Investigations of a Robotic Testbed with Viscoelastic Liquid Cooled Actuators

Donghyun Kim, Member, IEEE, Junhyeok Ahn, Orion Campbell, Nicholas Paine, and Luis Sentis, Member, IEEE

Abstract—We design, build, and empirically test a robotic leg prototype using a new type of high performance device dubbed a viscoelastic liquid cooled actuator (VLCA). VLCA excel in the following five critical axes of performance, which are essential for dynamic locomotion of legged systems: energy efficiency, torque density, mechanical robustness, position and force controllability. We first study the design objectives and choices of the VLCA to enhance the performance on the needed criteria. We follow by an investigation on viscoelastic materials in terms of their damping, viscous and hysteresis properties as well as parameters related to the long-term performance. As part of the actuator design, we configure a disturbance observer to provide high-fidelity force control to enable a wide range of impedance control capabilities. After designing the VLCA, we proceed to design a robotic system capable to lift payloads of 32 kg, which is three times larger than its own weight. In addition, we experiment with Cartesian trajectory control up to 2 Hz with a vertical range of motion of 32 cm while carrying a payload of 10 kg. Finally, we perform experiments on impedance control by studying the response of the leg testbed to hammering impacts and external force interactions.

Index Terms—Viscoelastic liquid cooled actuator, Torque feedback control, Impedance control.

I. INTRODUCTION

Biped robots are an alternative technology over wheeled systems due to their maneuverability in various terrains, their small footprint for operations in tight spaces, and their omnidirectional movements. However, to be a practical tool for humans, several critical issues must be addressed. A bipedal system must be robust to external disturbances, especially impact from the ground. Moreover, it must be energy-efficient, powerful, and agile to execute a variety of desired tasks such as exploring urban environments, carrying heavy loads, or walking and running over irregular terrains. To be a reliable and useful mobile platform, bipeds also need to provide high-quality position and force feedback control. However, simultaneously achieving impact-resistance, energy efficiency, torque and power density, and controllability presents many technical challenges and requires a significant engineering effort to improve the current state-of-the-art actuator design.

From our previous study of point-foot biped robots with series elastic actuators (SEAs) [1], we have learned that SEAs effectively protect drivetrains and motors from damage due to external impacts. We have devised our own SEAs, which are more compact, energy efficient, and powerful than other SEAs available today [2]. One drawback of SEAs is the difficulty that arises when using a position controller due to the elastic element in the drivetrain. In this paper, we introduce a novel SEA using a viscoelastic material instead of a metal spring. In addition to changing the material of the elastic component, we replaced traditional air-convection cooling with liquid cooling, which significantly enhances the power of the electromagnetic (EM) motor. These two major changes successfully address the issues of position control and power density. However, much of the innovation lies in effectively integrating these new ideas into the existing technology while preserving the original beneficial features of the SEA design.

The challenge in using elastomers comes from the complex force versus displacement characteristics. Existing studies of elastomer-based actuators also discussed the difficulty of force control. [3] experimented with a damped SEA based on a piezoelectric damper. Although it had a metal spring with controllable dampers, the results showed the beneficial features of dampers for dynamic performance, stability, and controllability of SEAs. [4] showed reasonably good estimated torque tracking and no steady-state errors. [5] studied the use of rubber for the viscoelastic material in a SEA. They model the force-displacement curve of rubber using a “standard linear model.” The estimated rubber force is employed in a closed-loop force controller. Unfortunately, the hysteresis in the urethane rubber destabilized the system at frequencies above 2 Hz. Previous studies imply the feasibility of torque control based on the deflection measurement of viscoelastic elements, but the performance is degraded due to material hysteresis.

To sufficiently address the nonlinear behavior of elastomers, which severely reduce force control performance, we empirically analyze various viscoelastic materials with our custom-built elastomer testbed. We measure each material’s linearity, creep, compression set, and damping under a preloaded condition, which is substantially under-documented. Based on these experimental results concerning the properties of viscoelastic materials, we found that polyurethane has high linearity (0.992), low compression set (2%), low creep (15.3%), and high damping (16 kN-m/s/n).

To achieve stable and robust force control, we also study various feedback control schemes. In our previous work, we have shown that the active passivity obtained from motor velocity feedback [1] and model-based control such as disturbance observer (DOB) [6] play an essential role in achieving high-fidelity force feedback control. We analyze the phase margins of various feedback controllers and empirically show how these effects appear in the actual system. We verify the stability and robustness of our controller by demonstrating impedance control including an impact test.

To test our novel actuator, we devised a two degree-of-

Luis Sentis is a professor in Aerospace Engineering, University of Texas at Austin, Austin, TX, 78712, USA
freedom (DOF), single leg testbed, shown in Fig. 5. This testbed integrates two of the new actuators, one in the ankle, and another in the knee, while restricting motions to the sagittal plane. With the foot bolted to the floor for initial tests, weight plates can be loaded on the hip joint to serve as an end-effector payload. We test a variety of Cartesian space trajectories using multi-body dynamics to compute torque inputs and inverse kinematics to calculate joint trajectories. We demonstrate dynamic motion with high payloads as well as high-fidelity impedance control to showcase another important aspect of our system, which is a unique cooling system to significantly enhance the power of the robot.

EM motors’ torque density is limited by core temperature. Additionally, the maximum continuous torque of these motors can be significantly enhanced with an effective cooling system. Our previous study [7] showed that we can compute the enhancement ratio of maximum power based on the motors’ thermal data that can be obtained from spec sheets. In our new viscoelastic liquid cooled actuator (VLCA), we use a 120 W Maxon EC-max 40, which is expected to exert 3.59 times larger continuous torque. We demonstrate the effectiveness of liquid cooling by showing 1 Hz up and down motion for longer than 4 min. We accurately control dynamic motion such as 2 Hz up and down trajectory while carrying a 10 kg payload. In the heavy lift test, the core temperatures of motors stay under the 80°C while lifting a 32.5 kg weight.

Of the five criteria: efficiency, force controllability, position controllability, impact resistance, and torque density, we mainly focus on the first four in this paper. Overall, our contributions include 1) designing a new actuator, dubbed the VLCA, 2) extensively studying viscoelastic materials, 3) extensively analyzing torque feedback control of VLCA.s, and 4) examining the performance of the new actuator in a leg prototype.

II. VISCOELASTIC LIQUID COOLED ACTUATION

The design objectives of the VLCA are 1) power density, 2) efficiency, 3) impact tolerance, 4) position controllability, and 5) force controllability. Our previous work [7] shows a significant improvement in motor current, torque, output power and system efficiency for liquid cooled commercial off-the-shelf (COTS) electric motors and studied several Maxon motors for comparison. As an extension of this previous work, in this new study we studied COTS motors and their thermal behavior models and selected the Maxon EC-max 40 brushless 120 W (Fig. 5(e)), with a custom housing designed for the liquid cooling system (Fig. 5(h)). The limit of continuous current increases by a factor of 3.59 when liquid convection is used for cooling the motor. Therefore, a continuous motor torque of 0.701 N·m is theoretically achievable. Energetically, this actuator is designed to achieve 366 W continuous power and 1098W short-term power output with an 85% ball screw efficiency (Fig. 5(b)) since short-term power is generally three time larger than continuous power. With the total actuator mass of 1.692 kg, this translates into a continuous power of 216W/kg and a short-term power of 650W/kg. By combining convection liquid cooling, high power brushless DC (BLDC) motors, and a high-efficiency ball screw, we aim to surpass existing electric actuation technologies with COTS motors in terms of power density.

In terms of controls, a common problem with conventional SEAs is their lack of physical damping at their mechanical output. As a result, active damping must be provided from torque produced by the motor [8]. However, the presence of signal latency and derivative signal filtering limit the amount by which this active damping can be increased, resulting in SEA driven robots achieving only relatively low output impedances [9] and thus operating with limited position control accuracy and bandwidth. Our VLCA design incorporates damping directly into the compliant element itself, reducing the requirements placed on active damping efforts from the controller. The incorporation of passive damping aims to increase the output impedance while retaining compliance properties, resulting in higher position control bandwidth. The material properties we took into consideration will be introduced in Section III. The retention of a compliant element in the VLCA drive enables the measurement of actuator forces based on deflection. The inclusion of a load cell (Fig. 1(c)) on the actuators output serves as a redundant force sensor and is used to calibrate the force displacement characteristics of the viscoelastic element.

Mechanical power is transmitted when the motor turns a ball nut via a low-loss timing belt and pulley (Fig. 1(a)), which causes a ball screw to apply a force to the actuator’s output (Fig. 1(d)). The rigid assembly consisting of the motor, ball screw, and ball nut connects in series to a compliant viscoelastic element (Fig. 1(j)), which connects to the mechanical
A. Force versus displacement

In the design of compliant actuation, it is essential to know how much a spring will compress given an applied force. This displacement determines the required sensitivity of a spring-deflection sensor and also affects mechanical aspects of the actuator such as usable actuator range of motion and clearance to other components due to Poisson ratio expansion. This experiment, we identify the force versus displacement curves for the various elastomer springs. Experimental data for all eight springs as shown in Fig. 2(b). Note that there is a disagreement between our empirical measurements and the analytic model relating stiffness to hardness, i.e. the Gent’s relation shown in [10]. This mismatch arises because in our experiments the materials are preloaded whereas the analytical models assume unloaded materials.

B. Stress relaxation

Stress-relaxation is an undesirable property in compliant actuators for two reasons. First, the time-varying force degrades the quality of the compliant material as a force sensor. When a material with significant stress-relaxation properties is used, the only way to accurately estimate actuator force based on deflection data is to model the effect and then pass deflection data through this model to obtain a force estimate. This model introduces complexity and more room for error. The second reason stress-relaxation can be problematic is that it can lead to the loss of contact forces in compression-based spring structures.

The experiment for stress relaxation is conducted as follow: 1) enforce a desired displacement to a material, 2) record the force data over time from the load cell, 3) subtract the initially measured force from all of the force data. Empirically measured stress-relaxation properties for each of the materials are shown in Fig. 2(c), which represents force offsets as time goes under the same displacement enforced. Note that each material shows different initial force due to the different stiffness and each initial force data is subtracted in the plot.

C. Compression set

Compression set is the reduction in length of an elastomer after prolonged compression. The drawback of using materials with compression set in compliant actuation is that the materials must be installed with larger amounts of preload.
forces to avoid the material sliding out of place during usage. To measure this property, we measured each elastomers free length both before and after the elastomer was placed in the preloaded testbed. The result of our compression set experiments are summarized in Table I.

### D. Dynamic response

In regards to compliant actuation, the primary benefit of using an elastomer spring is its viscous properties, which can characterize the dynamic response of an actuator in series with such a component. To perform this experiment, we generate output relation of the system, we can fit a second order transfer function to the experimental data to obtain an estimate of the system’s viscous properties. However, this measure also includes the viscoelastic testbed’s ball screw drive train friction (Fig. 2(a)). To quantify the elastomer spring damping independent of the damping of the testbed drive train, the latter (8000 N s/m) was first characterized using a metal die spring, and then subtracted from subsequent tests of the elastomer springs to obtain estimates for the viscous properties of the elastomer materials. Fig. 2(d) shows the frequency response results for current input and force output of three different springs, while controlling the damping ratio. The elastomers have higher stiffness than the metal spring, hence their natural frequencies are higher.

### E. Selection of Polyurethane 90A

A variety of other experiments were conducted to strengthen our analysis and are summarized in Table I. Based on these results, Polyurethane 90A appears to be a strong candidate for viscoelastic actuators based on its high linearity (0.992), low compression set (2%), low creep (15%), and reasonably high damping (16000 N s/m). It is also the cheapest of the materials and comes in the largest variety of hardnesses and sizes.

### IV. DISTRIBUTED JOINT FEEDBACK CONTROL

Each joint has a feedback controller executing the commands delivered from a central motion controller. We have two control modes: one is a torque feedback and the other is a motor position feedback control. We choose one of these control modes depending on the desired behavior - torque control for operational space impedance control and motor position control when the system needs to be robust to external disturbances.

#### A. Force Feedback Control

To demonstrate various impedance behaviors in operational space, robots must have a robust torque (or force) controller. Stable and robust operational space control (OSC) is not trivial to achieve because of the bandwidth interference between outer position feedback control (OSC) and inner torque feedback control [1]. Since stable torque control is a critical component for a successful OSC implementation, we extensively study various force feedback controls.

The first step in this analysis is to identify the actuator dynamics. The transfer functions of the reaction force sensed in the series elastic actuators (rubber deflection) are well explained in [1]. The transfer function from motor torque to rubber deflection is

\[ \frac{x_r}{\tau_m} = \frac{N_m^{-1} P_m P_r}{N_m^{-2} P_m + P_l + P_r}, \tag{1} \]

where

\[ P_m(s) = \frac{1}{J_m s^2 + b_m s}, \]

\[ P_r(s) = \frac{1}{m_r s^2 + b_r s + k_r}, \tag{2} \]

\[ P_l(s) = \frac{1}{(l_l/N_l^2)^2}. \]

\(N_m\) is the speed ratio of the motor to the ball screw, and \(N_l\) is the length of the effective moment arm of the linkage at each joint. The equations follow the nomenclature in Fig. 2(a). When the actuator output is fixed, which is equivalent to \(P_l = \infty (P_l = 0)\), the transfer function from the motor current input to the rubber deflection is given by

\[ \frac{P_x}{i_m} = \frac{\eta k_r N_m}{(J_m N_m^2 + m_r) s^2 + (b_m N_m^2 + b_r) s + k_r}, \tag{3} \]

where \(\eta, k_r,\) and \(i_m\) are the ball screw efficiency, the torque constant of a motor, and the current input for the motor, respectively. We can find \(\eta, k_r,\) and \(N_m\) in data sheets, which are 0.9, 0.0448 N·m/A, and 3316 respectively. However, we need to experimentally identify \(k_r, b_r, J_m,\) and \(b_m\). We infer \(k_r\) by dividing the force measurement from the
load cell by the rubber deflection. The other parameters are estimated by comparing the frequency response of the model and experimental data. The frequency response test is done with an ankle actuator while prohibiting joint movement with a load and an offset force command. The results are presented in Fig. 3 with solid gray lines. Note that the dotted gray lines are the estimated response from the transfer function using the parameters of Table II. The estimated response and experimental result match closely with one another, implying that the parameters we found are close to the actual values.

We also study the frequency response for different load masses to understand how the dynamics changes as the joint moves. When 10 kg is attached to the end of link, the reflected mass to the actuator varies from 1500 kg to 2500 kg because the length of the effective moment arm changes depending on joint position. In Fig. 3(b), the bode plots are presented and the response is not significantly different than the fixed output case. Therefore, we design and analyze the feedback controller based on the fixed output dynamics.

For the force feedback controller, we first compare two options, which we have used in our previous studies [1], [6]:

1) Proportional (P) + Derivative (D) using velocity signal obtained by a low-pass derivative filter
2) Proportional (P) + Derivative (D_f) using motor velocity signal measured by a quadrature encoder connected to a motor axis

The second controller (PD_m) has benefits over the first one (PD_t) with respect to sensor signal quality. The velocity of motor is directly measured by a quadrature encoder rather than low-pass filtered rubber deflection data, which is relatively noisy and lagged. In addition, Fig. 4 shows that the phase margin of the second controller (47.6) is larger than the first one (17.1).

To remove the error at low frequencies, we consider two options: augmenting the controller either with integral control or with a DOB on the PD_m controller. To compare the two controllers, we analyzed the phase margins of all the mentioned controllers. First, we chose to focus on the location where the sensor data returns in order to address the time delay of digital controllers (Fig. 4(a) and (c)). Next, we have to compute the open-loop transfer function for each closed loop system. For example, the PD_t controller’s closed loop transfer function is

\[ F_k = \frac{k_f P_{x}}{N} \left( k_p (F_r - e^{-Ts} F_k) + F_r - k_d f Q_d e^{-Ts} F_k \right), \]

(4)

where \( T \) is the time delay and

\[ Q_d = \frac{s}{(s/w_c)^2 + 1.414(s/w_c) + 1}, \]

(5)

which is a low pass derivative filter. For convenience, we use \( N \) instead of the multiplication of three terms, \( \eta k_f N_m \). When
controllers. We can apply the same method for the

Then it becomes

Then, the open-loop transfer function of the closed system

gathering the term with $e^{-Ts}$ of Eq. (4), we obtain

Then the open-loop transfer function of the closed system with the time delay is

We can apply the same method for the PID$m$ and PD$m$+DOB controllers.

The transfer function of PID$m$, which is presented in Fig. 4(c), is

Then it becomes

When we apply a DOB instead of integral control, we need the inverse of the plant. In our case, the plant of the DOB is PD$m$, which is similar to Eq. (9) except that $K_i$ and $e^{-Ts}$ are omitted:

The formulation of PD$m$ including the DOB, which is shown in Fig. 4(c), is

where $Q_{rd}$ is a second order low-pass filter. Then the transfer function is

The open-loop transfer function is

The bode plots of $P_{PDm}$, $P_{PDm}^{open}$, and $P_{PDm+DOB}^{open}$ are presented in Fig. 4(b). The gains ($K_p$, $K_d$, $K_i$) are the same as the values that we use in the experiments presented in Section VI-A, which are 4, 15, and 300, respectively. The PD$f$ controller uses $K_dN/m/k_r$ for $K_d,f$ to normalize the derivative gain. The cutoff frequency of the DOB is set to 15Hz because this is where the PD$m$+DOB shows a magnitude trend similar to the integral controller (PID$m$). The results imply that the PD$m$+DOB controller is more stable than PID$m$ with respect to phase margin and maximum phase lag. This analysis is also experimentally verified in Section VI-A

B. Motor Position Feedback Control

When accurate and robust motion control is desired (rather than accurate impedance behavior), we use motor position feedback control instead of force feedback control. In this control mode, the distributed controllers take three commands—desired joint position, velocity, and torque. Then the desired motor position is computed with the equation

where $\theta, q,$ and $f()$ are motor position, joint position, and the mapping from a joint position to a motor position, respectively. Converting motor positions to joint positions requires nonlinear mapping functions, which we can find by solving a closed chain kinematic equation for each joint. As in the previous section, $N_m$, $k_r$, and $N_i$ are the speed reduction ratio of the drivetrain, the rubber stiffness, and the moment arm length. The second term on the right hand side of Eq. (14) is the expected rubber deflection required to produce the desired torque command. Desired motor velocity is the product of the commanded joint velocity, the moment arm length, and the drivetrain speed ratio, $\dot{\theta}_{des} = \dot{q}_{des}N/m/N_i$. Then the final current command is

The motor position control uses the quadrature encoders connected to the motor’s axis depicted in Fig. 1(f). Since quadrature encoders do not provide absolute position (rather they return the incremental count), we obtain the joint positions by synchronizing the joint position and motor position when the robot is powered on, and then accumulating the tick count from the initial positions. The signal is far less noisy than using the joint encoders. Additionally, this strategy removes the possibility of linkage backlash affecting the stability of the controller.
V. CARTESIAN MOTION CONTROL

As a pilot test for a future full biped robot, we built a single leg testbed presented in Fig. 5. To demonstrate dynamic motion, we implemented an operational space controller (OSC) incorporating the multi-body dynamics of the robot.

A. Single Leg Testbed

We designed and built a single leg testbed (Fig. 5) consisting of two VLCAs - one for the ankle (qₐ) and one for the knee (qₖ). The design constrains motion to the sagittal plane, the leg carries 10kg, 23.0kg, or 32.5kg of weight on the hip, and the foot is fixed on the ground. With this testbed, we intended to demonstrate coordinated position control with two VLCAs, the viability of liquid cooling on an articulated platform, cartesian motion control, and to demonstrate position control with two VLCAs, the viability of liquid cooling on an articulated platform, cartesian motion control.

The two joints each have a different linkage structure that was carefully designed so that the moment arm accommodates the expected torques and joint behaviors as the link posture changes (Fig. 5). For example, each joint can exert a peak torque of approximately 270 Nm and the maximum joint velocity ranges between 7.5 rad/s and 20+ rad/s depending on the mechanical advantage of the linkage along the configurations. The joints can exert a maximum continuous torque of 91 Nm at the point of highest mechanical advantage. This posture dependent ratio of torque and velocity is a unique benefit of prismatic actuators.

B. Dynamic Motion Control

Given cartesian motion trajectories, which are 2nd order B-spline or sinusoidal functions, the centralized controller computes the commands for distributed joint controllers, which are desired joint positions (q), velocities (q), and torques (τ). The commands are delivered to each embedded microcontroller. In the microcontrollers, we implement the functions converting joint positions, velocities, and torques to motor positions (θ), velocities (θ), and feedforward current commands (i). The computation of the current commands account not only for the speed reduction (n) but also the approximate transmission efficiency (η = 0.9). The control diagram is shown in Fig. 6.

To obtain the desired configurations and joint velocities, we use inverse kinematics and the inverse of the Jacobian matrix. Since our testbed has two DoF and the control point is also two dimensional (x, y), there is a unique solution for the commanded cartesian position and velocity within the joints’ workspace. When we compute feedforward torque inputs, we use multi-body dynamics and an operational space control formulation.

\[
\tau = A \dot{J} \rightleftharpoons \ddot{x} - \dot{J} \dot{q} + b + g
\]

where A, b, and g represent inertia, coriolis, and gravity joint forces, respectively. \( \dot{q} \in \mathbb{R}^2 \) is the joint velocity of the leg and \( \tau \) is the joint torque. \( \dot{J} \) is a jacobian of the hip and \( \ddot{J} \) is a dynamically consistent pseudo inverse, which is defined as \( \ddot{J} = A^{-1} J^T (J A^{-1} J^T)^{-1} \). In our case, \( \dot{J} \) is a 2x2 square matrix and assumed to be full-rank. Therefore, the jacobian has a unique inverse, which means \( \ddot{J} = J^{-1} \).

Note that we need to account for reflected rotor inertia at the joint when computing the feedforward torque command. The current input is transferred to the joint through the drive-train and linkage described in Fig. 5(a) and Fig. 5(b). To account for the rotor inertia in the dynamics, we need to add a diagonal matrix representing these inertias.

\[
A_{\text{link+rotor}} = A_{\text{link}} + \begin{bmatrix}
  n_i^2 I_1 & 0 \\
  0 & n_i^2 I_2
\end{bmatrix}
\]

where \( I_i \) is a rotor inertia and \( n_i = N_i N_m \). Since \( N_i \) is a function of a joint position, \( n_i \) is not constant but changes as joint position changes. As stated in Section V-A, we obtain \( N_i \) by solving for the kinematics of the closed chain linkages shown in Fig. 5.

VI. RESULTS

In our experiments with the single leg testbed, we analyze 1) torque and position controllability, 2) motor core temperature behavior, 3) actuator efficiency, and 4) mechanical power and output torque. We note that the experimental numbers presented here are smaller than the maximum power and torque that the actuators can achieve because we used fairly conservative test motions to prevent damage to the actuators. For example, the datasheet lists the maximum core temperature for the Maxon motors we used of 155 °C, but all of our experiments were designed to maintain the motor core temperature below 90 °C. Even with this large safety factor, our results verify that the proposed hardware can achieve both controllability and high power and torque output competitive with the state-of-art.
A. Torque Feedback Control Test

We test with two types of experiments to demonstrate the performance of our torque feedback controller. First, we test the frequency response of three different feedback controllers with a single actuator. Second, we demonstrate cartesian space impedance control using PD + DOB torque feedback controllers with the single leg testbed. In both tests, we rely only on the rubber deflection measurement and do not use load cell data except for calibrating the rubber deflection offset. To obtain clear deflection data, we used accumulated quadrature encoder data (Fig. 1(l)) offset by the initial deflection measured by an absolute encoder (Fig. 1(m)) when the robot was powered on.

Fig. 7(a) shows the experimental results of our frequency response testing as well as the estimated response based on the transfer functions. We compare three types of controllers: \( \text{PD}_m \), \( \text{PID}_m \), and \( \text{PD}_m + \text{DOB} \). As we predicted in the analysis of Section VI-A, the \( \text{PD}_m + \text{DOB} \) controller shows less phase drop and overshoot than \( \text{PID}_m \). The integral control feedback gain used in the experiment is 300 and the cutoff frequency of DOB’s \( \tau_d \) is 60 Hz, which shows similar error to the \( \text{PID}_m \) controller (Fig. 7(b)).

Fig. 7(c) shows operational space impedance control, which is explained in Section V-B. The commanded behavior is to be compliant in the horizontal direction (\( x \)) and to be stiff in the vertical direction (\( y \)). When pushing the hip with a sponge in the \( x \) direction, the robot smoothly moves back to comply with the push, but it strongly resists the given vertical disturbance to maintain the commanded height. To show the stability of our controller, we also test impact by hitting the weight with a hammer. Even when there are sudden disturbances, the torque controllers rapidly respond to maintain good torque tracking performance as shown in Fig. 7(d).

B. Core Temperature of Motors

To show the motors’ core temperature behavior, we let the hip move up and down by 0.3 m with 1 Hz frequency until the core temperature reaches 80°C, which is much lower than the specified temperature limit (155°C). The results presented in Fig. 8 clearly show the different core temperature trends of the system with and without liquid cooling. In the latter case, the knee joint overheats in 70s, in contrast to the case with liquid cooling, which maintains the core temperature below 80°C for more than 250s, a \(~ 3.5x\) increase in the temperature-limited endurance.

C. Efficiency Analysis

The ratio of mechanical power output (force \( \times \) velocity) to electrical power input (voltage \( \times \) current) is widely used as a metric to characterize the efficiency of actuators, but it is informative to accurately decompose this efficiency measure into the multiple contributions of the major components of an actuator. For example, different electric motors show different efficiencies with respect to the input current and voltage. To correctly characterize drivetrain efficiency, we present both the ratio of mechanical power output versus the electric motor’s...
power and mechanical power output versus the power supply’s power input, all shown in Fig. 9.

Fig. 8 explains the power flow from the power supply to the robot joint. Input current ($I_b$) and voltage ($V_b$) are measured in the micro-controllers and the product of those two yields the input power from the power supply. $\theta_m$ is measured by the quadrature encoder connected to the motor’s axis (Fig. 10(a)) and $\tau_m$ is computed from $k_v i_m$ with $i_m$ measured in the micro-controller. Joint velocity is also computed based on the quadrature encoder of the motor to avoid errors emanating from any backlash at the joint. The torque ($\tau_k$) is computed from projecting the load cell data across the linkage’s effective moment arm.

In this test, the leg lifts a 23 kg load using five different durations to observe efficiency over a range of different speeds and torques. The results are presented in Fig. 9 with the description of three different power measures. The sensed torque data measured by a load cell is noisy; therefore, we compute the average of the drivetrain efficiency for a clearer comparison. The averages are the integrations of efficiency divided by the time durations. Here we only integrate efficiency while the mechanical power is positive, to prevent confounding our results by incorporating the work done by gravity.

The experimental results show that the drivetrain efficiency is approximately 0.93, which means that we lose only a small amount of power in the drivetrain and most of the torque from the motor is delivered to the joint. This high efficiency indicates only minor drivetrain friction, which is beneficial for dynamics-based motion controllers.

D. High Power Motion Experiment

When we demonstrate high power motions such as fast up and down motion and heavy payload lifting, we use the motor position control mode explained in Section IV-B. Fig. 10(a) presents the results of a test comprised of 2 Hz up and down motion with 0.32 m of travel while carrying a load of 10 kg at the hip. The joint position is measured by two sensors, which are the joint encoders and motor quadrature encoders. By careful inspection, one can see that joint encoder data is closer to the commanded trajectory than motor encoder data, which is accomplished by accounting for the rubber deflection when computing the motor position command. With respect to mechanical power, the knee joint repeatedly exerts 305 W, which is close to the predicted constant power (360 W). Although the limited range of motion makes it hard to demonstrate continuous mechanical power, this results convincingly support our claim about enhanced continuous power caused by liquid cooling.

Fig. 10(b) presents another test in which the leg lifts a 32.5 kg weight. Since the joint torque limits depend on the posture, we plot the linear actuator force instead to show how much capacity remains between the measured output and the theoretical force limit (7300 N). We can see that the leg operates in the safe region (≤ 7300 N and ≤ 155°C) while demonstrating high power motion.

VII. Conclusion

When devising the new viscoelastic liquid cooled actuation technology, we targeted high power, robust and accurate position control while protecting the drivetrain from external impact. We carefully designed the proposed actuator to satisfy various requirements for practical legged systems such as high torque and power density, high efficiency, high speed operation, and robustness to external disturbance. We have presented an experimental study with several viscoelastic materials and various key results with a single leg testbed which strongly support our performance claims.

The current testbed is fixed to the ground, but in the next study we plan to install a sliding platform as the body of the leg and to add one more VLCA at the hip joint to make it possible to jump. At the same time, we are upgrading the power supply in anticipation of the jumping experiment, since the current
power supply cannot provide a large enough electrical input to limit out the VLCAs. We expect that we can show more impressive energetic motions as well as empirically prove the actuator’s power and torque in future experiments.

**APPENDIX A**

**BACKGROUND**

Existing actuators can be categorized using four criteria: power source (electric or hydraulic), cooling type (air or liquid), elasticity of the drivetrain (rigid or elastic), and drivetrain type (direct, harmonic drive, ball screw, etc.) [2], [12]. One of the most powerful and common solutions is a hydraulic, liquid-cooled, rigid, and direct drive actuator, with a high power-to-weight ratio, torque-to-weight ratio, position controllability, and shock tolerance. Existing robots that use this type of actuator include Atlas, Spot, Big Dog, and Wildcat of Boston Dynamics, BLEEX of Berkeley [13], and HyQ of IIT [14], [15]. However, hydraulics are less energy efficient primarily because they require more energy transformations [16]. Typically, a gasoline engine or electric motor spins a pump, which compresses hydraulic fluid, which is modulated by a hydraulic servo valve, which finally causes a hydraulic piston to apply a force. Each stage in this process incurs some efficiency loss, and the total losses can be very significant.

Electric, air-cooled, rigid, and harmonic drive actuators are other widely used actuation types. Some robots utilizing these actuator types include Asimo of Honda, HRP2,3,4 of AIST [17], HUbo of KAISt [18], REEM-C of PAL Robotics, JOHNNIE and LOLA of Tech. Univ. of Munich [19], [20], CHIMP of CMU [21], Robosimian of NASA JPL [22], and more. These actuators have precise position control and high torque density. Additionally, they are significantly more energy efficient when compared to hydraulic actuation. On the other hand, low shock tolerance, low fidelity force sensing, and low efficiency gearboxes are common drawbacks of these actuators. According to Harmonic Drive AGs catalog, the efficiency of harmonic drives may be as poor as 25% and only increases above 80% when optimal combinations of input shaft speed, ambient temperature, gear ratio, and lubrication are present. [23] used liquid cooling for electric, rigid, harmonic drive actuators to enhance continuous power-to-weight ratio. The robots using this type of actuation include SCHAFT and Jaxon [24]. These actuators share the advantages and disadvantages of electric, rigid, harmonic drive actuators, but have a significant increase of the continuous power output and torque density. One of our studies [7] indicates a 2x increase in sustained power output by retrofitting an electric motor with liquid cooling. Other published results indicate a 6x increase in torque density through liquid cooling [12], [25], though such performance required custom-designing a motor specifically for liquid cooling. In our case we use an off-the-shelf electric motor.

Although the increased power density achieved via liquid cooling amplifies an electric actuators’ power, the rigid drivetrain is still vulnerable to external impacts, which are common in dynamic locomotion. To improve the impact tolerance, many robots (e.g. Walkman and COMAN of IIT [26], Valkyrie of NASA [27], MABEL and MARLO in UMich [28], [29], and StarETH of ETH [30]) adopt electric, air-cooled, elastic, harmonic drive actuators. This type of actuation provides high quality force sensing, force control, impact resistance, and energy efficiency. However, precise position control is difficult because of the elasticity in the drivetrain and the coupled effect of force feedback control and realtime latencies [9]. Low efficiency originating from the harmonic drives is another drawback.

As an alternative to harmonic drives, ball screws are great drives for mechanical power transmission. SAFFIR, THOR, and ESCHER of Virginia Tech [31]–[33], M2V2 of IHMC [34], Spring Flamingo of MIT [35], Hume of UT Austin [1], and the X1 Mina exoskeleton of NASA [36] use electric, air-cooled, elastic, ball-screw drives. These actuators show energy efficiency, good power and force density, low noise force sensing, high fidelity force controllability, and low backlash. However, most ball screw configurations make the actuator bulky. Another drawback is poor position controllability.

**Fig. 10. High power motion experiment.** (a) Joint position data from joint encoder and motor encoder are presented. When we watch closely, we can see the joint position measured by joint encoder is closer to the reference signal than joint position computed by motor encoder. This is the result of motor position offsets based on torque commands. In this experiment, the maximum observed torque of the ankle joint is 250 N·m and the maximum observed mechanical power of the knee joint is 310 W. (b) The leg lifts by 0.3 m a 32.5 kg load during 0.4 s. There is still large margin to the limit (7300 N, 150°C). However, we meet the limit of a power supply, which is 1500 W.
caused by the electric elements. There are some other actuators that have special features such as the electric actuators used in MIT’s cheetah [27], which allow for shock resistance through a transparent but backlash-prone drivetrain. In summary, nearly all existing actuators have at least one critical drawback with respect to the dynamic motion control of legged systems.

ACKNOWLEDGMENT

The authors would like to thank the members of the Human Centered Robotics Laboratory at The University of Texas at Austin for their great help and support. This work was supported by the Office of Naval Research, ONR Grant [grant #N000141512507] and NASA Johnson Space Center, NSF/NASA NRI Grant [grant #NNX12AM03G].

REFERENCES

[1] D. Kim, Y. Zhao, G. Thomas, B. R. Fernandez, and L. Sents, “Stabilizing Series-Elastic Point-Foot Bipeds Using Whole-Body Operational Space Control,” Transactions on Robotics, vol. 32, no. 6, pp. 1362–1379, 2016.
[2] N. A. Paine, “High-performance Series Elastic Actuation,” Ph.D. dissertation, Austin, 2014.
[3] N. Kashiri, G. A. Medrano-Cerda, N. G. Tsagarakis, M. Laffranchi, and C. Paul, and A. Ruina, “Low-bandwidth reflex-based control for lower leg of the hyq robot,” in International Conference on Intelligent Robots and Systems (IROS). IEEE, 2010, pp. 3640–3645.
[4] J. E. Pratt and B. Krupp, “Design of a bipedal walking robot,” in Proc. of American Control Conference (ACC). IEEE, 2009, pp. 2030–2036.
[5] A. B. Zoss, H. Kazerooni, and A. Chu, “Biomechanical design of the berkeley lower extremity exoskeleton (bleex),” Transactions On Mechatronics, vol. 11, no. 2, pp. 128–138, 2006.
[6] C. Semini, “Hyq-design and development of a hydraulically actuated quadruped robot,” Doctor of Philosophy (Ph. D.), University of Genoa, Italy, 2010.
[7] C. Semini, N. G. Tsagarakis, E. Guglielmino, and D. G. Caldwell, “Design and experimental evaluation of the hydraulically actuated prototype leg of the hyq robot,” in International Conference on Intelligent Robots and Systems (IROS). IEEE, 2010, pp. 3640–3645.
[8] P. A. Bhoumik, A. Cortell, A. Grewal, B. Hendriksen, J. D. Karssen, C. Paul, and A. Ruina, “Low-bandwidth reflex-based control for lower power walking: 65 km on a single battery charge,” The International Journal of Robotics Research, vol. 33, no. 10, pp. 1305–1321, 2014.
[9] N. Kanehira, T. Kawasaki, S. Ohra, T. Isumi, T. Kawada, F. Kanehiro, S. Kajita, and K. Kaneko, “Design and experiments of advanced leg module (hrp-2) for humanoid robot (hrp-2) development,” in International Conference on Intelligent Robots and Systems (IROS), vol. 3. IEEE, 2002, pp. 2455–2460.
[10] I.-W. Park, J.-Y. Kim, J. Lee, and J.-H. Oh, “Mechanical design of humanoid robot platform hpr-3 (kaist humanoid robot 3: Hubo),” in 5th International Conference on Humanoid Robots. IEEE, 2005, pp. 321–326.
[11] M. Gienger, K. Loffler, and F. Pfeiffer, “Towards the design of a biped jogging robot,” in International Conference on Robotics and Automation, vol. 4. IEEE, 2001, pp. 4140–4145.
[12] S. Lohmeier, T. Buschmann, H. Ulbrich, and F. Pfeiffer, “Modular joint design for performance enhanced humanoid robot lola,” in International Conference on Robotics and Automation. IEEE, 2006, pp. 88–93.
[13] A. Stenz, H. Herman, A. Kelly, E. Meyhofer, G. C. Haynes, D. Stager, B. Zajac, J. A. Bagnell, J. Brindza, C. Dellin et al., “Chimp, the cmu highly intelligent mobile platform,” Journal of Field Robotics, vol. 32, no. 2, pp. 209–228, 2015.
[14] S. Karunamachi, K. Edelberg, I. Baldwin, J. Nash, J. Reid, C. Bergh, J. Leichty, K. Carpenter, M. Shekels, M. Gildner et al., “Team robosimian: Semi-autonomous mobile manipulation at the 2015 darpa robotics challenges finals,” Journal of Field Robotics, vol. 34, no. 2, pp. 305–332, 2017.
[15] J. Urata, Y. Nakanishi, K. Okada, and M. Inaba, “Design of high torque and high speed leg module for high power humanoid,” in International Conference on Intelligent Robots and Systems. IEEE, 2010, pp. 4497–4502.
[16] K. Kojima, T. Karasawa, T. Kozuki, E. Kuroiwa, S. Yukiizaki, S. Iwaiashi, T. Ishikawa, R. Koyama, S. Noda, F. Sugai, S. Nozawa, Y. Kachiuchi, K. Okada, and M. Inaba, “Development of life-sized high-power humanoid robot JAXON for real-world use,” in 15th International Conference on Humanoid Robots. IEEE, 2015, pp. 838–843.
[17] F. Aghili, J. M. Hollerbach, and M. Buehler, “A modular and high-precision motion control system with an integrated motor,” Transactions on Mechatronics, vol. 12, no. 3, pp. 317–329, 2007.
[18] N. G. Tsagarakis, S. M. G. M. Cerda, L. Zhibin, and D. G. Caldwell, “Compliant humanoid coman: Optimal joint stiffness tuning for modular frequency control,” in International Conference on Robotics and Automation (ICRA). IEEE, 2015, pp. 673–678.
[19] N. A. Radford, P. Strawser, K. Hambuchen, J. S. Mehling, W. K. Verdeyen, A. S. Donnan, J. Holley, J. Sanchez, V. Nguyen, L. Bridgwater et al., “Valkyrie: Nasa’s first bipedal humanoid robot,” Journal of Field Robotics, vol. 32, no. 3, pp. 397–419, 2015.
[20] J. W. Grizzle, J. Hurst, B. Morris, H.-W. Park, and K. Sreenath, “Mabel, a new robotic bipedal walker and runner,” in American Control Conference (ACC). IEEE, 2009, pp. 2030–2036.
[21] A. Ramezani, “Feedback control design for marlo, a 3d-bipedal robot.”
[22] M. Hutter, M. Gehring, M. Bloesch, C. D. Remy, R. Siegwart, and M. A. Hoepflinger, “Starlet9: A compliant quadruped robot for fast, efficient, and versatile locomotion.” 2012.
[23] D. Lahr, V. Orekhov, B. Lee, and D. Hong, “Early developments of a parallelly actuated humanoid, safir,” in ASME 2013 international design engineering technical conferences and computers and information in engineering conference, 2013, pp. V00BT07A054–V00BT07A054.
[24] B. Lee, C. Knabe, V. Orekhov, and D. Hong, “Design of a humanoid-like range of motion hip joint for humanoid robots,” in International Design Engineering Technical Conferences and Computers and Information in Engineering Conference. American Society of Mechanical Engineers, Aug. 2014.
[25] C. Knabe, J. Seminatore, J. Webb, M. Hopkins, T. Furukawa, A. Leonessa, and B. Latimer, “Design of a series elastic humanoid for the darpa robotics challenge,” in 15th International Conference on Humanoid Robots (Humanoids). IEEE, 2015, pp. 738–743.
[26] J. Pratt and B. Krupp, “Design of a bipedal walking robot,” in Proc. of SPIE, vol. 6962, 2008, pp. 69621F–69621F.
[27] J. Pratt, “Exploiting inherent robustness and natural dynamics in the control of bipedal walking robots,” Massachusetts Inst. of Tech. Dept. of Electr. Eng. and Comp. Science, Tech. Rep., 2000.
[28] R. Rea, C. Beck, R. Rovekamp, M. Diftler, and P. Neuhaus, “X1: A robotic exoskeleton for in-space countermeasures and dynanometry,” 2013.
[29] S. Seok, A. Wang, M. Y. M. Chua, D. J. Hyun, J. Lee, D. M. Otten, J. H. Lang, and S. Kim, “Design principles for energy-efficient legged locomotion and implementation on the mit cheetah robot,” Transactions on Mechatronics, vol. 20, no. 3, pp. 1117–1129, 2015.