**Development of an Experimental Aircraft/Ship
Dynamic Interface Analysis Motion Facility
for the Investigation of Helicopter Manoeuvring**

by

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Abstract

Motivated by the lack of experimental motion facilities for simulating shipboard helicopter manoeuvring operations, the Carleton Experimental Shipboard Helicopter Interface Platform (C.E.S.H.I.P.) was developed as part of a collaborative project sponsored jointly by Indal Technologies Inc. (ITI), Materials and Manufacturing Ontario (MMO), and Carleton University (CU). This thesis describes the design and commissioning of the experimental motion platform, a scale-model of the Indal Technologies Inc. (ITI) Aircraft/Ship Integrated Secure and Traverse (ASIST) system with two degrees of aircraft actuation, and a scale-model reconfigurable Dead-Load Test Vehicle (DLTV) representative of a variety of maritime helicopters. The experimental motion platform uses a single degree of actuation to produce combinations of surge, sway, heave, roll, and pitch motions with maximum amplitudes approximately representative of the tenth-scale motions of a typical frigate operating in upper sea state 5 conditions. The motion platform and associated test vehicle constitute a fundamental research tool that in the current application will be used for aircraft manoeuvring testing and to support development of autonomous manoeuvring algorithms in the presence of ship motion. The experimental platform's motion and kinematic equations were validated using a 3-axis gyro-enhanced orientation sensor. A typical manual manoeuvring procedure was then tested on the motion platform with and without ship motion and the resulting probe forces were compared to demonstrate the effects of ship motion on helicopter manoeuvring. An autonomous manoeuvring algorithm was proposed to demonstrate the potential for helicopter straightening and traversing without human intervention. Developed using an existing planar simulation, *Heliman*, the autonomous algorithm was tested on the motion platform, and the results are discussed.

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Dedicated to my family and friends
for their continuous encouragement and support.

Glossary

AGV	Automated guided vehicle
ASIST	Aircraft/Ship Integrated Secure and Traverse
CESHIP	Carleton Experimental Shipboard Helicopter Interface Platform
CM	Centre of mass
CU	Carleton University
DAQ	Data acquisition
DFMA	Design for manufacture and assembly
DLA	Designated landing area
DLTV	Dead-Load Test Vehicle
DRDC	Defence Research and Development Canada
<i>HelMan</i>	Helicopter manoeuvring simulation
HPSE	Helicopter position sensing equipment
ITF	Integrated Test Facility
ITI	Indal Technologies Inc.
JSHIP	Joint Shipboard Helicopter Integration Process
MI	Moments of inertia
MMO	Materials and Manufacturing Ontario
NBDL	Naval Biodynamics Laboratory
NWG	Nose wheel guide
PD	Proportional-plus-derivative
PI	Proportional-plus-integral
PID	Proportional-plus-integral-plus-derivative
RAO	Response amplitude operator
RMS	Root mean-squared

RSD	Rapid Securing Device
RT	Real time
SMS	Ship Motion Simulator
ST	Simulation time
TPI	Turns per inch
UHMW	Ultra-high molecular weight
UTIAS	University of Toronto Institute for Aerospace Studies
VMS	Vertical Motion Simulator
VR	Virtual reality
WOD	Wind-over-deck

List of Symbols

Overscripts:

$\cdot$	(dot) First time derivative
$\ddot{}$	(double-dot) Second time derivative
$\rightarrow$	Vector

Superscripts:

$'$	(prime) Adjusted value
$i - j - k$	Local coordinate system located at the roll axis
$m - n - o$	Local coordinate system located at the yaw axis when $\Theta = 0$
$sensor$	Local coordinate system located at the centre of the accelerometer sensor
$x - y - z$	Local coordinate system located at the yaw axis on the deck
$X - Y - Z$	Global coordinate system

Subscripts:

0	Initial value
act	Actuator
$A \rightarrow I$	Points and indexes used in analysis
$claw$	Lateral claw value
cw	Caster wheel
d	Deck
exp	Expected value
f	Final value
i	Index
$joint$	Universal joint (u-joint) centre of rotation

<i>joystick</i>	Joystick input used for <i>HeliMan</i>
<i>m</i>	DLTV model
<i>max</i>	Maximum value
<i>mw</i>	Main wheel
<i>n</i>	Neutral position of DLTV trailing arm suspension
<i>p</i>	Point of interest, P
<i>post</i>	Centre post
<i>r</i>	Rotating platform
<i>RMS</i>	Root mean-squared value
<i>RSD</i>	Longitudinal RSD value
<i>s</i>	Shock assembly
<i>se</i>	Shock assembly fully extended
<i>sn</i>	Shock assembly in neutral position
<i>unit</i>	Unit length
<i>x, y, z</i>	Surge, sway, and heave directions

Variables:

A_i	Sinusoidal waveform amplitude
a	Translational acceleration magnitude
(a, b)	Coordinates of the centre of a circle, used in intersecting circles method
α	Suspension deflection angle from full shock extension
β	Angular variable used in motion platform and DLTV analysis
c	Cosine
c_i	Constants, where $i=1,2,3$
CS	Critical speed of the lead screw
Δ	Change in length, deflection, or displacement

Δt	Time period in which to generate ramping function or cubic polynomial
d_{screw}	Root diameter of the lead screw
$e(t)$	Actuating error signal of a PID controller
F	Force magnitude
$F_{support}$	End support factor used in lead screw analysis
γ	Caster wheel orientation
h_1	Height from base to u-joint
h_2	Height from u-joint to the underside of the rotating platform
h_3	Height from the underside of the rotating plate to the top side of the deck
I_{xx}, I_{yy}, I_{zz}	DLTV moments of inertia
K_{cr}	Critical proportional gain using Ziegler-Nichols tuning rules
K_p	Proportional gain
k	Suspension stiffness
κ	Kingpin inclination angle
ℓ	Constant length
L	Variable length
L_{stroke}	The total actuator displacement required to move the platform through the full angular range of motion
$L_{support}$	Length between the lead screw supports
λ	Deck input yaw angular displacement
M	Mass
P	Critical thrust rating of the lead screw
P_{cr}	Period corresponding to the critical gain using Ziegler-Nichols tuning rules
Φ	Motion platform input angular displacement in the pitch direction
ϕ	Resulting deck pitch angular displacement
ψ_i	Sinusoidal waveform phase angle

q_i	Generalized coordinates in the helicopter model
r_1, r_2	Radii of circles 1 and 2 used in the intersecting circles method
$r(t)$	Reference signal of a PID controller
ρ	DLTV orientation
s	Sine
s_1	Constant dimension used in analysis
t	Time
$[T]$	Transformation matrix
T_d	Derivative time
T_i	Integral time
Θ	Motion platform input angular displacement in the roll direction
θ	Resulting deck roll angular displacement
$u(t)$	Output of a PID controller
v	Translational velocity magnitude
ω	Sinusoidal waveform frequency
w	Constant width
W	Weight
x_i, y_i	Coordinates of the point of intersection in the intersecting circles method
x_p, y_p, z_p	Coordinates of the point of interest on the deck

Vectors:

$\vec{a}$	Acceleration vector
$\vec{F}_p$	Helicopter probe force vector
$\vec{r}$	Position vector
$\vec{v}$	Velocity vector

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Chapter 1

Introduction

1.1 Motivation

The vital role of helicopter/ship dynamic interface analysis has been steadily increasing as shipboard helicopters are required to operate in more extreme ranges of sea conditions, as interoperability requirements become more prevalent, and as the demands placed on the efficiency of on-deck operations - both in terms of minimizing the number of deck personnel and reducing the required time for on-deck operations increase. The research described in this thesis addresses the increasingly important shipboard manoeuvring and traversing aspect of embarked helicopter operation using the Aircraft/Ship Integrated Secure and Traverse (ASIST) shipboard helicopter recovery and handling system, shown in Figure 1.1.

Developed by Indal Technologies Inc. (ITI), the ASIST system provides ships the capability for safely landing and immediately securing helicopters in severe sea conditions, remotely aligning them with a deck-mounted track, and traversing them into the safety of a hangar without strictly requiring that any personnel expose themselves to the risks associated with deck operations in severe conditions. With most shipboard helicopter recoveries the helicopter does not land parallel to the track, especially if operating in poor

Figure 1.1: ASIST Rapid Securing Device (RSD) (photo courtesy of ITI)

weather and sea conditions. Therefore, the helicopter must be straightened to traverse it along the deck. With the current system, a skilled operator is required to manually perform aircraft manoeuvring operations, which may be complex due to ASIST's actuation method and the helicopter's resulting motion.

The phases of shipboard helicopter operations include: approach, hover and landing, on-deck securing, rotor engage and disengage, and manoeuvring and traversing. As explained by Linn [1], there were many studies conducted for the hover, landing, and securing phases, yet few on the aircraft manoeuvring and traversing on-deck operations which are considered in this thesis. More specifically, experimental facilities that simulate both the ship motion and on-deck handling aspects of shipboard operations are lacking in this field [2]. This is the main motivation for the development of a dynamic interface analysis experimental motion platform with shipboard helicopter manoeuvring capabilities as part of a collaborative project sponsored jointly by Indal Technologies Inc. (ITI), Materials and Manufacturing Ontario (MMO), and Carleton University (CU).

ITI currently uses a full-scale Integrated Test Facility (ITF), which consists of a full-scale ASIST system and a Dead-Load Test Vehicle (DLTV) representative of a full-scale shipboard helicopter, for system analysis and evaluation, development of manoeuvring procedures, and operator training.

Figure 1.2: Navair Lakehurst fixed-base elevated platform [3]

Similar to Indal's ITF, the NAVAIR Lakehurst facility¹ [3] consists of a fixed-base elevated platform used for the training of shipboard-type landings with ITI's helicopter Recovery Assist, Securing and Traverse system (RAST) and an operational helicopter. Shown in Figure 1.2, the 60×80 ft steel deck platform is elevated, providing pilots with a realistic change in ground effects as they approach the flight deck. However, the view of the surrounding area and the lack of a hangar impairs the realism. The limitation of a fixed-base facility is that it cannot simulate the effects of ship motion on the manoeuvring of the aircraft. The ITF and the NAVAIR Lakehurst elevated platform are the only known facilities that contain full-scale securing systems, however, they lack the capability to simulate the effects due to ship motion, which is the primary motivation for shipboard securing systems.

NAVAIR also contains a six-degree-of-freedom motion platform, shown in Figure 1.3, available inside the concrete building. This is used to test equipment under simulated ship motion conditions of up to sea state² 5 before being installed on the ship. Similarly,

¹NAVAIR is the largest naval aviation facility in the Northeast, and is part of the Naval Air Warfare Center Aircraft Division (NAWCAD) in NAS Patuxent River, MD.

²The sea state scale [5] is used to quantify the severity of the sea environment in terms of wave height, modal period, and sustained wind speed. For many navies, sea state 5 represents the upper limit for shipboard helicopter operations.

Figure 1.3: Navair Lakehurst six-degree-of-freedom motion platform [3]

Figure 1.4: AUGUST six-degree-of-freedom motion platform with force feedback [4]

August Design, Inc. [4] has developed a six-degree-of-freedom motion platform to simulate the surface of the ocean under various sea states. The platform, shown in Figure 1.4, not only moves as though it were at sea but also reacts to forces acting on it, as would the actual vessel being simulated.

There are several slightly-different human-in-the-loop motion simulators that are used for shipboard operations, yet do not consider on-deck manoeuvring. Advanced Rotorcraft Technology, Inc. (ART) [2] created HeliFlightTM, a reconfigurable shipboard operations pilot training simulator available with or without a motion base, shown in Figure 1.5. HeliFlightTM uses FLIGHTLAB [6], a rotorcraft flight dynamics model developed as a

Figure 1.5: HeliFlight™ rotorcraft shipboard landing trainer [2]

simulation program capable of dynamic interface simulation and analysis of rotorcraft shipboard on-deck operation [7]. The rotorcraft/ship dynamic interface model involves all aspects relating to the effect of the ship on shipboard rotorcraft operations, including the aerodynamic interference of the rotor wake, the ship airwake, the ship turbulence, and the shipboard flight manoeuvres such as approach, descent, landing, liftoff, and departure. However, the model does not analyze the manoeuvring and traversing phases.

The Naval Biodynamics Laboratory (NBDL) [8] is a research facility located at the University of New Orleans that performs studies of human performance under various sea states using a three-degree-of-freedom (heave, pitch, and roll) Ship Motion Simulator (SMS), shown in Figure 1.6. A *Moog* six-degree-of-freedom motion platform is also available at NBDL to place human subjects in varying low level motion environments to measure their cognitive and physiological responses to the motion [9]. A motion simulator at DRA Bedford [10] contains five degrees-of-freedom to replicate realistic ship motions and perform studies of human performance with two additional degrees of freedom over the NBDL SMS. Atlantis Systems International [11], the University of Toronto Institute for Aerospace Studies (UTIAS), and Defence Research and Development Canada-Toronto (DRDC-Toronto) developed a six-degree-of-freedom reconfigurable motion platform train-

Figure 1.6: Three-degree-of-freedom Ship Motion Simulator (SMS) [12]

Figure 1.7: Atlantis six-degree-of-freedom simulator for shipboard operations [11]

ing simulator, shown in Figure 1.7, using Virtual Reality (VR) headgear for Sea King helicopter deck landings aboard a Canadian patrol frigate.

Finally, the NASA Ames Research Center [13] developed a Vertical Motion Simulator (VMS) to represent any aerospace vehicle by simply interchanging the vehicle cockpit. NASA Ames has teamed with the Joint Shipboard Helicopter Integration Process (JSHIP) program to develop a man-in-the-loop simulator that replicates the dynamic interface of an LHA-class ship and a UH-60A Blackhawk helicopter [14]. The objective for JSHIP is to develop and test the processes and mechanisms that facilitate joint helicopter and

Figure 1.8: Modelling the helicopter/ship interface with ADAMS software [16]

ship operations. The simulator is capable of being used to define wind-over-deck (WOD) launch and recovery flight scenarios and determine the limitations of the helicopter in the presence of a ship when operated by a trained pilot. Simulation trials were carried out in the NASA Ames VMS.

Simulations are available without human-in-the-loop capabilities to study the hover, landing, and securing phases. ITI developed Fortran-based simulation software, *Dynaface*[®] [15], to model the helicopter securing process in the presence of ship motion. A Riva Calzoni Helicopter-Ship Dynamic Interface Modelling Software (ARCHIMEDES) [16] uses ADAMS mechanical system simulation software to develop a general purpose stand-alone helicopter/ship dynamic interface analysis tool for helicopter securing. This model, shown in Figure 1.8 is currently available with the ASIST system, and is reconfigurable for any helicopter, ship, or securing device.

Manoeuvring has also been investigated in other contexts. Brennan and Alleyne [17] developed the Illinois Roadway Simulator, a scaled testbed used to study vehicle dynamics and controls. This facility uses a scaled vehicle that is held fixed and a road surface that moves relative to the vehicle. The research used scaling methods and model parameter experiments similar to those used in this thesis. Other related research includes: the ground handling characteristics of fixed-wing aircraft [18]; the manoeuvring of small au-

onomous helicopters in flight [19]; control strategies for automated guided vehicle (AGV) systems [20]; the control of semi-autonomous robots [21] using PID and fuzzy-logic control; and the manoeuvring of surface vessels using a fuzzy-logic controller [22]. These applications cover a variety of manoeuvring situations where the mathematical model is known. However, the manoeuvring system in this thesis consists of a three-degree-of-freedom model that is actuated using two degrees-of-freedom (ASIST) at slow speeds and large angles. In addition, the mathematical model is not available, therefore, alternative approaches are required.

As shown, to the author's knowledge, there is no existing experimental or simulation facility that addresses helicopter manoeuvring in the presence of ship motion. The specific objectives of this project are as follows:

1. develop a planar helicopter manoeuvring simulation, called *HeliMan*;
2. develop a flexible scaled motion test facility, including a scaled DLTV and ASIST model;
3. test the effects of ship motion on helicopter manoeuvring; and
4. investigate the possibility of autonomous on-deck helicopter manoeuvring using *HeliMan* and the experimental facility.

The first objective, the development of *HeliMan*, was completed and is described in the thesis entitled "Development and Validation of a Planar On-Deck Helicopter Manoeuvring Simulation" by D. R. Linn [1]. The purpose of this thesis project was to complete the remaining three objectives of the collaborative project.

This introductory chapter provides a detailed description of the project objectives, background of on-deck operations, as well as an overview of the thesis.

1.2 Background for On-Deck Operations

Shipboard helicopters have expanded the capabilities of navies and coastal patrol authorities since launching from a ship deck provides a larger operational envelope for helicopters. Shipboard helicopter launch and recovery systems, such as ASIST, have expanded the capabilities of shipboard helicopters by operating them in rougher sea conditions, such as those in the Canadian North Atlantic ocean.

Operational requirements studies for shipboard operations have been conducted by ITI for determining maximum sea states, wind envelopes, ship headings and speeds, and securing requirements, for specific combinations of ships and aircraft [23, 24]. ITI has also performed on-deck studies where securing and traversing forces on the helicopter-mounted probe, during ship motion, have been analyzed [25, 26, 27, 28]. Other studies that investigate rotorcraft on-deck handling and securing issues and establish safe operating limits are described in References [29, 30, 31, 32].

This section will provide a background of ASIST on-deck operations, characteristics of shipboard helicopters, examples of manual manoeuvring, and the generation of ship motion.

1.2.1 ASIST Securing and Traversing System

The ASIST system is categorized as an *integrated function system* [33] as it provides helicopter security during landing and a capability to safely manoeuvre and traverse a helicopter on a ship's flight deck in an integrated manner, while preventing exposure of personnel to the risks associated with deck operations. Shown in Figure 1.9, ASIST is composed of the following components:

1. Helicopter Position Sensing Equipment (HPSE) and pilot visual cues;
2. Rapid Securing Device (RSD) and track;

Figure 1.9: ITI's ASIST system (photo courtesy of ITI)

3. helicopter-mounted probe; and
4. traverse winch and hydraulic power unit.

The HPSE uses deck-mounted tracking cameras and helicopter-mounted laser beacons to monitor the helicopter's position and orientation during hover and landing. This information is used to display visual cues to direct the pilot to the Designated Landing Area (DLA), and place the RSD under the helicopter during hover to capture the helicopter-mounted probe within two seconds of landing.

The RSD manoeuvres the helicopter following landing, as shown in Figure 1.10, by applying two degrees of actuation to the probe: longitudinal ASIST actuation from the RSD, and lateral ASIST actuation from the claw. Note that the claw restrains the probe radially, but allows the probe to slide up and down in the claw, as well as roll and pitch to reduce stress on the probe and claw during manoeuvring and ship motion-induced relative motion between the helicopter and ship. The probe contains a flange at its lower end to prevent the probe from sliding out of the claw and thereby preventing excessive vertical probe travel yet allowing relative helicopter roll and pitch.

Figure 1.10: RSD claw and helicopter-mounted probe (photo courtesy of ITI)

The two independent degrees of actuation provided by the RSD are used to control the longitudinal and lateral positions of the helicopter-mounted probe. Since the helicopter is equipped with either a casterable nose or tail wheel or steerable nose wheel, a change in the helicopter orientation during manoeuvring also occurs. Therefore, the system in question contains three degrees of freedom, and two degrees of actuation, which makes the control of the helicopter's orientation challenging as the system is, in a sense, underactuated.

The longitudinal actuation of the RSD is achieved by a hydraulic motor and traverse winch using a wire rope that can drive the RSD at any velocity up to the maximum speed of 1 ft/s in a typical installation. The lateral motion of the claw uses a hydraulic cylinder for actuation and can either have a constant positive velocity of 0.13 ft/s, constant negative velocity of -0.11 ft/s, or zero velocity. The lateral claw also has travel limits of 2.96 ft to port and 2.63 ft to starboard with respect to the ship-mounted track in a typical installation. The differences in positive and negative travel limits and velocities are due to the physical properties of the RSD and hydraulic actuator.

Two features of the ASIST RSD that are used during manoeuvring include *centre-lock* and *free-float*. When activated, centre-lock allows the claw to stop at the centreline during manual manoeuvring to assist the operator in locating the centre of the track. When

Figure 1.11: Sikorsky S-70B Seahawk helicopter [34]

engaged, free-float is a feature that relieves the stress on the probe and landing gear when the helicopter is not aligned and is being traversed longitudinally. For lateral float, the lateral reaction force that occurs on the probe will move the claw towards the direction that reduces the probe force. In some installations, longitudinal float is also available and works similar to the lateral float, yet in the longitudinal direction. With the actual ASIST system that uses hydraulics, free-float is accomplished by opening the appropriate valve to allow the probe to traverse freely in the appropriate direction. Since the developed model of ASIST uses electric motors, free-float is more complicated, as will be discussed in Section 3.3.3.

1.2.2 Maritime Helicopters

According to Langlois et al. [28], differences in helicopter configuration can significantly affect helicopter securing requirements, landing gear reaction forces, and relative aircraft motions in the presence of ship motion. Therefore, the helicopter model was designed to be either reconfigurable or easily modifiable to accommodate various helicopter models and configurations by changing such properties as:

Figure 1.12: ITI's DLTV configured as a Sikorsky Seahawk

- nose or tail steerable or casterable configurations;
- leading arm, trailing arm, or prismatic oleo suspensions;
- single or dual wheels; and
- mass, dimensions, and inertial properties.

The casterable tail wheel Sikorsky S-70B Seahawk, shown in Figure 1.11, has been used as the principal aircraft in this project since a Seahawk-configured DLTV is currently available in ITI's ITF, as shown in Figure 1.12. The Seahawk is a medium-sized aircraft, with a weight of approximately 20,000 lbs. The suspension systems contain a single wheel trailing arm suspension at the main wheels and a dual wheel prismatic oleo suspension at the caster wheel with a slight kingpin inclination angle of approximately +6 degrees. Kingpin inclination, shown in Figure 1.13, is inclination of the rotational axis of the caster joint, which essentially creates a self-aligning torque to help straighten the caster assembly [1]. Tail wheel casterable helicopters may contain a tail probe, a small probe that extends into the ASIST track, preventing an unstable yawing effect during the

Figure 1.13: Kingpin inclination angle [1]

traversing stage. The tail probe is operated from the cockpit and is used during manual aircraft manoeuvring. Similarly, a Nose Wheel Guide (NWG) can be used to facilitate the traversing of nose wheel helicopters into the hangar. A secondary securing point has not been included in this project since the investigation into autonomous manoeuvring is expected to eliminate the need for such a device.

1.2.3 Aircraft Manoeuvring

Once landed and secured, the operator of the ASIST system must select and execute an appropriate sequence of manoeuvres to straighten the aircraft before traversing it into the hangar. An appropriate straightening procedure has been developed by ITI for each landing situation and helicopter configuration to satisfy the following three straightening conditions, shown in Figure 1.14:

1. the probe needs to be centred on the track;
2. the body must be aligned with the track centreline; and
3. the nose/tail wheel must be parallel to the track centreline.

Satisfying these conditions will ensure that the aircraft is aligned for traversing into the ship's hangar.

Figure 1.14: Straightening conditions for a secured helicopter (illustration courtesy of ITI)

Figure 1.15 illustrates a step-by-step manual manoeuvring example in which a cast-erable tail wheel helicopter has landed oriented towards the centreline. Notice that a reference line has been shown to illustrate where the right main wheel is aligned during straightening. Once the main wheel has been aligned with respect to the reference line, the tail wheel is swung around towards the centreline. During manual manoeuvring, it is difficult to accurately control both the longitudinal and lateral actuation simultaneously. Therefore, as shown in the figure, each step, for the most part, actuates only one of the controllable degrees of actuation while allowing the claw to free-float in the other direction. Occasionally, as in Step 2, a small amount of longitudinal motion is required during lateral actuation to reduce the forces on the probe and tires. After Step 3, the helicopter appears to be straightened, however the tail wheel is reversed and needs to be rotated 180 degrees before the three straightening conditions are satisfied.

In some installations, a *V slot* at the end of the ASIST track is used to guide a tail probe during traversing. In this case, once the helicopter has landed, it must be manoeuvred so that the tail probe can be dropped into the V slot and traversing can occur without difficulty. Step 4 reverses the helicopter past the V slot to allow the tail wheel to be rotated parallel to the centreline during Steps 5 and 6. Finally, in Step 7, the tail probe is dropped in the V slot, which will guide the tail wheel to the centreline. In Step 8, with the centre lock on and while moving forward, the claw is moved laterally until it reaches the centre

Figure 1.15: Tail wheel helicopter straightening example oriented towards the centreline (illustration courtesy of ITI)

where it will stop, due to the centre-lock, completing the straightening procedure.

Figure 1.16 illustrates a manual manoeuvring example in which the helicopter has landed oriented away from the centreline. This example is shown because it uses free-float in Step 2 to relieve any stress on the probe and landing gear while actively moving the claw aft, allowing the probe to coast away from the edge. The remainder of the steps are similar to those in the first example.

Figure 1.16: Tail wheel helicopter straightening example oriented away from the centreline (illustration courtesy of ITI)

1.2.4 Ship Motion

The motion of a ship at sea is governed by the ship design and by the wave conditions in which the ship operates [35]. The conventional and most widely used method for generating ship motion is to calculate the ship motion based on a linear frequency-domain approach [24]. This assumes that ship motion amplitudes and related forces are proportional to the wave amplitude [36]. This method has been demonstrated to work well for small to moderate ship motions resulting from sea conditions up to and including sea state 5 in the case of frigates. However, for larger amplitude ship motion, resulting from extreme sea conditions (beyond sea state 5 in the case of frigates), it is necessary to include signif-

icant nonlinearities in the ship kinematics and hydrodynamics which limit the accuracy of linear methods [24].

Established methods and corresponding computer programs, such as SMP [37] and SHIPMO [38] are widely available for calculating response amplitude operators (RAOs) based on a description of the ship and the operating environment. The resulting RAOs are the amplitudes, frequencies, and phases of the sinusoidal ship response solution components. The proprietary program SHIPSIM [39], developed by ITI, then combines the sea spectrum with RAOs to produce ship response spectra for each component of the ship motion [35]. This process is then repeated for each combination of ship heading, ship speed, and sea state.

Evaluation of prescribed ship motion generated from RAOs reduces to evaluating a time varying trigonometric series for each ship degree of freedom to obtain the displacements and similarly to obtain the velocities and accelerations. Consequently, long sequences of ship motion can be generated very efficiently. Further, because the ship motion is a prescribed function of time, particular ship motion time histories can be identified by their times of occurrence [24].

Large amplitude ship motion in extreme seas, however, must be evaluated using transient dynamic time-domain computer simulation methods such that the significant nonlinearities associated with ship kinematics and hydrodynamics can be included [24]. To model nonlinear effects accurately, these methods predict ship motions in the time domain, which results in a slower solution speed than linear methods. MARIN's FREDYN Program [40] is a widely used nonlinear ship motion program. FREDYN does not have load prediction capability; however, it can model ship manoeuvring [36].

While either of these methods can be used within the current project, the most widely used method, linear RAO-based motion, has been the primary source of ship motion as motions can be generated very efficiently using thoroughly validated methodology and can

be generated analytically for a complete range of headings, speeds, and sea conditions [24].

For each of the ship configuration coordinates, representative ship motion can be generated by combining N sinusoidal waveforms described by appropriate amplitude, frequency, and pseudo-random phase parameters for each of the six degrees-of-freedom. In this case, with only one controllable degree of freedom, the appropriate parameters are extracted for roll from an ASCII data file allowing an input file of desired roll motion (Θ) to be created by summing the N sinusoidal waveforms over time using the following equation:

$$\Theta = \sum_{i=1}^N A_i \sin(\omega_i t + \psi_i) \quad (1.1)$$

where A_i is the roll amplitude of the waveform (deg), ω_i is the wave frequency encountered by the ship (rad/s), and ψ_i is the phase angle (rad). As will be described in Section 3.3.4, this input roll data is saved to a file and input to the motion control program.

The input roll velocities ($\dot{\Theta}$) and accelerations ($\ddot{\Theta}$) can be calculated similarly by taking the time derivative and second time derivative, respectively, of Equation 1.1:

$$\dot{\Theta} = \sum_{i=1}^N A_i \omega_i \cos(\omega_i t + \psi_i) \quad (1.2)$$

$$\ddot{\Theta} = \sum_{i=1}^N -A_i \omega_i^2 \sin(\omega_i t + \psi_i) \quad (1.3)$$

Ship motion data files were generated for sea states 4 to 6, each with 15 degree increments of ship headings between 0 and 180 degrees, and ship speed increments of 5 knots between 5 and 25 knots, for a total of 195 data files. When choosing an appropriate input file, it is important to choose a sea state, speed, and heading combination that will not exceed the peak conditions for which the motion platform was designed.

1.3 Project Objectives

This collaborative project is the first step towards a helicopter/ship dynamic interface analysis test facility at Carleton University. The following sections briefly describe the four phases of the project dealing with shipboard helicopter manoeuvring.

1.3.1 Phase I: Development of a Planar Manoeuvring Simulation

The first phase of the project involved developing an interactive transient-dynamic simulation of a shipboard helicopter and handling system, called *HeliMan*, including a four-degree-of-freedom helicopter model with a casterable nose or tail wheel, two-degree-of-freedom ASIST securing and actuation device, and relevant elements of the ASIST control system. The simulation was validated successfully using ITI's DLTV and ITF. In the simulation, aircraft motions result from commanded force profiles applied by the operator to the helicopter-mounted ASIST securing probe by the ASIST RSD claw. A detailed low-speed tire model is used to accurately represent complex tire-deck interactions since modelling a slow moving tire, as in this application, requires accurate methods for modelling relaxation length and cornering force generation. This required using non-steady-state properties to predict the behaviour of the tires, thereby making the tire model the most complex part of the simulation.

Figure 1.17 illustrates the four degrees of freedom included in the helicopter model, where the generalized coordinates q_i are: the linear x position of the helicopter centre of mass (CM) (q_1); the linear y position of the helicopter CM (q_2); the orientation of the helicopter body (q_3); and the orientation of the optionally casterable or steerable auxiliary wheel assembly relative to the helicopter body (q_4).

Figure 1.17: Schematic of the dynamic helicopter model used in *HeliMan* [1]

1.3.2 Phase II: Development of an Experimental Motion Platform

In the second phase of the project, an experimental facility has been developed such that the effect of ship motion on the manoeuvring sequences can be investigated. The scaled motion test facility contains a flexible motion platform, two degrees of aircraft manoeuvring actuation representing ASIST, and a DLTV capable of representing a variety of scaled maritime helicopters.

A challenge in this project was to use a single degree of actuation to produce ship motion effects in multiple directions, since ship motion is generally fully three-dimensional, thereby involving six degrees-of-freedom. Simulating representative ship motion was accomplished by using a single linear actuator on the motion platform to produce roll motion while allowing the deck to be reoriented (yawed) to produce a combination of roll and pitch motions. In addition, as the result of kinematic coupling between linear and angular

Figure 1.18: Experimental motion platform

motions, the model can experience a combination of roll, pitch, surge, sway, and heave motions with the single linear actuator, depending of the location of the model on the deck and the yaw orientation. The drawback is that the resulting motions are coupled and in phase, which is generally not the case with three-dimensional ship motion.

Since we are interested in examining the manoeuvring procedure once the helicopter has been secured, a tenth-scale representation of the ASIST RSD has been developed. The physical model contains the two degrees of on-deck aircraft actuation: the longitudinal motion of the RSD and the lateral motion of the RSD claw (the actual securing point), which interfaces with the DLTV through a representative probe.

A reconfigurable DLTV has been developed to represent a tenth-scale model of a casterable nose or tail-wheel aircraft, such as the widely used Sikorsky S-70B Seahawk. The model contains a full suspension system and an adjustable frame to represent different helicopter configurations (nose or tail wheel and steerable or casterable wheel designs). The DLTV is also instrumented to measure helicopter orientation, caster wheel orientation, probe force, and suspension deflections during manoeuvring with and without ship motion.

The completed motion platform and associated test vehicle, shown in Figure 1.18, constitutes a fundamental research tool that in the current application is used primarily for investigation of autonomous manoeuvring by testing controllers developed using *HeliMan*. However, the motion facility is also beneficial as a training facility and for dynamic interface analysis. In addition, the motion platform is flexible so that it can be used to explore a variety of dynamic interface issues beyond validating the simulation models.

1.3.3 Phase III: Development of a Motion Control System

The third phase of the project involved developing a motion control system to control the three degrees of platform actuation and accommodate the required sensors for the purpose of validating the platform's motion and performing testing on the DLTV. The motion control hardware components were chosen to be compatible with National Instruments (NI) LabView software and the NI LabView programs were developed to allow several control modes. Validation tests were carried out to explore the platform's full range of operation and ensure accurate motions, after which reliable DLTV manoeuvring tests were performed.

1.3.4 Phase IV: Investigate Autonomous Manoeuvring

The fourth phase of the project involved investigating the possibility of autonomous manoeuvring. This consisted of developing and testing a manoeuvring algorithm using the planar manoeuvring simulation from Phase I, the experimental motion platform facility developed in Phase II, and the motion control capabilities of Phase III of the project.

It has been assumed that with minor adjustments to the HPSE hardware and photogrammetry algorithm, the position and orientation of the helicopter while on deck could be available to a controller, in addition to the position and orientation above the deck during recovery. Therefore, the availability of on-deck aircraft position and orientation

information, from the HPSE, combined with the actuation capabilities provided by the existing ASIST system hardware, provide the potential for completely autonomous aircraft securing, manoeuvring, and traversing capability. This would ease the operator's task by automatically selecting the optimal manoeuvring sequence and executing it to within tighter tolerances and with greater efficiency and reliability than those that a human operator could achieve. This may be of particular importance in the future where 180° reorientation manoeuvres and traversing may be required for operational reasons or when a skilled ASIST operator is not available on the ship. All that is required to meet this goal is the appropriate autonomous manoeuvring control algorithm.

Autonomous on-deck manoeuvring has previously not been addressed in part due to the difficulty in developing a mathematical model of the manoeuvring process caused by the significant nonlinearity and complexities involved, such as the complex tire-deck interactions modelled in Phase I. Development of a planar manoeuvring simulation and experimental motion platform in the previous phases of this project have provided the analytical tools required to develop and test the autonomous manoeuvring algorithm.

1.4 Thesis Overview

This introductory chapter provided a detailed description of the thesis objectives, a brief introduction to ITI's ASIST system and shipboard helicopter manoeuvring operations, and a background of ship motion generation techniques. In addition, a detailed literature review was included to provide an overview of the existing shipboard helicopter simulation facilities.

The next three chapters each describe one of the three objectives accomplished in this thesis. Chapter 2 describes the development of the motion platform facility and associated DLTV, including the required scaling laws, the design requirements, and the

analysis performed. Chapter 3 describes the development, validation, and testing of the motion control system required to operate the motion facility using LabView software. Chapter 4 describes the investigation into autonomous manoeuvring, the development of the autonomous manoeuvring algorithm, and the results of the tests performed using *Heliman* and the experimental motion platform. Chapter 5 contains conclusions and recommendations for future research.

Chapter 2

Experimental Motion Test Facility

This chapter discusses Phase II of the project, the development of the experimental motion platform and associated DLTV. The motion platform and DLTV were designed and fully-manufactured by the author using the facilities at CU and the assistance of the mechanical and aerospace machine shop staff. Aspects of the motion platform development that will be discussed in this chapter include: the design requirements and scaling laws used to accurately design the scaled helicopter model and produce the appropriate scaled ship and ASIST motions; the conceptual design for the motion platform, ASIST system, and DLTV; and the analysis of the platform motions used for actuation selection and motion control, discussed in Chapter 3. The detailed drawings and in-depth design details can be found in the motion platform design report [41].

2.1 Design Requirements

When defining the design requirements for the motion platform facility, a scaling factor of $R = \frac{1}{10}$ was chosen because it resulted in a reasonable helicopter model size and weight, as will be described in this section. The design requirements are as follows:

1. simulate various magnitudes of ship motion for a tenth-scale frigate in up to sea state 5 conditions;
2. accommodate a representative scale model of ASIST;
3. represent a tenth-scale reconfigurable helicopter with a full suspension system and deformable tires;
4. support the required instrumentation;
5. incorporate adjustability for future applications; and
6. design for manufacture and assembly (DFMA).

As shown, the motion platform facility design requirements are functional, rather than optimized. Therefore, for the most part, it was not important to optimize the strength and weight of each component since the loads are relatively small, and the greater challenge was to meet the operational design requirements, which will be further discussed in Section 2.2.

Ship motion in up to sea state 5 conditions was chosen because this is within the operational limits of ASIST and spans the range adopted by many navies for shipboard helicopter operations. A speed of 10 knots has been chosen since this is the more common operational ship speed for fuel efficiency [26]. The ship motion convention that is used consistently throughout this thesis is a right-hand coordinate system, where the translational directions are surge (x), sway (y), and heave (z), and the angular directions are roll (θ), pitch (ϕ), and yaw (λ). The positive directions are identified in Figure 2.1.

2.1.1 Scaling Laws

According to Baker [42], a model may be used to accurately predict the performance of a physical system if the model has specific similarities with the physical system. Various

Figure 2.1: Ship motion convention

similarity types have been summarized in a report by Linn [43]; however, Linn used dimensional similarity to develop a set of scaling laws. This process is called dimensional analysis and is used as a tool to achieve dynamic similarity, which requires the scaling of dimensions, masses, accelerations, and forces. Dimensional similarity is achieved by defining the physical system with a set of dimensionless parameters. Two systems are defined to be dimensionally similar if all the dimensionless variables describing the systems are identical in construction and in magnitude [44]. It can then be concluded that the behaviour of the two dimensionally-similar systems can be closely correlated and hence the results of measurements on either one can be projected to the other [44]. Therefore, once properly scaled, the forces on the DLTV, for example, that are easily measurable on the scale model can be used to predict the full-scale values.

The dynamic system in question contains a tenth-scale model of a typical helicopter, currently in the configuration of Sikorsky Seahawk. Therefore, the model's mass and dimensions must be properly scaled, as well as the motions that result from both the ASIST system and the ship motion of a typical frigate. Using dimensional analysis, Linn [43] developed a set of scaling laws to be used to scale the ship motions:

$$(\theta, \phi, \lambda)_{\frac{1}{10}} = (\theta, \phi, \lambda)_1 \quad (2.1)$$

$$(\dot{\theta}, \dot{\phi}, \dot{\lambda})_{\frac{1}{10}} = \sqrt{\frac{1}{R}}(\dot{\theta}, \dot{\phi}, \dot{\lambda})_1 \quad (2.2)$$

$$(\ddot{\theta}, \ddot{\phi}, \ddot{\lambda})_{\frac{1}{10}} = \frac{1}{R}(\ddot{\theta}, \ddot{\phi}, \ddot{\lambda})_1 \quad (2.3)$$

$$(\Delta r_x, \Delta r_y, \Delta r_z)_{\frac{1}{10}} = R(\Delta r_x, \Delta r_y, \Delta r_z)_1 \quad (2.4)$$

$$(\Delta \dot{r}_x, \Delta \dot{r}_y, \Delta \dot{r}_z)_{\frac{1}{10}} = \sqrt{R}(\Delta \dot{r}_x, \Delta \dot{r}_y, \Delta \dot{r}_z)_1 \quad (2.5)$$

$$(\Delta \ddot{r}_x, \Delta \ddot{r}_y, \Delta \ddot{r}_z)_{\frac{1}{10}} = (\Delta \ddot{r}_x, \Delta \ddot{r}_y, \Delta \ddot{r}_z)_1 \quad (2.6)$$

$$(2.7)$$

where R is the scaling ratio, $R = \frac{1}{10}$; θ , ϕ , and λ are the roll, pitch, and yaw angular displacements, respectively; Δr_x , Δr_y , and Δr_z are the translational displacements in the surge, sway, and heave directions; and $(\dot{})$ and $(\ddot{})$ indicate first and second time derivatives, respectively, of the above translational and angular motions.

Other scaling laws of importance that affect the scaling of the model helicopter and ASIST system motions are [44, 45]:

$$(M)_{\frac{1}{10}} = R^3(M)_1 \quad (2.8)$$

$$(\ell)_{\frac{1}{10}} = R(\ell)_1 \quad (2.9)$$

$$(v)_{\frac{1}{10}} = \sqrt{R}(v)_1 \quad (2.10)$$

$$(a)_{\frac{1}{10}} = (a)_1 \quad (2.11)$$

$$(t)_{\frac{1}{10}} = \sqrt{R}(t)_1 \quad (2.12)$$

$$(\omega)_{\frac{1}{10}} = \sqrt{\frac{1}{R}}(\omega)_1 \quad (2.13)$$

$$(F)_{\frac{1}{10}} = R^3(F)_1 \quad (2.14)$$

where M is mass, ℓ is length, v is translational velocity, a is translational acceleration, t is time, ω is frequency, and F is force.

As shown in Equation 2.12, time scaling is required for dimensional similarity between the model and the full-scale helicopter. However, during training, when time cannot be scaled, the time-dependant motions need to be modified appropriately to account for the time difference. For example, during simulation, translational velocity can be expressed as:

$$v_{\frac{1}{10}} = \frac{(\ell)_{\frac{1}{10}}}{(t)_{\frac{1}{10}}} = \frac{R(\ell)_1}{\sqrt{R}(t)_1} = \sqrt{R}(v)_1 \quad (2.15)$$

However, if $(t)_{\frac{1}{10}} = (t)_1$, then Equation 2.15 becomes:

$$v_{\frac{1}{10} RT} = \frac{(\ell)_{\frac{1}{10}}}{(t)_{\frac{1}{10}}} = \frac{R(\ell)_1}{(t)_1} = R(v)_1 \quad (2.16)$$

where $v_{\frac{1}{10} RT}$ represents the scaled linear velocity used during training (in real time). Similarly, other time-dependent variables become:

$$(\dot{\theta}, \dot{\phi}, \dot{\lambda})_{\frac{1}{10} RT} = (\dot{\theta}, \dot{\phi}, \dot{\lambda})_1 \quad (2.17)$$

$$(\ddot{\theta}, \ddot{\phi}, \ddot{\lambda})_{\frac{1}{10} RT} = (\ddot{\theta}, \ddot{\phi}, \ddot{\lambda})_1 \quad (2.18)$$

$$(a)_{\frac{1}{10} RT} = R(a)_1 \quad (2.19)$$

$$(\omega)_{\frac{1}{10} RT} = (\omega)_1 \quad (2.20)$$

Table 2.1: Typical peak motions of a full-scale typical frigate operating in an upper sea state 5 condition [43]

Motions	Displacement	Velocity	Acceleration
Roll	20.4 deg	12.7 deg/s	8.4 deg/s ²
Pitch	5.4 deg	6.1 deg/s	7.4 deg/s ²
Yaw	13.0 deg	2.8 deg/s	2.1 deg/s ²
Surge	238.2 ft	18.3 ft/s	22.1 ft/s ²
Sway	71.2 ft	11.2 ft/s	11.0 ft/s ²
Heave	37.1 ft	15.9 ft/s	17.6 ft/s ²

2.1.2 Representative Ship Motions

Table 2.1 shows a set of peak motions that has been tabulated for a typical frigate in upper sea state 5 conditions over a full range of headings and speeds¹[43]. These values include the peak displacement, velocity, and acceleration for all six ship degrees-of-freedom.

The desired scaled peak motions to be used during simulation were determined by multiplying the peak motions shown in Table 2.1 by the appropriate scaling factors indicated in Equations 2.1 to 2.14. The resulting desired peak motions used during simulation are shown in Table 2.2.

The desired scaled motions to be used during training were determined by multiplying the time-dependent peak motions shown in Table 2.1 by the appropriate real time scaling factors indicated in Equations 2.16 to 2.20. The resulting peak motions to be used during training are shown in Table 2.3.

The representative period for the full-scale roll motion can be calculated from Table 2.1 to be approximately 10 sec. However, the representative period for the scaled roll motion can be calculated from Table 2.2 to be approximately 3 s, which agrees with the frequency scaling law from Equation 2.13. This will make the motion platform appear to be moving relatively quickly compared to a full-scale ship. However, this scaled motion is required during simulations to obtain the proper dynamic similarity.

Due to technical design and budgetary considerations, attention was focussed on the motions of greatest importance. By performing various tests using the helicopter/ship dynamic interface

¹Includes headings from 0 to 360 degrees relative to the principal wave direction in 15 degree increments, and ship speeds from 5 to 25 knots in 5 knot increments.

Table 2.2: Peak motions of a tenth-scale model of a typical frigate in simulation time

Motions	Displacement	Velocity	Acceleration
Roll	20.4 deg	40.1 deg/s	84.2 deg/s ²
Pitch	5.4 deg	19.2 deg/s	74.1 deg/s ²
Yaw	13.0 deg	2.4 deg/s	20.6 deg/s ²
Surge	23.8 ft	5.8 ft/s	22.1 ft/s ²
Sway	7.1 ft	3.5 ft/s	11.0 ft/s ²
Heave	3.7 ft	5.0 ft/s	17.6 ft/s ²

Table 2.3: Peak motions of a tenth-scale model of a typical frigate in real time

Motions	Displacement	Velocity	Acceleration
Roll	20.4 deg	12.7 deg/s	8.4 deg/s ²
Pitch	5.4 deg	6.1 deg/s	7.4 deg/s ²
Yaw	13.0 deg	2.8 deg/s	2.1 deg/s ²
Surge	23.8 ft	1.8 ft/s	2.2 ft/s ²
Sway	7.1 ft	1.1 ft/s	1.1 ft/s ²
Heave	3.7 ft	1.6 ft/s	1.8 ft/s ²

simulation program *Dynaface*[®], it was found that the motions of greatest importance are roll and pitch. In the current configuration with a single degree of actuation, the roll motion will be scaled appropriately, and reach the magnitudes shown in Table 2.2. The pitch motion and secondary translational motions will occur in phase with the roll motion as a result of kinematic coupling between translational and angular motions by rotating the yaw orientation of the deck, yet will not necessarily reach the full magnitudes of the motions shown in Table 2.2. However, the yaw orientation of the deck allows the capability for controlling the resulting magnitudes of the coupled motions, described further in Section 2.3.1. Since *Dynaface*[®] simulations have shown that yaw motion has negligible effect on helicopter/deck interface forces, it has been ignored.

In addition to the roll and pitch motions described in Table 2.2, it was decided that it would be desirable to allow the motion platform to have a greater angular displacement for potential subsequent studies in more extreme conditions. Therefore, the motion platform was designed with the ability to produce angular displacements of up to 45 degrees with little incremental cost associated with this capability.

Table 2.4: Full-scale and tenth-scale RSD displacement limits and velocities in simulation time

	Full-Scale	Tenth-Scale
Positive Claw Velocity	1.56 in/s	0.49 in/s
Negative Claw Velocity	1.32 in/s	0.42 in/s
RSD Velocity	12.0 in/s	3.79 in/s
Positive Claw Travel	35.5 in	3.55 in
Negative Claw Travel	31.6 in	3.16 in
RSD Travel	550 in	55.0 in

Table 2.5: Scaled RSD velocities in real time

	Tenth-Scale
Positive Claw Velocity	0.16 in/s
Negative Claw Velocity	0.13 in/s
RSD Velocity	1.20 in/s

2.1.3 Representative ASIST Motions

The full-scale RSD dimensions and velocities were scaled using Equations 2.9 and 2.10 for RSD translational displacements and velocities, respectively. Table 2.4 shows the full-scale and resulting dynamically-scaled dimensions and velocities required during simulation to provide the appropriate RSD travel limits and velocities. However, during training, where time cannot be scaled, the full-scale velocities from Table 2.4 must be scaled using Equation 2.16, resulting in the real-time scaled velocities shown in Table 2.5.

2.1.4 DLTV Model Scaling

The scaling of the DLTV was accomplished using Equations 2.8, 2.9, and 2.14 for the scaling of mass, length, and force, respectively. Since the weight of a full-scale Seahawk helicopter is approximately 20,000 lb, the scaled DLTV must be approximately 20 lb. The resulting scaled DLTV dimensions are shown in Figure 2.2.

Figure 2.2: Scaled DLTV dimensions

2.2 Conceptual Design

The motion platform was designed to satisfy the motion requirements defined in Section 2.1, accommodate the linear actuators required for the ASIST model and interface with a DLTV. A description of each of these components and their functions will be discussed throughout this section. A design requirement of the motion platform facility that will be highlighted is its adjustability for future applications, such as: the addition of a second angular degree of freedom; the representation of other typical helicopter models; and the modelling of other aircraft securing systems.

Figure 2.3: Top and bottom views of the experimental motion platform

2.2.1 Motion Platform Design

The motion platform structure, shown in Figure 2.3, consists of four main components: the top platform, the rotating plate, the support assembly, and the bottom platform. A total weight of 100 lbs was allocated for the actuated components, mainly the top platform and internal components (including the ASIST motors and actuators), and the rotating plate. However, as a result of using aluminum components whenever possible and a light weight deck material to reduce the total load actuated, the total actuated weight was approximately 75 lbs. Other than the material choice, no other optimization techniques were used since, as stated in the design objectives, meeting the functionality requirements imposed the greater design constraint. The majority of the non-actuated components were chosen to be steel for low cost and ease of welding. A description of each of these components and their functions will be discussed here.

Top Platform

The top platform, shown in Figure 2.4, consists mainly of the top deck and the aluminum support frame. This assembly was designed to accommodate the ASIST actuation, described in Section 2.2.2, and support the DLTV, described in Section 2.2.3.

Figure 2.4: Exploded view of the top platform

The deck is the top surface on which the DLTV is manoeuvred and therefore must accommodate both the DLTV weight, as well as the ASIST RSD requirements. Laminated high-density foamboard, painted with non-skid paint to represent the actual ship deck surface, was chosen due to its high stiffness-to-weight ratio and low cost compared with alternatives. The deck was sized to allow the scale-model helicopter to perform a full 360-degree rotation and to travel along the deck a distance of approximately four times the length of the model, resulting in a 47 in $\times$ 80 in $\times$ 1 in-thick slab. This allows ample room for the model's manoeuvring to be performed.

As shown in Figure 2.4, an aluminum-reinforced centre track is required in the deck to allow the longitudinal ASIST actuator to run below the deck, yet attach to the lateral ASIST actuator that runs above the deck, similar to the full-scale design. To support the lateral ASIST actuator, two aluminum runners were installed parallel to the centre track to allow the RSD to run smoothly along the deck.

Figure 2.5: Rotating plate and support assembly

Finally, to allow ventilation to the longitudinal ASIST motor that is installed below the deck, a cut-out was provided through the deck and a perforated steel plate was installed above the motor. It is important that the DLTV be capable of passing over this area, as well as the centre track and runners, with minimal disturbance to the wheels. Therefore, $\frac{1}{4}$ in thick adhesive foam strips were installed in the track to allow the ASIST hardware to pass through, yet prevent the wheels from dropping into the slot.

A welded aluminum frame is used to support the deck and to house the ASIST longitudinal linear actuator. It was decided to allow 1 in clearance between the frame and the deck for the longitudinal ASIST actuator. Therefore, the perimeter of the frame was made from welded 2 in $\times$ 1 in $\times$ 0.120 in-thick aluminum 6063-T5 architectural rectangular tubing. Velcro is used to securely attach the deck to the frame, yet allow easy removal. The inner strengthening of the frame uses 1 in $\times$ 1 in $\times$ 0.120 in aluminum 6061-T6 structural square tubing, allocating a 1 in space between the deck and the inner frame as clearance for the ASIST linear actuator. This small gap created some difficulty in obtaining an appropriate actuator. As will be discussed in Section 2.2.2, the solution created additional deck support along the ASIST actuator that runs below the centre track, in addition to the support by the perimeter of the frame. It was found

Figure 2.6: Bottom platform, support assembly, and linear actuator

that further additional supports were required between the deck and the frame, running parallel to the track, along the quarter points of the frame, as shown in Figure 2.4.

Rotating Plate

The underside of the rotating plate, shown in Figure 2.5, is attached to the actuator and posts and allows the frame and deck to rotate about the centre post to allow coupled angular deck motions. The rotating plate yaw orientation remains constant, while the frame and deck rotate about a centre pin on the top side of the plate. Four clamps are used to secure the frame to the rotating plate at the desired orientation before actuating the platform.

The rotating plate is a 36 in diameter solid aluminum $\frac{1}{4}$ in plate and is attached to the support assembly through removable and interchangeable brackets to allow for adjustability.

Figure 2.7: Adjustable bracket for the centre post support bars

Support Assembly

The support assembly, shown in Figure 2.6, is composed of the following components: the centre post, the linear actuator, the support post, and the centre post supports. The steel centre post is attached to the rotating plate by a universal joint to eliminate yaw but allow ± 45 degrees of both roll and pitch of the platform. To achieve greater magnitudes of translational motion, the centre post assembly is invertible, placing the motion platform's axis of rotation at the bottom of the centre post assembly. However, in this configuration, the steel centre post becomes an actuated component and adds to the total accelerated mass. However, no problems have been anticipated with this larger moving mass.

Four support bars were installed from the bottom platform to the centre post to eliminate deflection and vibration of the centre post during motion. They were designed to be removable and adjustable along the length of the centre post to allow for its inversion, as shown in Figure 2.7.

A steel support post is required to prevent the platform from rotating about the non-actuated pitch axis. This post is temporary, as it was designed to be easily replaced in the future with a second actuator, identical to the roll actuator, for uncoupled roll and pitch angular degrees of freedom.

Figure 2.8: Model of the RSD actuator installed in the frame

At the lower attachment points, both the support post and the linear actuator use universal joints, identical to the one used on the centre post to allow the pitch and roll of those members. At the top attachment point, they use spherical joints, to allow slight misalignment of these members. It is important that the axis of rotation at the centre universal joint be at the same height as the support post's and linear actuator's spherical joints to ensure rotation about the proper axis. Also, when the centre post is inverted, the universal joints at the bottom of the support post and linear actuator are required to be at the same height.

Bottom Platform

The bottom platform, also shown in Figure 2.6, is the base of the structure and was made to be stable enough to not require securing to the floor. The dimensions are 36 in x 36 in and were made from steel channel C4 @ 7.25 lb per ft. The bolts used to secure the brackets to the base, as well as the centre post support brackets, are threaded into the channel for simplicity.

2.2.2 ASIST RSD Design

The ASIST RSD model was designed to have the longitudinal RSD actuator installed in the 1 in space between the frame and the deck and the lateral claw actuator installed above the deck, as shown in Figure 2.8. The limited space allocated for the linear actuator and motor within

Figure 2.9: Model of the lateral RSD actuator

the frame was the biggest design challenge for the longitudinal actuator since actuators of that length are not available with such a low profile. The only challenge for the lateral actuator was to make the actuator and motor assembly compact enough to fit within the DLTV's main wheels and below the DLTV's frame, which was not feasible with an off-the-shelf actuator.

Therefore, it was required to build a custom ACME lead screw actuator, for both the longitudinal and lateral degrees of actuation, using 2 in $\times$ 1 in rectangular tubing and embedding an ACME nut in an Ultra-High Molecular Weight (UHMW) polyethylene low-friction, non-abrasive block to slide within the rectangular tubing, as shown in Figure 2.9. The rectangular tubing serves two purposes: to support the deck below the centre track; and to prevent the UHMW block and nut assembly from rotating with the lead screw. The sizing of the ASIST linear actuator is further discussed in Section 3.1.2.

2.2.3 DLTV Design

The DLTV, shown in Figure 2.10, represents a tenth-scale model of the widely used Sikorsky Seahawk, yet contains an adjustable frame to represent different helicopter configurations. Features of the DLTV include: adjustable wheel base and track width; adjustable casterable nose or tail wheel; adjustable probe length and position; adjustable centre of gravity and inertial properties; and instrumentation to measure probe securing forces, DLTV position and orientation, caster wheel orientation, and suspension deflection.

Figure 2.10: DLTV in the Sikorsky Seahawk configuration

Figure 2.11: DLTV in the Sikorsky Seaking configuration

Adjustable Structure

The DLTV has the capability to expand its wheel base and track width to represent helicopter models having a footprint as small as a Seahawk, or as large as a Seaking, as shown in Figure 2.11. It should be noted that while some in-service maritime helicopters, such as the EH-101 and H-92 are heavier than the Seaking, their footprints remain smaller.

The main frame was fabricated from 1 in $\times$ 1 in $\times$ $\frac{1}{8}$ in-thick steel square tubing, and each suspension assembly uses $\frac{3}{4}$ in $\times$ $\frac{3}{4}$ in $\times$ $\frac{1}{8}$ in-thick steel square tubing to slide easily inside the main frame for convenient adjustability. Weights (not shown in figures) have been added to the frame and distributed to correctly locate the CM and mass moment of inertia of the helicopter model.

Suspension

The model has main and auxiliary trailing arm suspensions that use fifth-scale car shocks and precision springs with parallel hydraulic damping, as shown in Figure 2.12. The suspension trailing arm axis of rotation is coupled to a rotary potentiometer to measure the suspension deflection at each wheel.

The tires are represented by commercially-available foam rubber wheels and are not scaled appropriately, due to the small selection of wheels available in that size. Experimental testing was performed to determine the actual tire and suspension characteristics. This testing is described in Section 3.5.

Casterable Nose/Tail Wheel Assembly

The casterable nose/tail wheel assembly is shown in Figure 2.13 with a dual tire configuration, but is capable of containing either single or dual tires. The auxiliary trailing arm suspension is similar to the suspension assembly on the main wheels, however it contains an extended caster wheel structure that is capable of pivoting at the point of attachment to set any kingpin axis inclination, which should be at about +6 degrees to conform with the full-scale helicopter.

Figure 2.12: Main wheel and suspension assembly

Figure 2.13: Caster wheel assembly

Figure 2.14 shows an exploded view of the caster assembly. As shown, the caster wheel assembly uses a $\frac{1}{2}$ in bolt as the caster axis of rotation that either swings freely in a bearing (casterable) or has the potential to lock in specific orientation increments (steerable). The bolt is secured in the bearing such that it remains possible to adjust the height of the caster wheel.

Figure 2.14: An exploded view of the caster wheel assembly

A dual-potentiometer assembly is attached to the caster wheel assembly along its axis of rotation to continuously measure the caster wheel's orientation. Two potentiometers are required because each potentiometer cannot sense orientation in its 350 to 360 degree range. By offsetting the orientation of two potentiometers and selecting each one when appropriate, this potential problem is overcome.

Probe Assembly

The probe assembly is complex because it has the following design requirements:

1. the probe must be secured to the RSD claw radially, but allow vertical and rotational movement;
2. the helicopter's orientation must be measured, since the probe is the DLTV's point of rotation; and
3. a force sensor must be incorporated axially along the probe to measure the probe forces.

The scale model of the probe is functionally the same as the full-scale probe, shown in Figure 1.10, such that at the point of attachment the model probe contains a universal joint to allow the helicopter to pitch and roll relative to the deck during manoeuvring and ship motion.

Figure 2.15: Top and bottom views of the probe

In addition, the universal joint is attached to the probe in a slotted flange, allowing the helicopter to heave within the slot's limits, as shown in Figure 2.15.

The slotted flange attaches to a 6-axis force/torque sensor (see Section 3.2.2) used to measure the probe forces during manoeuvring and/or ship motion. The sensor is then attached by a flange to a $\frac{1}{2}$ in bolt, which is used as the axis of rotation of the helicopter, similar to the caster wheel assembly, except that the probe is fixed to the RSD and the helicopter rotates around the probe in the bearing. The placement of the probe along the DLTV, as well as the probe's height above the flight deck, are adjustable. A dual-potentiometer assembly is attached to the probe along its axis of rotation to continuously measure the helicopter's orientation.

2.3 Analysis

This section describes the analysis performed on the motion platform and DLTV during the design process and in support of the actuation selection. The equations derived in this section are also used for motion control of the actuators and analysis of the measured data, described further in Chapter 3.

2.3.1 Motion Platform Analysis

Kinematic equations were developed based on the motion platform's conceptual design geometry, and the available inputs - platform roll (Θ) and yaw (λ) angles. These equations were used to: determine the requirements for the roll linear actuator; determine the required position of the linear actuator's end attachment point for a desired roll angle; determine the translational and angular motions at any point on the motion platform's deck; and control the linear actuator's motion through LabView by specifying the desired input roll angles.

Equations are also required in the LabView control software to recalculate Θ based on the instantaneous position of the linear actuator (ΔL_{act}). Two methods were used to develop these equations: a direct method based on trigonometry; and an intersecting circles method, which was an interesting approach used to validate the first method. Both approaches were confirmed to produce identical results. Also, ADAMS [46] multibody dynamics simulation software was used as a method for determining the actuator forces and motions to specify the linear actuator requirements.

A Matlab program [47], *Statmo*, was developed to implement these hand-derived equations for any peak roll displacement, velocity, and acceleration of the platform, and yaw orientation of the deck. *Statmo* also provides the ability to display two-dimensional images of the motion platform orientation, shown in Figure 2.16, indicating the position of the platform at the indicated peak orientation and location of the scaled helicopter model, which aided in validating the kinematic equations.

Kinematic Equations

For an input platform roll angle, Θ , the helicopter model on the ship deck will experience simultaneous in-phase motions in the roll (θ), pitch (ϕ), surge (x), sway (y), and heave (z) directions as the result of kinematic coupling between translational and angular motions. The relative motion amplitudes depend on the deck yaw orientation (λ), the location of the axis of rotation (h_1), and the position of the point of interest, point P (x_p, y_p, z_p), on either the model or the deck.

Figure 2.16: Two-dimensional plots generated using the *Statmo* Matlab program

Kinematic equations were derived to determine the translational and angular displacements, velocities, and accelerations at this point of interest. Figures 2.17 and 2.18 show the front and top views, respectively, of the key reference points and coordinate systems on the motion platform, as well as the variables referred to in subsequent equations.

The global coordinate system, $X - Y - Z$, is located at the base of the centre post at point A . A local coordinate system, $i - j - k$ is located at the axis of rotation of the platform at point B and exhibits angular roll motion, Θ , about the i -axis for any points above point B due to the actuator. This roll transformation matrix, $[T_{ijk \rightarrow XYZ}]$ is:

$$[T_{ijk \rightarrow XYZ}] = \begin{bmatrix} 1 & 0 & 0 \\ 0 & \cos \Theta & -\sin \Theta \\ 0 & \sin \Theta & \cos \Theta \end{bmatrix} \quad (2.21)$$

A second local coordinate system, $x - y - z$, is located at the centre point of the deck, point E , which allows the change in angular yaw orientation, λ , about the z -axis for all points on the deck. This yaw transformation matrix, $[T_{xyz \rightarrow ijk}]$ is:

Figure 2.17: Front view of the motion platform showing points and coordinate systems used for analysis

Figure 2.18: Top view of the motion platform showing points and coordinate systems used for analysis

$$[T_{xyz \rightarrow ijk}] = \begin{bmatrix} \cos \lambda & -\sin \lambda & 0 \\ \sin \lambda & \cos \lambda & 0 \\ 0 & 0 & 1 \end{bmatrix} \quad (2.22)$$

Therefore, the combined roll and yaw local-to-global transformation matrix, $[T_{xyz \rightarrow XYZ}]$ is:

$$\begin{aligned} [T_{xyz \rightarrow XYZ}] &= [T_{ijk \rightarrow XYZ}] [T_{xyz \rightarrow ijk}] \\ &= \begin{bmatrix} \cos \lambda & -\sin \lambda & 0 \\ \cos \Theta \sin \lambda & \cos \Theta \cos \lambda & -\sin \Theta \\ \sin \Theta \sin \lambda & \sin \Theta \cos \lambda & \cos \Theta \end{bmatrix} \end{aligned} \quad (2.23)$$

A third local coordinate system, $m - n - o$, is also located at point E on the deck and is set at the same angular yaw orientation as the $x - y - z$ coordinate system, yet maintains a platform roll angle of $\Theta = 0$. This serves as a reference system in which to measure the resulting angular motions of the point of interest on the deck, point P, at the set yaw orientation.

The global positions of key points on the motion platform can be determined from:

$$\vec{r}_E^{XYZ} = \vec{r}_B^{XYZ} + [T_{ijk \rightarrow XYZ}] \vec{r}_{E/B}^{ijk} \quad (2.24)$$

$$\vec{r}_G^{XYZ} = \vec{r}_B^{XYZ} + [T_{ijk \rightarrow XYZ}] \vec{r}_{G/B}^{ijk} \quad (2.25)$$

$$\vec{r}_P^{XYZ} = \vec{r}_E^{XYZ} + [T_{xyz \rightarrow XYZ}] \vec{r}_{P/E}^{xyz} \quad (2.26)$$

where the superscripts indicate the local or global coordinate systems. These key points represent: the centre point on the deck at which the origin of local coordinate system $x - y - z$ is located; the attachment point for the moving actuator; and the point of interest on the deck, respectively. These points are used in the analysis that follows.

Translational Motions

The point of interest on the deck, point P, could represent any arbitrary point, the DLTV CM, the location of the claw, or the position of a sensor. In the $x-y-z$ local deck coordinate system, point P is represented as follows:

$$\vec{r}_{P/E}^{xyz} = \begin{Bmatrix} x_p \\ y_p \\ z_p \end{Bmatrix} \quad (2.27)$$

The global position of point P, from Equation 2.26, is:

$$\begin{aligned} \vec{r}_P^{XYZ} &= \vec{r}_E^{XYZ} + [T_{xyz \rightarrow XYZ}] \vec{r}_{P/E}^{xyz} \\ &= \begin{Bmatrix} x_p \cos \lambda - y_p \sin \lambda \\ -\sin \Theta (h_2 + h_3) + x_p \cos \Theta \sin \lambda + y_p \cos \Theta \cos \lambda - z_p \sin \Theta \\ h_1 + \cos \Theta (h_2 + h_3) + x_p \sin \Theta \sin \lambda + y_p \sin \Theta \cos \lambda + z_p \cos \Theta \end{Bmatrix} \end{aligned} \quad (2.28)$$

The global displacement of point P, $\Delta \vec{r}_P^{XYZ}$ is determined by subtracting its original position, $\vec{r}_{P_0}^{XYZ}$ (when $\Theta = 0$) from $\vec{r}_P^{XYZ}$, where:

$$\begin{aligned} \vec{r}_{P_0}^{XYZ} &= \begin{Bmatrix} 0 \\ 0 \\ h_1 + h_2 + h_3 \end{Bmatrix} + [T_{xyz \rightarrow XYZ}] \vec{r}_{P/E}^{xyz} \\ &= \begin{Bmatrix} x_p \cos \lambda - y_p \sin \lambda \\ x_p \sin \lambda + y_p \cos \lambda \\ h_1 + h_2 + h_3 + z_p \end{Bmatrix} \end{aligned} \quad (2.29)$$

Figure 2.19: Front view of $\Delta \vec{r}_P^{XYZ}$ with respect to the local $m - n - o$ coordinate system

and therefore

$$\Delta \vec{r}_P^{XYZ} = \begin{Bmatrix} 0 \\ -s\Theta(h_2 + h_3) + x_p c\Theta s\lambda + y_p(c\Theta c\lambda - c\lambda) - z_p s\lambda \\ c\Theta(h_2 + h_3) + x_p s\Theta s\lambda + y_p s\Theta c\lambda + z_p(c\Theta - 1) - (h_2 + h_3) \end{Bmatrix} \quad (2.30)$$

where $c = \cos$ and $s = \sin$.

To calculate the linear displacements of point P, $\Delta \vec{r}_P^{XYZ}$ is projected onto the local $m - n - o$ coordinate system, as shown in Figure 2.19. This is accomplished by pre-multiplying $\Delta \vec{r}_P^{XYZ}$ by the inverse² of the local-to-global yaw transformation matrix, $[T_{xyz \rightarrow ijk}]$, resulting in $\Delta \vec{r}_P^{mno}$:

$$\Delta \vec{r}_P^{mno} = \begin{Bmatrix} s\lambda(-s\Theta(h_2 + h_3) + x_p c\Theta s\lambda + y_p(c\Theta c\lambda - c\lambda) - z_p s\lambda) \\ c\lambda(-s\Theta(h_2 + h_3) + x_p c\Theta s\lambda + y_p(c\Theta c\lambda - c\lambda) - z_p s\lambda) \\ c\Theta(h_2 + h_3) + x_p s\Theta s\lambda + y_p s\Theta c\lambda + z_p(c\Theta - 1) - (h_2 + h_3) \end{Bmatrix} \quad (2.31)$$

²Note that for a proper orthogonal rotation transformation matrix, the inverse is simply equal to the transpose.

where surge (Δr_x), sway (Δr_y), and heave (Δr_z) can be extracted as:

$$\Delta r_x = (\Delta \vec{r}_P^{mno})_x \quad (2.32)$$

$$\Delta r_y = (\Delta \vec{r}_P^{mno})_y \quad (2.33)$$

$$\Delta r_z = (\Delta \vec{r}_P^{mno})_z \quad (2.34)$$

The global linear velocity of point P, $\vec{v}_P^{XYZ}$, can be calculated by differentiating Equation 2.28 with respect to time:

$$\begin{aligned} \vec{v}_P^{XYZ} &= \dot{\vec{r}}_E^{XYZ} + [\dot{T}_{xyz \rightarrow XYZ}] \vec{r}_{P/E}^{xyz} + [T_{xyz \rightarrow XYZ}] \dot{\vec{r}}_{P/E}^{xyz} \\ &= \dot{\vec{r}}_B^{XYZ} + [\dot{T}_{ijk \rightarrow XYZ}] \vec{r}_{E/B}^{ijk} + [T_{ijk \rightarrow XYZ}] \dot{\vec{r}}_{E/B}^{ijk} + \dot{\vec{r}}_E^{XYZ} \\ &\quad + [\dot{T}_{xyz \rightarrow XYZ}] \vec{r}_{P/E}^{xyz} + [T_{xyz \rightarrow XYZ}] \dot{\vec{r}}_{P/E}^{xyz} \end{aligned} \quad (2.35)$$

Since $\dot{\vec{r}}_B^{XYZ} = \dot{\vec{r}}_{P/E}^{xyz} = \dot{\vec{r}}_{E/B}^{ijk} = 0$, then Equation 2.35 can be simplified to:

$$\begin{aligned} \vec{v}_P^{XYZ} &= [\dot{T}_{ijk \rightarrow XYZ}] \vec{r}_{E/B}^{ijk} + [\dot{T}_{xyz \rightarrow XYZ}] \vec{r}_{P/E}^{xyz} \\ &= \dot{\Theta} \begin{Bmatrix} 0 \\ -\cos \Theta (h_2 + h_3) - x_p \sin \Theta \sin \lambda - y_p \sin \Theta \cos \lambda - z_p \cos \Theta \\ -\sin \Theta (h_2 + h_3) + x_p \cos \Theta \sin \lambda + y_p \cos \Theta \cos \lambda - z_p \sin \Theta \end{Bmatrix} \end{aligned} \quad (2.36)$$

To project $\vec{v}_P^{XYZ}$ onto the $m - n - o$ coordinate system, Equation 2.36 is pre-multiplied by the inverse of the local-to-global yaw transformation matrix, $[T_{xyz \rightarrow ijk}]$, resulting in $\vec{v}_P^{mno}$:

$$\vec{v}_P^{mno} = \dot{\Theta} \left\{ \begin{array}{l} -\sin \lambda (\cos \Theta (h_2 + h_3) + x_p \sin \Theta \sin \lambda + y_p \sin \Theta \cos \lambda + z_p \cos \Theta) \\ -\cos \lambda (\cos \Theta (h_2 + h_3) + x_p \sin \Theta \sin \lambda + y_p \sin \Theta \cos \lambda + z_p \cos \Theta) \\ -\sin \Theta (h_2 + h_3) + x_p \cos \Theta \sin \lambda + y_p \cos \Theta \cos \lambda - z_p \sin \Theta \end{array} \right\} \quad (2.37)$$

Similarly, the global linear acceleration of point P, $\vec{a}_P^{XYZ}$, can be calculated by differentiating Equation 2.36 with respect to time, which simplifies to:

$$\begin{aligned} \vec{a}_P^{XYZ} &= [\ddot{T}_{ijk \rightarrow XYZ}] \vec{r}_{E/B}^{ijk} + [\ddot{T}_{xyz \rightarrow XYZ}] \vec{r}_{P/E}^{xyz} \\ &= \dot{\Theta}^2 \left\{ \begin{array}{l} 0 \\ \sin \Theta (h_2 + h_3) - x_p \cos \Theta \sin \lambda - y_p \cos \Theta \cos \lambda + z_p \sin \Theta \\ -\cos \Theta (h_2 + h_3) - x_p \sin \Theta \sin \lambda - y_p \sin \Theta \cos \lambda - z_p \cos \Theta \end{array} \right\} \\ &\quad + \ddot{\Theta} \left\{ \begin{array}{l} 0 \\ -\cos \Theta (h_2 + h_3) - x_p \sin \Theta \sin \lambda - y_p \sin \Theta \cos \lambda - z_p \cos \Theta \\ -\sin \Theta (h_2 + h_3) + x_p \cos \Theta \sin \lambda + y_p \cos \Theta \cos \lambda - z_p \sin \Theta \end{array} \right\} \quad (2.38) \end{aligned}$$

Expressed in $m - n - o$ local coordinates, $\vec{a}_P^{mno}$ is:

$$\begin{aligned} \vec{a}_P^{mno} &= \dot{\Theta}^2 \left\{ \begin{array}{l} -\sin \Theta (\sin \Theta (h_2 + h_3) + x_p \cos \Theta \sin \lambda + y_p \cos \Theta \cos \lambda - z_p \sin \Theta) \\ -\cos \Theta (-\sin \Theta (h_2 + h_3) + x_p \cos \Theta \sin \lambda + y_p \cos \Theta \cos \lambda - z_p \sin \Theta) \\ -\cos \Theta (h_2 + h_3) - x_p \sin \Theta \sin \lambda - y_p \sin \Theta \cos \lambda - z_p \cos \Theta \end{array} \right\} \\ &\quad + \ddot{\Theta} \left\{ \begin{array}{l} -\sin \Theta (\cos \Theta (h_2 + h_3) + x_p \sin \Theta \sin \lambda + y_p \sin \Theta \cos \lambda + z_p \cos \Theta) \\ -\cos \Theta (\cos \Theta (h_2 + h_3) + x_p \sin \Theta \sin \lambda + y_p \sin \Theta \cos \lambda + z_p \cos \Theta) \\ -\sin \Theta (h_2 + h_3) + x_p \cos \Theta \sin \lambda + y_p \cos \Theta \cos \lambda - z_p \sin \Theta \end{array} \right\} \quad (2.39) \end{aligned}$$

This local translational acceleration of point P is used in the motion validation process and is discussed further in Section 3.4.

Angular Deck Motions

The angular motions experienced at the deck depend on the input roll angle of the platform (Θ) and the yaw orientation of the deck (λ). Note that when $\lambda = 0$, the deck roll angle, $\theta = \Theta$ and the deck pitch angle, $\phi = 0$. However, when $\lambda \neq 0$, Θ is divided into deck roll (θ) and pitch (ϕ) angles. To determine the relationship between the platform and deck angular motions, a unit vector is placed at point E in the $x - y - z$ coordinate system, $\vec{r}_{unit}^{xyz}$, where:

$$\vec{r}_{unit}^{xyz} = \begin{Bmatrix} 0 \\ 0 \\ 1 \end{Bmatrix} \quad (2.40)$$

This unit vector is then transformed to global coordinates, $X - Y - Z$, as follows:

$$\begin{aligned} \vec{r}_{unit}^{XYZ} &= [T_{xyz \rightarrow XYZ}] \vec{r}_{unit}^{xyz} \\ &= \begin{Bmatrix} 0 \\ -\sin \Theta \\ \cos \Theta \end{Bmatrix} \end{aligned} \quad (2.41)$$

Similar to the calculation of translational deck motions, the deck roll and pitch angles are determined by projecting $\vec{r}_{unit}^{XYZ}$ onto the local $m - n - o$ coordinate system. This is accomplished by pre-multiplying $\vec{r}_{unit}^{XYZ}$ by the inverse of the yaw transformation matrix, $[T_{xyz \rightarrow ijk}]$, resulting in $\vec{r}_{unit}^{mno}$:

$$\vec{r}_{unit}^{mno} = \begin{Bmatrix} -\sin \lambda \sin \Theta \\ -\cos \lambda \sin \Theta \\ \cos \Theta \end{Bmatrix} \quad (2.42)$$

Figure 2.20: The projection of $\Delta \vec{r}_{unit}^{mno}$ onto $m - o$ and $n - o$ planes

Shown in Figure 2.20, the projection of $\vec{r}_{unit}^{mno}$ onto the $n - o$ and $m - o$ planes are used to calculate θ and ϕ , respectively, as:

$$\theta = \tan^{-1}(\cos \lambda \tan \Theta) \quad (2.43)$$

$$\phi = \tan^{-1}(-\sin \lambda \tan \Theta) \quad (2.44)$$

Figure 2.21 shows the relationship between the deck roll (θ) and pitch (ϕ) angles at various yaw angles (λ) for a unit input platform roll angle (Θ). Also shown, if the input roll angle is adjusted appropriately, represented by Θ' , the resulting adjusted deck roll angle (θ') would be amplified appropriately to produce the desired unit roll amplitude for any λ . The resulting adjusted deck pitch angle is also shown.

For the adjusted deck roll angle, θ' , to be equal to Θ for any λ , the adjusted input roll angle, Θ' , is calculated by modifying Equation 2.43 as follows:

$$\theta' = \tan^{-1}(\cos \lambda \tan \Theta') \quad (2.45)$$

and rearranging to solve for Θ' in the case when $\theta' = \Theta$:

Figure 2.21: Effects of varying yaw angle on resulting deck roll and pitch

$$\Theta' = \tan^{-1} \left(\frac{\tan \Theta}{\cos \lambda} \right) \quad (2.46)$$

This results in adjusted roll and pitch angles as follows:

$$\theta' = \tan^{-1} (\cos \lambda \tan \Theta') \quad (2.47)$$

$$\phi' = \tan^{-1} (-\sin \lambda \tan \Theta') \quad (2.48)$$

Figure 2.21 highlights that by proper selection of the deck yaw angle λ , any combination of in-phase or out-of-phase roll and pitch motion can be achieved. Therefore, a desired RMS pitch, Φ_{RMS} , can be produced due to Θ' by selecting an appropriate λ . Testing using a LabView program verified that RMS values for Θ and Θ' could be related using Equation 2.46 as follows:

$$\Theta'_{RMS} = \tan^{-1} \left(\frac{\tan \Theta_{RMS}}{\cos \lambda} \right) \quad (2.49)$$

From this relationship, it can be concluded that the same relationship is applicable to the unadjusted and adjusted deck roll and pitch angles:

$$\theta_{RMS} = \tan^{-1} (\cos \lambda \tan \Theta_{RMS}) \quad (2.50)$$

$$\phi_{RMS} = \tan^{-1} (-\sin \lambda \tan \Theta_{RMS}) \quad (2.51)$$

$$\theta'_{RMS} = \tan^{-1} (\cos \lambda \tan \Theta'_{RMS}) \quad (2.52)$$

$$\phi'_{RMS} = \tan^{-1} (-\sin \lambda \tan \Theta'_{RMS}) \quad (2.53)$$

By substituting $\phi'_{RMS} = \Phi_{RMS}$ and Equation 2.49 into Equation 2.53, the required λ can be determined as follows:

$$\lambda = \tan^{-1} \left(-\frac{\tan \Phi_{RMS}}{\tan \Theta_{RMS}} \right) \quad (2.54)$$

For the remaining analysis, the unadjusted input roll angle (Θ) will be used unless it is stated that specific motion requirements are to be met, in which case Θ' will be used.

Actuator Requirements

By monitoring the global positions of point G, the required translational displacements, velocities, and accelerations of the linear actuator, which is located between points F and G , can then be determined for any input roll angle, Θ .

From Equation 2.25, the position of point G with respect to point F, $\vec{r}_{G/F}^{XYZ}$, is defined as:

$$\begin{aligned}
\vec{r}_{G/F}^{XYZ} &= \vec{r}_G^{XYZ} - \vec{r}_F^{XYZ} \\
&= \begin{pmatrix} 0 \\ \frac{w_r}{2} \cos \Theta - \sin \Theta (h_2 - \ell_{joint} - \frac{w_r}{2}) \\ h_1 + \frac{w_r}{2} \sin \Theta + \cos \Theta (h_2 - \ell_{joint}) - \ell_{joint} \end{pmatrix}
\end{aligned} \tag{2.55}$$

since $\vec{r}_F^{XYZ}$ is:

$$\vec{r}_F^{XYZ} = \begin{pmatrix} 0 \\ \frac{w_r}{2} \\ \ell_{joint} \end{pmatrix} \tag{2.56}$$

Therefore, the linear actuator displacement, ΔL_{act} , required for a specific roll angle is given by:

$$\Delta L_{act} = \left| \vec{r}_{G/F}^{XYZ} \right| - L_{act0} \tag{2.57}$$

where L_{act0} is the original length of the actuator when in the neutral position ($\Theta = 0$). The translational actuator velocity, v_{act} , is defined by :

$$v_{act} = \vec{v}_{G/F}^{XYZ} \cdot (\vec{r}_{G/F}^{XYZ})_{UNIT} \tag{2.58}$$

where $\vec{v}_{G/F}^{XYZ}$ is determined by taking the time derivative of Equation 2.55, as follows:

$$\vec{v}_{G/F}^{XYZ} = \dot{\Theta} \begin{pmatrix} 0 \\ -\frac{w_r}{2} \sin \Theta - \cos \Theta (h_2 - \ell_{joint}) \\ \frac{w_r}{2} \cos \Theta - \sin \Theta (h_2 - \ell_{joint}) \end{pmatrix} \tag{2.59}$$

and $(\vec{r}_{G/F}^{XYZ})_{UNIT}$ is:

$$(\vec{r}_{G/F}^{XYZ})_{UNIT} = \frac{\vec{r}_{G/F}^{XYZ}}{|\vec{r}_{G/F}^{XYZ}|} \quad (2.60)$$

The linear actuator acceleration, a_{act} , is defined by :

$$a_{act} = \vec{a}_{G/F}^{XYZ} \cdot (\vec{r}_{G/F}^{XYZ})_{UNIT} \quad (2.61)$$

where $\vec{a}_{G/F}^{XYZ}$ is determined by taking the time derivative of Equation 2.59, as follows:

$$\begin{aligned} \vec{a}_{G/F}^{XYZ} = & \dot{\Theta}^2 \left\{ \begin{array}{c} 0 \\ -\frac{w_r}{2} \cos \Theta + \sin \Theta (h_2 - \ell_{joint}) \\ -\frac{w_r}{2} \sin \Theta - \cos \Theta (h_2 - \ell_{joint}) \end{array} \right\} \\ & + \ddot{\Theta} \left\{ \begin{array}{c} 0 \\ -\frac{w_r}{2} \sin \Theta - \cos \Theta (h_2 - \ell_{joint}) \\ -\frac{w_r}{2} \cos \Theta - \sin \Theta (h_2 - \ell_{joint}) \end{array} \right\} \end{aligned} \quad (2.62)$$

The actuator selection, based in part on platform kinematics, is discussed in Section 3.1.1.

Alternate Equations

Since it is difficult to modify these kinematic equations to extract the platform roll angles (Θ) based on the actuator displacement (ΔL_{act}), a simplified approach based on trigonometry was investigated. A direct relationship between Θ and ΔL_{act} is required during motion control to calculate the roll angle based on the instantaneous position of the linear actuator's end point relative to the neutral position, where $\Theta = 0$. Figures 2.22 and 2.23 show the platform variables in the two configurations, respectively: upright configuration with the u-joint installed at the

top of the centre post ($h_1 = \ell_{post}, h_2 = \ell_{joint}$), and the inverted configuration with the u-joint installed at the bottom of the centre post ($h_1 = \ell_{joint}, h_2 = \ell_{post}$). The equations have been derived to satisfy either configuration. Two methods were used and compared to validate the results: the direct method and the intersecting circles method.

Direct Method

The direct method uses the platform geometry, defined in Figures 2.22 and 2.23, to calculate Θ (in degrees) from the actuator displacement, ΔL_{act} (in inches). The original total actuator length (when $\Theta = 0$) is:

$$L_{act0} = \ell_{post} - \ell_{joint} \quad (2.63)$$

and the total instantaneous actuator length is the addition of the original total actuator length and the actuator displacement, as follows:

$$L_{act} = L_{act0} + \Delta L_{act} \quad (2.64)$$

The following equations were required as intermediate steps in calculating Θ :

$$s_1 = \sqrt{h_2^2 + \frac{w_r^2}{4}} \quad (2.65)$$

$$\beta_1 = \sin^{-1} \left(\frac{w_r}{2s_1} \right) \quad (2.66)$$

$$\beta_2 = \cos^{-1} \left(\frac{s_1^2 + \frac{w_r^2}{4} - L_{act}^2}{w_r s_1} \right) \quad (2.67)$$

$$\beta_3 = \frac{3\pi}{2} - (\beta_1 + \beta_2) \quad (2.68)$$

$$\Theta = (\pi - \beta_3) * \frac{180}{\pi} \quad (2.69)$$

where length, s_1 , and angles, β_1 , β_2 , and β_3 are defined in Figures 2.22 and 2.23.

Figure 2.24: Platform motion limits for upright configuration

Figure 2.25: Platform motion limits for inverted configuration

Equation 2.69 provides the necessary direct relationship between actuator displacement (ΔL_{act}) and platform roll angle (Θ). The direct method produces a result within $\Delta L_{act} = -12.6$ in and $\Delta L_{act} = +17.3$ in, beyond which β_2 becomes undefined. Generated using Matlab, Figures 2.24 and 2.25 show the platform's motion limits for the upright and inverted configurations, respectively, where any further extension or retraction of the linear actuator is mechanically impossible. At these limits, the roll angle, Θ , becomes -70.8 deg and 100.7 deg, respectively, which are well beyond the defined platform design requirements of ± 45 deg and beyond the physical limits of the actuator.

Intersecting Circles Method

The intersecting circles method was an alternative method investigated, used to validate the results obtained from the direct method. This method evaluates the coordinates of the point of intersection, point G, of two arcs made by the rotation of the platform and the rotation of the linear actuator. Shown in Figures 2.26 and 2.27 are the upright and inverted platform configurations, respectively. The arcs can be represented by the equation of a circle with the origin of the circle at the point of rotation of the structure and the radius as the distance from the point of rotation to the point of attachment of the linear actuator's end effector, point G. The global origin for this system is located at point B for simplification, which is the point of rotation for the platform.

The equation of a circle is represented by:

$$(x_i - a)^2 + (y_i - b)^2 = r^2 \quad (2.70)$$

where x_i and y_i are the coordinates of the circle, and a and b are the offset x-position and y-position of the circle's origin from the coordinate system's origin, respectively, and r is the radius of the circle. The equation of the circle representing the arc created at point G due to the rotation of the platform has an origin at point B and a radius, r_1 , from point B to point G where:

$$r_1^2 = (h_2 - \ell_{joint})^2 + \left(\frac{w_r}{2}\right)^2 \quad (2.71)$$

This is represented for either the upright or inverted configuration as:

$$x_i^2 + y_i^2 = (h_2 - \ell_{joint})^2 + \left(\frac{w_r}{2}\right)^2 \quad (2.72)$$

since the origin of the circle is at the origin of the coordinate system. Rearranging Equation 2.72 to isolate y_i , we obtain:

Figure 2.26: Diagram defining motion platform geometry in the upright configuration for the intersecting circles method.

Figure 2.27: Diagram defining motion platform geometry in the inverted configuration for the intersecting circles method

$$y_i = \sqrt{c_1^2 + \frac{w_r^2}{2} - x^2} \quad (2.73)$$

where c_1 is a constant defined by:

$$c_1 = h_2 - \ell_{joint} \quad (2.74)$$

The equation of the circle representing the arc created at point G due to the rotation of the linear actuator has an origin at point F and a radius, r_2 , from point F to point G where:

$$r_2^2 = L_{act}^2 \quad (2.75)$$

This is represented for either the upright or inverted configuration as:

$$\begin{aligned} (x_i - \frac{w_r}{2})^2 + (y_i - (-h_1 + \ell_{joint}))^2 &= L_{act}^2 \\ (x_i - \frac{w_r}{2})^2 + (y_i + c_2)^2 &= L_{act}^2 \end{aligned} \quad (2.76)$$

where L_{act} is defined in Equation 2.64 and c_2 is a constant defined by:

$$c_2 = h_1 - \ell_{joint} \quad (2.77)$$

To solve for the point of intersection between the two circles defined above, Equation 2.76 is expanded and Equation 2.73 is substituted into Equation 2.76, resulting in a quadratic equation in terms of x . Equation 2.78 shows the expansion of Equation 2.76, Equation 2.79 shows the substitution of Equation 2.73 into Equation 2.78, and Equation 2.80 shows the grouping of constant terms on the right-hand side of the equation.

$$x_i^2 - w_r x_i + \frac{w_r^2}{4} + y_i^2 + 2c_2 y_i + c_2^2 = L_{act}^2 \quad (2.78)$$

$$x_i^2 - w_r x_i + \frac{w_r^2}{4} + c_1^2 + \frac{w_r^2}{2} - x_i^2 + 2c_2 \sqrt{c_1^2 + \frac{w_r^2}{2} - x_i^2} + c_2^2 = L_{act}^2 \quad (2.79)$$

$$2c_2\sqrt{c_1^2 + \frac{w_r^2}{2} - x_i^2} = L_{act}^2 - \frac{w_r^2}{2} - c_1^2 - c_2^2 + w_r x_i \quad (2.80)$$

For simplification,

$$c_3 = L_{act}^2 - \frac{w_r^2}{2} - c_1^2 - c_2^2 \quad (2.81)$$

Equation 2.80 is squared, and rearranged in a quadratic form:

$$4c_2^2(c_1^2 + \frac{w_r^2}{2} - x_i^2) = (c_3 + w_r x_i)^2 \quad (2.82)$$

$$4c_1^2 c_2^2 + c_2^2 w_r^2 - 4c_2^2 x_i^2 = c_3^2 + 2c_3 w_r x_i + w_r^2 x_i^2 \quad (2.83)$$

$$(w_r^2 + 4c_2^2)x_i^2 + (2c_3 w_r)x_i + c_3^2 - c_2^2 w_r^2 - 4c_1^2 c_2^2 = 0 \quad (2.84)$$

Equation 2.84 is a quadratic equation since it is in the form of $ax_i^2 + bx_i + c = 0$ where:

$$a = w_r^2 + 4c_2^2$$

$$b = 2c_3 w_r$$

$$c = c_3^2 - c_2^2 w_r^2 - 4c_1^2 c_2^2$$

Equation 2.84 can be solved using the quadratic formula resulting in two possible solutions for x_i . However, it was determined that the discriminant is always zero within the platform motion limits and $x_1 = x_2$. Therefore, x_1 is substituted back into Equation 2.73 to find the coordinates of the point of intersection of the two circles.

To determine Θ , the initial angle β_0 (when $\Theta=0$) must be subtracted from angle β when the platform is rotated:

$$\begin{aligned} \Theta &= \beta - \beta_0 \\ &= \tan^{-1}\left(\frac{y_i}{x_i}\right) - \arctan\left(\frac{c_1}{\frac{w_r}{2}}\right) \end{aligned} \quad (2.85)$$

where β is defined in Figures 2.26 and 2.27. Note that Equation 2.85 is also valid for the upright post configuration since $\beta_0 = 0$ at $\Theta = 0$ and the second term becomes zero.

Some additional considerations can be seen from Figure 2.26 where for the upright post configuration, x_i will always be positive, and y_i may be positive or negative. However, from Equation 2.73 it can be seen that y_i will always result in a positive value. Therefore, for the upright post configuration only, when $\Delta L_{act} \neq 0$, to obtain the correct sign for Θ , Equation 2.85 must be multiplied by the sign of ΔL_{act} , which becomes:

$$\Theta = \tan^{-1} \left(\frac{y_i}{x_i} \right) \frac{\Delta L_{act}}{\sqrt{(\Delta L_{act}^2)}} \quad (2.86)$$

since $c_1 = 0$ for the upright post configuration.

The intersecting circles method produces results within $\Delta L_{act} = -12.6$ in and $\Delta L_{act} = +17.3$ in, similar to the direct geometry method, and are beyond the actuator physical limits. Comparing both methods, it can be seen that they both yield the same results in the range of operation, yet the direct method is simpler, requires fewer steps, and thus is used during motion control, as will be explained in Section 3.3.4.

2.3.2 DLTV Analysis

The DLTV analysis was required to derive the suspension kinematics to monitor the suspension deflection as well as to determine the required suspension stiffness.

Figure 2.28 shows side and top views of the DLTV, along with reference points and variables that are referred to in the subsequent analysis.

Suspension Kinematics

Figure 2.29 shows the suspension geometry of a single suspension station and reference points used in the derivation of the suspension kinematic equations used to calculate suspension deflection and stiffness. The caster suspension is used for this analysis; however, other than the tire location, the trailing arm suspension geometry is the same for the main wheels.

Figure 2.28: Side view and top view of DLTV showing reference points and variables

Suspension deflection is calculated using the angle measured at the point of rotation of the trailing arm suspension at each wheel. The suspension deflection is determined by subtracting the deflected shock assembly length, L_s , from the length of the shock assembly at the neutral position, L_{sn} .

Figure 2.30 shows the side view of the key reference points and coordinate systems on the trailing arm suspension, as well as the dimensions referred to in the upcoming equations. Note that the global $X - Y - Z$ coordinate system shown for this derivation is attached to the moving DLTV, the purpose of which is to investigate relative suspension kinematics. By monitoring the global position of the point of attachment of the shock assembly to the trailing arm, the suspension deflection at each wheel can then be determined. As shown in Figure 2.30, the global

Figure 2.29: Side view of DLTV trailing arm suspension

coordinate system, $X - Y - Z$, has its X -axis aligned along the trailing arm when the shock is in its fully-extended position. A local coordinate system, $i - j - k$, has its i -axis along the trailing arm, and follows it through its rotation.

Figure 2.31 shows the geometry of the side view of the trailing arm suspension when the shock length, L_s is in its fully-extended position and has maximum length L_{se} . At this position, the trailing arm angle, α , is set to zero. The angle, β_2 , between ℓ_H and ℓ_I at this position is required in the analysis and is calculated from the cosine law as follows:

$$\beta_2 = \cos^{-1} \left(\frac{L_{se}^2 - \ell_H^2 - \ell_I^2}{-2\ell_H\ell_I} \right) \quad (2.87)$$

The global positions of points E and F are constant and defined as:

$$\vec{r}_E^{XYZ} = \begin{Bmatrix} 0 \\ 0 \\ 0 \end{Bmatrix} \quad (2.88)$$

Figure 2.30: Side view of the trailing arm suspension

Figure 2.31: Side view of the fully-extended position trailing arm suspension

$$\vec{r}_F^{XYZ} = \begin{Bmatrix} 0 \\ \ell_I \cos \beta_2 \\ \ell_I \sin \beta_2 \end{Bmatrix} \quad (2.89)$$

The global position of the point of attachment of the shock assembly to the trailing arm, D , is defined as:

$$\vec{r}_D^{XYZ} = \vec{r}_E^{XYZ} + [T_{ijk \rightarrow XYZ}] \vec{r}_{D/E}^{ijk} \quad (2.90)$$

where

$$\vec{r}_{D/E}^{ijk} = \begin{Bmatrix} 0 \\ \ell_H \\ 0 \end{Bmatrix} \quad (2.91)$$

and

$$[T_{ijk \rightarrow XYZ}] = \begin{bmatrix} 1 & 0 & 0 \\ 0 & \cos \alpha & -\sin \alpha \\ 0 & \sin \alpha & \cos \alpha \end{bmatrix} \quad (2.92)$$

Therefore,

$$\vec{r}_D^{XYZ} = \begin{Bmatrix} 0 \\ \ell_H \cos \alpha \\ \ell_H \sin \alpha \end{Bmatrix} \quad (2.93)$$

The vector representing the shock assembly axis, $\vec{r}_{F/D}^{XYZ}$, is:

$$\begin{aligned} \vec{r}_{F/D}^{XYZ} &= \vec{r}_F^{XYZ} - \vec{r}_D^{XYZ} \\ &= \begin{Bmatrix} 0 \\ \ell_I \cos \beta_2 - \ell_H \cos \alpha \\ \ell_I \sin \beta_2 - \ell_H \sin \alpha \end{Bmatrix} \end{aligned} \quad (2.94)$$

The length of the deflected spring, L_s , is the magnitude of $\vec{r}_{F/D}^{XYZ}$:

$$L_s = |\vec{r}_F^{XYZ} - \vec{r}_D^{XYZ}| \quad (2.95)$$

which simplifies to:

$$L_s = \sqrt{2\ell_H^2 + 2\ell_I^2 - 2\ell_H\ell_I \cos(\beta_2 + \alpha)} \quad (2.96)$$

The original length of the shock assembly in the neutral position, L_{sn} , is calculated using Equation 2.96 with the angle of the trailing arm at the neutral position, α_n instead of α . Therefore, the neutral shock assembly length, L_{sn} , is:

Figure 2.32: Free body diagram of DLTV showing reaction forces at the wheels due to its weight

$$L_{sn} = \sqrt{2\ell_H^2 + 2\ell_I^2 - 2\ell_H\ell_I \cos(\beta_2 + \alpha_n)} \quad (2.97)$$

and the suspension deflection, Δ_s , is:

$$\Delta_s = L_s - L_{sn} \quad (2.98)$$

Suspension Stiffness Selection

The suspension stiffness required at each wheel can be calculated by determining the forces acting at each wheel and projecting these forces along the suspension shock assembly axes with the objective of maintaining the DLTV approximately level. Figure 2.32 shows the side view of the DLTV and the reaction forces at the tire contact points due to its weight.

By performing a force and moment balance about point A , and assuming that the reaction forces at A and C are equal, the reaction forces are:

$$F_A = \frac{F_m}{2} \left(1 - \frac{\ell_A}{(\ell_A + \ell_B)} \right) \quad (2.99)$$

Figure 2.33: Free body diagram of caster suspension showing reaction forces at the wheels and joints

$$F_B = \frac{F_m \ell_A}{(\ell_A + \ell_B)} \quad (2.100)$$

$$F_C = F_A \quad (2.101)$$

For the main wheels, the reaction force at the tire contact point also passes through the shock assembly attachment point. However, for the caster wheel assembly, the wheel is offset from the shock assembly attachment point, therefore, a further force breakdown is required. Figure 2.33 shows a free body diagram of the caster wheel suspension and through a moment balance about point E , the reaction force at the shock assembly attachment point, D , is:

$$F_D = \frac{F_m \ell_A (\ell_D + \ell_E)}{\ell_E (\ell_A + \ell_B)} \quad (2.102)$$

Therefore, the forces at the shock assembly attachment points for the two main wheels and caster wheel are F_A , F_C , and F_D , respectively.

As shown in Figure 2.30, the shock assembly has an angle, β_1 , measured from vertical. This angle is calculated using Equation 2.94 as follows:

$$\begin{aligned}
\beta_1 &= \tan \left(\frac{(\vec{r}_{F/D}^{XYZ})_z}{(\vec{r}_{F/D}^{XYZ})_y} \right) \\
&= \tan \frac{\ell_I \sin \beta_2 - \ell_H \sin \alpha}{\ell_I \cos \beta_2 - \ell_H \cos \alpha}
\end{aligned} \tag{2.103}$$

Therefore, the force along the shock assembly is calculated by projecting the reaction force at the joint along the shock, as follows:

$$F_{mw} = F_A \cos \beta_1 \tag{2.104}$$

$$F_{cw} = F_D \cos \beta_1 \tag{2.105}$$

where F_{mw} is the force magnitude acting along the main wheel springs, and F_{cw} is the force magnitude acting along the caster wheel springs, assuming that $F_A = F_C$.

Finally, the spring stiffness required so that that force due to the DLTV weight compresses the shock assembly approximately half the stroke length, Δ_{sn} (neutral position), is determined as follows:

$$k_{mw} = F_{mw} \Delta_{sn} \tag{2.106}$$

$$k_{cw} = F_{cw} \Delta_{sn} \tag{2.107}$$

where

$$\Delta_{sn} = L_{se} - L_{sn} \tag{2.108}$$

The spring selection was based on availability as well as compatibility with the fifth-scale car shock chosen. As a result, the chosen springs have stiffness, k_{mw} and k_{cw} , of 15.5 lbs/in and 26.0 lbs/in, respectively.

Figure 2.34: ADAMS motion platform dynamic model

2.3.3 ADAMS Multibody Dynamic Model

A model of the motion platform was created using ADAMS, which is multibody dynamics simulation software that can simulate the full-motion behaviour of complex mechanical systems [46]. This model, shown in Figure 2.34, was used to perform a dynamic analysis of the motion platform and determine the forces and moments acting on components during the most extreme conditions. This data, along with the results from *Statmo*, were used to size the components and compare alternative motion platform designs. Since the resulting forces and moments on the structural components during these extreme conditions were determined to be acceptable, the ADAMS results presented here deal with the sizing of the linear actuator.

The ADAMS model is representative of the motion platform, such that it contains the main components, their dimensions, and their inertial properties. These components include: the deck, the upper platform, the rotating plate, the support assembly, the base, and the required universal and spherical joints. However, details such as the ASIST system, brackets, and centre post supports are not included. To compensate, the upper platform mass input includes the ASIST components and other miscellaneous components that comprise of the actuated mass. The DLTV is represented by a rectangular block, yet uses the correct scaled DLTV parameters, determined using the experimental approaches outlined in Section 3.5.2.

The actuator stroke (L_{stroke}) is defined as the total actuator displacement required to move

the platform through the full angular range of motion. Using Equation 2.57 with $\Theta = \pm 45$ deg, L_{stroke} was determined to be approximately 21.1 in. An additional 1 in is required at each end to prevent the actuator from hitting its mechanical stops, resulting in a requirement of 23.1 in of stroke for the actuator.

Using ADAMS software, a sinusoidal waveform was generated to represent the peak roll motions from Table 2.2 to determine the required actuator thrust. The actuator motions required to produce the peak angular motions were determined from *Statmo* to be $\Delta L_{act}=5.24$ in, $v_{act}=9.91$ in/s, and $a_{act}=18.37$ in/s². Therefore, in terms of the amplitude (A_{act}) and the frequency (ω_{act}) of the actuator sinusoidal motion, ΔL_{act} is expressed as:

$$\Delta L_{act} = A_{act} \sin(\omega_{act} t) \quad (2.109)$$

where $A_{act} = 5.24$ in and $\omega_{act} = 1.89$ rad/s.

It is important to note that during the simulation, the translational displacements, velocities, and accelerations measured from the ADAMS simulation corresponded to the input, as expected. Additionally, the resulting angular roll displacement, velocity, and acceleration were found to correspond to the peak roll values from Table 2.2.

To predict the most extreme loads on the actuator, the deck was yawed 90 deg and the 20 lb DLTV was placed at the aft of the track (away from the actuator). Figure 2.35 shows the z -force that occurs at the actuator end attachment point due to the sinusoidal motion with the centre post in the upright configuration. Since the corresponding x and y forces were found to be negligible, the expected peak thrust is approximately 50 lb. With the centre post in the inverted configuration, this extreme ship motion condition produces a peak actuator force approaching 200 lb, as shown in Figure 2.36.

As was mentioned previously, there are added capabilities for motion expansion that, when combined, could potentially exceed the thrust specifications of the actuator. Therefore, the specific simulation case should be analyzed before being attempted. The final linear actuator specifications are discussed in Section 3.1.1.

Figure 2.35: Actuator thrust determined using ADAMS during representative sea state 5 peak motions in the upright platform configuration

Figure 2.36: Actuator thrust determined using ADAMS during representative sea state 5 peak motions in the inverted platform configuration

Chapter 3

Motion Control and Testing

In the previous chapter, the design and analysis of the motion platform and DLTV were described. This chapter describes Phase III of the project the development and testing of the motion control system. This includes: the selection of the motion platform actuators and instrumentation; the development of the motion control programs required to control the actuation and data acquisition (DAQ) systems; the validation of the motion control system; and the testing of manoeuvring operations in the presence of motion. An in-depth review and cost analysis of available instrumentation, actuation, and motion control hardware was required during the facility's development. However, only the chosen components will be discussed in this chapter.

3.1 Actuation Selection

The motion system currently consists of three actuation axes: two axes each using a *Nema* stepper motor to drive the planar ASIST linear actuators; and one axis using a *Tolomatic* rod screw linear actuator and an MRB series brushed servo DC motor to generate angular ship motion. A National Instruments (NI) PCI-7344 4-axis stepper/servo motion controller is used to control the three axes and allow an expansion to include a second axis of angular ship motion in the future. An NI controller was chosen because it is directly compatible with NI LabView 7 Express, a graphical development environment for creating flexible and customized test, measurement, and control

Figure 3.1: *Tolomatic* linear actuator installed on the motion platform

applications [48]. This section describes the actuation specifications for the 3 motion axes.

3.1.1 Ship Motion Linear Actuator

A *Tolomatic* rod screw linear actuator, shown installed on the platform in Figure 3.1, was selected to produce the angular ship motions. Rod screw actuators are designed to push guided and supported loads, such as with the motion platform configuration and is able to meet the design requirements at a lower cost to the alternatives. The actuator configuration chosen came ready to mount a spherical rod eye at the extension end and the ability to modify the base connection to mount a universal joint, as described in Section 2.2.1. The rod eye allows for slight misalignment between the load and the actuator (radial and angular), while the universal joint allows the actuator to pivot during actuation.

The brushed DC motor and 1000 line optical encoder comes standard in an inline direct-drive configuration attached to the base of the linear actuator. However, to facilitate the attachment of the actuator's base to the universal joint, the motor was mounted in a reverse-parallel con-

Figure 3.2: Linear actuator motor mounted on the linear actuator

figuration to allow the offset mounting of the motor to either side of the actuator, as shown in Figure 3.2. The reverse-parallel motor mounting couples the motor to the actuator screw shaft using a quiet, zero-backlash coupling with a 2:1 drive ratio to alter the output torque and velocity [49].

The linear actuator requirements were determined using the kinematic equations discussed in Section 2.3.1 and the ADAMS simulation software discussed in Section 2.3.3. Specific values of interest are the stroke, velocity, and thrust required to produce the peak scaled ship motions described in Section 2.1.2. The actuation system chosen to meet the requirements is the *Tolomatic* rod screw actuator, model No. RSA24-SN02-MRB21Y, which contains: a 24 in screw using a 0.625 in diameter ACME screw with 2 turns per inch (TPI) and a solid brass nut. Capabilities include a maximum speed of 29 in/s and a peak thrust of 200 lb, which satisfies the requirements determined in Section 2.3.3.

3.1.2 ASIST RSD Actuation

The critical aspect of the ASIST RSD actuation design was the sizing of the lead screw assemblies, used for both the RSD and claw actuation. Due to the length required for the longitudinal RSD actuator, the lead screw diameter was chosen based on this more critical actuator and used for the lateral claw actuator as well. Since the screw and nut assembly must fit within the 2 in $\times$ 1 in rectangular tubing, the screw diameter must be as small as possible, yet still be able to span a 54 in track and run at a speed of 3.79 in/s (see Table 2.4).

The following equations [50] were used to simplify the selection of the proper lead screw so as to avoid lengths, speeds, and loads that will result in buckling and vibration of the assembly:

$$CS = F_{support}(4.76 \times 10^6) \frac{d_{screw}}{L_{support}^2} \quad (3.1)$$

$$P = F_{support}(14.3 \times 10^6) \frac{d_{screw}^4}{L_{support}^2} \quad (3.2)$$

where CS is the critical rotational speed of the lead screw (rpm), P is the critical thrust rating (lb), d_{screw} is the root diameter of the screw (in), $L_{support}$ is the length between the supports (in), and $F_{support}$ is the end support factor.

The end support factor is assigned based on the chosen end support configuration, shown in Figure 3.3: fixed-free, simple-simple, fixed-simple, and fixed-fixed. Due to the longitudinal lead screw length required, a fixed-fixed end support configuration was chosen, which results in the highest value for $F_{support}$. For the shorter lateral lead screw, a simple-simple end support configuration is adequate. Note that there are different values given for the end support factor,

Figure 3.3: Possible lead screw end support configurations [50]

$F_{support}$, depending on the Equation 3.1 or 3.2 being used.

An ACME lead screw with a 0.25 in diameter and 3 turns per inch (TPI) was chosen. With this selection, the maximum speed that will occur is 682.2 rpm, which is below the critical speed of 852.8 rpm calculated from Equation 3.1.

By approximating acceleration forces, external forces, and frictional forces, the peak thrust was estimated to be $P_{req} = 25$ lbs, which is well below the compressive thrust rating of the screw chosen, which was calculated to be 75.2 lbs from Equation 3.2. Using these specifications, *Nema* size-23 (# HT23-401) stepper motors are used to actuate the physical model of the ASIST system.

3.2 Instrumentation

Values to be measured on the DLTV and motion platform include: platform headings, orientations, angular rates, and translational accelerations using a triaxial accelerometer and orientation sensor package; probe forces and moments using a 6-axis force/torque sensor; DLTV position using the encoders on the ASIST stepper motors; DLTV orientation and caster wheel orientation using a pair of potentiometers for each on the DLTV; and DLTV suspension deflection using a potentiometer at each wheel. As a result, the DLTV contains 7 potentiometers and a 6-axis force/torque sensor, while the triaxial accelerometer and orientation sensor package is available to be placed at any point of interest.

3.2.1 Potentiometers

Potentiometers are either continuous rotation or available with 1, 2, 5 or 10 turns of maximum travel. The disadvantage with continuous potentiometers is that there is only 350 degrees of continuous measurement, after which there is a *dead zone* where the wiper resets to zero. However, finite turn potentiometers could potentially limit the range of motion of a manoeuvre. Therefore, seven continuous 350 degree *Bourns* precision potentiometers [51] are installed on the DLTV, where four are used for helicopter and caster wheel orientations, and one is required for each

Figure 3.4: *Mini45* Force/Torque sensor

of the three suspension deflection measurements. Two potentiometers are required on both the caster wheel assembly and the probe assembly to measure the caster wheel orientation and helicopter orientations, respectively since the second potentiometer is required to take measurements when the primary potentiometer is in its *dead zone*, as previously described in Section 2.2.3.

Digital encoders were considered for these two orientation measurements; however, it was found that they were not the ideal choice due to the increase in cost and the reduction in resolution as compared to analog potentiometers.

3.2.2 Force Torque Sensor

An *ATI Industrial Automation Mini45* multi-axis force/torque transducer, one of the smallest 6-axis sensors in the world [52], was acquired to measure the probe securing forces on the DLTV. The *Mini45 Transducer*, shown in Figure 3.4, was chosen due to its small size and capabilities to measure dynamic or static three-dimensional forces and moments using silicon strain gauge technology¹. Calibrated in the US-60-80 sensor range, the peak forces and torques are: $F_x = F_y = 60$ lb, $F_z = 120$ lb, and $T_x = T_y = T_z = 80$ in-lb. This sensor is also compatible with LabView hardware and software, making its integration into the current motion control software straightforward. Other force/torque sensors were researched, such as piezoelectric sensors [53], however were only measurable in dynamic conditions.

¹Silicon strain gauges provide a signal 75 times stronger than conventional foil gauges [52].

Figure 3.5: 3DM-G sensor local coordinate system [54]

3.2.3 Accelerometer Sensor

The Microstrain 3DM-G 3-axis Gyro-Enhanced Orientation sensor is a self-contained sensor system that incorporates [54]: 3 orthogonal DC accelerometer sensors to measure Earth's gravity; 3 orthogonal magnetometer sensors to measure magnetic fields; and 3 angular rate gyroscope sensors to measure the rate of rotation about their sensitive axes. All together, its orientation, translational accelerations, and angular rates can be measured during motion platform operation. Figure 3.5 shows the sensor's local coordinate system, which is a right-hand system. If rotated 180 deg about the x -axis, this coordinate system agrees with the deck and platform global coordinate systems, which simplifies the analysis of test results.

The sensor contains an on-board timer that uses *timer ticks* (6.5536 ms per timer tick) that are stamped to the data before being sent to the computer's serial port. The standard method is to assign a time while the data is recorded to a file (using the PC clock). Both methods were attempted during testing and the results agree, so either method can be used.

3.3 Motion Control Programs

The motion control programs developed for the motion platform facility consist of: a main control panel used to control all important parameters during operation and share information

Figure 3.6: Main control panel

between programs; an ASIST program to control the ASIST actuators; a ship motion program to control the linear actuator; as well as a DAQ program to read the instrumentation on the DLTV. Other programs, such as a *Write-to-file* program used to synchronize the data being written to a spreadsheet file, and a program that was made available by *ATI Industrial Automation* to measure the 6-axis force/torque transducer, will not be discussed here.

3.3.1 Main Control Panel

The main control panel, shown in Figure 3.6, is used to control and monitor the operation of the experimental facility by accessing variables from both motion control and data acquisition programs. These variables are controllable through the main control panel and become accessible to all motion control and data acquisition programs.

The left side of the main control panel consists of controls and indicators dealing with the ASIST manoeuvring control, including: numerical controls to calibrate the control joystick; nu-

Figure 3.7: Actual ASIST control panel (courtesy of ITI)

merical controls to set RSD properties; graphical indicators to show current probe position; numerical indicators to show the position and velocities of the probe and orientation of both the helicopter and caster wheel; controls for centre-lock and the ASIST control mode; and indicator lights to indicate when the claw is at its centre of travel, when the helicopter orientation is zero, and when free-float is activated. It is desirable to design the control panel similar to the actual ASIST control panel, shown in Figure 3.7, where similar useful indicator lights are available to facilitate manoeuvring.

The right side of the main control panel consists of the controls and indicators dealing with ship motion generation, including: the position of the centre post's universal joint; a control to indicate sinusoidal or random ship motion; sinusoidal motion controls such as magnitude, period, and phase of the waveform that can be adjusted during operation; numerical indicators that output linear and angular ship displacements; and data recording controls that indicate what data is sent to the *Write-to-file* program.

Figure 3.8: Potentiometer DAQ front panel

3.3.2 Potentiometer Data Acquisition

A basic DAQ program was provided by Indal Technologies Inc. and modified to measure the voltage signals from the seven potentiometers used. Figure 3.8 shows the front panel of the program, entitled *Potentiometer Recorder Chart*, which allows calibration of the potentiometers, displays a chart of the seven potentiometer voltages, and displays the calculated values of interest: helicopter orientation, caster wheel orientation, and suspension deflections. These values of interest are also displayed on the main control panel.

The DAQ program has settings to control the *scan rate* (per channel) and the *scans averaged*. The scan rate is the total number of samples per second. However, the scans averaged is the number of scans to be averaged to produce one point, used to reduce the noise in the measurements. Therefore, the sample rate, in samples per second, is:

Figure 3.9: Potentiometer calibration graph

$$\text{sample rate} = \frac{\text{scan rate}}{\text{scans averaged}} \quad (3.3)$$

The scan rate must be chosen such that the total scan rate for all channels doesn't exceed the capabilities of the DAQ card, which is 200 ks/sec. The value chosen for scans averaged is a compromise between noise level and sample rate.

The modifications to this program, which will be discussed in more detail, include converting the potentiometer voltage readings to helicopter and caster wheel orientations and suspension deflections.

Helicopter and Caster Wheel Orientations

The DAQ program has been modified to determine the appropriate potentiometer to be used and to calculate the resulting orientation. Each potentiometer needs to be set up such that it is fully-measurable in the range in which it is used. It is also required to use the zero balance on the front panel, shown in Figure 3.8, to alter the reading on Potentiometers 2 and 4 for a smooth transition between the potentiometers.

Figure 3.9 shows that the voltage measured from the potentiometer is linear throughout its range from 0 to 5V and hence the voltages can be converted to angles through a linear relationship by specifying the voltages at 0, 180, and -180 degrees. Separate slopes are calculated for each potentiometer to improve accuracy. Since the orientation operates around 0, it was important to divide the slope at the origin to improve accuracy at the operating point. If a single slope from -180 to 180 degrees was used, any slight error at either end will affect the value around the operating point. The caster wheel potentiometers are calibrated similarly.

Suspension Deflection

A single potentiometer is placed at the pivot point of the trailing arm suspension at each wheel to monitor the suspension deflection. The suspension deflection is determined by subtracting the deflected shock assembly length, L_s (Equation 2.96), from the length of the shock assembly at the neutral position, L_{sn} (Equation 2.97), which was derived in Section 2.3.2. However, to determine the absolute orientation of the trailing arm, the orientation is zeroed when the suspension is at full extension. Therefore, the zero balance on the front panel, shown in Figure 3.8, is used to set the fully-extended angle to zero. Also, the left main wheel potentiometer is mounted opposite to the other two, so its measurement needs to be multiplied by -1 to obtain the correct deflection sign. This is done by using the scaling option on the front panel.

3.3.3 ASIST Helicopter Manoeuvring

The LabView program developed to control the two degrees of ASIST actuation allows for three possible control methods: input from a joystick to manually control the longitudinal and lateral RSD motions; input from a file to accept pre-programmed trajectories; and input from an autonomous manoeuvring control algorithm, covered in Section 4.1. This program, entitled *ASIST Main*, is used to control the velocities of the two stepper motors, representing the ASIST actuation, using any of the above-mentioned control modes. All activity is controlled and monitored through the main control panel described in Section 3.3.1.

ASIST Main uses a while loop to interpret the joystick and main control panel commands and send the commanded velocities to the stepper motors approximately every 100 ms. Such commands from either the joystick or the main control panel include: longitudinal (RSD) and lateral (claw) velocities, the manoeuvring control mode, the centre-lock on/off toggle switch, the free-float on/off button, and the stop button. Other inputs from the main control panel that remain constant are kept outside the while loop and are only read at the start of the program. These inputs therefore must be set before the program is run. Some of the more important values include: the maximum RSD and claw velocities for both real time and simulation time operation; dead-band velocities²; and the control method desired.

A home position control allows the two ASIST actuators to be placed at their starting position, which is typically the centre of the track for both axes. Once both axes are moved to the home position, the position is zeroed and fore and aft displacements of the RSD are positive and negative measured from that point, respectively, and port and starboard displacements of the claw are positive and negative, respectively. The zeroing of the RSD is done at the start of each test session to assure the claw is at the centre of the track.

Manual Manoeuvring

The manual manoeuvring control mode mimics the joystick controller used in the real ASIST system. The analog joystick inputs in the fore-aft and left-right directions are converted to RSD longitudinal and lateral actuator velocities, respectively, by scaling the maximum velocity value (from the main control panel) by the percentage of the analog input measurement to its maximum value. The maximum analog values are determined by calibrating the joystick at the start of the program by moving it through its full range and measuring the centre, maximum, and minimum values for each axis. Since the RSD lateral velocity is either on or off in the actual ASIST system, the dead-band velocity is chosen to be just below the required ASIST claw velocity so that there is minimal ramping to its required velocity magnitude.

²The dead-band velocity is the minimum possible velocity interpreted from the joystick input. Any joystick input below this value produces zero velocity to eliminate noise.

Similar to the manual manoeuvring features of the actual ASIST RSD claw centre-lock and free-float are features that are included in this program to help the operator during the manoeuvring procedure. Centre-lock allows the claw to stop at the centreline during manual manoeuvring to assist the operator in finding the centre of the track. In this program, this is accomplished by multiplying the current claw position with the previous claw position to monitor when this product becomes negative. When this happens, the claw has crossed the centreline, and an indicator light illuminates on the main control panel to indicate that the claw is centred. Also, if the centre-lock feature is turned on, the program pauses for 1 second, simulating the centre-lock feature. In the actual system, the claw remains locked at the centre until a switch is released at the main control console.

Free-float is activated by pressing on the top left joystick trigger button while moving the probe either longitudinally or laterally. For example, during lateral free-float, as long as the button is enabled, the velocity is no longer read from the joystick analog input in the lateral direction. Instead, the probe forces in the lateral direction are measured and a Proportional-plus-Integral-plus-Derivative (PID) feedback controller is used to calculate the velocity required to reduce the lateral force on the probe to zero. Longitudinal free-float operates similarly. If motion is occurring in both directions when free-float is activated, the probe will be actuated in both directions to fully relieve the stress on the probe, much like the actual system. The PID controller was tuned experimentally using Ziegler-Nichols tuning rules. The theory of PID controller design and tuning is described in Section 3.5.1.

Pre-Programmed Input File

The pre-programmed input file control mode can replace the output velocities from the joystick read function with an input file generated using *HeliMan*, hand calculations, experimental results from *ASIST Main*, or other methods.

Autonomous Manoeuvring

The autonomous manoeuvring control mode replaces the output velocities from the joystick read function with a program that contains the autonomous manoeuvring controller, written in Matlab, called *Matlab Manoeuvring*, the development of which is described in Section 4.1. This mode requires the Potentiometer DAQ program to run, described in Section 3.3.2, to continuously monitor the helicopter's orientation, which is sent to *Matlab Manoeuvring* along with the helicopter's position³ from *ASIST Main*. Based on the helicopter position and orientation, the controller then interprets the system error and calculates the appropriate RSD and claw velocities. In this mode, the VI stops when the RSD activates the forward limit switch, indicating that the helicopter is safely in the hangar.

3.3.4 Ship Motion

The LabView program developed to generate the ship motion using the single roll actuator, entitled *Ship Motion*, must allow for the three types of ship motion: sinusoidal motion where the amplitude and period are controlled through the main control panel during operation; input from a file to accept pre-programmed random ship motion; and input from a file to accept experimental data from sea trials. The last two types of motion are similar since they are both considered random ship motion and are input through a file and only differ in the method of generating the data. The first type of motion, however, is different because the data is not pre-recorded and it is not random, rather it is controlled in real time during operation. This program can generate any of the above-mentioned ship motions. The generation of continuous ship motion with predefined motions will be described in this section and the differences with sinusoidal motion mode will be pointed out when required.

The front panel, shown in Figure 3.10 contains: numerical controls to set different properties of the ship motion, which typically remain constant; numerical controls for platform dimensions, which typically remain constant; numerical controls to set the start time; a graphical indica-

³The helicopter's position is continuously monitored by using a *Read Position* function built in to LabView that counts the absolute positions (in steps) of the two RSD stepper motors.

Figure 3.10: *Ship Motion* program front panel

tor of the angular ship motion created, in degrees; and the option to analyze the angular and translational ship motions at either the probe location along the deck during manoeuvring, or any arbitrary stationary point on the deck at which an accelerometer can be secured. These values are displayed on the main control panel and updated with each program loop. This is a stand-alone program when using pre-defined motions, however, for sinusoidal motion, the main control panel contains the controls to change the magnitude and period of the motion input.

To create continuous ship motion, an appropriate number of points, 300 in this case, are written to a buffer and are sent to the motion controller. The motion controller then sends these points to the linear actuator drive at 10 ms intervals between points. To keep the motion continuous the buffer is monitored to write another group of points to the buffer once it is below half full.

There are several steps required to generate continuous ship motion. These include:

1. generating an array of desired roll angles;
2. converting the desired ship motion to motor angular displacement;
3. writing the data to the buffer and continuously checking the buffer to determine when more data points are needed; and
4. calculating angular and translational ship motion to be displayed in real time.

These steps are discussed briefly within this section.

Generating an Array of Input Roll Displacements

A Matlab program uses linear RAO-based motion, described in Section 1.2.4, to create three separate files consisting of a 1-D array of absolute angular roll displacements (deg), velocities (deg/s), and accelerations (deg/s²). The roll displacement file is read by LabView into an array at the start of the program and before motion begins to prevent interruptions during the motion. The roll velocity and acceleration files are used during post-processing in the motion validation process. It is important to note the time step chosen when generating the data in the Matlab program, as this will determine if the motion is generated in real time or simulation time. When implementing the motion in real time, the time step must be the same as the 10 ms buffer interval time. However, to properly scale the ship motion dynamically, time must be scaled by $\sqrt{R}$, as described in Section 2.1.1. Therefore, in simulation time, the data must be generated in the Matlab program with a time interval of $\frac{10}{\sqrt{R}}$ ms $\doteq 31.6$ ms. When sent to the buffer at 10 ms intervals, the motion will appear faster than the real time case.

The array of input roll displacements may require adjustments due to the yaw angle, as discussed in Section 2.3.1. To adjust for a deck yaw angle (λ), the array of input roll displacements (Θ) is converted to adjusted roll displacements (Θ') using Equation 2.46.

For the case when the starting angle is non-zero, two methods are available to blend the zeroed platform angle to the first data point: a ramping function and a cubic polynomial. The

Figure 3.11: Comparison of input, adjusted, and ramped roll angles

ramping function multiplies the angular displacements by a ramp function over a defined time period, Δt . This results in 0% of the angular position at $t = 0$ and 100% of the angular position at $t = \Delta t$. The alternative method required appending a cubic polynomial to the beginning of the array. The polynomial blends an initial starting angle of zero and slope of zero to the initial data point and initial slope of the array. The problem with the cubic polynomial is that when the program is run, it may exceed the expected maximum angular roll displacement. Therefore the ramping function was determined to be the preferred method.

Figure 3.11 shows example input data (Θ), the adjusted data due to a yaw angle of 45 deg (Θ') and the final ramped data, which only differs from Θ' in the first $\Delta t = 3$ sec, in this case. This ramped data is now ready for the next step, which is the conversion of ship motion to linear actuator displacements.

Sinusoidal Motion

When simulating sinusoidal ship motion, the pre-generation of data is not required. Instead, the magnitude and period of this waveform to be generated is controlled through the main control panel and can be adjusted during operation. Therefore, when assembling the 300 points to be sent to the buffer, the waveform is calculated using the instantaneous main control panel settings. Neither the adjustment of the input angle due to yaw nor the adjustment to blend the starting motion are required.

Converting Ship Motion to Linear Actuator Displacement

Before sending the first group of 300 points to the buffer, the angular roll data⁴ (deg) must be converted to the equivalent absolute linear actuator position required, ΔL_{act} (in), using the kinematic equations that were outlined in Section 2.3.1, resulting in:

$$\Delta L_{act} = \sqrt{\left[\frac{w_r}{2}\text{c}\Theta - (h_2 - \ell_{joint})\text{s}\Theta - \frac{w_r}{2}\right]^2 + \left[h_1 + \frac{w_r}{2}\text{s}\Theta + (h_2 - \ell_{joint})\text{c}\Theta\right]^2} - (\ell_{post} - \ell_{joint}) \quad (3.4)$$

where Θ is in radians and the platform dimensions are input through the main control panel.

The program *Inch-to-Step Conversion* is used to convert the linear actuator position to angular motor position (steps). This is a function of the servo motor encoder's resolution (steps/rev), and the lead screw's pitch (rev/in). The conversion is thus as follows:

$$\text{motor rotation(steps)} = \text{disp(in)} \cdot \text{resolution}\left(\frac{\text{steps}}{\text{rev}}\right) \cdot \text{pitch}\left(\frac{\text{turns}}{\text{in}}\right) \cdot \text{gear ratio}\left(\frac{\text{rev}}{\text{turn}}\right) \quad (3.5)$$

where the resolution is 4000 steps/rev, the pitch is 2 TPI, and the gear ratio is 2:1, resulting in 16000 steps/in.

Calculating Linear Ship Motions

The instantaneous position of the actuator (steps) is continuously monitored to calculate and display the current translational ship motions that occur. Since the kinematic equations that were outlined in Section 2.3.1 are in terms of the platform roll angle (Θ), the given position of the actuator (steps) must be converted back to the platform roll angle, using Equation 2.69, before applying the kinematic equations to determine the translational and angular ship motions: roll and pitch angles (Equations 2.43 and 2.44); and surge, sway, and heave translational displacements (Equations 2.32 to 2.34). These are displayed on the main control panel during operation.

⁴Since the adjustment due to roll and the ramping of the data is optional, Θ will still be used for the remaining discussion as the input platform roll to avoid confusion between the different adjustments of Θ .

3.4 Motion Validation

The validation of the motion platform has two purposes: to verify that *Ship Motion* correctly implements the kinematic equations used to convert the desired ship motions to linear actuator displacements; and to verify that the deck motion corresponds to the commanded motion. These are confirmed by direct measurement of deck motion using the accelerometer sensor package. Once validated, the platform motions from either the input file, the LabView ASIST Main program, or the accelerometer sensor are known to be legitimate and consistent.

As was described in Section 3.3.4, the desired roll displacements, Θ , roll velocities, $\dot{\Theta}$, and roll accelerations, $\ddot{\Theta}$, are pre-generated using Equations 1.1 to 1.3, respectively, since the generation of this data is computationally intensive due to the trigonometric function evaluations associated with the summation of the N waveforms at each time step. Therefore, this data is generated once prior to operation for each sea state, heading, and speed combination desired.

The LabView program *Ship Motion* converts the desired roll displacements to motor revolutions that are sent to a buffer. The instantaneous position of the motor is constantly monitored and converted into deck angular and translational displacements, using the kinematic equations from Section 2.3.1. This information is displayed on the main control panel in real time and must be validated to assure that: the real-time data displayed on the main control panel is accurate; the calculations performed are correct; and the *data write* file function correctly synchronizes the data recorded. This is accomplished by comparing these results with the expected deck roll and pitch motions calculated from the input file and the angular measurements from the accelerometer sensor.

The accelerometer sensor is placed on the deck at the point of interest, aligning its local coordinate system with that of the deck. The measured translational accelerations are expressed in the sensor's local coordinate system. Transforming the data to global coordinates was found to introduce noise in the results due to a limited number of significant digits for orientation provided by the accelerometer sensor. Hence, it was determined that it would be easier and more accurate to transform the calculated linear accelerations from the input file to local sensor

Figure 3.12: Deck roll angle for sea state 5 conditions and $\lambda = 0$ deg

coordinates, $\vec{a}_p^{sensor}$ (that align with the deck). This is done by adding the acceleration required to oppose gravity ($-\vec{g}$) to $\vec{a}_p^{XYZ}$ (Equation 2.38) and pre-multiplying the result by the inverse of $[T_{mno \rightarrow XYZ}]$ (Equation 2.23) as follows:

$$\begin{aligned} \vec{a}_P^{sensor} &= [T_{mno \rightarrow XYZ}]^{-1} (\vec{a}_P^{XYZ} - \vec{g}) \\ &= \dot{\Theta}^2 \begin{bmatrix} -s\lambda(x_p s\lambda + y_p c\lambda) \\ -c\lambda(x_p s\lambda + y_p c\lambda) \\ -(h_2 + h_3 + z_p) \end{bmatrix} + \ddot{\Theta} \begin{bmatrix} -s\lambda(h_2 + h_3 + z_p) \\ -c\lambda(h_2 + h_3 + z_p) \\ x_p s\lambda + y_p c\lambda \end{bmatrix} + \begin{bmatrix} s\lambda s\Theta g \\ c\lambda s\Theta g \\ -c\Theta g \end{bmatrix} \quad (3.6) \end{aligned}$$

where $c = \cos$, $s = \sin$, and x_p , y_p , and z_p are the local coordinates of the point of interest (in this case, the location of the accelerometer's origin). This validates that the real-time translational motion data (surge, sway, and heave displacements) displayed on the main control panel are accurate.

3.4.1 Angular Motions

The first validation test consisted of inputting a platform roll displacement input file to the *Ship Motion* LabView program and recording both the LabView real-time deck motions as well as the measured experimental results from the accelerometer sensor. The input file selected was for a typical representative ship in sea state 5, a 120 deg heading, and a speed of 10 knots,

Figure 3.13: Adjusted deck roll angle for sea state 5 conditions and $\lambda = 10.4$ deg

Figure 3.14: Adjusted deck pitch angle for sea state 5 conditions and $\lambda = 10.4$ deg

resulting in a maximum input roll angle of approximately 20 deg. Figure 3.12 shows that for $\lambda = 0$ deg, the resulting deck roll angle recorded from the output of the LabView program and the measured experimental data from the accelerometer sensor agree closely with the expected deck roll calculated from the input file. This validates that the calculations used to control the linear actuator are correct and that the *data write* file function correctly synchronizes the data recorded.

Figures 3.13 and 3.14 show that for $\lambda = 10.4$ deg, the results obtained from the two sources agree closely with the expected adjusted deck roll and pitch angles calculated from the input file. This validates: the relationship between the input platform roll (Θ) and the resulting deck roll

Figure 3.15: Analytical and experimental local surge accelerations for $\lambda=0$ deg

Figure 3.16: Analytical and experimental local sway accelerations for $\lambda=0$ deg

Figure 3.17: Analytical and experimental local heave accelerations for $\lambda=0$ deg

(θ) and pitch (ϕ) due to λ ; and the equation required to adjust Θ due to λ , Equation 2.46. In addition, λ was calculated using Equation 2.54 for a $\Phi_{RMS} = 0.95$ deg, which was obtained as the pitch RMS value from the same input file.

3.4.2 Linear Motions

The second validation test, using the same input data, compared the unfiltered experimental translational accelerations measured by the sensor with the calculated expected translational accelerations from the input data file, from Equation 2.38, transformed to the sensor's local coordinate system using Equation 3.6. Figures 3.15 to 3.17 show the validation results for the local surge, sway, and heave translational accelerations, respectively, when $\lambda = 0$.

The calculated and measured sway and heave accelerations agree quite well, while some noise is present in the surge direction, since no surge should occur in the case when $\lambda = 0$ deg. This may be caused by the accelerometer sensor coordinate system being slightly misaligned with respect to the deck coordinate system. It is important to note that the heave linear accelerations oscillate around 386.4 in/s^2 , instead of 0, as is the case for surge and sway accelerations. This is due to the accelerometer sensor measuring the acceleration required to oppose gravity.

Figures 3.18 to 3.20 show the validation results for the local surge, sway, and heave translational accelerations, respectively when $\lambda = 10.4$ deg. Similarly, the calculated and measured local surge, sway and heave accelerations agree quite well. These results validate the calculated and displayed translational and angular motions.

3.5 Experimental Testing

As the final step in Phase III of the project, experimental testing was performed to: tune the operation of the ASIST free-float capability experimentally using Ziegler-Nichols tuning rules; obtain a set of parameters characterizing the DLTV; and investigate DLTV securing forces during manoeuvring in the presence of ship motion. These tests will be described in this section, along with a background of PID controllers, used to achieve free-float.

Figure 3.18: Analytical and experimental local surge accelerations for $\lambda=10.4$ deg

Figure 3.19: Analytical and experimental local sway accelerations for $\lambda=10.4$ deg

Figure 3.20: Analytical and experimental local heave accelerations for $\lambda=10.4$ deg

3.5.1 PID Controllers and Experimental Tuning

A PID controller is a control action that uses adjustable constants to interpret the system error and produce a control signal to reduce the error to zero, resulting in the control of the system behaviour [55]. PID controllers are used in this project to tune the servo motor for the linear actuator; operate free-float using the force/torque sensor as the feedback device; and operate ASIST autonomously, as will be discussed in Chapter 4.

Other forms of controllers include the PI controller (Proportional-plus-Integral) and the PD controller (Proportional-plus-Derivative). However, PID controllers combine the characteristics of each of the PI and PD controllers.

The integral control action of a PI controller changes the value of the controller output at a rate proportional to the time integral of the actuating error signal [55]. Characteristics of the PI controller include:

1. elimination of steady-state error;
2. initiation of oscillatory response;
3. reduction of maximum overshoot; and
4. increase of rise time, settling time.

The derivative control action of a PD controller responds to the rate of change of the actuating error and makes a correction before the actuating error becomes too large. Derivative control thus anticipates the actuating error, initiates an early corrective action, and tends to increase the stability of the system [55]. Characteristics of the PD controller include:

1. initiation of an anticipatory response;
2. amplification of noise signals;
3. improvement of damping; and
4. reduction of maximum overshoot, rise time, and settling time.

The relationship between the output of the controller, $u(t)$, and the actuating error signal, $e(t)$, is:

$$u(t) = K_p e(t) + \frac{K_p}{T_i} \int e(t) dt + K_p T_d \frac{de(t)}{dt} \quad (3.7)$$

where K_p is termed the *proportional gain*, T_i is the *integral time*, and T_d is the *derivative time*.

The process of selecting the controller gains to meet given performance specifications is known as *controller tuning* [55]. If the mathematical model of the plant is available, there are various analytical design techniques for determining the control gains, such as the transient-response or frequency-response analysis of the dominant poles, the Root Locus method, Routh's Stability Criterion, the Nyquist Stability Criterion, and the pole-placement method, to name a few [55]. However, since the mathematical model of the plant is not available, experimental approaches to the tuning of PID controllers, such as the methods proposed by Ziegler and Nichols [56] are used.

Experimental Tuning Using Ziegler-Nichols

To tune a PID controller experimentally, Ziegler-Nichols tuning offers two methods to approximate values K_p , T_i , and T_d from Equation 3.7 as a starting point for fine tuning. The first method is based on obtaining the open-loop step response, assuming that the response exhibits an S-shaped curve, and hence does not contain an integrator. The second method is a closed-loop tuning procedure based on finding the critical proportional gain, K_{cr} , that results in sustained steady-state oscillation when only the proportional control action is used [55].

Ziegler-Nichols experimental tuning was used to determine the appropriate gains for the free-float PID controller. In this control system, the force/torque sensor measures the forces and the controller interprets the error to produce an appropriate claw velocity to reduce the error to zero. The second method was used since the system proved to exhibit sustained oscillations for a large value of K_p .

The second method requires setting $T_i = \infty$ and $T_d = 0$ and actuating the probe at a constant velocity with free-float activated. By slowly increasing K_p , a critical value, K_{cr} , is attained

Figure 3.21: Sustained oscillation with period P_{cr} [55]

Table 3.1: Ziegler-Nichols tuning rule based on critical gain and critical period (second method) [55]

Type of Controller	K_p	T_i	T_d
P	$0.5K_{cr}$	∞	0
PI	$0.45K_{cr}$	$\frac{1}{1.2}P_{cr}$	0
PID	$0.6K_{cr}$	$0.5P_{cr}$	$0.125P_{cr}$

Table 3.2: Results using the second method of the Ziegler-Nichols tuning rules

	X-Float	Y-Float
K_{cr}	-6300	-9950
P_{cr}	1.03	0.92
K_p	-3780	-5970
T_i	0.513	0.460
T_d	0.128	0.115

where sustained oscillations are observed. Therefore, the critical gains, K_{cr_x} and K_{cr_y} and the corresponding period P_{cr_x} and P_{cr_y} can be determined experimentally, as shown in Figure 3.21. The determined values for K_p , T_i , and T_d are then calculated according to Table 3.1, and then fine-tuned accordingly.

The transfer function of the system resulting from the second method of Ziegler-Nichols rules can then be approximated as [55]:

$$\begin{aligned}
 u(s) &= K_p \left(1 + \frac{1}{T_i s} + T_d s\right) \\
 &= 0.6 K_{cr} \left(1 + \frac{1}{0.5 P_{cr} s} + 0.125 P_{cr} s\right) \\
 &= 0.075 K_{cr} P_{cr} \frac{\left(s + \frac{4}{P_{cr}}\right)^2}{s}
 \end{aligned} \tag{3.8}$$

and therefore, the PID controller has a pole at the origin and double zeros at $s = -\frac{4}{P_{cr}}$, which describes the dynamic behaviour of the second-order system.

The resulting gains for both float directions using a PID controller are shown in Table 3.2. Note that negative gains are required to produce a positive velocity if a negative force is measured.

3.5.2 DLTV Testing

A set of DLTV parameters was obtained experimentally⁵ as inputs into simulations, such as *He-Man*[1] or Dynaface[®] [15]. DLTV testing first required designing and building the apparatuses for the desired tests [57]. An objective was to use as few apparatuses and parts as possible to acquire the DLTV properties experimentally. As a result, the scaled model of ASIST, as well as two other apparatuses were used to determine a total of 12 values required, grouped into the following tests:

1. main suspension, caster suspension, lateral, radial, and longitudinal tire stiffness;
2. main suspension, caster suspension, lateral, radial, and longitudinal tire damping;
3. tire coefficients; and
4. inertial properties (I_{xx} , I_{yy} , I_{zz}).

⁵The designs, experimental testing, and analysis were performed by R. Henderson [57], a 2nd-year CU Engineering co-op student under the supervision of Dr. R. G. Langlois.

Figure 3.22: Apparatus used for stiffness and damping tests

One apparatus, shown in Figure 3.22, was designed to perform Tests 1 and 2 using several interchangeable parts for the different components. The stiffnesses of the components are measured by placing known weights at the end of the lever arm, above the component, and using a potentiometer to measure the rotation of the lever arm. Using the same apparatus, the damping was measured by recording the lever arm rotation over time while a known mass is dropped on the spring, or the tire is dropped from an elevated position. The data is then converted to vertical displacements and plotted versus time to determine the logarithmic decrement⁶. The damping ratios can then be determined from the measured ratio of decay of the oscillations.

A second apparatus, shown in Figure 3.23, was designed to determine the tire cornering coefficient in Test 3. The apparatus is attached to the RSD claw and uses the force/torque sensor described in Section 3.2.2 to measure the forces that occur at various tire slip angles⁷. Figure 3.24 shows the tire deflection that occurs at large slip angles. The tire's lateral force is called the cornering force and is plotted for a range of slip angles, resulting in the cornering coefficient for the tire.

A third apparatus, known as a *trifilar suspension* shown in Figure 3.25, was designed to determine the moments of inertia (MI) in Test 4. The DLTV MI can be easily determined by aligning its CM with the plate CM and determining the period of oscillation of the DLTV-plate

⁶The logarithmic decrement is the logarithm of the ratio of successive amplitudes of an exponentially damped sinusoid [55]

⁷A slip angle is the angle between the tire's direction of motion and the direction of the tire.

Figure 3.23: Apparatus used for tire cornering coefficient tests

Figure 3.24: Tire deflections that occur at large slip angles

combination. Knowing the MI of the aluminum plate, the MI of the DLTV can be determined by subtracting the MI of the plate from the measured MI of the DLTV-plate combination, determined from the period of oscillation. This is done with the DLTV oriented in its three planes, resulting in I_{xx} , I_{yy} , and I_{zz} .

The remaining DLTV properties, such as mass, dimensions, and location of the CM were determined using direct measurement methods. The dimensions are shown in Figure 2.2 and the resulting properties are summarized in Table 3.3. The detailed test plan and calculations for each of the tests are described in the helicopter model parameter testing design report [57].

The objective of this testing was to input the scaled DLTV parameters in the *HeliMan* manoeuvring simulation to compare the analytical results with those obtained on the motion

Figure 3.25: Trifilar suspension used to determine DLTV inertial properties

Table 3.3: DLTV properties obtained though experimental testing

DLTV mass	0.62 slugs
Main suspension stiffness, damping	22.45 lb/in, 0.83 lb s/in
Caster suspension stiffness, damping	40.0 lb/in, 0.17 lb s/in
Longitudinal tire stiffness, damping	902.9 lb/in, -
Lateral tire stiffness, damping	91.6 lb/in, -0.908 lb s/in
Radial tire stiffness, damping	7219.8 lb/in, 0.2761 lb s/in
Moments of Inertia (I_{xx} , I_{yy} , I_{zz})	15.1, 8.2, 18.6 slugs in ²

platform without motion. However, due to differences between the foam tires used on the DLTV and the pneumatic tire model available in *HeliMan*, an alternative approach was required. Instead, the test results obtained on the motion platform were scaled up using the scaling laws from Section 2.1.1 and compared with the full-scale results obtained from *HeliMan*. However, due to the differences in tire properties, some differences in manoeuvring behaviour must be expected.

3.5.3 Securing and Manoeuvring Testing

To demonstrate the operability of the motion platform, the following tests were performed: the securing of the DLTV on the motion platform with ship motion and the manoeuvring of the DLTV on the motion platform both with and without ship motion. The manoeuvring trajectories both with and without ship motion were then scaled up and compared with the manoeuvring trajectory of the full-scale DLTV using *HeliMan*.

A simple manual manoeuvring procedure was performed on the scaled DLTV in real time using the motion platform where the x- and y-velocities were recorded in an input file for the purpose of replaying the same manoeuvring procedure for each of the tests. Figures 3.26 to 3.28 show the resulting probe securing forces F_x , F_y , and F_z , during each of the tests on the motion platform. For the case with motion, the same sea state, ship heading, and ship speed were used as for the validation tests from Section 3.4 with a deck yaw angle of 10.4 deg. As shown in the Figures, the effects of the RSD and claw velocities greatly magnify the securing forces experienced on the DLTV in the presence of ship motion. As a result, the forces due to the motion component oscillate about the force curve due to manoeuvring. This is shown very clearly in the y -direction since there is a large motion affect due to roll. Hence, simulations that analyze the securing forces due to ship motion only are neglecting a significant force component.

The initial position and orientation of the DLTV during testing was $(x_p, y_p) = (-3.05 \text{ in}, 0 \text{ in})$ and $\rho = 18.0 \text{ deg}$, respectively. To test this manual manoeuvring procedure using *HeliMan*, the initial position, orientation, and velocities from the motion platform were scaled up using the appropriate real-time scaling laws from Section 2.1.1. Since the longitudinal RSD velocity is variable and normally input into *HeliMan* as analog joystick commands, joystick conversion factors are required for positive ($+\dot{x}_{joystick}$) and negative ($-\dot{x}_{joystick}$) longitudinal velocities:

$$+\dot{x}_{joystick} = \text{ROUND}((\dot{x} * 33) + 32, 0) \quad (3.9)$$

$$-\dot{x}_{joystick} = \text{ROUND}((\dot{x} * 32) + 32, 0) \quad (3.10)$$

Figure 3.26: Probe forces in the x-direction during manual manoeuvring with and without motion, and securing with motion

Figure 3.27: Probe forces in the y-direction during manual manoeuvring with and without motion, and securing with motion

Figure 3.28: Probe forces in the z-direction during manual manoeuvring with and without motion, and securing with motion

The resulting trajectories obtained on the motion platform were scaled up and compared to the full-scale trajectories obtained from *HeliMan*. Figures 3.29 to 3.31 show the resulting x- and y-positions and helicopter orientations during the simulation, respectively, from *HeliMan*, and the motion platform both with and without motion. In Figures 3.29 and 3.30, the results from the motion platform with and without motion are identical, as expected, since ship motion shouldn't affect the position of the RSD and claw. In Figure 3.29, the results obtained from *HeliMan* tend to show that the x-position of the probe does not reach the same magnitudes as on the motion platform. This is due to the conversion of the longitudinal velocities from the motion platform to *HeliMan* joystick inputs since, as shown in Equations 3.9 and 3.10, the joystick velocity inputs are rounded to the nearest integer. This results in an error in the x-position over time.

Figure 3.30 shows that the *HeliMan* lateral position agrees with the results from the motion platform because the claw velocities are input directly. This is possible since the claw can either have a constant positive velocity, constant negative velocity, or zero velocity, and hence does not require variable input from the joystick. Figure 3.31 shows that the helicopter orientation results

Figure 3.29: Full-scale DLTV x-position during manual manoeuvring using *HeliMan* and the experimental motion platform (with and without motion)

on the motion platform with and without motion agree closely, except for a slight oscillation due to the ship motion. However, the orientation results from *HeliMan* tend to differ slightly. For example, after 30 seconds, there is a large difference in orientation between *HeliMan* and both of the motion platform results. This is due to both the difference in probe position during the simulation as well as the differences in tire and inertial properties between the full-scale DLTV and the scaled DLTV used with the motion platform. The slightest difference in the trajectory can affect the position of the caster wheel, which dictates the direction of rotation, and causes the difference in helicopter orientation.

Alterations to the *HeliMan* joystick conversion factor and input of the proper DLTV properties are expected to greatly improve these results. Although modifying *HeliMan* was beyond the scope of this thesis, basic comparisons with *HeliMan* were still considered with the differences in RSD motion in mind.

Figure 3.30: Full-scale DLTV y-position during manual manoeuvring using *HeliMan* and the experimental motion platform (with and without motion)

Figure 3.31: Full-scale DLTV orientation during manual manoeuvring using *HeliMan* and the experimental motion platform (with and without motion)

Chapter 4

Autonomous Helicopter Manoeuvring

As helicopters landing on moving decks will seldom land perfectly positioned and aligned, the ASIST system operator must select and execute an appropriate sequence of manoeuvres to straighten the aircraft. At the present time, ASIST requires a skilled operator to achieve this. However, as the on-deck position and orientation of the aircraft are measured continuously by the ASIST system, it is potentially possible to ease the operator's task by automatically selecting the optimal manoeuvring sequence and executing it to within tighter tolerances and with greater efficiency and reliability than those that a human operator could achieve.

The autonomous manoeuvring algorithms were developed and tested using *Heliman*. Two approaches to autonomous manoeuvring were examined in developing the autonomous manoeuvring sequences: trajectory planning and the sequential method. Trajectory planning [58] uses the initial position and orientation of the aircraft on the flight deck and the final desired conditions to predict the ideal path for the helicopter to follow during straightening. During the evaluation of this method using *Heliman*, it was determined that due to the behaviour of the nose/tail wheel, this method can only work partially with a casterable nose/tail wheel helicopter configuration, and in general does not work with a steerable wheel. The sequential method uses

a sequence of steps to manoeuvre the helicopter, much like the manual method, yet with fewer moves and less elapsed time than with a human operator. The sequential method proved to be the most efficient method for developing the autonomous manoeuvring algorithms and will be discussed in more detail in Section 4.3. The sequential method may also have the added capability of being usable with steerable nose wheel helicopters, such as the EH-101, H-92, Cougar, Dauphin, Lynx, and NH-90.

Some important aspects that were considered during the development of the autonomous manoeuvring algorithms include: using only the measured information currently available from the HPSE; decreasing the time required to manoeuvre the helicopter; reducing the lateral forces on the tires to produce moderate tire deflection; and monitoring the system robustness since the control system must remain stable in the presence of disturbances, such as ship motion. These aspects will be discussed throughout this chapter and will be used as a measure of the efficiency of the autonomous manoeuvring algorithm.

4.1 Manoeuvring Control System

The control system involved in the autonomous manoeuvring of an on-deck helicopter is different than a conventional control system, such that the physical system is difficult to model mathematically. *HeliMan* was developed and implemented as an interactive real-time simulation, in part, to provide a convenient means for developing and testing the autonomous manoeuvring algorithms without requiring a linearized mathematical model as is often used in controller design.

Irrespective of the approach used to determine the manoeuvring sequences, the same steps are taken in implementing the autonomous manoeuvring algorithm. Figure 4.1 shows a block diagram of the autonomous manoeuvring control system, which consists of a controller, an actuator, a plant, and a sensor. *HeliMan* represents the plant, where the inputs to the simulation are the lateral and longitudinal forces from the RSD on the helicopter-mounted probe, and the outputs from the simulation are the three variables used to track the helicopter position and orientation: the new x - and y -positions of the RSD, as well as the orientation of the helicopter, ρ .

Figure 4.1: Autonomous manoeuvring control system

Designing a controller for the autonomous manoeuvring control system requires forcing the output of the system to converge to the desired reference signal, $r(t)$, by computing appropriate RSD and claw velocities. These outputs are measured by the sensors and are then compared to the desired reference signals, where the difference is the error, $e(t)$. A PID controller interprets the system error and computes the new RSD longitudinal velocities, $\dot{x}$, required to reduce the error to zero, since, unlike the claw, the RSD is pulley-driven longitudinally and can accept varying velocities. This will activate the actuator, providing both power amplification and motion to the plant [59].

Disturbances are generated externally to the loop and are signals that act on the plant to cause definite changes in operation [59]. The disturbances that will be considered include those due to ship motion. Since the plant does not consider the effects on the helicopter due to ship motion, the control system should be sufficiently robust to compensate for any differences due to these disturbances.

What will be discussed in the sections that follow are the steps taken in developing the autonomous manoeuvring sequences, as well as different strategies for refining the selected manoeuvring method.

4.2 Nose/Tail Wheel Behaviour

The two independent degrees of actuation provided by the RSD are used to control the longitudinal and lateral positions of the helicopter-mounted probe. Since the helicopter is equipped with either a casterable nose or tail wheel or steerable nose wheel, a change in the helicopter orientation during manoeuvring also occurs. Therefore, the system in question contains three degrees of freedom, and two degrees of actuation, which makes the control of the helicopter's orientation challenging. However, a steerable nose wheel configuration incorporates input from the pilot and contains an additional degree of actuation over the casterable nose/tail wheel configuration, making it easier to manoeuvre. Therefore, this research focusses on the more challenging condition where a casterable nose/tail wheel is present.

To obtain the required straightening conditions efficiently, we must study the nose/tail wheel behaviour during ASIST operation since it dictates the change in orientation of the helicopter. Figures 4.2 and 4.3 illustrate the four generalized situations that can occur during helicopter landing for both a nose wheel configuration and a tail wheel configuration, respectively, as well as the changes in orientation that occur for each direction of motion of the RSD during manoeuvring. The directions that are highlighted indicate the required directions that the probe must travel to place it at the centreline and traverse the helicopter into the hangar, assuming the hangar is to the right.

The four situations that can occur during landing can be described with two cases: oriented towards the centreline, and oriented away from the centreline. Since the probability of the helicopter landing exactly parallel to the centreline is relatively small, this situation is not shown in the figures. However, if this situation did occur, it would be treated as being oriented away from the centreline because lateral actuation of the claw towards the centreline will cause the helicopter to rotate away from the centreline.

For the two cases where the nose wheel helicopter is oriented away from the centreline, it is shown that the desired RSD directions of motion (highlighted) will result in the helicopter reducing its angle of rotation, which is desirable. However, for the two situations shown where

Figure 4.2: Helicopter motions due to a nose caster wheel

the nose wheel helicopter is oriented towards the centreline, the desired RSD directions of motion have the tendency to rotate the helicopter in opposite directions. Therefore, these two RSD motions have to be properly proportioned to reduce the angle of rotation, as well as bring the claw to the centreline. Therefore, it can be concluded that the motion in the forward direction for a nose wheel configuration is stable since it will always tend to rotate the helicopter towards the centreline and it may be possible to straighten the helicopter in just one move independent of the landing situation. Also, traversing the helicopter into the hangar after straightening will be stable as well, as it will only require forward RSD motion.

For the two cases where the tail wheel helicopter is oriented towards the centreline, the desired RSD directions of motion will result in conflicting changes in the helicopter orientation. Therefore, these two RSD motions have to be properly proportioned to reduce the angle of rotation, as well as bring the claw to the centreline. For the two situations shown where the tail wheel helicopter is oriented away from the centreline, both the lateral and longitudinal RSD directions of motion have the tendency to rotate the helicopter in the opposite direction to that required to straighten the helicopter. Therefore, for these landing conditions, it is impossible to straighten the helicopter directly and satisfy the desired three straightening conditions without first moving the RSD in a direction other than those highlighted, potentially making the manoeuvring procedure longer, and more complex.

Additionally, it is shown that the forward manoeuvring of a tail wheel helicopter configuration tends to be directionally unstable since it will always tend to rotate the helicopter away from

Figure 4.3: Helicopter motions due to a tail caster wheel

the centreline. Consequently, it is challenging to keep it aligned with the centreline during traversing. However, many helicopters use a tail probe that is dropped into the track or a wheel guide that is used to guide the tail wheel along the track centreline to facilitate traversing. Yet, the autonomous manoeuvring algorithm described here addresses the more challenging problem by assuming that a tail probe is not available to permit the development of a control system that is able to maintain the helicopter's heading during traversing despite the expected directional instability.

It is evident that the forward manoeuvring procedure for a tail-casterable wheel configuration is more complex than that for a nose-casterable wheel configuration. However, while reversing, the manoeuvring of a nose-casterable wheel configuration is more challenging. As this research is centred around the Sikorsky Seahawk, which contains a casterable tail wheel, the autonomous manoeuvring algorithms were determined for this configuration and will require minor changes to function for the nose wheel configuration due to the opposite reactions to forward and reverse manoeuvring. The understanding of the caster wheel behaviour during the operation of ASIST is crucial to helicopter manoeuvring, as well as the development of the autonomous manoeuvring algorithms, as will be discussed in the following sections.

4.3 Sequential Method

The sequential method was developed by considering nose/tail wheel behaviour and using a sequence of steps to manoeuvre the helicopter, similar to manual manoeuvring. This method selects the helicopter's path based on whether the helicopter is oriented towards the centreline or away from the centreline. The ideal manoeuvre would obtain all three straightening conditions at the same time by properly proportioning the RSD longitudinal velocity, $\dot{x}$, with respect to the claw lateral velocity, $\dot{y}$. Additional steps were added to the sequential method to assure that if the velocities were not properly proportioned or straightening were affected adversely by disturbances, another step is available to reduce the angle of rotation, ρ , as well as bring the claw to the centreline. Through testing using the planar manoeuvring simulation, this method has been shown to be robust for any landing position within the claw limits and with a landing orientation up to $\rho_{max} = \pm 30^\circ$.

In the current design of ASIST the claw lateral velocity, $\dot{y}$, has a constant positive or constant negative value. The RSD longitudinal velocity, however, is variable and can have any velocity up to the maximum RSD velocity, $\dot{x}_{max}$. Therefore, when the two RSD motions have to be properly proportioned, the RSD longitudinal velocity must always be varied with respect to the lateral claw velocity.

As was previously discussed, the forward manoeuvring of the tail-wheel helicopter will always tend to increase its orientation away from the centreline. Therefore, as ρ increases, $\dot{x}$ should be decreased to reduce the effect that it has on increasing the magnitude of ρ in the forward direction. This method, therefore, uses $e(t) = \rho$ as the control system error with a reference point of zero. During reverse manoeuvring, however, the helicopter's orientation is continuously reduced and the maximum RSD velocity can be used. Therefore, the longitudinal RSD velocity, $\dot{x}$, for forward manoeuvring is:

$$\dot{x} = K_p e(\rho) + \frac{K_p}{T_i} \int e(\rho) dt + K_p T_d \frac{de(\rho)}{dt} \quad (4.1)$$

Figure 4.4: Helicopter straightening and traversing with the sequential method

where an error function, $e(\rho)$, is defined as:

$$e(\rho) = 1 - \frac{|e(t)|}{\Delta\rho_{max}} \quad (4.2)$$

The PID gains are adjusted to obtain the desired system performance and $\Delta\rho_{max}$ is the maximum allowed deviation of ρ , which can be chosen as a way of tuning the controller's sensitivity. Notice that Equation 4.1 can result in a negative value of $\dot{x}$ if $e(t)$ becomes too large, in which case the helicopter would be manoeuvred in reverse to quickly reduce ρ after which $\dot{x}$ would become positive again.

Figure 4.4 shows an example of the sequential method with the helicopter oriented towards the centreline. Figure 4.4(a) shows the landing position for this example, which happens to be at $y = -1$ ft and $\rho = 15$ deg.

There are 3 main steps in the sequential method. Steps 1 and 2 are involved in straightening the helicopter, while Step 3 is involved in traversing the helicopter into the hangar. The first step checks if the helicopter is oriented towards or away from the centreline. Since the helicopter

is oriented towards the centreline, the helicopter claw is moved laterally towards the centreline at a constant speed and forward at a speed calculated using Equation 4.1, which must be slow enough to allow the helicopter to rotate in the proper direction. If the helicopter was oriented away from the centreline, the RSD claw would be moved laterally towards the centreline at a constant speed and reversed at the maximum RSD velocity. This step is executed until the claw reaches the centreline, after which point the algorithm moves on to Step 2.

Step 2 is required in the case that the orientation of the helicopter, ρ , is still relatively large once the claw reaches the centreline and additional straightening is required. This step merely reverses the helicopter at the maximum RSD velocity, $\dot{x}_{max}$, until the orientation is reduced to zero. Figure 4.4(b) shows that during Step 1, the claw reached the centreline before the helicopter orientation was adequately reduced. Therefore, Figures 4.4(b) and 4.4(c) show Step 2 being applied until the orientation is reduced to near zero.

Step 3 occurs when the helicopter's orientation is close to zero (within a user-selected tolerance) and the claw is at the centreline. During this final step traversing begins, where the helicopter moves forward towards the hangar. Because the forward manoeuvring of a casterable tail wheel helicopter configuration is directionally unstable and causes the tail wheel to diverge from the centreline, the claw is moved laterally to correct the rotation of the helicopter, as shown in Figures 4.4(d) and 4.4(e). This results in a triangular wave motion of the claw to keep the orientation close to zero, as shown in Figure 4.5. Since the orientation error remains small, $\Delta\rho_{max}$ is adjustable in Equation 4.1 during traversing, to allow the error to have a greater effect on reducing $\dot{x}$ when the orientation deviates from the centreline. This final step can be greatly simplified if the helicopter contains a tail probe to keep the helicopter aligned during traversing.

Note that the conditional statements do not allow a step to be repeated once it has passed to prevent the control system from oscillating between two steps. However, during traversing, if the sinusoidal motion of the claw becomes too large, the helicopter's position and orientation can be refined from Step 1 again. In manual manoeuvring it is not uncommon to make additional adjustments during the manoeuvre, therefore, this option in Step 3 is available to improve the results.

Figure 4.5: Position and orientation of the helicopter during autonomous straightening and traversing with the sequential method

Figure 4.6: Tire deflections during autonomous straightening and traversing with the sequential method

Figure 4.6 shows the desired tire deflection limits during manoeuvring and the resulting tire deflections that occur using the sequential method. The maximum tire deflections occur on the main wheels when changing direction and during traversing. Using a tail probe once the helicopter has been straightened would reduce the deflections shown in the figure that occur during traversing. Notice that this autonomous manoeuvring example requires just over 30 seconds to complete, compared to between 2 to 5 minutes using manual methods.

Figure 4.7: Time history showing desired claw position and helicopter orientation during straightening

4.4 Refinement of the Sequential Method

The refined sequential method addresses two possible improvements that could make both the straightening and traversing steps more efficient: obtaining a helicopter orientation of zero by the time the claw reaches the centreline during straightening; and reducing the magnitude of the claw oscillation during traversing.

4.4.1 Refining the Straightening Procedure

For the case when the helicopter lands oriented towards the centreline it would be desirable to obtain a helicopter orientation of zero at the same time that the claw reaches the centreline. This was attempted in the sequential method by using Equation 4.1 to calculate an appropriate value for $\dot{x}$ based on the value of ρ . However this method has been subsequently refined by describing a desired helicopter orientation over time and using a controller to regulate $\dot{x}$ to force the helicopter to follow this commanded orientation.

This is accomplished by plotting the claw's travel from its initial position to the centreline, computing the time required, t_f :

$$t_f = \left| \frac{y_0}{\dot{y}} \right| \quad (4.3)$$

and forcing the helicopter orientation to reach zero by that time.

Figure 4.7 shows the time required for the claw to reach the centreline and three possible

functions for the helicopter's orientation to follow: linear, exponential, and quadratic. It was decided to use the quadratic function since it is a compromise between the initial slopes of the linear and exponential functions. Reducing the orientation more rapidly at the beginning of the procedure allows more time to correct for residual error before the claw reaches the centreline. However, a slope that is too steep or not steep enough will result in overshoot or undershoot, respectively, of the helicopter's orientation.

For the orientation of the helicopter to start at ρ_0 at $t = 0$ and reach zero by $t = t_f$, the expected orientation, ρ_{exp} , for the helicopter to follow is expressed as a quadratic function:

$$\rho_{exp} = \frac{\rho_0}{t_f^2}(t - t_f)^2 \quad (4.4)$$

The error is $e(t) = \rho - \rho_{exp}$ and is used with Equations 4.1 and 4.2 to monitor the error, $e(t)$, and correct the helicopter orientation by selecting an appropriate longitudinal RSD velocity, $\dot{x}$.

When the difference between ρ_{exp} and ρ , or $e(t)$, becomes larger than $\Delta\rho_{max}$, $e(\rho)$ becomes negative, resulting in a negative $\dot{x}$, as shown in Equation 4.1. A negative RSD velocity reduces the helicopter orientation and corrects for the error more quickly until $e(t)$ is less than $\Delta\rho_{max}$.

4.4.2 Refining the Traversing Procedure

During traversing, it would be desirable to reduce the magnitude of the claw's lateral oscillation to maintain the helicopter's orientation close to zero, making the traversing operation smoother and potentially reducing the lateral tire deflections that occur. Several attempts were considered to reduce the magnitude of oscillations, including: incorporating a tail probe; allowing variable claw velocity, $\dot{y}$; monitoring the caster wheel orientation, γ ; and monitoring the helicopter's angular velocity, $\dot{\rho}$.

Incorporating a tail probe has been considered since many helicopters use a tail probe that is dropped into the track to facilitate traversing. However, for the purposes of this thesis it was decided to pursue the more general and challenging case by assuming that an external tail guide was not available.

Figure 4.8: Comparison of the traversing process using on-off claw control and variable claw control

The capability of variable claw velocity instead of on-off control was investigated to determine if controlling the magnitude of the velocity could lead to better overall control during the traversing stage. Using a PID controller, similar to Equation 4.1. This capability was tested using a range of gains, however, as shown in Figure 4.8, there is very little difference between the on-off and variable control of the claw with respect to reducing the magnitude of oscillation during traversing.

The next possibility was to monitor the caster wheel orientation, since it may indicate the direction that the helicopter will rotate. Yet, due to the unwillingness to add orientation sensors to every helicopter at the caster wheel, this was not attempted. However, it would be possible to monitor the helicopter's angular velocity using the HPSE, which results in the final option for refining the traversing operation.

Originally, when the helicopter's orientation was nonzero, the claw moved laterally to continuously correct the orientation. However, it was found that less claw motion was achievable by monitoring the helicopter's angular velocity to predict when the helicopter tended to rotate in the proper direction without the aid of the claw, thereby reducing the total amount of claw motion required. When this condition prevails, the claw may be moved back towards the centreline, reducing the magnitude of lateral motion away from the centreline.

Figure 4.9: Example of helicopter straightening with the refined sequential method

Figure 4.9 shows an example of the straightening procedure using the refined sequential method with the helicopter oriented towards the centreline and the same initial conditions as in the previous example to show the effect of the controller on forcing the position and orientation to reach the centreline at the same time.

Figure 4.9(a) shows the landing position for this example, and the initial direction of actuation on the probe. Figure 4.9(b) shows that if the $e(t)$ becomes too large, $e(\rho)$ becomes negative, and hence $\dot{x}$ becomes negative to reduce the orientation more quickly. In Figure 4.9(c), the helicopter orientation, and the error have been reduced sufficiently so that the helicopter moves forward again, and Figure 4.9(d) shows that the helicopter orientation and claw position reach zero at the same time and traversing can begin. Therefore, Step 2 of the sequential method, which is required in the case that additional straightening is needed once the claw reaches the centreline, is no longer required.

Figure 4.10 shows the lateral position and orientation of the helicopter versus the longitudinal position with the refined sequential method. As shown, the helicopter position and orientation reach zero at the same time. It is also shown that the claw oscillation has been reduced significantly to within ± 0.14 in from the centreline. Figure 4.11 shows the resulting maximum tire

Figure 4.10: Position and orientation of the helicopter during autonomous manoeuvring with the refined sequential method

Figure 4.11: Tire deflections during autonomous straightening and traversing with the refined sequential method

deflections versus time of the refined sequential method example described. A very significant reduction in tire deflection during traversing to within ± 0.15 in is evident (refer to Figures 4.5 and 4.6).

The sequential method was tested using the planar manoeuvring simulation and controller gains were chosen such that the straightening and traversing was successful for a range of initial conditions within $\rho_0 = \pm 30^\circ$ and $y_0 = \pm 3$ ft.

4.5 Experimental Testing

To fulfill the final project objective of Phase IV, the refined sequential method straightening and traversing procedures that were developed using *HeliMan*, were attempted on the motion platform using the scaled DLTV, with and without ship motion. During straightening, despite having different helicopter model parameters than those modelled in *HeliMan*, the algorithm was capable of monitoring the helicopter's position and orientation and was successful in straightening the helicopter using the same controller gains. Due to the differences in parameters used for tuning the controller, further improvement was achieved by refining the gains used for the experimental facility. However, the straightening procedure didn't appear to be overly sensitive to gain selection, since a variety of gains were attempted, all successful in straightening the helicopter. The gains merely affect the resulting trajectory, which is determined by the error, and number of manoeuvres required. Optimal gains can be selected to minimize the error and the manoeuvres required.

Traversing, however, is very sensitive to gain selection since forward traversing causes directional instability of the helicopter. Unlike the straightening procedure, the error is not corrected by simply reversing the helicopter since this would cause the helicopter to oscillate fore and aft. Instead, the refined traversing procedure attempts to use the claw to correct the helicopter orientation, as well as reduce the longitudinal velocity as the error increases. However, if the gains are not chosen properly, the claw's position and/or the helicopter's orientation will drift slowly over time. Most of the time, this drift tends to reach upper limits of $y = 0.5$ in and $\rho = 5$ degrees by the time the helicopter reaches the end of the deck (the hangar) on the motion platform, which is not severe. Using some gains, this drift can be larger. Therefore, the traversing gains need to be selected carefully to achieve autonomous traversing operation.

Figures 4.12 to 4.14 show the resulting trajectory of the autonomous manoeuvring algorithm tested using: the motion platform without ship motion; the motion platform with ship motion; and *HeliMan*, respectively. The motion platform results have been scaled up to be compared with the *HeliMan* results. This requires scaling of position, as well as time. For these tests,

Figure 4.12: Autonomous manoeuvring procedure on the motion platform without motion

Figure 4.13: Autonomous manoeuvring procedure on the motion platform with ship motion

controller gains were selected on the motion platform to generate improved experimental results and were used for all tests. Therefore, it was expected that the resulting trajectory would differ slightly between the experimental results and *HeliMan*.

Figure 4.12 shows the resulting trajectory on the motion platform without motion. As shown, The initial RSD and claw position and DLTV orientation are $x_p=0$ in, $y_p=-24.5$ in, and $\rho=19.9$ deg, respectively. As shown, using the selected gains, the DLTV is straightening in just under 17 seconds. During traversing, the lateral motion of the claw oscillates to keep the helicopter orientation small, yet remains stable.

Figure 4.14: Autonomous manoeuvring procedure using *HeliMan*

Figure 4.13 shows the same autonomous manoeuvring procedure in the presence of ship motion. This was attempted for a sea state 5, 30 deg heading, and 10 knot ship speed case, since manoeuvring is performed when the motion is quiescent (less severe). As shown, there is little difference between the autonomous manoeuvring with and without motion. The only noticeable differences are the oscillations in the orientation angle due to the roll motion.

Manoeuvring tests were conducted under extreme manoeuvring conditions where it was determined that under these conditions manoeuvring was not accomplished successfully. However, as noted previously, helicopter manoeuvring is not performed under extreme motion conditions. Otherwise, there seems to be little difference due to ship motion.

Figure 4.14 shows the full-scale helicopter trajectory using *HeliMan*. Instead of using the ideal gains for the helicopter modelled in the simulation, the same gains were used as those determined experimentally on the motion platform to compare the results. As shown in the figure, the straightening procedure required more manoeuvres and more track, yet still accomplished the straightening procedure at the same time as was achieved experimentally. This shows that the gain selection can be improved, yet the straightening algorithm is robust to allow adjustments to be made when the error becomes too large.

Manoeuvring tests were conducted experimentally for a range of initial conditions and gains. The refined straightening procedure was successful during these tests, the only differences being

the number of manoeuvres (adjustments) required before the claw reaches the centreline. In the worst case, the helicopter orientation does not reach zero by the time the claw reaches the centreline, in which case an additional step is available to finish straightening the helicopter before traversing begins. During the traversing procedure, however, the error that occurs tends to cause the claw to move away from the centreline and/or causes the helicopter orientation to increase over time. However, when the gains were not appropriate, this same behaviour was experienced in *Heliman*. Therefore, this confirms that there are gains that correct this problem both on the motion platform and in *Heliman*.

Chapter 5

Conclusions and Recommendations

5.1 Conclusions

This project achieved the objectives of Phases II, III, and IV of the collaborative project dealing with shipboard helicopter manoeuvring. The specific achievements were:

1. the development of a flexible scaled motion test facility, including a scaled DLTV and ASIST model;
2. the testing of the effects of ship motion on helicopter manoeuvring; and
3. the investigation into the possibility of autonomous on-deck helicopter manoeuvring using *Heliman* and the experimental facility.

The result of this project is the CESHIP facility, capable of dynamic interface analysis in the presence of ship motion and on-deck helicopter manoeuvring, a new capability that has previously not been achieved. Additionally, the potential for autonomous helicopter straightening has been demonstrated - something that can significantly impact future development of the ASIST system.

Some specific conclusions can be made from this thesis.

- The motion platform is capable of achieving angular ship motions exceeding sea state 5 conditions using a single linear actuator and the capability of reorienting (yawing) the deck. By inverting the centre post, larger translational motions are possible.
- In the tail wheel casterable configuration, the scaled DLTV proved to be capable of manoeuvring much like the full-scale DLTV despite some inconsistencies in physical parameter scaling. The future modifications of *HeliMan* to incorporate the scaled DLTV parameters are expected to facilitate comparisons between simulation and experiment.
- Experimental tests revealed that the effects of the RSD and claw velocities are additive to the securing forces experienced on the DLTV in the presence of ship motion. Hence, simulations that only analyze the securing forces due to ship motion are neglecting a significant force component. However, severe ship motion and manoeuvring do not occur simultaneously. Therefore, the maximum securing forces calculated during severe ship motion may not be affected.
- Autonomous manoeuvring is possible using the sequential method. This method was shown to be robust for a range of gains for both *HeliMan* and the experimental motion platform. Even if the controller gains selected are not ideal, the autonomous straightening algorithm developed is robust to allow adjustments to be made when the error becomes too large.
- Controller performance for the autonomous manoeuvring algorithm was shown to be virtually unaffected by the presence of representative quiescent ship motion. The effect of ship motion on the autonomous manoeuvring during representative quiescent periods as with manual manoeuvring were shown to cause little difference in the controller performance.
- A casterable tail wheel aircraft configuration was used for the development of the manoeuvring algorithm, and was shown to be directionally unstable during forward manoeuvring. A casterable nose wheel aircraft, however, tends to be stable in the forward direction and unstable in the reverse direction. Therefore, it is expected that with minor adjustments to

the sequential method, a casterable nose wheel aircraft could be autonomously manoeuvred.

5.2 Recommendations

Several recommendations, which follow, have been made to continue this project for the purpose of improvement, as well as expanding it to encompass other areas of research.

- A second angular degree of freedom should be implemented to uncouple the roll and pitch motions to produce more realistic ship motion. The motion platform facility was designed with this expansion in mind, as follows:
 - the support post can be easily replaced with a linear actuator identical to the roll actuator;
 - the motion control hardware is capable of controlling a fourth axis; and
 - the *Ship Motion* LabView program will only require small modifications to indicate a second axis of ship actuation.
- The full capabilities of the motion platform facility should be investigated. Such capabilities include:
 - inverting the centre post;
 - interchanging the actuator with the centre post to achieve greater heave motions; and
 - replacing the ASIST system with another actuation system.

While the platform was designed for each of these capabilities, the research described focussed on the basic configuration.

- Some issues related to inputting scaled values into *HeliMan* should be investigated. These issues include:

- inputting the scaled tire properties directly;
 - adjusting the joystick conversion factors to allow velocity inputs from the motion platform without significant rounding error; and
 - adjusting the graphics to be easily scaled when the scaled DLTV properties are used.
- The DLTV should be reconfigured to test the adjustable design capabilities. The dimensions and inertial properties can be easily adjusted, however, to test the DLTV as a steerable nose wheel helicopter such as the Sikorsky H-92 Cyclone (Canada’s selected replacement maritime helicopter), a slight modification to the caster wheel is required to lock it in place at known orientations.
 - The tuning of the controller gains for autonomous operations should be investigated thoroughly. Different controller approaches, such as adaptive control or fuzzy logic may offer potential for improved results.

As the CESHIP facility has been developed to be flexible and fill a void in existing dynamic interface analysis capability, the direction of future work can easily be tailored to suit evolving dynamic interface analysis requirements.

References

- [1] D.R. Linn. Development and Validation of a Planar On-Deck Helicopter Manoeuvring Simulation. Master's thesis, Carleton University, Ottawa, ON, Canada, December 2003.
- [2] Advanced Rotorcraft Technology Inc. Website. www.flightlab.com, August 2004.
- [3] Naval Air Systems Command Website. www.navair.com, August 2004.
- [4] August Design Inc. Website. www.august.com, August 2004.
- [5] Military Agency for Standardization. Standardized Wave and Wind Environments and Shipboard Reporting of Sea Conditions. Standardization Agreement 4194, North Atlantic Treaty Organization, April 1983.
- [6] Inc. Advanced Rotorcraft Technology. *FLIGHTLAB Theory Manual*, April 2001.
- [7] Hao Kang, Chengjian He, and Hossein Saberi. Dynamic Interface Simulation of Rotorcraft Shipboard On-Deck Operation. *Proceedings of the Institution of Mechanical Engineers, Part G: Journal of Aerospace Engineering*, 211(2):91–102, 1997.
- [8] System Documentation. A Brief Description of NBDL's Ship Motion Simulator. Technical Report SM1000B, National Biodynamics Laboratory, New Orleans, LA, US 70189-0407, February 2001.
- [9] System Documentation. A Brief Description of NBDL's Six Degree of Freedom Motion Platform. Technical Report SD1000A, National Biodynamics Laboratory, New Orleans, LA, US 70189-0407, July 1999.

- [10] P. Crossland and K. J. N. C. Rich. Using the Large Motion Simulator (LMS) at DRA Bedford to Investigate the Effects of Ship Motions on Motion Induced Interruptions. Technical Report DRA/SS/SSHE/CR96008, Defence Research Agency, Farnborough, Hampshire, UK GU14 6TD, February 1996.
- [11] Atlantis Systems International Website. www.atlantissi.com, August 2004.
- [12] Naval Biodynamics Laboratory Website. www.nbdl.org, August 2004.
- [13] SimLabs Ames Research Center Website. www.simlabs.arc.nasa.gov, August 2004.
- [14] M. F. Roscoe and C. H. Wilkinson. DIMSS - JSHIP's Modeling and Simulation Process for Ship/Helicopter Testing and Training. *AIAA*.
- [15] R. G. Langlois, M. LaRosa, and A. R. Tadros. Development, Validation, and Application of the Dynaface Helicopter/Ship Dynamic Interface Simulation Software Package. In *SCSC 2003 Summer Computer Simulation Conference*, Montreal, QC Canada, July 2003. The Society for Modeling and Simulation International.
- [16] C. Pedrazzi and G. Barbieri. ARCHIMEDES: A Riva Calzoni Helicopter-Ship Dynamic Interface Modelling Software. In *WARSHIP '97: International Symposium on Air Power at Sea*, London, U.K., June 1997. The Royal Institution of Naval Architects.
- [17] S. Brennan and A. Alleyne. The Illinois Roadway Simulator: A Mechatronic Testbed for Vehicle Dynamics and Control. *IEEE/ASME Transactions on Mechatronics*, 5(4):349–359, December 2000.
- [18] D. H. Klyde, J. G. Reinsberg, and E. Sanders. Development and Evaluation of a Stability Augmentation Steering System for Aircraft Ground Handling. Technical report, Systems Technology, Inc., May 2002.
- [19] V. Gavrillets et al. Aggressive Manoeuvring of Small Autonomous Helicopters: A Human-Centered Approach. *The International Journal of Robotics Research*, 20(10):795–807, October 2001.

- [20] S. Berman and Y. Edan. Simulation in the Development of a Control Strategy for an Automated Guided Vehicle System. *SAMS*, 42(4):575–583, 2002.
- [21] J. K. Ong and K. Bouazza-Marouf. Design of a Semi-Autonomous Modular Robotic Vehicle for Gas Pipeline Inspection. *Part I: Systems and Control Engineering*, 217:109–122, October 2003.
- [22] Y. Cao and T. Lee. Manoeuvring of Surface Vessels Using Fuzzy Logic Controller. *Journal of Ship Research*, 47(2):101–130, June 2003.
- [23] A. R. Tadros, R. G. Langlois, and M. LaRosa. Investigation of Ship Flight Deck Motion Limits for Safe Embarked Helicopter Operation. In *IPLC*, Arlington, Virginia, U.S.A., October 2000. American Helicopter Society, Inc.
- [24] R. G. Langlois and A. R. Tadros. State-of-the-Art On-Deck Dynamic Interface Analysis. In *American Helicopter Society 55th Annual Forum*, Montreal, Quebec, Canada, May 1999.
- [25] A. R. Tadros, G. Sopher, and R. G. Langlois. A new approach for passive securing at landing and powered handling of Helicopters Onboard Small Ships. In *WARSHIP '98: International Symposium on Surface Warships: The next Generation*, London, U.K., June 1998. The Royal Institution of Naval Architects.
- [26] R.G. Langlois, D. Moguin, and B. Rocconella. Consideration of the effect of securing and traversing loads on the fatigue life of the Kaman Seasprite RAST/ASIST PROBE Installation. In *American Helicopter Society 55th Annual Forum*, Montreal, Quebec, Canada, May 1999.
- [27] R. G. Langlois and C. Tessier. Dynamic Interface Analysis of the Embarked Operation of the CL-327 'Guardian' Unmanned Air Vehicle. In *American Helicopter Society 56th Annual Forum*, Virginia Beach, Virginia, U.S.A., May 2000.

- [28] R. G. Langlois, A. R. Tadros, and M. LaRosa. Investigation of the Relationship Between Helicopter Geometrical and Inertial Parameters and Shipboard Securing Requirements. In *American Helicopter Society 58th Annual Forum*, Montreal, Quebec, Canada, June 2002.
- [29] F. Wei, E. Baitis, and W. Meyers. Analytical Modeling of SH-2F Helicopter Shipboard Operation. *Journal of Aircraft*, 29(5):877–885, September/October 1992.
- [30] J. Blackwell et al. Modelling the helicopter/ship dynamic interface for the Seahawk/FFG-7. In *Proceedings of the 5th Australian Aeronautical Conference. Part 2 (of 2)*, volume 2, pages 277–284, Melbourne, Australia, September 1993. The Institution of Engineers, Australia; The Royal Aeronautical Society, Australian Division, IE Aust.
- [31] J. L. Colwell. Maritime Helicopter Ship Motion Criteria - Challenges for Operational Guidance. In *RTO SCI Symposium on, Challenges in Dynamics, System identification, Control and Handling Qualities for Land, Air, Sea and Space Vehicles*, Berlin, Germany, May 2002. Research and Technology Organisation, NATO.
- [32] H. Kang, C. He, and H. Saberi. Dynamic interface simulation of rotorcraft shipboard on-deck operation. In *American Helicopter Society 59th Annual Forum*, Phoenix, Arizona, U.S.A., May 2003.
- [33] E. R. Joelson. Shipborne Helicopter - Landing, Security, Manoeuvring, and Traversing: Options and Implications. In *WARSHIP '97: International Symposium on Air Power at Sea*, London, U.K., June 1997. The Royal Institution of Naval Architects.
- [34] Sikorsky Website. <http://www.sikorsky.com>, 08 2004.
- [35] J. C. McKee. Simulating the Effects of Ship Motion on Postural Stability Using Articulated Dynamic Models. Master's thesis, Carleton University, Ottawa, ON, Canada, March 2004.
- [36] K. A. McTaggart and S. Ando. A Review of Eight Seakeeping and Hydrodynamic Load Prediction Codes. Technical Report 92/315, Defence Research Establishment Atlantic (DREA), Nova Scotia, Canada, September 1992.

- [37] W. G. Meyers, T. R. Applebee, and A. E. Baitis. User's Manual for the Standard Ship Motion Program, SMP. Technical Report Report No. DTNSRDC/SPD-0936-01, David W. Taylor Naval Ship Research and Development Centre, Bethesda, Maryland, USA, September 1981.
- [38] K. A. McTaggart. SHIPMO 7: An Updated Strip Theory Program for Predicting Ship Motions and Sea Loads in Waves. Technical Report Technical Memorandum 96/243, Defence Research Establishment Atlantic (DREA), Nova Scotia, Canada, March 1996.
- [39] L. Vernooy and R. G. Langlois. *User's Guide for the SHIPSIM 4.2 Ship Motion Simulation Program*. Indal Technologies Inc., Mississauga, ON, Canada, August 1999.
- [40] J. P. Hooft. Evaluation of the Improved FREDYN Ship Behaviour Simulation Program. Technical Report Report No. 48883-1-OE, MARIN, Nova Scotia, Canada, June 1990.
- [41] A.R. Feldman. Design and Development of an Experimental Motion Platform. Technical Report DL-04-ARF-01, Carleton University, 2004.
- [42] W. Baker et al. *Similarity Methods in Engineering Dynamics*. Elsevier, Amsterdam, 1991.
- [43] D. Linn. Investigation of motion platform systems and model scaling laws for the validation of helicopter/ship deck simulations. Technical Report DL/02/DRL/1, Mechanical and Aerospace Engineering, Carleton University, 2002.
- [44] T. Szirtes. *Applied Dimensional Analysis and Modeling*. McGraw-Hill, 1998.
- [45] A. R. J. M. Lloyd. *SEAKEEPING: Ship Behaviour in Rough Weather*. Published by A. R. J. M. Lloyd, 26 Spithead Avenue, Gosport, Hampshire, United Kingdom, revised edition edition, 1998.
- [46] MSC ADAMS Technical Staff. *Road Map to ADAMS/View Documentation*. Mechanical Dynamics, Inc., 2002.
- [47] E. W. Kamen and B. S. Heck. *Fundamentals of Signals and Systems Using the Web and Matlab*. Prentice Hall, Upper Saddle River, New Jersey 07458, second edition, 2000.

- [48] A. Samant. *LabView Signal Processing*. Prentice Hall, Upper Saddle River, New Jersey 07458, second edition, June 1998.
- [49] Tolomatic, Inc., www.tolomatic.com. *Electric Linear Motion Products from Tolomatic*, 3600-4609-02.00 edition, 2003.
- [50] Techno Inc. Website. www.techno-isel.com, February 2003.
- [51] Bourns Website. www.bourns.com, June 2003.
- [52] ATI Industrial Automation. *Multi-Axis Force/Torque Sensor System Installation and Operation Manual*. ATI Industrial Automation, Apex, NC 27539, January 2003.
- [53] PCB Piezotronics Website. www.pcb.com, September 2003.
- [54] Microstrain, Inc., Williston, VT 05495 USA. *3DM-G Gyro Enhanced Orientation Sensor User Manual*, first edition, 2003.
- [55] K. Ogata. *Modern Control Engineering*. Prentice Hall Inc., Upper Saddle River, New Jersey 07458, third edition, 1997.
- [56] J. G. Ziegler and N. B. Nichols. Optimum Settings for Automatic Controllers. *ASME*, 64:759–68, 1942.
- [57] R. Henderson. Course Preparation and Helicopter Model Parameter Testing. Technical Report DL-04-RH-01, Carleton University, 2004.
- [58] A. R. Feldman and R. G. Langlois. Autonomous straightening and traversing of shipboard helicopters. In *CSME*, London, ON CANADA, June 2004.
- [59] Dutton, Thompson, and Barraclough. *The Art of Control Engineering*. Addison-Wesley, Boston, MA 02116, 1997.