A Passivity Based Admittance Control for Stabilizing the Compliant Humanoid COMAN

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Abstract—This paper presents a generic stabilization framework which is applicable for both compliant and stiff humanoids. The proposed control framework is applied to the passive compliant humanoid robot COMAN which is equipped with series elastic actuators. The stabilization control framework combines the compliance control and the intrinsic angular momentum modulation to achieve an agile and compliant interaction against external perturbations. The admittance based compliance control uses the force/torque sensing in both feet to regulate the active compliance for the position controlled system. The physical elasticity in the new full body COMAN is exploited for the reduction and absorption of the instantaneous impacts while the admittance control further dissipates the excessive elastic energy. The angular momentum controller reduces the overall inertia effect for providing more rapid reactions. Both the theoretical work and experimental validation were presented. The effectiveness of the control scheme is demonstrated by COMAN’s capabilities of withstanding various types of perturbations applied over the body, balancing on a moving platform and stabilizing while walking. Experimental data of the ground reaction force/torque, center of mass references and estimations, and the stored elastic energy are presented and analyzed.

I. INTRODUCTION

The humanoids’ capability of stable and soft interactions while operating in the human environment is a vital feature for satisfying the safety demands for both humans and robots. Our previous work formulated a heuristic stabilization of the compliant humanoid by analogy to soft martial arts [1], whereas the work in this paper elaborates the fundamental principles in terms of physics and classical control approach. The stability during interaction is resolved by controlling the humanoid robot in a compliant manner using a passivity based admittance control. Honda developed stabilization techniques for their walking robots [2]. Their “ground reaction force” control shifts the real center of pressure (COP) with respect to the desired zero moment point (ZMP) manipulated by the “model ZMP control”. The stabilization of HRP-4C on-line modifies the desired ZMP with a delay as a first-order system for the pre-view controller to generate stable walking [3]. The simulation study in [4] proposed a decoupled method for controlling center of mass (COM) and ZMP separately and superimposed the modification directly in the joint trajectories. The above techniques were developed for the robots with stiff actuators. The work in [5] involved an intrinsically compliant robot “Pneumat-BS” which used feedbacks of the gyroscope and the accelerometer to control the ankle muscles for the stabilization against impacts. Sugihara et. al [6] proposed the whole body balancing control by using the COM Jacobian. His strategies used the force and COM position feedbacks to compute a deviation of the COM references which simulated a deformation effect similar to the admittance control.

To guarantee the stability while interacting with the environment, a passivity based compliant property of the system is desired. This can be achieved by a fully torque controlled systems such as the Sarcos robot [7], [8], [9] or the DLR-biped [10], [11]. Their common groundwork is to compute the desired force to recover balance at the overall COM level and distribute the desired contact force through the support polygon by using the joint torque control capability. However, since most bipedal robots have position controlled actuators, an alternative to achieve the compliant behavior can be done using the admittance control based on the torque feedback to modulate the positional references.

The new release of the Compliant HuManoid COMAN is a full body robot based on the compliant leg prototype developed in [12]. Fig. 1 shows its kinematics and the allocation of compliant joints. In this new design, the stiffness of the series elastic actuator (SEA) is increased to 400N/rad compared to 120N/rad in [1], [13]. This is necessary to accommodate the increase of the robot weight. The passive elastic elements are selected to retain the capability of instantaneous shock absorption while at the same time to shift the system resonance out of the operation frequency.

Regarding the performance of the admittance control, the work in [14] pointed out that the stability of admittance
control becomes poorer when the stiffness of the environment is higher. Fortunately, the rubber cushion on the bottom of the feet together with the intrinsic compliance in COMAN’s joints reduce the effective stiffness at the foot-ground interface, which significantly improves the stability of the admittance control presented in this paper.

The proposed control framework consists of the admittance control to obtain the active compliance and the angular momentum controller to regulate the effective inertia. The integration of these controllers enable compliant interactions and rapid reactions for dealing with various types of disturbances. The robot could be so compliant to be moved away from the nominal posture by small force of a finger tip. The physical compliance in the COMAN humanoid, namely the springs, are exploited for absorbing impact energy with no control delay while the active admittance control subsequently dissipates the excessive elastic energy. The implementation of these algorithms demonstrates that the COMAN robot was capable of recovering from force disturbances as well as performing stable bipedal walking.

The paper is organized as follows. Section II presents the modeling of the COMAN robot and the identification of the effective stiffness caused by the physical compliance. Section III elaborates the principles of the passivity based admittance control and the angular momentum controller. Section IV shows the experimental results of different types of stabilization tasks of COMAN. We conclude the work in the final section.

II. MODELING AND STIFFNESS IDENTIFICATION

As shown in Fig. 1, the COMAN robot has 6 degree of freedoms (DOFs) in each leg, 3 DOFs in the waist, 4 DOFs in each arm, and 2 DOFs in its neck. Among these actuated joints, the SEAs are located at the hip flexion, knee, ankle flexion, waist pitch and yaw joints, shoulder flexion, shoulder abduction, and elbow flexion.

As shown in Fig. 2, the robot is modeled as a single composite rigid body mounted on the top of an inverted pendulum with mass m and moment of inertia \( I_c \) around the COM, and a practical size of foot with a torsional spring connected between the ideal COM referential position and the real link. Define the local coordinates \( \sum O_x \) and \( \sum O_y \) respectively for the left and right foot where the origins are the horizontal projections of the intersection points of the ankle dorsiflexion and abduction axes. The reference frame used in the following formulation is the local coordinate \( \sum O \) whose origin coincides with the midpoint of the vector between the origins of \( \sum O_x \) and \( \sum O_y \).

In the coordinate \( \sum O \), the overall torsion stiffness of the mass pendulum model is due to the passive compliance in all joints. The torsion stiffness around the \( x \) axis is high since no compliant element is in the lateral joints hence only the coupled compliance is provided by the sagittal joints when knees are bent. Moreover, the two feet are placed aside so the leg structure provides high stiffness, and a small difference in vertical ground reaction force in the left/right feet creates high moment around the COM. In contrast, the stiffness around \( y \) axis can not be neglected because the articulated leg has three compliant joints successively in the sagittal plane, and the structure has very limited support around the \( y \) axis. Therefore, the stiffness around \( y \) is contributed by both the motor controller gains and the stiffness of the springs.

Experimental identifications of the effective stiffness were done by fixing a constant COM position reference and applying small torque perturbations around \( x \) and \( y \) axes. The real COM position was computed from the absolute link angles and converted to the rotational angles with respect to the coordinate \( \sum O \). The perturbations were conducted statically to exclude the dynamical effect so the torque measured from the F/T sensor in feet were equal to the torque applied by the external load and gravity. Using the linear square fitting, it was identified that the average torsion stiffness \( K_x \) and \( K_y \) are \( 6130 \text{Nm/rad} \) and \( 572 \text{Nm/rad} \) around the \( x \) and \( y \) axes corresponding to the stiffness of \( 3249 \text{Nm/m} \) and \( 303 \text{Nm/m} \) at the COM along the \( y \) and \( x \) axes in the Cartesian space. Note that the measurement of \( K_x \) and \( K_y \) groups the compliance caused by motor PID gains and the real elasticity.
III. PRINCIPLES OF THE ADMITTANCE CONTROL

The admittance control uses F/T sensors mounted in feet to compute the torque generated by the ground reaction force, and modulates the COM references to replicate the compliant behavior as that of a spring-damper system. In this scheme, the admittance controller obeys the physics rule of “force→motion” causality, and all the real force/torque applied by the external force, gravity and actuators cause the motion of the real robot. All these interactions subsequently cause the changes of the ground contact force which are the feedback of the admittance control, so the active control action is able to reinforce the system behavior by updating a model-based reference trajectories.

A. Admittance based Compliance Control

As shown in Fig. 2(a), \( \theta \) is the position reference and \( q \) is the real link position. Define \( K_s \) as the real stiffness and \( B \) as the real viscous coefficient, and \( l \) as the distance from COM to the origin. The dynamic equation of the model is

\[
Iq + Bq + K_s q - K_s \theta - \tau_g = \tau_{ext},
\]

where \( \tau_g = mglsin(q) \) is the gravitational torque and \( \tau_{ext} \) is the external torque.

In the frequency domain, the equation is

\[
(Is^2 + Bs + K_s)q(s) - K_s \theta(s) - \tau_g(s) = \tau_{ext}(s).
\]

Let \( Z_{ext}(s) \) be the output impedance observed from the external perturbation. In the frequency domain, the effective impedance \( Z_{ext}(s) \) that appears at the output is

\[
Z_{ext}(s) = I\frac{q(s)}{\theta(s)} + B \frac{\tau_g(s)}{\theta(s)} - K_s \frac{\theta(s)}{\theta(s)}.
\]

Equation (3) suggests that the output impedance can be modified by actively controlling \( q \). The system to be emulated is a spring-damper model without the gravity. The dynamics is described by

\[
Iq\ddot{q} + Bq + K_s q - K_s \dot{\theta} = \tau_{ext}.
\]

Hence, given the equilibrium \( q_0 \), the desired impedance \( Z_d(s) \) is

\[
Z_d(s) = I\frac{q(s)}{\dot{q}(s)} + B \frac{\tau_g(s)}{\dot{q}(s)} - K_d \frac{\dot{\theta}(s)}{\dot{\theta}(s)},
\]

where \( K_d > 0 \) is the desired stiffness, \( B_d = 2\zeta\sqrt{I_dK_d} \) is the desired viscous coefficient, and \( \zeta \) is the damping ratio. The impedance replication only modulates the stiffness and damping property, while the desired inertia \( I_d \) is the same as the real inertia \( I \) of the physical system.

Let \( Z_d(s) \) be equal to \( Z_{ext}(s) \), yields

\[
\theta(s) = \frac{K_d}{K_s}q_0(s) + \frac{K_s - K_d}{K_s}q(s) + \frac{B - B_d}{K_s}s(s) - \frac{\tau_g(s)}{K_s}.
\]

Rewrite the equation in the time domain, we obtain the formula of the referential position \( \theta \) based on the link feedback \( q \).

\[
\theta = \frac{K_d}{K_s}q_0 + \frac{K_s - K_d}{K_s}q + B - B_d - \frac{\tau_g}{K_s}.
\]

Since the link position sensor has limited resolution, we wish to reformulate the equation that uses high resolution torque sensor as the feedback. Denote \( \theta_g \) as the deflexion of the spring and \( \tau \) as the resultant torque applied on the pendulum model from the ground in the coordinate \( \sum_O \).

\[
\begin{align*}
q &= \theta + \theta_g, \\
\tau &= -K_s \theta_g.
\end{align*}
\]

Substitute (8) into (7), yields

\[
\theta = \frac{K_d}{K_s}q_0 + \frac{K_s - K_d}{K_s}(\theta - \frac{\tau}{K_s}) + B - B_d \left( \dot{\theta} - \frac{\dot{\tau}}{K_s} \right) - \frac{\tau_g}{K_s}.
\]

The term \( \theta \) in the above equations is the ideal referential position, therefore its derivative term can be replaced using the backward Euler method in the discrete form

\[
\theta(i) = \frac{\theta(i) - \theta(i-1)}{T},
\]

where \( i \) is the number of control loops and \( T \) is the sampling time.

Substitute (10) into (9), we can derive the referential position \( \theta_d \) in the discrete form given the feedback \( \tau(i) \),

\[
\theta_d(i) = \frac{K_dq_0 + \frac{K_s - K_d}{K_s}\tau(i) + \frac{B - B_d}{K_s}\dot{\theta}(i)}{K_d + \frac{B - B_d}{K_s}},
\]

where the gravitational torque is approximated as

\[
\tau_g(i) = mglsin(\theta_d(i - 1) - \frac{\tau(i)}{K_s} - \frac{\tau}{K_s}).
\]

Equation (11) is the general equation for a 1-DOF compliant system to achieve the admittance control based on the mass pendulum model. Given the limited size of support area and the height of the COM, the pendulum’s rotation about the \( x \) and \( y \) axes are small enough to neglect the coupling effect due to the variation of inertia tensor. The formulas for the admittance control about \( x \) and \( y \) axis, \( \theta_d^x \) and \( \theta_d^y \), can be obtained in a decoupled form by substituting \( K_s^x, \tau_x, \tau_y, \) and \( q_0^x, q_0^y \) respectively into (11).

Equation (11) can also be altered for a stiff system by setting \( K_s \to \infty \),

\[
\theta_d(i) = \frac{K_dq_0 + \frac{B - B_d}{T}\theta_d(i - 1) - \tau(i) - \tau_g(i)}{K_d + \frac{B - B_d}{T}}.
\]

The desired reference \( \theta_d \) is generated based on the torque feedback computed from the F/T sensors in feet. This scheme is therefore termed as “Admittance Control” since the torque
is the input and the position is the output. In such a case, the admittance control follows the “force→motion” causality while the real robot serves as the impedance.

The referential COM position in the Cartesian space are

\[
\begin{aligned}
    z_d &= l \cdot \cos(\text{atan}2(\sqrt{\tan^2(-\theta_d^i) + \tan^2(\theta_d^i)}), 1)) \\
    x_d &= z_d \cdot \tan(\theta_d^i) \\
    y_d &= z_d \cdot \tan(-\theta_d^i)
\end{aligned}
\]

(14)

\[O^0\]

\[\begin{aligned}
    \text{Calculate COM by Forward Kinematics} \\
    \text{Link based Inverse Kinematics} \\
    \text{Initial guess } P^{\text{hip}}_{i=1} \\
    \text{Joint angles of upper body}
\end{aligned}\]

Fig. 4. The concept and diagram of the COM based inverse kinematics

B. COM Based Inverse Kinematics

The solution of inverse kinematics based on the kinematic chain is well presented in the literature [15], [16]. In this study, a link based inverse kinematics is developed regarding the hip as the base link and the ankle as the end effector. The COM based inverse kinematics is solved via the numerical iteration using the forward kinematics for computing the COM and the link based inverse kinematics for numerically minimizing the COM error. The position and orientation of the ankle, the position of the COM and the orientation of the pelvis are the inputs for the COM based inverse kinematics. Eq. 4(a) presents the concept of regulating the COM position by modifying the hip position given the fixed position/orientation of the feet and the fixed hip orientation.

\[
P^{\text{hip}}_{i}(k + 1) = P^{\text{hip}}_{i}(k) + K_{3 \times 3}(P^{\text{com}}_{i}(k) - P^{\text{com}}_{i}(k)),
\]

(15)

where \( k = 0, 1, \cdots, N \) is the number of iterations at the \( i \)th control loop, and \( K_{3 \times 3} \) is a diagonal matrix with proportional gains. Fig. 4(b) illustrates the numerical iteration where the previous hip position is the initial guess. The inner loop of the link based inverse kinematics updates the modification of hip position in proportional to the COM error calculated by the forward kinematics. We set \( N = 5 \) in the implemented code which results in good precision.

C. Angular Momentum Control

The admittance scheme only simulates the variable stiffness and the viscosity but keeps the inertia tensor unchanged. It is hence interesting to explore how the dynamics can be affected by the effective inertia in the impedance term. The proposed angular momentum controller exploits the redundant DOF of the upper body to achieve the zero intrinsic angular momentum for reducing the overall inertia effect. By using this algorithm, it was found that the system has a faster response to disturbances due to the reduced inertia.

In the coordinate \( \sum_{i=0} \omega_{i} \), the total angular momentum of the robot consists of two terms: one is the orbital term caused by the linear momentum of the overall COM about the pivot, which is unavoidable; the other is the intrinsic angular momentum caused by all the segments spinning around the overall COM. So zero intrinsic angular momentum means that the entire robot behaves as a point mass and the moment of inertia felt by the external load is only that of a point mass around the pivot \( ml^2 \). It is the minimum achievable inertia tensor in the impedance term (5). In this case, the net torque around the COM is zero, so the external torque only drives the COM around the pivot, as depicted in the right picture in Fig. 5(a). Without the control of the angular momentum, the exhibited inertia tensor would be coupled with \( I_c \), as shown in the left figure in Fig. 5(a).

As shown in Fig. 5(b), by rotating the upper body in an opposite direction to the lower limbs, the angular momentum created by the upper body and the legs could cancels out each other. Therefore, the net angular momentum around the COM is zero, \( L_{x,y} = 0 \). This creates a more agile reaction since the effective inertia is smaller.

The full expression of \( L_{c} \) can be calculated as follows

\[
L_{c} = \sum_{i=0}^{n-1} \left[ (r_{i} - r_{\text{com}}) \times m_{i}(v_{i} - v_{\text{com}}) + R_{i}I_{c}R_{i}^{T}\omega_{i} \right],
\]

(16)

where

\[
r_{\text{com}} = \frac{\sum_{i=0}^{n-1} r_{i} \times m_{i}}{\sum_{i=0}^{n-1} m_{i}}, v_{\text{com}} = \frac{\sum_{i=0}^{n-1} v_{i} \times m_{i}}{\sum_{i=0}^{n-1} m_{i}}.
\]

(17)
In (16), the first term is the angular momentum created by the particle mass of the i-th segment with respect to the overall COM. The second term is the angular momentum of i-th segment around its local COM. \( I_1 \) is the inertia of the i-th segment in the local frame and \( I_1, I_1^T \) is the inertia in \( \Sigma_O \). \( \omega_i \) is the angular velocity of the i-th segment in \( \Sigma_O \). In (16), \( \mathbf{r}_{com} \) and \( \mathbf{v}_{com} \) are the position and velocity vectors of the overall COM. \( m_1 \) and \( m \) are the mass of i-th segment and the robot respectively. \( \mathbf{l}_{c} \) is the sum of the angular momentum of all the segments spinning around its overall COM.

The directly application of (16) demands significant computation time, and the derivatives of the position vectors are noisy. Therefore, an approximation is adapted for on-line computation instead of (16). Define \( \alpha \) and \( \beta \) are the roll and pitch angles of the vector from the origin of \( \Sigma_O \) to the midpoint between two hips. \( x_{hip}, y_{hip}, z_{hip} \) are the elements of vector \( \mathbf{p}^{hip} \) in the COM based inverse kinematics module.

\[
\begin{align*}
\alpha &= \text{atan}(y_{hip}), \\
\beta &= \text{atan}(x_{hip}),
\end{align*}
\]

The following equations are used to approximate the zero intrinsic angular momentum for on-line computation.

\[
\begin{align*}
I_{xx} \varphi + J_{xx} \alpha &= 0, \\
I_{yy} \theta + J_{yy} \beta &= 0.
\end{align*}
\]

where \( I \) and \( J \) are the inertia tensor of the upper body (including arms) and the lower limbs (excluding feet) respectively, \( \varphi, \theta, \psi \) are the roll, pitch and yaw angles of the upper body. The pseudo code in Table I explains the integration of the angular momentum control with the COM based inverse kinematics module.

The horizontal components of the torque applied by the ground are

\[
\mathbf{\tau}_{x,y} = \{ \tau_i + \tau_r + \mathbf{r}_f \times \mathbf{\theta}_f + \mathbf{r}_l \times \mathbf{\phi}_l \} x,y.
\]
rapid reactions by the number of cycles versus time, which can also be observed in the accompanied video.

To obtain the quantitative comparison, the power spectrum of the torque measurement $\tau_{x,y}$ were analyzed. As shown in Fig. 11, without the momentum control, the maximum responsive frequencies were typically around 0.5 Hz for $\tau_y$ and 1 Hz for $\tau_x$. However, with the momentum modulation, the maximum responsive frequency of $\tau_y$ shifted up to 2.6 Hz, while the frequency band of $\tau_y$ increased to 2 – 3 Hz. The angular momentum control is fairly effective for the lateral response because of the high physical stiffness. The effectiveness of sagittal responses is less because the springs acted as mechanical low-pass filters which reduced the control bandwidth of the actuation. The maximum elastic energy stored in springs was 4.8 J, as shown in Fig. 12.

The snapshots in Fig. 13, Fig. 14 and Fig. 15 show COMAN’s posture recovery from kick/push and the disturbances produced by an accelerating/decelerating platform. An orange ruler was marked on the top of the snapshots to visualize the movement of the robot.

Finally, the feasibility of implementing the proposed scheme for bipedal walking was preliminarily explored for the lateral stabilization. In both walking experiments with and without the stabilizer, the COMAN robot walked only 10 steps since the robot was more likely to fall down without the stabilizer. Fig. 16 displays the snapshots of the stabilized walking. The vectors $r_{f_s}$ and $r_{f_o}$ in (20) were updated according to the gait parameter of the constant step width. The walking trajectories were generated by the COM state based pattern generator [13]. The stabilizer module computed the modifications of the COM position $\Delta y, \Delta z$ using (14) and added to the desired COM position from the walking pattern generator. The vertical ground reaction force measured on feet are plotted from the experimental data. The robot weight is approximately 26 kg. As Fig. 17 shows, each foot had landing impacts and hence the force fluctuated during the stance. When the robot started to stop after 12.2 s, the fluctuation of force implied an oscillation introduced by the compliant structure. However, for the gait with stabilizer, the force shown in Fig. 18 indicated neither the severe foot landing impacts during walking nor the undesired oscillations after the gait stopped.

V. CONCLUSION

This paper presented a stabilization framework for both stiff and intrinsically compliant systems. The method was validated on COMAN experimentally by demonstrating the capability of withstanding kicks/pushes, stabilizing posture in a moving cart, and reducing foot landing impacts during walking. The admittance control with passivity was effective for stabilizing COMAN in a compliant way under these different disturbances. Only the force/torque sensing in both feet is required to realize the active compliance regulation for this position controlled robot. The angular momentum modulation reduces the effective inertia so the robot was able to respond to faster perturbations. The preliminary analysis of the torque sensing showed the increased frequency response.

REFERENCES


