with (9) it yields $(\hat{\tau}_{f_{cs}} - \tau_{f_{cs}}) \neq 0$ at steady state. In order to prevent that, the profile of $\varphi(\dot{\theta})$ can be chosen

$$\varphi(\dot{\theta}) = \begin{cases} 1 & \text{if} \quad |\dot{\theta}| < \epsilon_1 \\ \tanh(\dfrac{\dot{\theta}^2}{\epsilon}) & \text{if} \quad |\dot{\theta}| \geq \epsilon_1 \end{cases} \tag{27}$$

with $\epsilon_1 \ll \epsilon$ being a positive constant. The property $(\hat{\tau}_{f_{cs}} - \tau_{f_{cs}}) = 0$ at the steady state is necessary for stability analysis in the next section.

5. STABILITY ANALYSIS OF THE STATE FEEDBACK CONTROLLER WITH OBSERVER BASED FRICTION COMPENSATION

In this section the passivity based control approach consisting of a state feedback controller and the proposed friction observer is composed and the stability of the controlled system is analyzed. Differently from a passive controller (e.g. PD controller), the state feedback controller is itself not passive, but can be shown to provide a passive subsystem together with (the part of) the robot dynamics, as for the torque feedback in our case. A passive controller will lead to stability for any passive plant, also for passive, but unmodeled dynamics, e.g. friction. This is a very convenient robustness property of passivity-based control. On the other hand, the robustness gets largely lost for the state feedback controller. We have seen that a torque feedback with general, non-diagonal K_T is not passive any more with respect to friction. The same situation is often encountered in literature, e.g. for passivity based tracking controllers [26].

Based on the passivity of the friction compensation, it is straightforward to show the stability of any system containing a passive plant, a passive controller and the friction compensation, and for which asymptotic stability can be shown in absence of friction (or, equivalently, assuming exact friction compensation). The interesting point with the presented state feedback controller is that while the position and velocity feedback terms have a simple passivity based interpretation (as spring and damper), the torque feedback itself does not represent a passive controller component. However, as shown e.g. in [14], the torque feedback can be interpreted as scaling of the actuator dynamics.

In order to achieve good performance, the friction observer can be combined with a state feedback controller in [1]. So, let us consider the following linear state feedback controller[2]

$$\begin{aligned} u_c = K_P e_\theta - K_D\dot{\theta} - K_T K^{-1}\tau - K_S K^{-1}\dot{\tau} \\ + (K + K_T)K^{-1}g(q_d) + \varphi(\dot{\theta})\hat{\tau}_f, \end{aligned} \tag{28}$$

where $e_\theta = \theta_d - \theta$ and $g(q_d) = K(\theta_d - q_d)$ in the equilibrium point. All the control matrices K_P, K_D, K_T, K_S are diagonal and positive definite.

By substituting (28), (22) into (1) one obtains the dynamics of the closed loop motor dynamics

$$\begin{aligned} J\ddot{\theta} = K_P e_\theta - K_D\dot{\theta} - (K + K_T)K^{-1}\tau + \varphi(\dot{\theta})\hat{\tau}_f - \tau_f \\ - (K_S + D)K^{-1}\dot{\tau} + (K + K_T)K^{-1}g(q_d). \end{aligned} \tag{29}$$

[2] Due to the fourth-order dynamics of flexible joint robots, a complete state is given by the motor position θ and velocity $\dot{\theta}$, as well as by the torque τ and its derivative $\dot{\tau}$.

For stability analysis the following Lyapunov function was chosen

$$\begin{aligned} V(\theta,\dot\theta,q,\dot q) = & \frac{1}{2}\dot\theta^T K(K+K_T)^{-1}J\dot\theta \\ + & \frac{1}{2}\dot q^T M(q)\dot q + \frac{1}{2}(e_\theta - e_q)^T K(e_\theta - e_q) \\ + & \frac{1}{2}{e_\theta}^T K(K+K_T)^{-1}K_P e_\theta + U_g(q) \\ - & U_g(q_d) + {e_q}^T g(q_d) \\ + & \frac{1}{2}\nu^T K(K+K_T)^{-1} f_v L^{-1}\nu \end{aligned} \tag{30}$$

with $e_q = q_d - q$. Beside the kinetic energy of the motors and the links, and the kinetic energy related to the viscose friction compensation, this Lyapunov function contains the potential energy of the joint springs, of the gravity field, and of the controller springs, as well. Moreover, note that the kinetic energy contains the motor inertia scaled down by the torque feedback gain. This corresponds to the interpretation of the torque feedback as a shaping of the motor inertia.

Using the property (P.3) leads to

$$\begin{aligned} V \geq & \frac{1}{2}(e_\theta - e_q)^T K(e_\theta - e_q) \\ + & \frac{1}{2}{e_\theta}^T K(K+K_T)^{-1}K_P e_\theta - \frac{1}{2}{e_q}^T \alpha e_q. \end{aligned} \tag{31}$$

So, if the right side of the inequation (31) is positive definite, then the Lyapunov function (30) is positive definite as well. This condition is fulfilled when

$$\alpha I < K(K+K_T+K_P)^{-1}K_P. \tag{32}$$

Loosely speaking, this condition requires that the controlled robot can sustain itself in the gravity field.

The derivative of the Lyapunov function (30) along the system trajectories is

$$\begin{aligned} \dot V = - & \dot\theta^T K(K+K_T)^{-1}(K_D + K_S + D)\dot\theta - \dot q^T D\dot q \\ + & \dot q D\dot\theta + \dot\theta^T K(K+K_T)^{-1}(K_S + D)\dot q \\ + & \dot\theta^T K(K+K_T)^{-1}(\varphi(\dot\theta)\hat\tau_f - \tau_f) \\ + & \nu^T K(K+K_T)^{-1} f_v(\dot\theta - \nu) \\ \equiv & \dot V_1 + \dot V_2 \end{aligned} \tag{33}$$

with

$$\begin{aligned} \dot V_1 = & -\dot\theta^T K(K+K_T)^{-1}(K_D + K_S + D)\dot\theta - \dot q^T D\dot q \\ & + \dot q D\dot\theta + \dot\theta^T K(K+K_T)^{-1}(K_S + D)\dot q, \end{aligned} \tag{34}$$

$$\begin{aligned} \dot V_2 = & \dot\theta^T K(K+K_T)^{-1}(\varphi(\dot\theta)\hat\tau_f - \tau_f) \\ & + \nu^T K(K+K_T)^{-1} f_v(\dot\theta - \nu). \end{aligned} \tag{35}$$

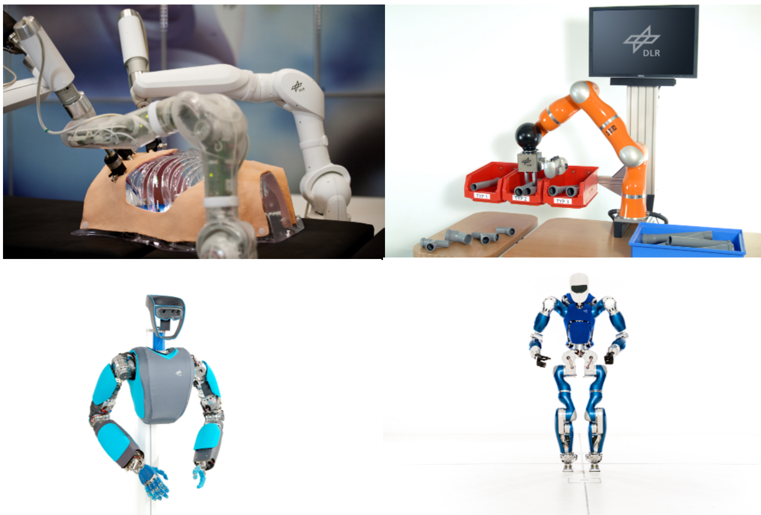

Figure 8. The friction observer was successfully used (from left to right, top to bottom) for the DLR medical robot, the DLR lightweight robot, the DLR hand-arm system and the DLR humanoid robot

In [1] it was shown that $\dot{V}_1$ is negative definite for large enough K_D with

$$K_D > \frac{1}{4} D^{-1} K^{-1} (K + K_T)^{-1} (K K_S - D K_T)^2. \tag{36}$$

For checking the negative definiteness of the function (35), we look at the Lyapunov function (30), in which the motor inertia and the potential energy of the controller spring are scaled down by the same factor. Let us define the matrix

$$A = K(K + K_T)^{-1}. \tag{37}$$

Obviously, $A \in \mathbb{R}^{nxn}$ is diagonal and positive definite, because K and K_T are diagonal and positive definite.

Differently from the case of one joint in Section 4., for the complete robot the power dissipated due to friction is now scaled by the matric A. According to (25) it leads to

$$\dot{V}_2 = A P_f \leq 0. \tag{38}$$

Therefore, $\dot{V} \leq 0$ and the system is stable.

Now, the equilibrium equations, for which $[\dot{\theta} = 0, \dot{q} = 0, \nu = 0]^T$, are given by

$$K_P(\theta_d - \theta) - (K + K_T)[(\theta - q) - (\theta_d - q_d)] + (\hat{\tau}_{f_{cs}} - \tau_{f_{cs}}) = 0 \tag{39}$$

$$K(\theta - q) = g(q). \tag{40}$$

Note that by choosing φ from (27), $(\hat{\tau}_{f_{cs}} - \tau_{f_{cs}}) = 0$ holds at steady state and therefore the equilibrium equations are the same as for exact friction compensation. This is not surprising, since the friction compensation provides exact friction compensation at steady state. According to the LaSalle invariance principle, the system converges to the largest invariant set, which is given by the unique point $[\theta = \theta_d, \dot{\theta} = 0, q = q_d, \dot{q} = 0, \nu = 0, \tau_{f_{cs}} = \hat{\tau}_{f_{cs}}]$. The system is therefore global asymptotically stable under the same conditions as in Section 4.

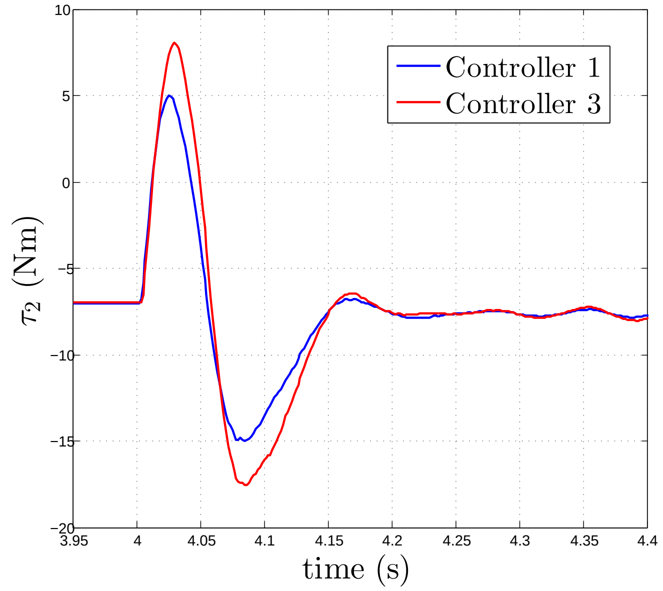

Figure 9. Measured link torque after a step of joint 2 with controller 1 and controller 3. The observer based friction compensation did not almost change the dynamic behavior of the joint

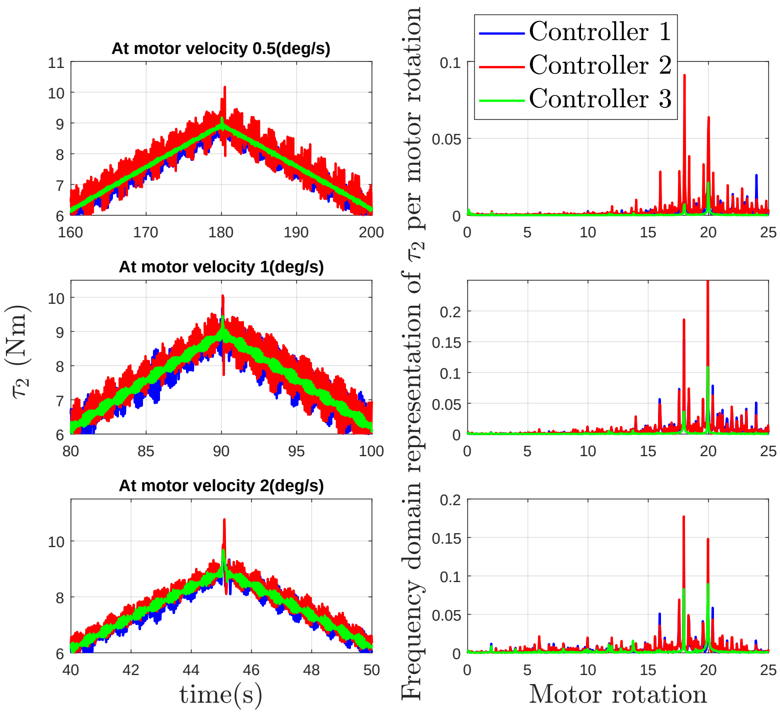

Figure 10. Measured link torque 2 and its analysis in frequency domain at different motor velocities from 0.5 deg/s to 2 deg/s

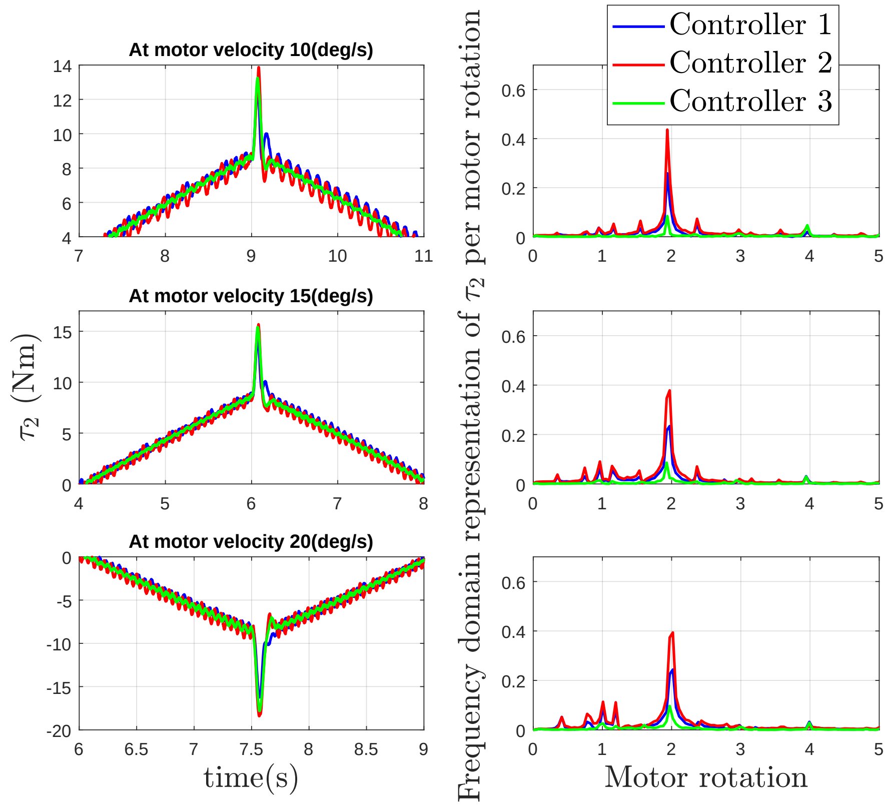

Figure 11. Measured link torque 2 and its analysis in frequency domain at different motor velocities from 10 deg/s to 20 deg/s

6. EXPERIMENTS

The proposed observer-based friction compensation has already been successfully used for several DLR robot systems, e.g. the DLR medical robot, the DLR lightweight robot, the DLR hand-arm system and the DLR humanoid robot, as well as the DLR SARA robot system (Safe Autonomous Robotic Assistant), see Figure 8.

Due to its high joint elasticity, high ripple-effects (of the Harmonic drive and the BLDC motor), and high motor friction, the state feedback controller with observer-based friction compensation in Section 4 can use the desired velocity as feedfordward terms in order to improve the position accuracy. So, in order to validate the control performances, the following experimental results are compared

1. Controller 1: A state feedback controller without friction compensation (regulation control),

2. Controller 2: A state feedback controller with model-based friction compensation (regulation control),

3. Controller 3: A state feedback controller with observer-based friction compensation from Section 5 (regulation control),

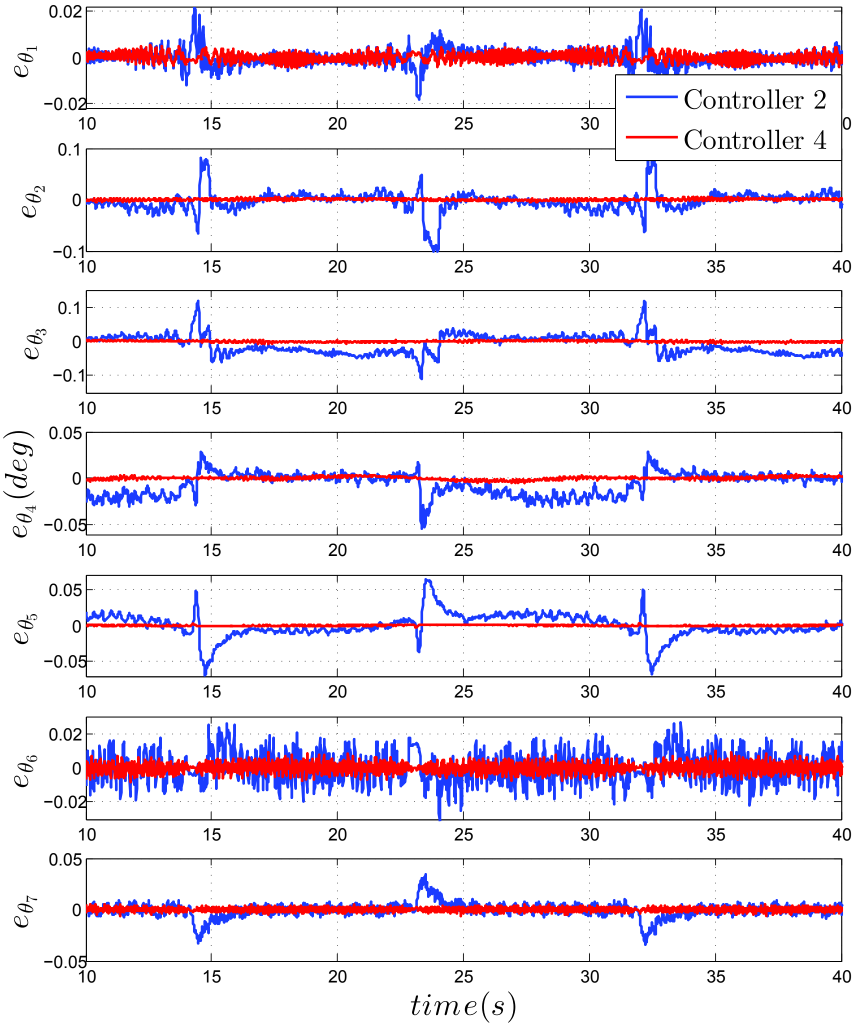

Figure 12. Joint position errors of the DLR medical robot for a periodic trajectory

4. Controller 4: A state feedback controller with observer-based friction compensation and additional desired motor velocity as feedforward terms (tracking control).

In this section all experiments are implemented with the DLR lightweight robots and the DLR medical robot. As an example, the Tables 1, 2 and 3 represent the joint parameters, the identified friction parameters, the friction observer parameters, and the control design parameters of the DLR medical robot, respectively.

At first, the control performance in terms of the dynamic behavior of controller 3 from Section 5 is validated by comparing step response results of joint 2 of the DLR medical robot with controller 1. It can be seen in Figure 9 that the controller 3 (the red curve) with observer-based friction compensation did almost not change the dynamic performance of the joint 2 and can damp oscillations of the link torques as well.

In the next experiments, the effects of the friction observer to eliminate the motor and drive ripple, as well as the control performance in terms of the dynamic behavior are inves-

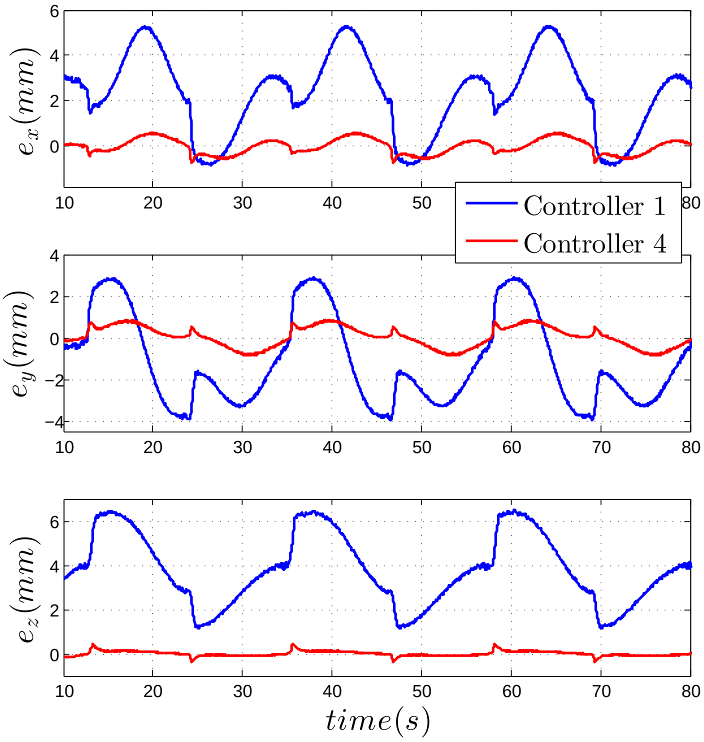

Figure 13. Cartesian translational position errors of the DLR lightweight robot for a periodic trajectory

Table 1. Joint parameters of the DLR medical robot

Joint	J (kgm^2)	k (Nm/rad)	d (Nms/rad)
1	0.6504	7743	2.18
2	1.2681	3965	3.83
3	1.2681	3271	5.18
4	0.9102	3250	4.50
5	0.9102	3900	1.65
6	0.0387	205	1.24
7	0.0139	103	0.93

tigated at the joint 2 of the DLR medical robot. Therefore, the joints followed a desired periodic trajectory with different velocities from 0.5 deg/s to 40 deg/s. The experimental results of the controllers 1, 2 and 3 are compared in Figures 10 and 11, in which the left hand side shows the measured link torques in time domain and the right hand side its frequency domain representation with respect to motor rotations. It can be seen in Figure 10 that controller 3 (state feedback controller with observer-based friction compensation) obviously reduces the dominant motor ripple effects (20 cycles per motor rotation) for low motor velocities from 0.5 deg/s to 2 deg/s. For high motor velocities from 10 deg/s to 40 deg/s in Figure 11, controller 3 keeps good damping performance of the state feedback controller

Table 2. Motor friction and disturbance observer parameters of the DLR medical robot

Joint	f_c (Nm)	f_s (Nm)	f_v (Nms/rad)	v_c (rad/s)	L (Nms/rad)	ϵ (rad/s)
1	2.42	0.12	9.21	0.00030	200	0.0015
2	9.18	0.23	17.66	0.00024	300	0.001
3	9.46	0.26	15.99	0.00021	300	0.001
4	6.25	0.16	4.91	0.00018	200	0.001
5	5.42	0.18	6.63	0.00013	200	0.001
6	0.68	0.01	0.37	0.00005	100	0.00025
7	0.33	0.01	0.12	0.00007	100	0.00025

Table 3. controller parameters for the DLR medical robot

Joint	k_P	k_D	$k_T k^{-1}$	$k_S k^{-1}$
1	9696	189	1.8778	0.00521065
2	7219	308	3.1098	0.01731589
3	7634	337	3.6560	0.00713452
4	6123	187	2.5772	0.00185809
5	6538	179	4.7887	0.02214089
6	673	4.3	0.2300	0.0001
7	685	3.8	0.1400	0.0001

and does not cause oscillations of the link torque at the reversal points of the trajectory. In addition, this controller can considerably reduce the dominant ripple effects from the drive (2 cycles per motor rotation).

Finally, some experiments were executed with the complete DLR medical robot as well as the DLR lightweight robot in order to show the position tracking accuracy. Therefor, the robots follow a desired periodic trajectory. Now, let us introduce the forward kinematics of the robot as $x = f(q) \in \mathbb{R}^6$, then the Cartesian position errors are defined $e_{cart} = f(q_d) - f(q) \in \mathbb{R}^6$. In Figures 12 and 13, the joint position errors of the DLR medical robot and the Cartesian translational position errors of the DLR lightweight robot are presented respectively. It can be seen that controller 4 (state feedback controller with observer-based friction compensation and feedforward terms) considerably reduces joint positioning errors of the DLR medical robot to zero. At the DLR lightweight robot, controller 4 can achieve Cartesian translational position errors under 1 mm, whereas controller 2 has bigger position errors because of the coarsely modeled friction torque and rigid body dynamics.

7. CONCLUSIONS

We have proposed in this paper an observer based friction compensation method that can be used together with passivity-based controllers in order to enhance the robot accuracy. The friction compensation, though similar to an integral action from the point of view of performance in free motion, has several advantages. First, it avoids saturation or overflow of the integrator in case of external disturbance torques (e.g. unexpected contacts). Second, only friction is compensated, instead of the sum of friction and external disturbance, so that it can be used also during impedance control in contact with the environment. Third, the friction observer can be designed independently of the state feedback controller, whereas when adding an integrator, all gains of the controllers have to be changed for good performance. Finally, our approach preserves the global asymptotic stability of the original state feedback controller even in the presence of friction. Experimental results validate the approach for the DLR robots.

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