Rover Manipulator Position Control and Coring Feasibility Evaluation

by

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Abstract

Three robot position controllers have been investigated to determine their feasibility in the position control of an experimental rover manipulator. The controllers under investigation are joint proportional-derivative control (PD), joint proportional-integral-derivative control (PID), and base sensor control (BSC). Previously, BSC has only been implemented on fixed-base manipulators. Simulation and experimental verification studies indicate that BSC work just as well for the experimental rover manipulator developed at the Field and Space Robotics Lab at MIT. They also indicate that BSC is the best position controller for the experimental rover manipulator. BSC yields the smallest endpoint and joint position errors on virtually all tests. BSC also performs twice as well as PD and PID controllers in endpoint repeatability tests.

An investigation into the feasibility of rover manipulator coring has been carried out. Coring disturbance force/torque characterization tests have been conducted for four different types of rocks using a fixed-base ADEPT robot manipulator. Observations made during the coring of each rock are presented. The coring characterization results prove it would be possible for the experimental rover manipulator to exert the minimum required thrust force to core into the tested rocks. The obtained data also indicate that coring disturbance forces/torques do not significantly affect the performance of fixed-base manipulator coring. Hence, as long as the vehicle/base excitations resulting from rover manipulator coring are small, rover manipulator coring is feasible.
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<th>Description</th>
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<tr>
<td>{inert}</td>
<td>inertial coordinate frame</td>
</tr>
<tr>
<td>{-1}</td>
<td>vehicle base coordinate frame</td>
</tr>
<tr>
<td>{0}</td>
<td>manipulator base force/torque sensor coordinate frame</td>
</tr>
<tr>
<td>{1}</td>
<td>joint 1 coordinate frame</td>
</tr>
<tr>
<td>{2}</td>
<td>joint 2 coordinate frame</td>
</tr>
<tr>
<td>{3}</td>
<td>joint 3 coordinate frame</td>
</tr>
<tr>
<td>$c_h$</td>
<td>damping force input to base in vertical direction</td>
</tr>
<tr>
<td>$c_p$</td>
<td>damping torque input to base in pitch direction</td>
</tr>
<tr>
<td>$\alpha_1$</td>
<td>coefficient for friction model</td>
</tr>
<tr>
<td>$\alpha_2$</td>
<td>coefficient for friction model</td>
</tr>
<tr>
<td>$\alpha_3$</td>
<td>coefficient for friction model</td>
</tr>
<tr>
<td>$\beta$</td>
<td>coefficient for friction model</td>
</tr>
<tr>
<td>$\theta_1$</td>
<td>joint 1 position</td>
</tr>
<tr>
<td>$\theta_2$</td>
<td>joint 2 position</td>
</tr>
<tr>
<td>$\theta_{20}$</td>
<td>angle between x coordinate axis of frame ${1}$ and endpoint z location</td>
</tr>
<tr>
<td>$\theta_3$</td>
<td>joint 3 position</td>
</tr>
<tr>
<td>$\theta_{des}$</td>
<td>desired joint position, with respect to local joint coordinate frame</td>
</tr>
<tr>
<td>$\theta_p$</td>
<td>pitch of base</td>
</tr>
<tr>
<td>$\theta_{po}$</td>
<td>initial pitch of base</td>
</tr>
<tr>
<td>$\dot{\theta}$</td>
<td>actual joint velocity in motor block diagram</td>
</tr>
<tr>
<td>$\dot{\theta}_{act}$</td>
<td>actual joint velocity</td>
</tr>
<tr>
<td>$\dot{\theta}_{des}$</td>
<td>desired joint velocity</td>
</tr>
<tr>
<td>$\dot{\theta}_j$</td>
<td>angular velocity of joint j</td>
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<tr>
<td>$\ddot{\theta}$</td>
<td>actual joint acceleration in motor block diagram</td>
</tr>
<tr>
<td>$\ddot{\theta}_j$</td>
<td>angular acceleration of joint j</td>
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<td>$\tau_{act}$</td>
<td>output joint torque</td>
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<td>$\tau_{2act}$</td>
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<td>$\tau_{3act}$</td>
<td>output torque for joint 3</td>
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<tr>
<td>$\tau_{command}$</td>
<td>commanded joint torque</td>
</tr>
<tr>
<td>$\tau_{des}$</td>
<td>desired output joint torque</td>
</tr>
<tr>
<td>$\tau_h$</td>
<td>vertical force transmitted to base</td>
</tr>
<tr>
<td>$\tau_p$</td>
<td>torque transmitted to base in pitch direction</td>
</tr>
<tr>
<td>$A$</td>
<td>1x6 vector that maps base force/torque sensor readings into joint torque</td>
</tr>
<tr>
<td>$A_{mI}$</td>
<td>armature inertia of motor</td>
</tr>
</tbody>
</table>
\( A^{\text{inert}}_{-1} \) transformation matrix of frame \{-1\} relative to frame \{inert\}
\( A_{0}^{-1} \) transformation matrix of frame \{0\} relative to frame \{-1\}
\( A_{1} \) transformation matrix of frame \{1\} relative to frame \{0\}
\( A_{2}^{1} \) transformation matrix of frame \{2\} relative to frame \{1\}
\( A_{3}^{2} \) transformation matrix of frame \{3\} relative to frame \{2\}
\( C \) damping force constant for base in vertical direction
\( C_{0} \) damping torque constant for base in pitch direction
\( F \) linearized friction coefficient
\( F_{x}^{\text{base}} \) manipulator base sensor force in x direction
\( F_{y}^{\text{base}} \) manipulator base sensor force in y direction
\( F_{z}^{\text{base}} \) manipulator base sensor force in z direction
\( F_{x} \) magnitude of coring disturbance force in the x direction
\( F_{xy} \) magnitude of coring disturbance force in the x and y directions
\( F_{y} \) magnitude of coring disturbance force in the y direction
\( F_{z} \) magnitude of coring thrust force in the z direction
\( G_{i} \) location of centroid for link \( i \), with respect to coordinate frame \{i\}
\( I_{j} \) inertia tensor of link \( j \) taken at its centroid
\( I_{x} \) moment of inertia of vehicle base about its centroid x axis
\( I_{y} \) moment of inertia of vehicle base about its centroid y axis
\( I_{z} \) moment of inertia of vehicle base about its centroid z axis
\( I_{1x} \) moment of inertia of link 1 about its centroid x axis
\( I_{1y} \) moment of inertia of link 1 about its centroid y axis
\( I_{1z} \) moment of inertia of link 1 about its centroid z axis
\( I_{2x} \) moment of inertia of link 2 about its centroid x axis
\( I_{2y} \) moment of inertia of link 2 about its centroid y axis
\( I_{2z} \) moment of inertia of link 2 about its centroid z axis
\( I_{3x} \) moment of inertia of link 3 about its centroid x axis
\( I_{3y} \) moment of inertia of link 3 about its centroid y axis
\( I_{3z} \) moment of inertia of link 3 about its centroid z axis
\( K \) spring (stiffness) force constant for vehicle base in vertical direction
\( K_{0} \) spring (stiffness) torque constant for vehicle base in pitch direction
\( K_{\text{BSC}} \) base force control (BSC) controller gain
\( K_{d} \) derivative controller gain
\( K_{i} \) integral controller gain
\( K_{p} \) proportional controller gain
\( L_{0} \) vertical distance between origins of vehicle base and manipulator base force/torque sensor frames
\( L_{1} \) length of link 1
\( L_{2} \) length of link 2
\( L_{3} \) length of link 3
\( M_{d}^{O} \) dynamic moment component of base wrench
\( M_{x}^{\text{base}} \) manipulator base sensor dynamic moment about the x axis

Nomenclature
\( M^Y_{\text{base}} \) manipulator base sensor dynamic moment about the \( y \) axis
\( M^Z_{\text{base}} \) manipulator base sensor dynamic moment about the \( z \) axis
\( M_x \) magnitude of coring disturbance moment about the sensor \( x \) axis
\( M_y \) magnitude of coring disturbance moment about the sensor \( y \) axis
\( M_z \) magnitude of coring disturbance moment about the sensor \( z \) axis
\( N \) joint motor gear ratio
\( O_i \) origin for coordinate frame of link \( i \)
\( P^{\text{inert}}_{\text{-1}} \) translation component of \( A^{\text{inert}}_{\text{-1}} \)
\( R^{\text{inert}}_{\text{-1}} \) rotation component of \( A^{\text{inert}}_{\text{-1}} \)
\( R_1 \) distance to center of mass of link 1 from origin of coordinate frame \{0\}
\( R_2 \) distance to center of mass of link 2 from origin of coordinate frame \{1\}
\( R_3 \) distance to center of mass of link 3 from origin of coordinate frame \{2\}
\( \dot{V}_{Gj} \) center of mass acceleration for link \( j \)
\( W_{\text{base}}^{\text{inert}} \) manipulator base wrench
\( \rightarrow X \) manipulator endpoint position vector, with respect to inertial frame \{inert\}
\( \rightarrow X_{\text{-1}} \) manipulator endpoint position vector, with respect to base frame \{-1\}
\( \rightarrow X_3 \) manipulator endpoint position vector, with respect to frame \{3\}
\( X \) \( x \) direction orientation for coordinate frame
\( X_i \) \( x \) direction orientation for link frame \{i\}
\( X^{\text{inert}} \) manipulator endpoint \( x \) position, with respect to inertial frame \{inert\}
\( X_c \) \( x \) location for vehicle base center of mass, with respect to inertial frame \{inert\}
\( X_{1c} \) \( x \) location of link 1 center of mass, with respect to inertial frame \{inert\}
\( X_{2c} \) \( x \) location of link 2 center of mass, with respect to inertial frame \{inert\}
\( X_{3c} \) \( x \) location of link 3 center of mass, with respect to inertial frame \{inert\}
\( X_{dm} \) \( x \) coordinate resulting from manipulator endpoint position projected onto the XZ vehicle base frame
\( X_{dn} \) manipulator endpoint \( x \) position, with respect to vehicle base frame \{-1\}
\( Y \) \( y \) direction orientation for coordinate frame
\( Y_i \) \( y \) direction orientation for link frame \{i\}
\( Y^{\text{inert}} \) manipulator endpoint \( y \) position, with respect to inertial frame \{inert\}
\( Y_c \) \( y \) location for vehicle base center of mass, with respect to inertial frame \{inert\}
\( Y_{1c} \) \( y \) location for link 1 center of mass, with respect to inertial frame \{inert\}
\( Y_{2c} \) \( y \) location for link 2 center of mass, with respect to inertial frame \{inert\}
\( Y_{3c} \) \( y \) location for link 3 center of mass, with respect to inertial frame \{inert\}
\( Y_{dn} \) manipulator endpoint \( y \) position, with respect to vehicle base frame \{-1\}
\( Z \) \( z \) direction orientation for coordinate frame
\( Z_i \) \( z \) direction orientation for link frame \{i\}
\( Z^{\text{inert}} \) endpoint \( z \) position, with respect to inertial frame \{inert\}
\( Z_c \) \( z \) location for vehicle base center of mass, with respect to inertial frame \{inert\}
\( Z_{1c} \) z location for link 1 center of mass, with respect to inertial frame \{inert\}
\( Z_{2c} \) z location for link 2 center of mass, with respect to inertial frame \{inert\}
\( Z_{3c} \) z location for link 3 center of mass, with respect to inertial frame \{inert\}
\( Z_{dn} \) endpoint z position, with respect to vehicle base frame \{-1\}
\( a \) horizontal distance between origins of vehicle base \{-1\} and manipulator base force/torque sensor \{0\} frames
\( g \) gravity
\( h \) vertical distance between vehicle base frame \{-1\} and inertial frame \{inert\}
\( h_0 \) initial vertical distance between vehicle base frame \{-1\} and inertial frame \{inert\}
\( m \) mass of base
\( m_{total} \) total mass of the manipulator
\( m_1 \) mass of link 1
\( m_2 \) mass of link 2
\( m_3 \) mass of link 3
\( r \) distance between projections of manipulator endpoint and link 1 positions onto the XY plane of vehicle base frame \{-1\}
\( r_0 \) distance between \((X_{dn}, Z_{dn})\) and \((a,0)\)
\( v \) center of mass velocity for vehicle base, with respect to inertial frame \{inert\}
\( v_1 \) center of mass velocity for link 1, with respect to inertial frame \{inert\}
\( v_2 \) center of mass velocity for link 2, with respect to inertial frame \{inert\}
\( v_3 \) center of mass velocity for link 3, with respect to inertial frame \{inert\}
Chapter 1

Introduction

1.1 Background and Literature Review

One of the major applications of mobile manipulators is in space explorations. During the successful Mars Pathfinder mission, a 12-kg six-wheeled autonomous mobile robot called Sojourner surveyed and gathered data from its nearby surroundings. The success of Sojourner in demonstrating the viability of mobile robot exploration of Mars has led to plans for long range surface explorations that would require rovers with greater manipulation, mobility, autonomy, and general functionality (Volpe 1997).

The ultimate goal of the rover would be to autonomously survey planetary climate, life and resources over multiple kilometers and many months’ duration while optimizing use of available mission time and climatic conditions (Schenker 1998). Information gathered from Mars will be crucial to the understanding of Martian geology, mineralogy, and weather. They may reveal a possible history of life on Mars and indicate suitability of the Martian environment for future human habitation.

A prototype developed by the Jet Propulsion Laboratory (JPL) for future missions is the Field Integrated Design and Operations (FIDO) rover. FIDO is a 50+ kg, six-wheeled, high-mobility, multi-km range science vehicle equipped with a mast-mounted multi-spectral stereo camera, a bore-sighted mid-infrared point spectrometer, a robot arm with attached microscope, and a body-mounted rock sampling mini-corer (Schenker 1998). It can be seen in Figure 1.1.
The rover manipulator plays a pivotal role in gathering data and samples. In addition, rover manipulators can be used for construction, serve as a mobility aid for the rover, and assist in failure recovery of the rover. The various instruments that can be handled by the rover manipulator include seismometer, spectrometer, microscope, camera, and color micro-imager. To accurately place the science instruments, rover manipulators must be capable of overcoming joint friction. For rover manipulators, which are lightweight and highly geared, this could present a problem.

Friction presents a serious challenge to precise manipulator control. Failure to compensate for friction can lead to tracking errors when velocity reversals are demanded and oscillations when very small motions are required (Leonard 1992). Friction is a very complex phenomenon caused by one or more nonlinearities such as stiction, hysteresis, Stibbeck effect, and stick-slip. Friction can be dependent on velocity, input frequency, and vary as a function of temperature and time (Du 1999).

There is a wide range of friction compensation algorithms proposed. They include fix compensation schemes (Southward 1991), different disturbance observers to estimate the disturbance torque and compensate the friction, and adaptive friction
compensation (De Wit 1989, Baril 1997). There have also been research conducted targeting specific types of friction phenomena, such as stick-slip friction (Cai 1993, Tataryn 1996) and low-velocity friction with bounds (Du 1999). The most well-known models used in friction compensation include: viscous model, coulomb model, Dahl model, exponential model, bristle model, reset integrator model, state variable model, and bristle based dynamic model (Du 1999).

A simple method of friction compensation in the absence of a friction model is high gain compensation. This method has several drawbacks in that non-linearities will dominate any compensation for small errors and limit cycles may appear as a consequence of the dynamic interaction of the friction forces and high gain controllers (De Wit 1989). In addition, joint torque saturation and instability at low error values can occur.

Another method of friction compensation is to utilize measurements from joint torque sensors in torque feedback control. This approach has several drawbacks. Most industrial manipulators are not equipped with joint torque sensors and installing them would be difficult. Joint torque sensors are expensive and are subject to damage due to manipulator vibrations or overloads. A simple, cost-effective method has been developed for joint friction compensation using a six-axis force/torque sensor mounted on the base of the manipulator (Morel and Dubowsky 1996). From the base wrench measurements, the joint torques applied are estimated using the Newton-Euler equations of successive bodies and fed back through a torque controller, that virtually eliminates friction and gravity effects. This unique base force/torque sensor control approach is called base sensor control (BSC).
In addition to joint friction, the dynamic interaction between a manipulator and its vehicle also presents a problem in the endpoint position control of rover manipulators. Hootsman and Dubowsky developed an extended jacobian transpose control algorithm that addressed the issue of large motion control of mobile manipulators (Hootsman and Dubowsky 1991). It was shown to perform well in the presence of modeling errors and the practical limitations imposed by the sensory information available for control in highly unstructured field environments. Alvarez developed a strategy that accounted for mobile robot dynamics in sensor-based motion planning (Alvarez 1998). Tahboub developed an observer-based control for manipulators with moving bases that considers base vibrations as unknown disturbances (Tahboub 1997). Chung and Velinsky incorporated Dugoff's non-linear tire friction model in their dynamic modeling of the mobile manipulator system (Dugoff 1970, Chung and Velinsky 1998). They also addressed control issues associated with wheel slip on the tracking of commanded motion.

Huang and Sugano addressed the issue of motion planning for a mobile manipulator considering stability and task constraints (Huang 1993). Colbaugh addressed the issue of adaptive stabilization of uncertain mobile manipulator systems (Colbaugh 1998). Yamamoto and Yun investigated the effects of the dynamic interaction between the manipulator and its vehicle on the coordinated control of mobile manipulators and presented a nonlinear feedback control solution (Yamamoto and Yun 1996). Seraji dealt specifically with configuration control of rover-mounted manipulators and exploited the redundancy introduced by the rover mobility to perform a set of user-specified additional tasks during the end-effector motion (Seraji 1995).
Once the rover manipulator can accurately place an instrument against its target, it can then proceed to extract information from it. One of the best ways to extract information from subsurface materials is to extract and store it for further analysis back on Earth. This may involve the extraction of sample cores with a coring device.

There have been various coring systems developed and used for space exploration missions. Eiden and Coste addressed the challenge of sample acquisition in a cometary environment with regards to the Rosetta mission (Eiden and Coste 1991). The drill unit developed is mounted at the bottom of a stiff guide rail to provide maximum stiffness at the start of the coring process when uneven surface conditions could lead to high torque variations and extreme dynamic effects due to non-continuous cutting process (Eiden and Coste 1991).

The ESB drill developed under the Exploration of Small Body Task at JPL consists of three axes of operation: drill axis for penetration, rotation axis for drilling, and arm axis for indexing (Ghavimi 1998). The RDC/Athena mini-corer developed under the Robotic Drilling and Containerization Task consists of four axes of operation: drill axis for penetration, rotation axis for drilling, break off axis for core breaking, and push-rod axis for core ejection (Ghavimi 1998). Both the ESB and RDC/Athena mini-corer units are vehicle-mounted. In addition to the ESB and RDC/Athena mini-corer, there are other coring devices designed for the rover (Gorevan 1997).

The expected axial drilling force for a vehicle mounted coring unit is comparable to the weight of the rover. These coring units must be actively controlled to limit the effect of reaction forces/torques imposed by the interaction between the coring bit and the sampling surface (Ghavimi 1998). In order to prevent the excitation of structural modes...
during sampling operations and account for uncertainty introduced by the unknown properties of the sampled material, an effective coring/drilling initiation strategy and appropriate control algorithms need to be developed. Ghavimi has developed a control system architecture for the ESB and RDC/Athena mini-corer units (Ghavimi 1998).

Robot manipulator drilling have been addressed by Furness (Furness 1999) and Alici and Daniel (Alici and Daniel 1994, 1996). One of the key concerns shared by robot manipulator drilling and coring is the effect of limited joint torque on the amount of endpoint force that can be generated. To address this problem, research has been conducted in the area of large wrench application using robotic systems with limited force or torque actuator.

Madhani developed the Force Workspace approach (Madhani 1991). A $2^n$ tree recursive subdivision procedure was used to generate a global representation of the system’s static force exertion capabilities by mapping kinematics, force, and friction constraints into the system’s configuration space as constraint obstacles. Papadopoulos and Gonthier developed the Force-Task Workspace approach (Papadopoulos and Gonthier 1999). A $2^n$ tree decomposition of Cartesian space that utilizes base mobility and redundancy was used. They also considered the application of large wrench along a given path. These two methods indicate that by utilizing rover-manipulator configurations, it may be possible for the rover manipulator to exert enough end-effector force to core into samples such as bedrock.

1.2 Objective of this Thesis

In order for the rover manipulator to accurately place science instruments, an effective position controller is needed. Since rover manipulators are lightweight and
highly geared, they are subject to high joint friction. One of the goals of this thesis is to determine the feasibility of three controllers in the position control of an experimental rover manipulator. The three position controllers under investigation are: joint proportional-derivative feedback control (PD), joint proportional-integral-derivative feedback control (PID), and base sensor control (BSC).

Of the three position controllers investigated, only BSC was designed to compensate for joint friction by utilizing data from a six-axis force/torque sensor mounted at the base of the manipulator. BSC has been successfully implemented on fixed-base industrial Schilling and PUMA 550 robots and have been shown to outperform standard position controllers in fine motion manipulator tasks (Morel 1996, Meggiolaro 1999). BSC has yet to be implemented on a mobile-base manipulator. Thus, one of the concerns in implementing BSC on a rover manipulator was whether the dynamic interaction between the manipulator and vehicle base would adversely affect its performance.

Analytical and experimental rover manipulator position control studies have been performed using the three controllers. An experimental rover designed at the Field and Space Robotics Lab (FSRL) at MIT was used to verify the feasibility of the proposed controllers in the accurate position control of a rover manipulator (Burn 1998, Wilhelm 1999). The results from those studies indicate that BSC is the best position controller for the experimental rover manipulator. In addition, endpoint repeatability tests were experimentally performed using the three controllers. Again, BSC performed the best. These studies also indicate that the dynamic interaction between the manipulator and
rover base during the commanded tasks were small enough that the performance of BSC was not compromised.

Another goal of this thesis is to determine the feasibility of rover manipulator coring. Coring units available today consist of large devices attached to the rover body. These units often increase the weight and complexity of the rover system and do not utilize the rover manipulator's capability in exerting large endpoint forces.

Before rover manipulator coring can be deemed feasible, it is important to know whether the limitations of the manipulator in applying large endpoint forces and coring disturbance forces/torques would make it unattractive. Due to the limitations of joint torque, the maximum endpoint force a manipulator can apply is limited. By utilizing coordinated motion between the rover body and the manipulator, this may allow the mobile manipulator to exert the required endpoint force for coring.

The FSRL rover manipulator was designed to exert an endpoint force equivalent to one-half the rover weight in bent position (Burn 1998). That force is 4 lbs. Thus, a focus of this research is to determine whether the FSRL rover will be capable of exerting the minimum required coring force to core into materials similar in properties to Martian terrain.

However, just because a rover manipulator may be capable of exerting the required coring force does not necessarily mean it will core successfully. Disturbance forces/torques resulting from the interaction between the coring bit and sampled material, and disturbance forces/torques introduced to the rover base can affect how successful a rover manipulator cores. Thus, another focus in the area of robot manipulator coring is on the characterization of coring disturbance forces/torques. To characterize the coring
disturbance force/torques, an industrial SCARA ADEPT robot, with a force torque sensor and coring unit attached to its end-effector, was used.

Results from coring disturbance force/torque characterization tests indicate that the FSRL rover manipulator is capable of exerting the minimum required coring force to core into the four different rocks tested. The disturbance forces/torques encountered remained relatively small in magnitudes that they did not adversely affect the ADEPT's coring performance. Thus, if the rover manipulator’s vehicle base were not significantly excited during coring, the rover manipulator should have no problem coring into the same test rocks and is indeed feasible.

1.3 Outline of Thesis

This thesis is divided into five chapters. This chapter serves as an introduction and overview of the work. In determining the feasibility of three controllers for the position control of the FSRL rover manipulator, a theoretical model of the rover-manipulator system was obtained along with experimentally derived joint friction models. They were used in the simulation studies and are presented in Chapter 2. In Chapter 3, experimental verifications of the simulation results are presented along with results for endpoint repeatability tests. In Chapter 4, coring disturbance force/torque characterization results are presented and a conclusion arrived at the feasibility of rover manipulator coring. In Chapter 5, the contributions of this thesis are reviewed and suggestions for future research presented.
Chapter 2

Analytical Study of Rover Manipulator Position Control

2.1 Introduction

Simulation studies were performed using PD, PID, and BSC controllers to determine which one would be most feasible in the position control of the FSRL rover manipulator. The theoretical background on the controllers under investigation is presented in Section 2.2. The analytical model of the rover manipulator is presented in Section 2.3. Simulation results are presented in Section 2.4.

2.2 Position Controllers Investigated

A simple joint proportional-derivative (PD) feedback torque controller has the following form:

$$\tau_{\text{command}} = \tau_{\text{des}} = K_p (\theta_{\text{des}} - \theta_{\text{act}}) + K_d \left( \dot{\theta}_{\text{des}} - \dot{\theta}_{\text{act}} \right)$$ (2.1)

where $\tau_{\text{command}}$ is the commanded joint torque, $\tau_{\text{des}}$ is the desired joint torque, $K_p$ is the proportional gain, $K_d$ is the derivative gain, $\theta_{\text{des}}$ is the desired joint position, $\theta_{\text{act}}$ is the actual joint position, $\dot{\theta}_{\text{des}}$ is the desired joint velocity (which is equal to zero), and $\dot{\theta}_{\text{act}}$ is the actual joint velocity. The PD controller is neither capable of rejecting joint torque disturbances nor capable of attaining zero steady-state position error.

A simple joint proportional-integral-derivative (PID) feedback torque controller has the following form, where $K_i$ is the integral gain:

$$\tau_{\text{command}} = \tau_{\text{des}} = K_p (\theta_{\text{des}} - \theta_{\text{act}}) + K_d \left( \dot{\theta}_{\text{des}} - \dot{\theta}_{\text{act}} \right) + K_i \int (\theta_{\text{des}} - \theta_{\text{act}}) \, dt$$ (2.2)
A PID controller is capable of rejecting linear joint torque disturbances and attaining zero steady-state position error. However, its performance is degraded in the presence of nonlinear joint torque disturbances.

The control system architecture of base sensor control, BSC, can be seen in Figure 2.1.

![Figure 2.1 BSC Control System Architecture (Morel 1996)](image)

BSC utilizes data from a six-axis force/torque sensor mounted at the base of the manipulator and the Newton-Euler equations of successive bodies to estimate the output joint torque. BSC is capable of rejecting nonlinear joint torque disturbances, namely friction. BSC control consists of an outer position feedback torque loop and an inner integral loop with feedforward friction compensation (Morel 1996). It has the following form:

\[
\tau_{\text{command}} = \tau_{\text{des}} + K_{\text{bsc}} \int (\tau_{\text{des}} - \tau_{\text{act}}) \, dt
\]

(2.3)

where \(K_{\text{bsc}}\) is the BSC gain and \(\tau_{\text{des}}\) is the desired torque, which can be a PD controller. The estimated output joint torque, \(\tau_{\text{act}}\), is computed from base force/torque sensor readings using the link frame orientation assignment seen in Figure 2.2.
\[ \tau_{i+1} = -z_i^T \left[ M_d^O_i + \sum_{j=1}^{i} \left( I_j \ddot{\theta}_j + \dot{\theta}_j \times I_j \dot{\theta}_j + O_j G_j \times m_j V_G_j \right) \right] \] (2.4)

where \( M_d^O_i \) is the dynamic moment component of the manipulator base wrench, \( I_j \) is the inertia tensor of link \( j \) about its center of mass, \( m_j \) is the mass of link \( j \), \( V_{G_j} \) is the center of mass acceleration of link \( j \), \( \ddot{\theta}_j \) is the angular acceleration of link \( j \), and \( \dot{\theta}_j \) is the angular velocity of link \( j \). For the small joint motion case, Equation 2.4 can be written as:

\[ \tau_{\text{act}} = A(\theta) W_{\text{base}}^O_i \] (2.5)

where the estimated output torque, \( \tau_{\text{act}} \), is computed via a static transformation of the manipulator base wrench, \( W_{\text{base}}^O_i \), to joint \( i \) using the matrix \( A(\theta) \) (Iagnemma 1997). The manipulator base wrench is defined as:
where \( F_{base}^x, F_{base}^y, F_{base}^z, M_{base}^x, M_{base}^y, \) and \( M_{base}^z \) are the dynamic force and moment components of the manipulator base wrench at the respective axis.

2.3 Analytical Model of Rover Manipulator

2.3.1 Rover Manipulator Model

The simulation rover manipulator system is modeled after the FSRL experimental rover manipulator, which can be seen in Figure 2.3.

Figure 2.3 Experimental Rover Manipulator System (Burn 1998)

A simplified rover manipulator system model used in the simulation can be seen in Figure 2.4.
The rover manipulator has 3 degrees of freedom (DOF) and the base has 6 DOF. The combined rover manipulator system has 9 DOF. To model the rover manipulator system for simulations, the Denavit-Hartenberg frame assignment convention presented by Asada was used (Asada 1986). It can be seen in Figure 2.5.

The non-moving inertial reference frame is denoted by \{\text{inert}\}, the vehicle/base frame by \{-1\}, the manipulator base force/torque sensor frame by \{0\}, link 1 frame by...
\{1\}, link 2 frame by \{2\}, and link 3 frame by \{3\}. The location of the third axis, not shown, and positive direction of rotation for each coordinate frame are assigned using the right hand rule. For simplicity, in the simulation studies, the location of the base center of mass is placed along the rocker joint axis. This axis corresponds to the Y axis of the base frame \{-1\}. The rocker joint is the joint through which the rocker-bogie wheels pivot about the rover body (Burn 1998). The rotational displacement of the base about the rocker joint axis will be referred to as the pitch angle.

The side and top views of the rover manipulator can be seen in Figures 2.6 and 2.7, respectively.

![Figure 2.6 Side View of Rover Manipulator System](image)

![Figure 2.7 Top View of Rover Manipulator System](image)
where the length of link 1 is \( L_1 \), link 2 is \( L_2 \), and link 3 is \( L_3 \). \( L_0 \) is the vertical distance between the base frame \{-1\} and the sensor frame \{0\}, \( a \) is the horizontal distance between the base frame \{-1\} and the sensor frame \{0\}, and \( h \) is the vertical distance between the base frame \{-1\} and the inertial frame \{inert\}. \( \theta_1 \), \( \theta_2 \), and \( \theta_3 \) correspond to the angles of rotation for joints 1, 2, and 3, respectively, and \( \theta_p \) is the pitch angle. The distance from the \( i-1 \) coordinate frame to the center of mass of the \( i \) link is denoted by \( R_i \).

For simulations, the vehicle base is modeled as a spring-mass-damper with the damping of the vehicle due to the differential mounted along the rocker joint axis. In Figure 2.6, the stiffness and damping of the base in the vertical direction are denoted by \( K \) and \( C \). Rotational stiffness and damping of the vehicle base in the pitch direction are denoted by \( K_0 \) and \( C_0 \).

For BSC control, \( A(0) \) for the rover manipulator, using the notations presented in Figures 2.5 - 2.7, is:

\[
\begin{bmatrix}
A_1(\theta) \\
A_2(\theta) \\
A_3(\theta)
\end{bmatrix} =
\begin{bmatrix}
0 & 0 & 0 & 0 & 0 & -1 \\
-L_1 \cos \theta_1 & -L_1 \sin \theta_1 & 0 & -\sin \theta_1 \cos \theta_1 \cos \theta_1 & 0 \\
-(L_1 + L_2 \sin \theta_2) \cos \theta_1 & -(L_1 + L_2 \sin \theta_2) \sin \theta_1 & L_2 \cos \theta_2 & -\sin \theta_1 \cos \theta_1 & 0
\end{bmatrix}
\] (2.7)

Plugging Equations 2.6 and 2.7 into Equation 2.5 yield the estimated output torques at joints 1 (\( \tau_{1act} \)), 2 (\( \tau_{2act} \)), and 3 (\( \tau_{3act} \)):

\[
\tau_{1act} = -M^Z_{base}
\] (2.8)

\[
\tau_{2act} = -M^X_{base} \sin \theta_1 + M^Y_{base} \cos \theta_1 - F^X_{base} L_1 \cos \theta_1 - F^Y_{base} L_1 \sin \theta_1
\] (2.9)

\[
\tau_{3act} = -M^X_{base} \sin \theta_1 + M^Y_{base} \cos \theta_1 - F^X_{base} (L_1 + L_2 \sin \theta_2) \cos \theta_1 - F^Y_{base} (L_1 + L_2 \sin \theta_2) \sin \theta_1 + F^Z_{base} L_2 \cos \theta_2
\] (2.10)
2.3.2 Simulation Software Architecture

There are two ways of deriving the equations of motion for the rover manipulator: the Lagrange energy method and the Newton-Euler iteration method. A step by step approach in deriving the Lagrange equations of motion for the rover manipulator system under investigation can be found in Appendix A.

The software used in running the simulations is called RIBS and it was written by Miguel Torres and Norbert Hootsman (Torres and Hootsman 1992). RIBS derives the equations of motion using the Newton-Euler iterative method. It utilizes the on-line computational scheme for mechanical manipulators presented by Luh, Walker, and Paul for inverse dynamics calculations (Luh, Walker, and Paul 1980) and the computational scheme presented by Walker and Orin for forward dynamics calculations (Walker and Orin 1982). The required inputs to the program are the physical parameters for the rover-manipulator system and the Denavit-Hartenberg frame assignments.

In RIBS, the initial joint positions can either be user specified or calculated via inverse kinematics if given initial endpoint positions relative to the inertial frame. The initial base positions are then calculated. An iteration scheme is then invoked until the time allotted for the manipulator to complete the commanded task has been reached. The order of operation during a single pass through this program is outlined as follows:

- Base forces/torques are first calculated. Then the endpoint trajectory generation algorithm is invoked to calculate the desired endpoint positions and velocities as a function of time. Inverse kinematics is invoked to derive the corresponding joint positions and velocities. The commanded/input torque to each joint is then calculated using the controller specified.
• Actuator effects are then calculated. These included joint torque saturation and joint friction torque. The output torque is then calculated for all joints.

• Lastly, the Runge-Kutta 4th order integration is invoked to calculate the next joint and base accelerations. During this step, the Newton-Euler iteration scheme and joint acceleration extraction algorithm are used. Basically, the acceleration extraction process involves two steps: 1. the gravity and centripetal/coriolis torques (B) are first derived and the difference between the output torque (A) and it is then calculated; 2. the inertial torque matrix (C) is then derived. The acceleration of the base and links is simply $C^{-1}(A-B)$.

During the entire iteration process, forward kinematics is used to calculate the end-effector position with respect to the inertial frame. This is done using transformation matrices calculated using the Denavit-Hartenberg frame assignment notation and can be found in Appendix B.

The physical parameters for the manipulator and joint motors used in the simulation are listed in Table 2.1.

<table>
<thead>
<tr>
<th>$L_1$ (m)</th>
<th>$0.0508$</th>
<th>$A_{m1}$ (kgm$^2$)</th>
<th>$5.93 \times 10^{-5}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$R_1$ (m)</td>
<td>$0.0127$</td>
<td>$A_{m2}$ (kgm$^2$)</td>
<td>$5.93 \times 10^{-5}$</td>
</tr>
<tr>
<td>$L_2$ (m)</td>
<td>$0.1016$</td>
<td>$A_{m3}$ (kgm$^2$)</td>
<td>$2.68 \times 10^{-5}$</td>
</tr>
<tr>
<td>$W_2$ (m)</td>
<td>$0.03604$</td>
<td>$I_{x1}$ (kgm$^2$)</td>
<td>$0.00004$</td>
</tr>
<tr>
<td>$H_2$ (m)</td>
<td>$0.03604$</td>
<td>$I_{y1}$ (kgm$^2$)</td>
<td>$0.05287$</td>
</tr>
<tr>
<td>$L_3$ (m)</td>
<td>$0.1016$</td>
<td>$I_{z1}$ (kgm$^2$)</td>
<td>$0.00004$</td>
</tr>
<tr>
<td>$W_3$ (m)</td>
<td>$0.02167$</td>
<td>$I_{x2}$ (kgm$^2$)</td>
<td>$0.00004$</td>
</tr>
<tr>
<td>$H_3$ (m)</td>
<td>$0.02167$</td>
<td>$I_{y2}$ (kgm$^2$)</td>
<td>$0.00017$</td>
</tr>
<tr>
<td>$M_1$ (kg)</td>
<td>$0.172$</td>
<td>$I_{z2}$ (kgm$^2$)</td>
<td>$0.56746$</td>
</tr>
<tr>
<td>$M_2$ (kg)</td>
<td>$0.172$</td>
<td>$I_{x3}$ (kgm$^2$)</td>
<td>$0.00001$</td>
</tr>
<tr>
<td>$M_3$ (kg)</td>
<td>$0.086$</td>
<td>$I_{y3}$ (kgm$^2$)</td>
<td>$0.00008$</td>
</tr>
<tr>
<td>$N_1$ (Gear Ratio)</td>
<td>944</td>
<td>$I_{z3}$ (kgm$^2$)</td>
<td>$0.23543$</td>
</tr>
<tr>
<td>$N_2$ (Gear Ratio)</td>
<td>3092.6</td>
<td></td>
<td></td>
</tr>
<tr>
<td>$N_3$ (Gear Ratio)</td>
<td>2961.7</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
where $A_m I_i$ is the armature inertia of the motor for link $i$, $N$ is gear ratio, $I_{xi}, I_{yi}, I_{zi}$ are the moments of inertia about the center of mass of link $i$ plus armature inertia $x N^2$. It should be noted that specifications listed for link 3 in Table 2.1 were modified accordingly in the simulations to take into account an attached science instrument.

### 2.3.3 Joint Friction Models

An experimentally derived friction model in the form proposed by De Wit (De Wit 1989) was obtained for each manipulator joint. It has the following form:

$$
\tau_{\text{friction}} = a_0 + a_1 |\dot{\theta}| + a_2 e^{-\beta |\dot{\theta}|} \text{ sign}(\dot{\theta})
$$

(2.11)

where $\tau_{\text{friction}}$ is the joint friction torque, and $a_0, a_1, a_2$, and $\beta$ are positive constants selected to yield the estimated friction torque profile.

The FSRL rover manipulator is voltage controlled. To obtain the Coulomb friction torque for each joint, small amplitude open loop voltage sinusoids were issued for one joint at a time. The commanded voltage and output joint position and velocity were recorded. The amplitude and frequency of the voltage sinusoids were varied and the test repeated for each joint. By plotting joint position versus time and commanded voltage versus time, it was possible to find the voltage that initiated joint movement. Since both plots were plotted with respect to time, simply note the time at which the joint position started to change from its initial position and record the corresponding commanded voltage at that time. This voltage was then multiplied with the PWM amplifier constant to arrive at the joint motor input voltage. The input voltage was then converted to torque using a conversion constant.

Joint 1 friction torque model can be seen in Figure 2.8 and is defined by:
\[ r_{1\text{friction}} = \left( 0.02 + 0.0003|\dot{\theta}_1| + 0.005e^{-4|\theta_1|} \right) \text{sign}\left( \dot{\theta}_1 \right) \]  

(2.12)

Figure 2.8 Joint 1 Friction Model

Joint 2 friction torque model can be seen in Figure 2.9 and is defined by:

\[ r_{2\text{friction}} = \left( 0.024 + 0.0003|\dot{\theta}_2| + 0.005e^{-4|\theta_2|} \right) \text{sign}\left( \dot{\theta}_2 \right) \]  

(2.13)

Figure 2.9 Joint 2 Friction Model
Joint 3 friction torque model can be seen in Figure 2.10 and is defined by:

\[
\tau_{3\text{friction}} = \left( 0.015 + 0.0003 |\dot{\theta}_3| + 0.005 e^{-|\dot{\theta}_3|} \right) \text{sign} (\dot{\theta}_3)
\]  \hspace{1cm} (2.14)

![Figure 2.10 Joint 3 Friction Model](image)

2.3.4 Feedback Modeling of Controllers

The controllers are designed to yield a natural frequency of 2 Hz and a damping ratio of 0.7. The PD controlled closed-loop block diagram used to design the controller gains can be seen in Figure 2.11.

![Figure 2.11 PD Controller Block Diagram with Linear Joint Friction](image)

The closed loop transfer function is:
\[
\frac{\theta_{\text{act}}}{\theta_{\text{des}}} = \frac{K_d s + K_p}{J s^2 + (F + K_d) s + K_p}
\]  \hspace{1cm} (2.15)

where \(\theta_{\text{des}}\) and \(\theta_{\text{act}}\) are the desired and actual joint positions, respectively, \(J\) is the moment of inertia taken about the joint axis of rotation, \(\dot{\theta}\) is the actual joint velocity, \(\ddot{\theta}\) is the actual joint acceleration, and \(F\) is the linearized friction coefficient. The linearized friction model, for the purpose of linear controller design, was chosen to be \(F \dot{\theta}\).

For the PID controller, its block diagram can be seen in Figure 2.12.

\[
\frac{\theta_{\text{act}}}{\theta_{\text{des}}} = \frac{K_d s^2 + K_p s + K_i}{J s^2 + (F + K_d) s^2 + K_p s + K_i}
\]  \hspace{1cm} (2.16)

To design the BSC gains, the following model was used:

\[
\frac{\dot{\theta}}{\theta_{\text{des}}} = \frac{K_d s + K_p}{J s^2 + (F + K_d) s^2 + K_p s + K_i}
\]
Note, this is one way of designing BSC gains. There is no easy way to design for BSC gains using linear control theory, since BSC is a nonlinear controller. The closed loop transfer function for the BSC block diagram is given by:

\[
\frac{\theta_{\text{act}}}{\theta_{\text{des}}} = \frac{K_d s + K_p}{J s^2 + (F + K_d)s + (K_p - K_{\text{bsc}} F)}
\]

(2.17)

where \(K_{\text{bsc}}\) is the BSC gain.

2.4 Rover Manipulator Position Control Simulation Results

In the simulation, the rover manipulator was commanded to accurately place a sampling instrument against targets that lie along an endpoint equilateral triangle trajectory.

![Figure 2.14 Rocky Rover Scanning Target (Volpe 1998)](image)

The rover manipulator was also commanded to stop at each vertex and midpoint along each side of the triangle path. From the desired endpoint trajectory, the desired joint trajectories were calculated using inverse kinematics, which is provided in Appendix B.

A series of three sets of tests were performed. The first set of test is comprised of Tests 1 and 2, the second set of test is comprised of Tests 3 and 4, and the third set of test is comprised of Tests 5 and 6. For any given set, the same endpoint and joint trajectories were commanded, the only difference being that the second test of each set takes twice as
long to complete. This was done to compare the performance of the controller at different joint velocities. These tests were designed to target individual joints to see how well the controllers were able to compensate for joint friction.

It was expected that during slow motion trajectory tracking, PD and PID controllers would have difficulty compensating for joint friction and the resulting joint position and velocity profiles would not closely match the desired profiles. It was expected that under the same test, BSC would yield resulting joint position and velocity profiles that would closely match the desired ones.

It was anticipated that the dynamic interaction between the rover manipulator and vehicle base would cause endpoint errors in the inertial frame if not taken into consideration. For the experimental rover, it was possible to obtain an online reading of the change in the pitch of the rover base, its greatest DOF. Since the experimental rover has limited base-sensing capabilities (through online accelerometer readings and pitch calculation), if the position of the vehicle base can be reliably measured, it will be compensated for in the inverse kinematics, joint trajectory generation algorithm.

To provide realistic comparisons, the limitations imposed by the sensory information available for the experimental rover was incorporated into the simulations. That is, when the motion of the vehicle base could not be reliably measured, it was not compensated for in the joint trajectory generation algorithm. For such a case, the issued commanded trajectories will be calculated using initial base positions.

Since FIDO was designed to yield a maximum endpoint error of approximately 5.0 mm for its workspace radius of 0.7 m (Schenker (2) 1998), the maximum allowable endpoint error for the experimental rover was chosen to be 1.5 mm given its workspace...
radius of 0.21 m (Burn 1998). From experiments, the base pitch angle can be reliably measured to within 0.5°. Thus, if and when the pitch displacement is less than 0.5° from the initial position, it will not be taken into consideration in the trajectory generation algorithm. All endpoint and joint errors were calculated with respect to the commanded trajectories. For base pitch displacement of as little as 0.5°, the endpoint error in the inertial frame can exceed the desired maximum error.

In the first set of tests carried out, the initial joint positions for the manipulator were as follows: 0° for joint 1, 30° for joint 2, and −70° for joint 3. The length of each side of the equilateral triangle the manipulator was commanded to track was 2.5 cm and the time allotted for the completion of the trajectory was 30 seconds. For Test 2, the commanded endpoint and joint trajectories were identical to Test 1, except it took twice as long to complete. Figures 2.15.1-2.15.3 show the desired endpoint trajectory, the desired joint trajectories, and the desired joint velocities for Test 1.

Figure 2.15.1 Desired Endpoint Trajectory for Tests 1 and 2
Chapter 2 Analytical Study of Rover Manipulator Position Control
In the second set of tests, the initial positions for the manipulator were as follows: 0° for joint 1, 45° for joint 2, and -90° for joint 3. The length of the triangle traversed was doubled to 5.0 cm and the time to complete the trajectory was also doubled to 60 seconds. For Test 4, the commanded endpoint and joint trajectories were identical to Test 3, except it took twice as long to complete. Figures 2.16.1-2.16.3 show the desired endpoint trajectory, the desired joint trajectories, and the desired joint velocities for Test 3.

![Figure 2.16.1 Desired Endpoint Trajectory for Tests 3 and 4](image-url)
Figure 2.16.2 Desired Joint Trajectories for Test 3

Figure 2.16.3 Desired Joint Velocities for Test 3
For the last set of tests performed, the initial joint positions for the manipulator were as follows: 0° for joint 1, 15° for joint 2, and -90° for joint 3. Like Test 3, the length of each side of the triangle traversed was 5.0 cm and the time to complete the trajectory was 60 seconds. For Test 6, the commanded endpoint and joint trajectories were identical to Test 5, except it took twice as long to complete. Figures 2.17.1-2.17.3 show the desired endpoint trajectory, the desired joint trajectories, and the desired joint velocities for Test 5.

Figure 2.17.1 Desired Endpoint Trajectory for Tests 5 and 6
Since the results obtained for the six tests were similar, the results for Test 1 will be presented in detail. The endpoint and joint errors along the commanded trajectory for
the various controllers during Test 1 can be seen in Figures 2.18-2.20.

Figure 2.18 Desired vs Actual Simulation Endpoint Trajectory Tracking for PD

Figure 2.19 Desired vs Actual Simulation Endpoint Trajectory Tracking for PID
The endpoint and joint errors at commanded stops (at midpoint of each triangle side and all vertices) for the various controllers during Test 1 can be seen in Figures 2.21-2.24.

Figure 2.20 Desired vs Actual Simulation Endpoint Trajectory Tracking for BSC

Figure 2.21 Endpoint Error at Commanded Stops for Simulation Test 1
Figure 2.22 Joint 1 Error at Commanded Stops for Simulation Test 1

Figure 2.23 Joint 2 Error at Commanded Stops for Simulation Test 1
Based on the results for Test 1, BSC showed much improvement over PD and PID controllers. It should be noted that for all six tests, the displacement of the base from its initial value never exceeded $0.3^\circ$ and the displacement of the base position in its other 5 DOFs were negligible ($\sim0.0^\circ$, $\sim0.0$ mm). Thus, the error introduced by the motion of the base was not compensated for. All errors calculated and presented for the simulations were with respect to the commanded trajectories, which were calculated using the initial base positions. The root mean square (RMS) errors for all tests are tabulated in Tables 2.2-2.5.

Table 2.2 Endpoint RMS Error Results for Simulation Tests 1-6

<table>
<thead>
<tr>
<th>Endpoint RMS (mm)</th>
<th>Test 1</th>
<th>Test 2</th>
<th>Test 3</th>
<th>Test 4</th>
<th>Test 5</th>
<th>Test 6</th>
</tr>
</thead>
<tbody>
<tr>
<td>PD</td>
<td>0.6</td>
<td>0.6</td>
<td>0.6</td>
<td>0.6</td>
<td>0.6</td>
<td>0.6</td>
</tr>
<tr>
<td>PID</td>
<td>0.4</td>
<td>0.3</td>
<td>0.3</td>
<td>0.2</td>
<td>0.3</td>
<td>0.3</td>
</tr>
<tr>
<td>BSC</td>
<td>0.2</td>
<td>0.2</td>
<td>0.2</td>
<td>0.2</td>
<td>0.3</td>
<td>0.3</td>
</tr>
</tbody>
</table>
Table 2.3 Joint 1 RMS Error Results for Simulation Tests 1-6

<table>
<thead>
<tr>
<th>Endpoint RMS (°)</th>
<th>Test 1</th>
<th>Test 2</th>
<th>Test 3</th>
<th>Test 4</th>
<th>Test 5</th>
<th>Test 6</th>
</tr>
</thead>
<tbody>
<tr>
<td>PD</td>
<td>0.1512</td>
<td>0.1526</td>
<td>0.1543</td>
<td>0.1525</td>
<td>0.1532</td>
<td>0.1522</td>
</tr>
<tr>
<td>PID</td>
<td>0.0948</td>
<td>0.0649</td>
<td>0.0674</td>
<td>0.0474</td>
<td>0.0674</td>
<td>0.0568</td>
</tr>
<tr>
<td>BSC</td>
<td>0.0235</td>
<td>0.0118</td>
<td>0.0193</td>
<td>0.0296</td>
<td>0.0212</td>
<td>0.0166</td>
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</tbody>
</table>

Table 2.4 Joint 2 RMS Error Results for Simulation Tests 1-6

<table>
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<tr>
<th>Endpoint RMS (°)</th>
<th>Test 1</th>
<th>Test 2</th>
<th>Test 3</th>
<th>Test 4</th>
<th>Test 5</th>
<th>Test 6</th>
</tr>
</thead>
<tbody>
<tr>
<td>PD</td>
<td>0.0699</td>
<td>0.0557</td>
<td>0.0723</td>
<td>0.0644</td>
<td>0.0739</td>
<td>0.0705</td>
</tr>
<tr>
<td>PID</td>
<td>0.0623</td>
<td>0.0454</td>
<td>0.0668</td>
<td>0.0509</td>
<td>0.0668</td>
<td>0.0601</td>
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<tr>
<td>BSC</td>
<td>0.0564</td>
<td>0.0506</td>
<td>0.0576</td>
<td>0.0443</td>
<td>0.0601</td>
<td>0.0577</td>
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</tbody>
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Table 2.5 Joint 3 RMS Error Results for Simulation Test 1-6

<table>
<thead>
<tr>
<th>Endpoint RMS (°)</th>
<th>Test 1</th>
<th>Test 2</th>
<th>Test 3</th>
<th>Test 4</th>
<th>Test 5</th>
<th>Test 6</th>
</tr>
</thead>
<tbody>
<tr>
<td>PD</td>
<td>0.0914</td>
<td>0.0911</td>
<td>0.0913</td>
<td>0.0928</td>
<td>0.0921</td>
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<td>0.0624</td>
<td>0.0474</td>
<td>0.0624</td>
<td>0.0590</td>
</tr>
<tr>
<td>BSC</td>
<td>0.0756</td>
<td>0.0742</td>
<td>0.0742</td>
<td>0.0747</td>
<td>0.0777</td>
<td>0.0736</td>
</tr>
</tbody>
</table>

The average endpoint and joint RMS errors of the six tests and the maximum endpoint error at commanded stops for all tests are tabulated in Tables 2.6-2.7.

Table 2.6 Average Endpoint and Joint RMS Errors for Simulation Tests 1-6

<table>
<thead>
<tr>
<th>Endpoint RMS (°)</th>
<th>Test 1</th>
<th>Test 2</th>
<th>Test 3</th>
<th>Test 4</th>
<th>Test 5</th>
<th>Test 6</th>
<th>Average</th>
</tr>
</thead>
<tbody>
<tr>
<td>PD</td>
<td>0.1526</td>
<td>0.0665</td>
<td>0.0203</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>PID</td>
<td>0.0676</td>
<td>0.0587</td>
<td>0.0544</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>BSC</td>
<td>0.0923</td>
<td>0.0610</td>
<td>0.0750</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 2.7 Maximum Endpoint Error at Commanded Stops for Simulation Tests 1-6

<table>
<thead>
<tr>
<th>Endpoint Error (mm)</th>
<th>Test 1</th>
<th>Test 2</th>
<th>Test 3</th>
<th>Test 4</th>
<th>Test 5</th>
<th>Test 6</th>
<th>Average</th>
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</thead>
<tbody>
<tr>
<td>PD</td>
<td>0.76</td>
<td>0.72</td>
<td>0.79</td>
<td>0.71</td>
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<td>0.70</td>
<td>0.73</td>
</tr>
<tr>
<td>PID</td>
<td>0.40</td>
<td>0.17</td>
<td>0.30</td>
<td>0.20</td>
<td>0.29</td>
<td>0.45</td>
<td>0.30</td>
</tr>
<tr>
<td>BSC</td>
<td>0.17</td>
<td>0.40</td>
<td>0.45</td>
<td>0.45</td>
<td>0.45</td>
<td>0.14</td>
<td>0.34</td>
</tr>
</tbody>
</table>

It is interesting to note that even though the endpoint RMS and joint 1 RMS errors for BSC were smaller compared to the other controllers, the RMS errors for joints 2 and 3 under BSC were not necessarily lower than PD and PID controllers. This can be attributed to the error introduced by the motion of the base, however small it may be. Small motions of the base could lead to incorrectly estimated dynamic torques for joints 2.
and 3 in that the gravity torques for these joints have changed but have not been accounted for under BSC. It should also be noted that sensor noise and drift were not programmed into the simulations, so all base sensory data computed were uncorrupted.

One of the attractive features of BSC is its ability to overcome joint friction and track near perfectly low amplitude joint and velocity trajectories. This was evident in the simulation results and one representative set of velocity tracking results can be seen in Figures 2.25–2.27 for Test 6.

Figure 2.25 Joint Velocities for Simulation Test 6 under PD Control
As the above velocity profiles indicate, BSC was able to compensate for joint friction and track the desired joint velocity trajectories better than PD and PID.
controllers. PD and PID controllers were not able to continuously overcome dynamic joint friction. This resulted in a sudden increase in joint velocity followed by an immediate decrease in joint velocity, then a sudden increase in joint velocity, and so on and so forth. The sudden increase in velocity results when the manipulator joint is able to overcome friction due to a higher commanded torque. The sudden decrease in velocity results when the manipulator joint is not able to overcome friction associated with higher joint velocities, based on the joint friction models in Figures 2.8-2.10.

2.5 Summary and Conclusions

Throughout the simulations, the displacement of the rover vehicle base could not be reliably measured (based on the limitations imposed by the sensory data available for the experimental rover system) and were not compensated for in the commanded trajectories. Thus, the performance of the three controllers under investigation, PD, PID, and BSC, were evaluated based on the commanded trajectories generated using the initial base positions. The dynamic interaction between the rover manipulator and vehicle base during the commanded tasks was not significant to the extent that the performance of BSC (designed for a fixed-base manipulator) was compromised. Thus, BSC as it currently stands, is effective for the slow motion position control of the FSRL rover manipulator. During all six tests, the endpoint errors calculated for all controllers never exceeded the maximum allowable error of 1.5 mm. Thus, all three controllers are acceptable for the accurate position control of the rover manipulator. However, BSC performed the best out of the three evaluated. BSC yielded the best endpoint and joint RMS errors for nearly all the tests. It is the most feasible controller in the accurate position control of the FSRL rover manipulator.
Chapter 3
Experimental Study of Rover Manipulator Position Control

3.1 Introduction

While simulations may yield insight into the performance of the rover manipulator under the various controllers, they cannot substitute experimental studies. Experimental studies were performed using PD, PID, and BSC controllers to determine which controller would perform best under the various commanded tasks for the position control of the FSRL rover manipulator. The controller gain selection process is presented in Section 3.2. The experimental trajectory tracking results and endpoint repeatability performance are presented in Section 3.3.

3.2 Gain Selection for Rover Manipulator

To design the controller gains, a linear joint motor model with linear velocity dependent friction is used. It can be seen in Figure 3.1.

Figure 3.1 Joint Motor Block Diagram with Linear Friction

where $K_{emf}$ is the back emf constant for the joint motor multiplied by the gear ratio, $K_m$ is the motor constant times the gear ratio, $R_m$ is the motor resistance, $I_m$ is the motor current input, $F$ is the linear friction coefficient, $\theta_{des}$ is the desire joint position, and $\theta_{act}$ is the
actual joint position. $\tau_{\text{des}}$ is the commanded and desired joint torque, $\tau_{\text{fric}}$ is the friction torque, $\tau_{\text{act}}$ is the output joint torque, $J$ is the link inertia taken about the joint axis of rotation, $\dot{\theta}$ is the actual joint velocity, $\ddot{\theta}$ is the actual joint acceleration, and 1.8 is the PWM amplifier constant. This system simplifies to Figure 3.2.

\[
\begin{align*}
\theta_{\text{des}} & \rightarrow \text{controller} \rightarrow 1.8 \frac{1}{K_{\text{emf}}(\tau_{\text{ms}}s + A)} \rightarrow \frac{1}{s} \rightarrow \theta_{\text{act}} \\
\end{align*}
\]

Figure 3.2 Simplified Joint Motor Block Diagram with Linear Friction

where $\tau_m = \frac{R_m J}{K_n K_{\text{emf}}}$, and $A = \frac{R_m F}{K_m K_{\text{emf}}} + 1$.

For joint PD controller, the closed loop transfer function is:

\[
\frac{\theta_{\text{act}}}{\theta_{\text{des}}} = \frac{1.8(K_d s + K_p)}{K_{\text{emf}} \tau_m s^2 + (K_{\text{emf}} A + 1.8K_d) s + 1.8K_p} \quad (3.1)
\]

For joint PID controller, the closed loop transfer function is:

\[
\frac{\theta_{\text{act}}}{\theta_{\text{des}}} = \frac{1.8(K_d s^2 + K_p s + K_i)}{K_{\text{emf}} \tau_m s^3 + (K_{\text{emf}} A + 1.8K_d) s^2 + 1.8K_p s + 1.8K_i} \quad (3.2)
\]

The motor used for joint 1 is a MicroMo #1624, for joint 2 a MicroMo #1624, and for joint 3 a MicroMo #1616 (Burn 1998). The specs associated with the motors and necessary constants for the design of the controller gains can be seen in Table 3.1.
Table 3.1 Design Specs for Controller Gains

<table>
<thead>
<tr>
<th></th>
<th>Kmi (Nm/A)</th>
<th>Keml (V/°/s)</th>
<th>0.1424</th>
</tr>
</thead>
<tbody>
<tr>
<td>Km2 (Nm/A)</td>
<td>26.72</td>
<td>Keml2 (V/°/s)</td>
<td>0.4660</td>
</tr>
<tr>
<td>Km3 (Nm/A)</td>
<td>19.133</td>
<td>Keml3 (V/°/s)</td>
<td>0.3342</td>
</tr>
<tr>
<td>Tmi1 (s)</td>
<td>1.104</td>
<td>A1</td>
<td>2.56</td>
</tr>
<tr>
<td>Tmi2 (s)</td>
<td>1.094</td>
<td>A2</td>
<td>1.45</td>
</tr>
<tr>
<td>Tmi3 (s)</td>
<td>3.056</td>
<td>A3</td>
<td>3.10</td>
</tr>
</tbody>
</table>

Same as the simulations, the experimental controller gains were designed to yield a bandwidth of 2 Hz and damping ratio of 0.7 for each joint.

3.3 Rover Manipulator Position Control Experimental Results

Experimental trajectory tracking results are presented in Section 3.3.1. These tests are identical to the ones performed in the simulations. Endpoint repeatability tests are presented in Section 3.3.2. When it becomes necessary for the rover manipulator to return exactly to a previous position, the repeatability tests provide a good indication of which controller would best accomplish that feat.

3.3.1 Trajectory Tracking Results

The experimental rover under investigation in the Field and Space Robotics Lab (FSRL) at MIT can be seen in Figure 3.3 holding an instrument.

Figure 3.3 Experimental Rover Scanning Rock
The experimental tests conducted were identical to the ones in the simulations. That is, three sets of endpoint and joint trajectories were commanded. Each set consisted of two tests, both identical with the exception that the second test took twice as long to complete. There is one difference between the experimental and simulation tests. That is, the experimental tests required the rover manipulator to track the same endpoint trajectory twice. This allowed for comparison of the repeatability performance of the controllers and depicted the case where the rover manipulator was required to retrace where it had been.

Since the results obtained for the six tests were similar, the results for Test 1 will be presented in detail. The endpoint and joint errors along the commanded trajectory for the various controllers during Test 1 can be seen in Figures 3.4-3.6.

![Figure 3.4 Desired vs Actual Experimental Endpoint Trajectory Tracking for PD](image-url)
The endpoint and joint errors at commanded stops (at midpoint of each triangle side and all vertices) for the various controllers during Test 1 can be seen in Figures 3.7-3.10.
Figure 3.7 Endpoint Error at Commanded Stops for Experimental Test 1

Figure 3.8 Joint 1 Error at Commanded Stops for Experimental Test 1
Similar to the results obtained for Test 1 in the simulations, BSC clearly outperformed PD and PID controllers. It is interesting to note that whereas the performance of joint 3 in simulations did not perform better than PID controller, the performance of joint 3 in experiments did. The repeatability of the controllers indicate
that overall, there were not any significant improvements in reducing error as the
trajectory was tracked a second time and that the results were repeatable. The root mean
square (RMS) errors for all tests are tabulated in Tables 3.2-3.5.

Table 3.2 Endpoint RMS Error Results for Experimental Tests 1-6

<table>
<thead>
<tr>
<th>Endpoint RMS (mm)</th>
<th>Test 1</th>
<th>Test 2</th>
<th>Test 3</th>
<th>Test 4</th>
<th>Test 5</th>
<th>Test 6</th>
</tr>
</thead>
<tbody>
<tr>
<td>PD</td>
<td>0.4</td>
<td>0.4</td>
<td>0.4</td>
<td>0.4</td>
<td>0.3</td>
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<tr>
<td>PID</td>
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<td>0.2</td>
<td>0.2</td>
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<tr>
<td>BSC</td>
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<td>0.1</td>
<td>0.1</td>
<td>0.1</td>
<td>0.1</td>
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</tbody>
</table>

Table 3.3 Joint 1 RMS Error Results for Experimental Tests 1-6

<table>
<thead>
<tr>
<th>Endpoint RMS (°)</th>
<th>Test 1</th>
<th>Test 2</th>
<th>Test 3</th>
<th>Test 4</th>
<th>Test 5</th>
<th>Test 6</th>
</tr>
</thead>
<tbody>
<tr>
<td>PD</td>
<td>0.1067</td>
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<td>0.1149</td>
<td>0.1074</td>
</tr>
<tr>
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<td>0.0487</td>
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Table 3.4 Joint 2 RMS Error Results for Experimental Tests 1-6

<table>
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<tr>
<th>Endpoint RMS (°)</th>
<th>Test 1</th>
<th>Test 2</th>
<th>Test 3</th>
<th>Test 4</th>
<th>Test 5</th>
<th>Test 6</th>
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<tr>
<td>BSC</td>
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<td>0.0145</td>
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<td>0.0196</td>
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</tbody>
</table>

Table 3.5 Joint 3 RMS Error Results for Experimental Test 1-6

<table>
<thead>
<tr>
<th>Endpoint RMS (°)</th>
<th>Test 1</th>
<th>Test 2</th>
<th>Test 3</th>
<th>Test 4</th>
<th>Test 5</th>
<th>Test 6</th>
</tr>
</thead>
<tbody>
<tr>
<td>PD</td>
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<td>0.0181</td>
</tr>
<tr>
<td>PID</td>
<td>0.0640</td>
<td>0.0393</td>
<td>0.0593</td>
<td>0.0379</td>
<td>0.0387</td>
<td>0.0277</td>
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<tr>
<td>BSC</td>
<td>0.0417</td>
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<td>0.0155</td>
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The average endpoint and joint RMS errors of the six tests and the maximum endpoint
error at commanded stops for all tests are tabulated in Tables 3.6-3.7.

Table 3.6 Average Endpoint and Joint RMS Errors for Experimental Tests 1-6

<table>
<thead>
<tr>
<th>Endpoint RMS (mm)</th>
<th>PD</th>
<th>PID</th>
<th>BSC</th>
</tr>
</thead>
<tbody>
<tr>
<td>Joint 1 RMS (°)</td>
<td>0.1076</td>
<td>0.0640</td>
<td>0.0455</td>
</tr>
<tr>
<td>Joint 2 RMS (°)</td>
<td>0.0321</td>
<td>0.0244</td>
<td>0.0181</td>
</tr>
<tr>
<td>Joint 3 RMS (°)</td>
<td>0.0411</td>
<td>0.0445</td>
<td>0.0282</td>
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</table>
Table 3.7 Maximum Endpoint Error at Commanded Stops for Experimental Tests 1-6

<table>
<thead>
<tr>
<th>Endpoint Error (mm)</th>
<th>Test 1</th>
<th>Test 2</th>
<th>Test 3</th>
<th>Test 4</th>
<th>Test 5</th>
<th>Test 6</th>
<th>Average</th>
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</thead>
<tbody>
<tr>
<td>PD</td>
<td>0.48</td>
<td>0.46</td>
<td>0.40</td>
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<td>0.42</td>
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<td>0.25</td>
<td>0.31</td>
<td>0.18</td>
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<td>0.24</td>
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<td>0.20</td>
<td>0.19</td>
<td>0.21</td>
<td>0.18</td>
<td>0.13</td>
<td>0.10</td>
<td>0.17</td>
</tr>
</tbody>
</table>

As predicted, for all tests, both BSC and PID controllers outperformed PD control. Overall, BSC yielded the lowest endpoint and joint RMS errors and the lowest maximum endpoint error at the commanded stops. It is the best controller evaluated for the accurate position control of the FSRL rover manipulator.

A distinct advantage BSC has over PID is its ability to track a trajectory as joint velocity changed signs. During all tests, it was noted that PID controller yielded large joint errors in instances where the joint velocity underwent a change in sign. This did not happen under BSC control.

For all tests, the motion of the base could not be accurately measured within the limitations of the sensory data available. That is, the dynamic interaction between the manipulator and vehicle base was so small that it could not be registered accurately by vehicle motion and position sensors. Thus, the performance of the controllers under investigation were evaluated based on the commanded trajectories generated using the initial base positions. The dynamic interaction between the manipulator and vehicle base during the commanded tasks did not adversely affect the performance of BSC (for a fixed-base manipulator) proving that BSC can be implemented successfully for an experimental rover manipulator undergoing slow trajectory tracking.

It was noted that although there were instances of high frequency, small amplitude base oscillations resembling noise that resulted when the controller was trying to account for joint slippage in the absence of gravity compensation, the performance of
BSC was not degraded in any significant manner. A Butterworth filter implemented on the vehicle motion and position sensor readings and manipulator base force/torque sensor readings effectively filtered out the small amplitude high frequency disturbances to the system.

It is interesting to note that there were several instances where BSC yielded larger endpoint errors compared with PID controller at various commanded stops, even though it yielded lower endpoint and joint RMS errors. Several factors can contribute to this phenomenon: sensor drift, external disturbance from joint motor cables, and the joint trajectory range commanded. During the experiment, base force/torque sensor drift was observed. An attempt was made to compensate for this problem by starting the experiments several seconds after the rover on-board computer started reading data off the manipulator base force/torque sensor and then resetting the sensor offset.

To lessen the effects of external disturbance forces/torques contributed by the joint motor cables, the joint motor cables were gathered and aligned along the z-axis of the force/torque sensor. The base force/torque sensor frame corresponds to frame \{0\} of Figure 2.5. This solution lessened but did not completely eliminate the external disturbances contributed by the joint motor cables.

The simplified version of BSC (Equation 2.5) is valid for small joint displacements. During large joint displacements, significant changes in gravity torque not adjusted for, coupled with the problems listed above can lead to over or under BSC compensation. To remedy this problem, at all commanded stops, BSC integral terms were zeroed and manipulator base force/torque sensor offset reset.
3.3.2 Repeatability Results

During the repeatability tests, the rover manipulator was commanded to return to its initial starting point from a different position (located as far as 16.0 cm away) fifty times. Endpoint repeatability is important in that it allows the rover manipulator to accurately place a science instrument over the same spot if the need arises and to acquire the desired data from a specified region that may occupy very little space multiple times. Figure 3.11 shows the FSRL rover manipulator during the repeatability test.

Figure 3.11 Experimental Rover Performing Repeatability Test

The results from the repeatability test can be seen in Figure 3.12.

Figure 3.12 Repeatability Results for BSC, PID, and PD Controllers
Clearly, BSC outperformed the other controllers in the repeatability test. The spread of data points under the various controllers is tabulated in Table 3.4.

<table>
<thead>
<tr>
<th>Spread (mm)</th>
<th>PD</th>
<th>PID</th>
<th>BSC</th>
</tr>
</thead>
<tbody>
<tr>
<td>2.4</td>
<td>1.6</td>
<td>1.0</td>
<td></td>
</tr>
</tbody>
</table>

The spread of data points under BSC was the smallest, whereas for PD it was the greatest.

### 3.4 Summary and Conclusions

Overall, the experimental results agreed with the simulations. Similar to the simulations, the dynamic interaction between the rover manipulator and vehicle base did not result in significant and measurable base displacements. Hence, the performance of the controllers were evaluated based on the commanded trajectories generated using the initial base positions. The dynamic interaction between the rover manipulator and vehicle base was not significant to the extent that the performance of BSC (designed for a fixed-base manipulator) was compromised. Thus, BSC as it currently stands, is effective for the slow motion trajectory control of an experimental rover manipulator.

A major difference between the simulation and experimental results can be found in the performance of joint 3 under BSC control. Whereas joint 3 did not perform as well under BSC control, compared to PD and PID in simulations, it performed better than PD and PID the majority of time in experiments. Another difference noted had to do with the joint velocity tracking capabilities of PD and PID controllers. The experimental joint velocity tracking capabilities of all three controllers yielded results very similar to the desired joint velocity profiles. This differed from the simulation results where PD and PID controllers were not able track the desired joint velocity profiles all the time. Which suggests that perhaps the estimated joint frictions were higher than the actual.
Similar to the simulation results, during all six tests, the endpoint errors calculated for all controllers never exceeded the maximum allowable error of 1.5 mm. However, like the simulations, BSC is the best controller evaluated for the accurate position control of the FSRL rover manipulator. BSC yielded the lowest endpoint and joint RMS errors and the lowest maximum endpoint error at the commanded stops for nearly all tests. For endpoint repeatability, BSC is clearly the superior controller. It performed nearly twice as well as PD and PID controllers.
Chapter 4

Experimental Study of Robot Manipulator Coring

4.1 Introduction

Rover manipulator coring is a novel idea for Mars explorations. It is practical in that by attaching a coring unit at the end of a rover manipulator, the range of available and reachable sites for bedrock and other sample extraction is increased dramatically. By utilizing rover manipulator-vehicle configurations, the rover manipulator should be capable of exerting the required forces and torques to core into the material sampled. Currently available rover coring systems are vehicle mounted. Two such examples are the RDC/Athena mini-corer and the ESB drill seen in Figure 4.1.

![Figure 4.1](image)

Figure 4.1 Examples of Rover Coring Systems (a) RDC/Athena Mini-Corer on a FIDO Rover (b) EBS Drill on a Flexible Rover (Ghavimi 1998)

As can be seen in the above pictures, the range of available and reachable coring sites for vehicle mounted coring systems are limited.

In order to determine whether rover manipulator coring is feasible, coring
disturbance force/torque characterization tests were performed. The experimental setup used is presented in Section 4.2. Coring characterization results for four test materials are presented in Section 4.3.

4.2 Experimental Setup

A rigid, fixed-base SCARA type robot called the ADEPT was used in the coring characterization tests. The coring unit is composed of a commercial Black and Decker power screwdriver capable of outputting 1.36 Nm (12 lb-in) torque and 180 rpm at 3.6 Volts, and a Starlite diamond core drill bit. The core drill bit has an outer diameter of 7.54 mm (19/64 in) and an inner diameter of 6.27 mm (0.247 in). The coring unit is mounted on a force/torque sensor attached to the end-effector of the ADEPT and powered by an external power supply. The experimental setup can be seen in Figure 4.2.

Figure 4.2 ADEPT with Coring Unit
The motor of the coring unit has a rate of 50 rpm/V. To set the motor to rotate at 200 revolutions per minute (rpm), set the voltage of the power supply to 4 Volts (V). An impedance controller was used for the position/force control of the ADEPT (Hogan 1987). To command the ADEPT to core with a constant thrust rate, a linear joint trajectory was issued for the joint that controls the z position (vertical displacement) of the manipulator end-effector. During the tests, the ADEPT was commanded only to move in the z direction. The measured difference between the current end-effector position and the initial contact position is referred to as the coring depth. The disturbance forces and torques presented in this thesis follow the sensor axes orientation shown in Figure 4.3.

![Figure 4.3 ADEPT Force/Torque Sensor Axes Orientation](image)

Four sample materials were used in the coring characterization tests. They were limestone, sedimentary rock, and two types of lava rocks. Limestone and sedimentary rocks were chosen because of their high compressive strengths. Martian rocks have properties similar to basalt, which has a compressive strength similar to limestone and sedimentary rock. The lava rock samples are porous and have low compressive
strengths. They were chosen to provide a basis of comparison against the other materials. These materials, along with the holes left by the coring unit, can be seen in Figure 4.4.

Figure 4.4 Coring Test Sample Materials (a) limestone (b) sedimentary rock (c) lava rock type 1 (d) lava rock type 2

4.3 Robot Manipulator Coring Characterization Results

One of the objectives of coring characterization tests is to determine the required coring thrust force needed to core into the sampled materials. If the rover manipulator can match the performance of the ADEPT when it cored into the sampled material, then it is capable of coring into the same material. However, just because the rover manipulator may be capable of exerting the required coring thrust force, it does not necessarily mean it will core successfully.
A major factor that can deter the performance of rover manipulator coring is coring disturbance forces/torques resulting from the interaction between the coring drill bit and the sampled material. If the coring disturbance forces/torques are large enough to excite dynamic interaction between the rover manipulator and vehicle base and/or cause the rover-manipulator system to go unstable, then solutions need to be proposed. The solution may come in the form of a new coring methodology, position/force controller, etc. It is the intent of this thesis to determine whether or not the coring disturbance forces/torques warrant new solutions, not propose them. It should also be made clear that it is not the intent of this thesis to evaluate coring system design and coring methodologies for planetary explorations.

The settings used in the coring experiments were chosen based on the performance capabilities of previous and current coring systems, such as coring rpm and thrust rate (Eiden and Coste 1991, Ghavimi 1998, Gorevan (2) 1999, Dolgin 1999). Typical planetary coring systems are not capable of outputting more than 400 rpm due to limited on-board power supply. Thus, the maximum allowable coring rpm was chosen to be 400 rpm. To determine the influence of coring rpm on coring disturbance forces/torques, a second coring rpm was chosen. It was 200 rpm. The second key parameter in coring, in addition to coring rpm, is the coring thrust rate.

How well the coring unit is designed and the properties of the sampled material determine the maximum thrust rate allowed. The thrust rate used is directly proportional to the coring torque. The higher the thrust rate, the higher the required coring torque. The coring torque depends on the efficiency of the drill tip design (Dolgin 1999). Slower thrust rates are appropriate for stronger rocks and higher penetration rates for weaker
rocks (Gorevan (2) 1999). Typical coring thrust rate for limestone is low, around 2.0 cm/hr (Ghavimi 1998, Gorevan (2) 1999). Thus the coring thrust rate for limestone was chosen to be 2.0 cm/hr. To determine the effect of the thrust rate on coring disturbance force/torque, a second faster thrust rate of 3.0 cm/hr was chosen.

For each material, two coring rpms and two thrust rates were used. This yielded a combination of four different test settings per material. For all tests, the coring rpms used were 200 and 400. For limestone and sedimentary rock, which share similar compressive strengths, the thrust rates used were 2.0 cm/hr and 3.0 cm/hr. For the lava rocks, which share similar compressive strengths, the thrust rates used were 0.2 cm/min and 0.5 cm/min. Since the compressive strengths of the lava rocks are less than one third of limestone and sedimentary rock, the thrust rates chosen for the lava rocks were more than three times faster than that of limestone and sedimentary rock. That is, the thrust rate chosen was inversely proportional to the compressive strength.

Results obtained for limestone are presented and analyzed in Section 4.3.1. Results obtained for sedimentary rock are presented and analyzed in Section 4.3.2. Results obtained for lava rock type 1 are presented and analyzed in Section 4.3.3 and for lava rock type 2 in Section 4.3.4.

4.3.1 Limestone

Limestone has a high compressive strength of approximately 40 MPa and therefore requires a low thrust rate. Four different test settings were used on limestone. The rpm used were 200 and 400 and the thrust rate used were 2.0 cm/hr and 3.0 cm/hr. A sample of the extracted core can be seen in Figure 4.5. In Figure 4.5, the ruler unit is in cm.
Figures 4.6.1-4.6.5 contain plots of the thrust force and disturbance force and moments encountered while coring into limestone. In these and all subsequent plots, $F_{xy}$ is the magnitude of the disturbance force perpendicular to the thrust direction (it is $\sqrt{F_x^2 + F_y^2}$, where $F_x$ and $F_y$ are the disturbance forces in the x and y directions, respectively). $F_z$ is the magnitude of the thrust force in the z-direction. $M_x$, $M_y$, and $M_z$ are the magnitudes of the disturbance torques measured about the sensor x, y, and z axis, respectively.

![Figure 4.6.1 Fx versus Coring Depth for Limestone](image)

**Figure 4.6.1 Fx versus Coring Depth for Limestone**
Figure 4.6.2 $F_z$ versus Coring Depth for Limestone

Figure 4.6.3 $M_x$ versus Coring Depth for Limestone
Several observations were made when coring into limestone and they are:

- A constant thrust force around 15 N, with a thrust rate of at most 2.0 cm/hr and a minimum of 200 rpm are good coring settings for limestone.
- Little to no skidding, the slipping and sliding of the coring drill bit due to interaction with the surface of the test material, was observed for limestone.

- $M_x$ and $M_y$ are related to $F_z$. As $F_z$ increased, so did $M_x$ and $M_y$. The thrust force was transmitted along the circumference of the coring drill bit. The circumference of the coring drill bit is offset from the z-axis of the sensor by a moment arm equal to the radius of the coring drill bit. This offset, when multiplied by the thrust force contributes a moment.

- For a fixed thrust rate, as the rpm of the corer increased, the thrust force decreased. A faster rpm allowed the corer to generate more cuts through the material at a given time than at a lower rpm. Thus, less resistance was presented to the corer in the thrust direction. Also, there was less of a tendency for the core that was lodged inside the coring drill bit to hamper the coring process. That is, at higher rpm, more of the core material that came in contact with the coring drill bit was removed, thus reducing friction between contacting surfaces.

- Thrust force tended to increase as coring depth increased for large thrust rate and low rpm settings. This was attributed to the controller used. An impedance controller indirectly controls force by controlling position. When position error built up due to the corer’s inability to track the commanded trajectory (which may be attributed to worn bit or lodged core), a higher torque was applied to force the manipulator to catch up and stay with the desired joint trajectory. This often leads to the stalling of the corer motor since eventually the coring drill bit will be forced hard enough against the material to prevent it from rotating anymore. If the coring drill bit cannot rotate to remove more contact material, it will not advance any deeper.
• For the 3.0 cm/hr and 200 rpm case in particular, the corer motor started to stall toward the later stages of the test. This attributed to the increase in thrust force. Since the disturbance moments are related to the thrust force, they too also increased. Disturbance force $F_{xy}$ also increased. As the ADEPT manipulator pushed the coring drill bit harder and harder against the test material, it forced the coring drill bit to start coring slightly off normal because it could not core any deeper in the original thrust direction. This will cause the coring drill bit to start coring sideways, which will end up increasing disturbance forces and moments.

• Coring disturbance forces and moments will have the greatest influence on the success of coring during the initial coring depth of up to approximately 5.0 mm. Beyond the initial coring depth, the disturbance forces and moments encountered will have less influence on the success of coring and are relatively constant as long as the coring motor does not start to stall. This is true because past the initial coring depth, the coring drill bit is well embedded in the material. The core retained in the coring drill bit (which has not been broken off from the sampled material) and the material surrounding the outside of the drill bit serve to lock the drill bit in place, restricting motion only in the axial thrust direction.

• Up to a coring depth of 5.0 mm, disturbance force $F_{xy}$ remained less than 1 N and disturbance moments less than 0.3 Nm. As long as the disturbance force and moments stay below those ranges during the initial coring depth, they will not affect the performance of robot manipulator coring and are considered small disturbances. Beyond the initial coring depth, the disturbance force and torques are seen to go up, mainly due to the fact that the corer motor started to stall. However, their effect on
coring will not be significant, as explained in the previous observation.

4.3.2 Sedimentary Rock

Sedimentary rock has a compressive strength similar to limestone. The same settings that were used to core into limestone were used for sedimentary rock. An extracted sedimentary rock core can be seen in Figure 4.7. The ruler unit is in cm.

![Figure 4.7 Extracted Sedimentary Rock Core](image)

The thrust force and disturbance force and torques encountered while coring into sedimentary rock can be seen in Figures 4.8.1-4.8.5.

![Figure 4.8.1 Fxy versus Coring Depth for Sedimentary Rock](image)
Chapter 4 Experimental Study of Robot Manipulator Coring
Several observations made when coring into sedimentary rock and they are:

- A constant thrust force around 18 N, with a thrust rate of at most 2.0 cm/hr and a minimum of 200 rpm are good coring settings for sedimentary rock.

- Very little skidding was experienced when initially coring into sedimentary rock.
- Up to a coring depth of 5.0 mm, disturbance force $F_{xy}$ remained less than 1.3 N and disturbance moments less than 0.4 Nm. These values are very close to the ones obtained for limestone and can also be considered small. Beyond the initial coring depth, a larger increase in thrust force and disturbance force/moments was observed for sedimentary rock than for limestone. This was because sedimentary rock was harder to core into. However, the disturbance force/torques did not hamper the coring process. Rather, the main source of problem when coring into sedimentary rock was the larger axial thrust force built-up, which eventually caused the motor to start stalling.

- Even though the coring drill bit was able to core greater than 1 cm into sedimentary rock, it was difficult to break off the core from the source material. To break off the core from the rock, it was necessary to apply a large wrench (by hand) to the coring drill bit because a core break-off mechanism has not been built into the coring unit.

- When the core became lodged inside the coring drill bit to the extent that it prevented the coring drill bit from rotating any further and caused the motor to stall, the corer became less and less able to core deeper. However, when the lodged core was broken off during the coring process, a sudden decrease in thrust force and disturbance forces/torques will occur, which will usually allow the corer to continue coring and core deeper into the rock. This explains the sudden decrease in thrust force and disturbance force/torques experienced for the 2.0 cm/hr, 200 rpm test setting at a coring depth of approximately 9.0 mm and why the corer was able to core deeper.

- Other general observations made for limestone also apply to sedimentary rock.
4.3.3. Lava Rock Type 1

Lava rock type 1 is highly porous and has a low compressive strength. The rpm used were 200 and 400 and the thrust rate used were 0.2 cm/min and 0.5 cm/min. An extracted sample can be seen in Figure 4.9. In Figure 4.9, the ruler unit is in cm.

![Figure 4.9 Extracted Lava Rock Type 1 Core](image)

The thrust force and disturbance force and torques encountered while coring into lava rock type 1 can be seen in Figures 4.10.1-4.10.5.

![Figure 4.10.1 F_xy versus Coring Depth for Lava Rock Type 1](image)
Figure 4.10.2 $F_z$ versus Coring Depth for Lava Rock Type 1

Figure 4.10.3 $M_x$ versus Coring Depth for Lava Rock Type 1
Several observations were made when coring into lava rock type 1 and they are:

- A constant thrust force around 5 N, with a thrust rate of at most 0.5 cm/min and a minimum of 200 rpm are good coring settings for lava rock type 1.
• It is definitely possible to core into this material with a faster thrust rate. However, a faster thrust rate will result in higher thrust force and may require higher coring rpm as well.

• Although this material has a lower compressible strength compared to limestone and sedimentary rock, its surface friction is very low and surface skidding can easily occur with minimal effort.

• Many repeated tests were needed to arrive at the results presented here. Skidding at the surface tended to cause the coring drill bit to core at an angle to the surface normal of the test material. This led to large disturbance forces and moments. It may also cause damage to the coring drill bit if not corrected. Damage to the coring drill bit can result if it is no longer aligned with the thrust axis of the corer as the thrust depth continues to increase. This may cause the drill bit to break off from the corer or cause excessive drill bit wear. This problem could be corrected by adjusting the manipulator thrust direction to follow the axial direction of the coring bit.

• Up to a coring depth of 5.0 mm, disturbance force $F_{xy}$ remained below 1.2 N and disturbance moments below 0.27 Nm. These ranges are similar to those obtained for limestone and sedimentary rock and are considered small.

• Other general observations made for limestone and sedimentary rock also apply to this material.

4.3.4 Lava Rock Type 2

Lava rock type 2 is similar to lava rock type 1 in that it has a very low compressive strength, even lower than that of lava rock type 1, and is also highly porous. The coring setting used for lava rock type 1 was also used for lava rock type 2.
Extracting a whole core was near impossible for this type of material because of its fibrous nature. As the coring drill bit cored into the material, it shredded the core and surrounding material. An extracted core for this material can be seen in Figure 4.11. Again, the ruler unit is in cm.

Figure 4.11 Extracted Lava Rock Type 2 Core

The thrust force and disturbance force and torques encountered while coring into lava rock type 2 can be seen in Figures 4.12.1-4.12.5.

Figure 4.12.1 \( F_{xy} \) versus Coring Depth for Lava Rock Type 2
Figure 4.12.2 $F_z$ versus Coring Depth for Lava Rock Type 2

Figure 4.12.3 $M_x$ versus Coring Depth for Lava Rock Type 2
Several observations were made when coring into lava rock type 2 and they are:

- Due to its low compressive strength and porous material property, lava rock type 2 was much easier to core into than lava rock type 1. A constant thrust force around 2.5
N, with a thrust rate of at most 0.5 cm/min and a minimum of 200 rpm are good coring settings for lava rock type 2.

- Similar to lava rock type 1, it is definitely possible to core into this material with a faster thrust rate. However, a faster thrust rate will result in higher thrust force and may require higher coring rpm as well.

- This material is easily fragmented and surface skidding can easily occur.

- All extracted cores were segmented. As the material was cored, the inner core and surrounding material became fragmented and less resistance was presented to keep the coring drill bit along its original thrust direction. This often caused the coring drill bit to start coring at an angle to the original thrust direction, which resulted in large disturbance forces and moments. Like for lava rock type 1, many repeated tests were conducted before good results were obtained. A good set of results is one that is obtained for the case where the axial direction of the coring bit was aligned with the corer and the original thrust direction throughout the entire test.

- Up to a coring depth of 5.0 mm, disturbance force $F_{xy}$ remained below 0.75 N and disturbance moments below 0.20 Nm. These ranges are similar to those obtained for the other rocks and are also considered small.

- General observations made for other test materials also apply to this material.

4.4 Summary and Conclusions

Based on the data gathered from the coring characterization tests, it should possible for the Field and Space Robotics Lab’s experimental rover manipulator to exert enough thrust force to core into the test materials. The FSRL rover manipulator is capable of exerting an end-effector force of 18 N (4-lb) in bent position, which is just enough for
coring into limestone and sedimentary rock. By applying the Force Workspace or the Force-Task Workspace approach (Madhani 1991, Papadopoulos and Gonthier 1999), the rover manipulator will be capable of coring into hard rocks from various positions.

Attention must be paid to the manipulator as it cores through the initial coring depth. It is during this interval that skidding can cause the coring drill bit to core at an angle to the surface normal of the material and result in large disturbance forces and moments. This is less of a problem for materials with high compressive strengths and more of a problem for materials with low compressive strength. Also, more skidding about the material surface would be expected for a rover manipulator since it is lightweight and not as rigid as the ADEPT manipulator.

Overall, the disturbance forces and moments encountered for all test materials during the initial coring depth of up to 5.0 mm were similar and small. They did not adversely affect the fixed-base ADEPT manipulator as it cored into the rocks and are not expected to for a rover manipulator if the dynamic interaction between the vehicle base and manipulator resulting from coring are small. The presence of coring disturbance forces/torques will not prevent a rover manipulator from coring continuously. However, it may make it difficult for the rover manipulator to core along its initial thrust direction. Based on the data gathered for coring disturbance force/torque characterization results, rover manipulator coring does appear feasible.
Chapter 5

Conclusions and Future Work

5.1 Contributions of This Work

An investigation has been carried out in determining the feasibility of three controllers for the position control of the FSRL rover manipulator. Of the three controllers investigated (PD, PID and BSC), BSC was determined the best controller for the accurate position control of the FSRL rover manipulator. BSC performed nearly twice as well as PID control and nearly four times as well as PD control in endpoint trajectory tracking. PID performed nearly twice as well as PD control. During the trajectory tracking tests, there were no significant base motions detected. Thus, BSC can be effectively implemented for the experimental rover manipulator without any base dynamics compensation terms. Endpoint repeatability tracking was also performed using the three controllers. BSC performed nearly twice as well as PID control and more than twice as well as PD control. PID control performed almost twice as well as PD control.

An investigation into the feasibility of rover manipulator coring has been conducted. First, coring disturbance force/torque characterization tests were performed using a fixed-base ADEPT manipulator. From the coring characterization results, it was deemed possible for the FSRL rover manipulator to exert the minimum required thrust force to core into the sampled rocks. Coring disturbance forces/torques encountered during the initial coring depth of 5.0 mm were insufficient in undermining the performance of the ADEPT manipulator. They were also expected to be insufficient in undermining rover manipulator coring as long as large and unstable vehicle base
dynamics are not excited during the process. Thus, rover manipulator coring does seem feasible.

5.2 Recommendations for Future Work

Base dynamics of the FSRL rover did not play a role in affecting the effectiveness of BSC under the commanded tasks. However, there may be instances for other mobile manipulator systems where the dynamic interaction between the vehicle base and manipulator would render BSC ineffective in carrying out its job. Thus, base dynamics compensation BSC could be developed.

Rover manipulator coring is a very challenging field. To ensure that rover manipulator coring will be met with success, an effective coring methodology and an efficient and lightweight mini-rover manipulator corer need to be developed. Investigation into vehicle base disturbances resulting from rover manipulator coring also needs to be made. A controller architecture that would effectively compensate for any disturbance that may result from rover manipulator coring should be developed as well.
References


Torres, M., and Hootsman, N.A.M., RIBS program located on the MIT Athena server


References
Appendix A

Lagrange Equations of Motion for Rover Manipulator

![Rover Manipulator Coordinate Frames Set-Up](image)

Figure A.1 Rover Manipulator Coordinate Frames Set-Up

The Lagrange approach to deriving the equations of motion involves the kinetic and potential energies of the system. The Lagrange function, L, is defined by:

\[ L = T^* - V \]  

(A.1)

where \( T^* \) is the kinetic energy of the system and \( V \) is the potential energy of the system.

Since the vehicle base of the FSRL rover demonstrated high stiffness in all directions except for the pitch and vertical directions, the Lagrange equations of motion were derived using only five independent variables: \( h \), \( \theta_p \), \( \theta_1 \), \( \theta_2 \), and \( \theta_3 \). That is, the base is assigned two degrees of freedom: in the vertical and pitch directions. That being said, the kinetic and potential energies of the system are given by:
\[
T = \frac{1}{2} m v^2 + \frac{1}{2} m_1 v_1^2 + \frac{1}{2} m_2 v_2^2 + \frac{1}{2} m_3 v_3^2 + \frac{1}{2} I_1 \ddot{\theta}_1 + \frac{1}{2} I_2 \ddot{\theta}_2 + \frac{1}{2} I_3 \ddot{\theta}_3 + \frac{1}{2} I_{1x} \dot{\theta}_2 \sin \theta_2 + \frac{1}{2} I_{1y} \dot{\theta}_1 - \frac{1}{2} I_{1z} \theta_2 + \frac{1}{2} I_{2x} \left( \dot{\theta}_1 \sin \theta_2 + \dot{\theta}_2 \sin \theta_2 \right) + \frac{1}{2} I_{2y} \left( \dot{\theta}_1 \cos \theta_2 - \dot{\theta}_2 \sin \theta_2 \right)^2 + \frac{1}{2} I_{2z} \left( \dot{\theta}_1 \sin \theta_2 + \dot{\theta}_2 \sin \theta_2 \right)^2 + \frac{1}{2} I_{3x} \left( \dot{\theta}_1 \sin \theta_3 + \dot{\theta}_3 \sin \theta_3 \right) + \frac{1}{2} I_{3y} \left( \dot{\theta}_1 \cos \theta_3 - \dot{\theta}_3 \sin \theta_3 \right)^2 + \frac{1}{2} I_{3z} \left( \dot{\theta}_1 \cos \theta_3 - \dot{\theta}_3 \sin \theta_3 \right)^2 \quad (A.2)
\]

\[
V = mgh + \frac{1}{2} k(h - h_o)^2 + m_1 g(h + \rho \cos \theta - a \sin \theta + R \cos \theta)
\]

\[
+ m_2 g(h + \rho \cos \theta - a \sin \theta + L \cos \theta) + m_3 g(h + \rho \cos \theta - a \sin \theta + L \cos \theta) - R \cos \theta \sin \theta - m_1 g(h + \rho \cos \theta - a \sin \theta + L \cos \theta) + m_2 g(h + \rho \cos \theta - a \sin \theta + L \cos \theta) - m_3 g(h + \rho \cos \theta - a \sin \theta + L \cos \theta) + \frac{1}{2} k(\rho - \rho_o)^2
\]

The magnitude of the center of mass velocities for vehicle base, \( v \), link 1, \( v_1 \), link 2, \( v_2 \), and link 3, \( v_3 \), are defined with respect to the non-moving inertial frame \{inert\}. They are:

\[
v^2 = \dot{X}_c + \dot{Y}_c + \dot{Z}_c \quad (A.4)
\]

\[
v_1^2 = \dot{X}_1c + \dot{Y}_1c + \dot{Z}_1c \quad (A.5)
\]

\[
v_2^2 = \dot{X}_2c + \dot{Y}_2c + \dot{Z}_2c \quad (A.6)
\]

\[
v_3^2 = \dot{X}_3c + \dot{Y}_3c + \dot{Z}_3c \quad (A.7)
\]

where \( \dot{X}_c \), \( \dot{Y}_c \), and \( \dot{Z}_c \) are vehicle base center of mass velocities in the respective directions. \( \dot{X}_1c \), \( \dot{Y}_1c \), and \( \dot{Z}_1c \) are link 1 center of mass velocities in the respective directions. \( \dot{X}_2c \), \( \dot{Y}_2c \), and \( \dot{Z}_2c \) are link 2 center of mass velocities in the respective
directions. \( \dot{X}_c, \dot{Y}_c, \) and \( \dot{Z}_c \) are link 3 center of mass velocities in the respective directions. The location of the center of mass of the vehicle base and links are defined with respect to the inertial frame. Throughout this Appendix, the following shorthand notation will be used in representing sines and cosines: \( s_i \) is \( \sin(\theta_i) \), \( s_{12} \) is \( \sin(\theta_1+\theta_2) \), \( c_i \) is \( \cos(\theta_i) \), and \( c_{12} \) is \( \cos(\theta_1+\theta_2) \).

The center of mass locations defined with respect to the inertial frame \( \{\text{inert}\} \) are:

\[
X_c=0 \quad \text{(A.8)} \\
Y_c=0 \quad \text{(A.9)} \\
Z_c=h \quad \text{(A.10)} \\
X_{1c}=L_0s_p+a_c p+R_1s_p \quad \text{(A.11)} \\
Y_{1c}=0 \quad \text{(A.12)} \\
Z_{1c}=h+L_0c_p-a_s p+R_1c_p \quad \text{(A.13)} \\
X_{2c}=L_0s_p+a_c p+L_1s_p+R_2c_1c_p+R_2s_2s_p \quad \text{(A.14)} \\
Y_{2c}=R_2c_2s_1 \quad \text{(A.15)} \\
Z_{2c}=h+L_0c_p-a_s p+L_1c_p-R_2c_1s_p+R_2s_2c_p \quad \text{(A.16)} \\
X_{3c}=L_0s_p+a_c p+L_1s_p+(L_2c_2+R_3c_3)c_1c_p+(L_2s_2+R_3s_3)s_p \quad \text{(A.17)} \\
Y_{3c}=(L_2c_2+R_3c_3)s_1 \quad \text{(A.18)} \\
Z_{3c}=h+L_0c_p-a_s p+L_1c_p-(L_2c_2+R_3c_3)c_1s_p+(L_2s_2+R_3s_3)c_p \quad \text{(A.19)}
\]

To obtain the Lagrange function for the system, calculate the time derivative of Equations A.8-A.19 and substitute them into Equations A.4-A.7 and A.1-A.3. To derive the Lagrange equations of motion for the system, compute the following:

\[
\tau_{iact} = \frac{d}{dt} \frac{\partial L}{\partial \dot{\theta}_i} - \frac{\partial L}{\partial \theta_i} \quad \text{(A.20)}
\]
\[ \tau_{2\text{act}} = \frac{d}{dt} \frac{\partial L}{\partial \dot{\theta}_2} - \frac{\partial L}{\partial \theta_2} \quad (A.21) \]

\[ \tau_{3\text{act}} = \frac{d}{dt} \frac{\partial L}{\partial \dot{\theta}_3} - \frac{\partial L}{\partial \theta_3} \quad (A.22) \]

\[ \tau_h = \frac{d}{dt} \frac{\partial L}{\partial \dot{h}} - \frac{\partial L}{\partial h} - \epsilon_h = 0 \quad (A.23) \]

\[ \tau_p = \frac{d}{dt} \frac{\partial L}{\partial \dot{\theta}_p} - \frac{\partial L}{\partial \theta_p} - \epsilon_p = 0 \quad (A.24) \]

where \( \tau_{1\text{act}} \) is the actual output torque for joint 1, \( \tau_{2\text{act}} \) is the actual output torque for joint 2, and \( \tau_{3\text{act}} \) is the actual output torque for joint 3. Since there are no torque inputs to the base, Equations A.23 and A.24 are set equal to 0. In Equation A.23, the vertical damping force acting on the base, \( \epsilon_h \), is equal to \(-C_h\). In Equation A.24, the damping force acting on the base in the pitch direction, \( \epsilon_p \), is equal to \(-C_p\theta_p\).
Appendix B

Forward and Inverse Kinematics of Rover Manipulator

The forward and inverse kinematics of the rover manipulator are obtained with reference to Figure B.1.

To obtain the endpoint position relative to the inertial frame, first multiply the individual transformation matrices between adjacent frames. Second, multiply the resulting matrix by the location of the manipulator end-effector relative to frame \( \{3\} \). This can be summarized by the following:

\[
\overrightarrow{X}^{\text{inert}} = A_1^{\text{inert}} A_0^{-1} A_1^0 A_2^1 A_3^2 \overrightarrow{X} \quad (B.1)
\]

where the transformation matrices of \( i \) frame relative to \( i-1 \) frame are defined as follows:
In Equations B.2-B.6, the shorthand notation $c_3$ is the equivalent of $\cos(\theta_3)$ and $s_3$ is the equivalent of $\sin(\theta_3)$.

After Equation B.1 is computed, the forward kinematics of the end-effector with respect to the inertial frame is obtained. It is the following:
\[ X^{\text{inert}} = L_0 s_p + ac_p + L_1 s_p + (L_2 c_2 + L_3 c_{23}) c_1 c_p + (L_2 s_2 + L_3 s_{23}) s_p \]  
\[ (B.8) \]

\[ Y^{\text{inert}} = (L_2 c_2 + L_3 c_{23}) s_1 \]  
\[ (B.9) \]

\[ Z^{\text{inert}} = h + L_0 c_p - a s_p + L_1 c_p - (L_2 c_2 + L_3 c_{23}) c_1 s_p + (L_2 s_2 + L_3 s_{23}) c_p \]  
\[ (B.10) \]

In order to command the rover manipulator to follow the desired endpoint trajectory, the desired joint positions must be calculated at every time step. This is accomplished through inverse kinematics, where the desired endpoint position is specified and the joint positions calculated as opposed to forward kinematics, where the joint positions are specified and the endpoint position calculated. The simple relations needed for inverse kinematics are the following:

\[ X^{\text{inert}} = A^{-1}_{-1} \times \]  
\[ (B.11) \]

\[ X^{-1} = [A^{-1}_{-1}]^{-1} \times^{\text{inert}} \]  
\[ (B.12) \]

\[ A^{-1}_{-1} = \begin{bmatrix} R^{-1}_{-1} & L^{-1}_{-1} \\ 0 & 1 \end{bmatrix} \]  
\[ (B.13) \]

\[ [A^{-1}_{-1}]^{-1} = \begin{bmatrix} (R^{-1}_{-1})^T & -(R^{-1}_{-1})^T \times P^{-1}_{-1} \\ 0 & 1 \end{bmatrix} \]  
\[ (B.14) \]

\[ X^{-1} = \begin{bmatrix} X_{dn} \\ Y_{dn} \\ Z_{dn} \end{bmatrix} \]  
\[ (B.15) \]

where \( X^{\text{inert}} \) is the vector of desired endpoint position relative to the base frame \( \{-1\} \). To obtain the joint positions needed to bring the rover manipulator end-effector into position, simple geometric relations were used. First, the distance \( r \) is obtained:

\[ r = \sqrt{(X_{dn} - a)^2 + Y_{dn}^2} \]  
\[ (B.16) \]
Using Equation B.16, the desired joint 1 position is simply the following:

\[ \theta_1 = \sin^{-1} \left( \frac{y_{da}}{r} \right) \]  

(B.17)

To obtain the other desired joint positions, rotate joint 1 by \(-\theta_1\) degrees to bring links 2 and 3 onto the XZ plane of the base frame \{-1\}. Thus, we arrive at the endpoint \((X_{dm}, 0, Z_{dn})\) defined relative to the base frame \{-1\}. From Figure B.1, the following relations were used to solve for the desired joint 2 position, \(\theta_2\), and joint 3 position, \(\theta_3\):

\[ r_0 = \sqrt{r^2 + (Z_{dn} - L_0 - L_1)^2} \]  

(B.18)

\[ \theta_2 - \theta_{20} = \cos^{-1} \left[ \frac{L_2^2 + r_0^2 - L_3^2}{2L_2r_0} \right] \]  

(B.19)

\[ \theta_{20} = \sin^{-1} \left[ \frac{Z_{dn} - (L_0 + L_1)}{r_0} \right] \]  

(B.20)

\[ \theta_2 = \cos^{-1} \left[ \frac{L_2^2 + r_0^2 - L_3^2}{2L_2r_0} \right] + \sin^{-1} \left[ \frac{Z_{dn} - (L_0 + L_1)}{r_0} \right] \]  

(B.21)

\[ \theta_3 = -\pi + \cos^{-1} \left[ \frac{L_2^2 + L_3^2 - r_0^2}{2L_2L_3} \right] \]  

(B.22)

The positive directions of rotations are defined with respect to the local coordinate frame using the right hand rule.