FREEFLYING ROBOTS - INERTIAL PARAMETERS IDENTIFICATION AND CONTROL STRATEGIES

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Abstract

A method is proposed for the identification of the inertial parameters of the base body of a freeflying robot directly in space. This can serve to improve the path tracking capabilities of the robotic system as well as to measure the parameters of a load on the end-effector. Furthermore, the ETS-VII Dynamic Motion experiments have shown that a non controlled spacecraft is subject to non negligible external disturbances. A control scheme is proposed to account for these disturbances while allowing the spacecraft to move in reaction to the robot motions.

1 Introduction

This paper discusses two issues in freeflying space robotics. The first is relative to the identification of what are commonly called inertial parameters, namely mass, centre of mass position and inertia tensor, of a robot in space. The particular problem of finding the assumed unknown parameters of the base body of the robot is addressed, while those of the remaining bodies constituting the mechanical system are given.

This task stems from the results found during the GETEX Dynamic Motion experiments carried out by DLR in collaboration with NASDA on the ETS-VII satellite [1] (see figure 1). The experiments consisted in the execution of manoeuvres of the six-degree-of-freedom manipulator mounted on the satellite in freefloating mode, for which the attitude control of the satellite was switched off. In this condition, a dynamical interaction between the satellite and the robot motions takes place [1] . After a detailed evaluation of the experimental data and a model update to account for so far unmodelled external actions, such as the gravity gradient torque, a discrepancy between the simulated and measured data was still found. This was attributed partly to an incorrect value of the inertial parameters of the satellite used in the data evaluation (mainly the inertia tensor). The fuel consumption during the spacecraft mission led to a variation of the inertial parameters which resulted in the undesired discrepancy.

A discrepancy in the inertial parameters clearly acts against the path tracking capabilities of the robotic system. This problem could be dealt with by an adaptive controller (see [2], pp. 422), although the solution of such a problem is not an issue of the present paper. Alternatively, a method for the determination of the inertial parameters is proposed which could be applied directly in space. The outcome of such a method could also serve to determine the inertial parameters of a load attached on

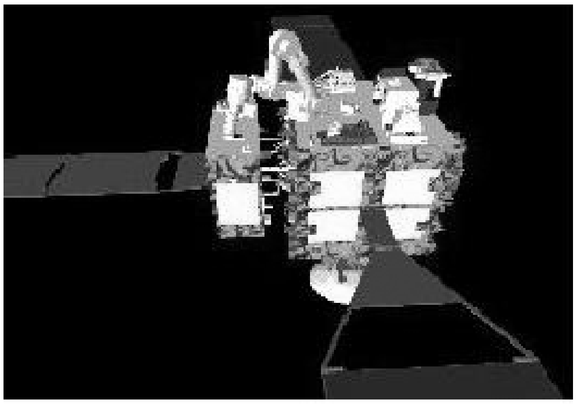

Figure 1: The ETS-VII satellite: docking manoeuvre between chaser (right) and target (left) (courtesy of NASDA)

the end-effector of the space robot when the parameters of the robot itself were known.

Inertial parameter identification has been discussed by Murotsu *et al.* [3] and by Katoh *et al.* [4], who apply identification methods to the equations of a two body system, for which only the parameters of one are known. However, both papers make the assumption that inertial measurements necessary for the validity of the equations of motion of the system are readily available and that these are between an inertial frame and the centre of mass of the bodies of the system. In this paper we focus our attention on the fact that by a suitable reformulation of the problem, the measure of inertial quantities (in particular those relative to the translational motion) does not need to be performed relative to an inertial frame on Earth but can be relative to a local orbital frame. This way the technological limitation for which inertial measurements (even in Low Earth Orbit (LEO)) are not possible to a sufficient accuracy, can be overcome. Furthermore, the problem is solved for a realistic situation in which the measurements are taken with respect to a sensor on the spacecraft which does not lie in its centre of mass but at an unknown distance from it (which is a function of the unknown centre of mass position). A two dimensional simulation is used to show the validity of the method. The technology which is required for the identification in terms of on-board sensors is analysed and described in terms of that currently available.

The second part of this paper focuses on control strategies for freeflying robots. Future applications for such robots, such as in-orbit construction, maintenance of large space structures, in-orbit satellite servicing (inspection, logistic support, repair), for which the load on the end-effector is not large with respect to the space robot, do not need to be controlled in base translation nor rotation to account for the dynamic coupling with the robot motions. While the translation control would require the use of thrusters, the rotation control could be achieved with reaction wheels. However, both of these are not always necessary, since the motion resulting from the coupling with the robot motion is for certain applications small and as such does not affect the performance of the robotic system greatly. Only when pointing accuracy for the communications antenna is required as for example, when using a Geostationary Earth Orbit relay satellite for Earth communications, is there a requirement for limited base rotation (although a mechanical pointing mechanism could be considered as an alternative). This is not a general requirement for LEO applications since communication can be achieved with control stations on Earth or on a space station with omnidirectional antennas. The control of the base motion due to robot motion coupling can therefore be eliminated, reducing the attitude control power requirements.

Furthermore, the ETS-VII experiments have shown that a non controlled base gives rise to an undesired drift arising from external in-orbit disturbances, such as the gravity gradient torque, which cannot be neglected [1]. A controller is therefore proposed to allow for the base of the freeflying robot to move in reaction to the robot motion but not in reaction to the external in-orbit actions. This proposed controller is described with simulation results of a two-dimensional system.

1.1 Notation

Scalar quantities are expressed with letters in italic, for example v. Vectors are expressed with underlined letters in italic, for example $\underline{v}$. Derivatives with respect to time are expressed with a dot, as for example $\dot{\phi}$.

Matrices are expressed with letters in bold. For example, for a matrix of dimensions ($n \times m$), the expression is $\mathbf{A} = [A_{ij}]$ where i=1,2,..,n and j=1,2,..,m. $\mathbf{E}$ is the identity matrix.

Superscripts refer to a specific body of the multibody system. For example, '1' is relative to body 1. When the superscript 'i' is used, this refers to the i^{th} element of a set.

A Cartesian reference frame is defined by an origin O with a superscript which refers to a particular frame, for example the inertial frame O^I.

2 Parameter Identification

2.1 Equations of motion of two-body system in freeflying conditions

Consider the two-body system depicted in figure 2, connected by a single revolute joint. The two bodies in the figure schematically represent a base body (body 1) and a robot (body 2), which is treated as a single body for the purpose of the parameter identification . The position of the revolute joint is described by the variable θ

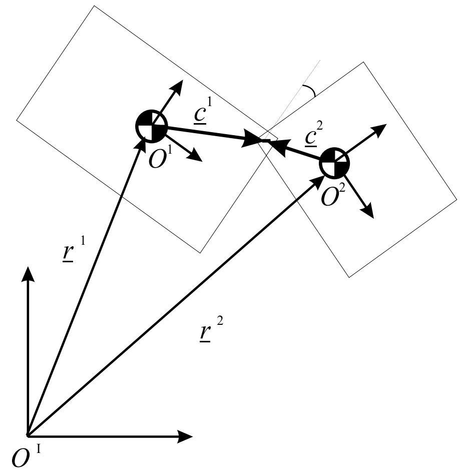

Figure 2: Reference frames for a two-body system

which is measured relative to an arbitrary initial reference value. The inertial frame of reference O^I is taken to be in the Earth centre. This choice of the inertial reference is dictated by the fact that the equations of motion of the mechanical system are only valid in an inertial frame. Therefore, the quantities $\underline{r}^1$ and $\underline{r}^2$ refer to the orbital position vectors of the two bodies and are clearly function of time.

The frame of reference O^i is placed in the centre of mass of the i^{th} body. The inertial parameters of body 1 are unknown while those of body 2 are assumed known. In the figure we can imagine $\underline{r}^1$ to be ideally measured by a sensor in some position $\underline{k}$ relative to the centre of mass of body 1 (see figure 4), such that the unknown inertial parameter which relates to the centre of mass position of body 1 becomes exactly $\underline{k}$. Once $\underline{k}$ is known then $\underline{c}^1$ follows from the known geometry of the spacecraft.

The equations of motion of the two-dimensional system are written using the Newtonian-Eulerian formulation as follows, where it is assumed that all bodies are rigid:

Kinematics:

$$\underline{\dot{r}}^1 = \underline{v}^1 \quad (1)$$

$$\underline{\dot{r}}^2 = \underline{v}^2 \quad (2)$$

$$\dot{\phi}^1 = \omega^1 \quad (3)$$

$$\dot{\phi}^2 = \omega^2 \quad (4)$$

Dynamics:

$$m^1\, \underline{\dot{v}}^1 = \underline{f}_e^1 + \underline{f}_c^1, \quad (5)$$

$$m^2\, \underline{\dot{v}}^2 = \underline{f}_e^2 + \underline{f}_c^2, \quad (6)$$

$$I^1\, \omega^1 = t_a^1 + t_c^1 \quad (7)$$

$$I^2\, \omega^2 = t_a^2 + t_c^2 \quad (8)$$

Constraints:

$$\underline{r}^2 = \underline{r}^1 + \underline{c}^1 - \underline{c}^2, \quad (9)$$

$$\phi^2 = \phi^1 + \theta \quad (10)$$

where ϕ^1, ϕ^2 and ω^1, ω^2 are the angular potions and velocities of frames O^1 and O^2 relative to frame O^I. Furthermore, m^i is the mass, I^i the inertia, $\underline{f}_e^{\,i}$ and $t_e^{\,i}$ are the sums of the external forces and torques and $\underline{f}_c^{\,i}$ and $t_c^{\,i}$ are the sums of the constraint forces and torques (arising from the revolute joint) on the i^{th} body . Equations (9) and (10) express the constraint on the motion of the two bodies to account for the revolute joint (for further details see [5]). Vectors $\underline{c}^1$ and $\underline{c}^2$ are defined in figure 2.

After choosing a suitable frame of reference in which to express the vector quantities, the equations can be written in matrix form. Choosing the inertial frame O^I the kinematic and dynamic equations become:

$$\dot{\mathbf{x}}_I = \mathbf{x}_{II} \tag{11}$$

$$\mathbf{J}\,\dot{\mathbf{x}}_{II} = \mathbf{\Lambda} = \mathbf{\Lambda}_e + \mathbf{\Lambda}_c \tag{12}$$

where

$$\mathbf{x}_I = \begin{bmatrix} \mathbf{r} \\ \boldsymbol{\phi} \end{bmatrix} \quad \mathbf{x}_{II} = \begin{bmatrix} \mathbf{v} \\ \boldsymbol{\omega} \end{bmatrix} \tag{13}$$

$$\mathbf{J} = \begin{bmatrix} \mathbf{E}_1\,\mathbf{m} & \mathbf{0} \\ \mathbf{0} & \mathbf{E}_2\,\mathbf{I} \end{bmatrix} \quad \mathbf{\Lambda} = \begin{bmatrix} \mathbf{f} \\ \mathbf{t} \end{bmatrix} \tag{14}$$

and

$$\mathbf{x}_I = [\,r^{11},\, r^{12},\, r^{21},\, r^{22},\, \phi^1,\, \phi^2]^T \tag{15}$$

Furthermore, $\mathbf{m} = [m^1\; m^1\; m^2\; m^2]^T$, $\mathbf{I} = [I^1\; I^2]^T$, $\mathbf{f} = [\mathbf{f}^1\, \mathbf{f}^2]^T$ and $\mathbf{t} = [t^1\, t^2]^T$. Also $\mathbf{E}_1$ is a 4x4 identity matrix whereas $\mathbf{E}_2$ is a 2x2 identity matrix.

The equations of motion are then transformed to a state space form for their resolution (for further details see [5]). The dependent position variables of the system given by equation (15) are replaced by the independent position state space variables which can be chosen to be

$$\mathbf{y}_I = [\,r^{11},\, r^{12},\, \phi^1,\, \theta\,]^T \tag{16}$$

and therefore, for the velocity state space variables,

$$\mathbf{y}_{II} = [\,v^{11},\, v^{12},\, \dot{\phi}^1,\, \dot{\theta}\,]^T. \tag{17}$$

The state space form of the equations of motion results to be

$$\dot{\mathbf{y}} = \begin{bmatrix} \dot{\mathbf{y}}_I \\ \dot{\mathbf{y}}_{II} \end{bmatrix} = \mathbf{Y}\,(t,\, \mathbf{y};\, \mathbf{m},\, \mathbf{I},\, \underline{k}) \tag{18}$$

having stressed the dependence of function $\mathbf{Y}$ on the inertial parameters $\mathbf{m}$, $\mathbf{I}$, and $\underline{k}$.

2.2 Motion decomposition

A parameter identification problem can be posed with equation (18), for which measurements of both the translational and the rotational motion of the base body are necessary. While for the latter measurements relative to an inertial frame are not a problem (as for example, with star sensors), this is not so for the former, if the measurement is relative to a point on Earth. However, the translational motion of the base body of the two-body system can be decomposed in two components, one expressing the orbital motion and one expressing the reaction to the robot motion superimposed on the first. A similar argument can be done for the second body.

Figure 3 schematically shows the motion of the centre of mass of the two bodies during a cyclic robot motion superimposed on what would be their motion if the robot did not move (dotted line). This motion decomposition

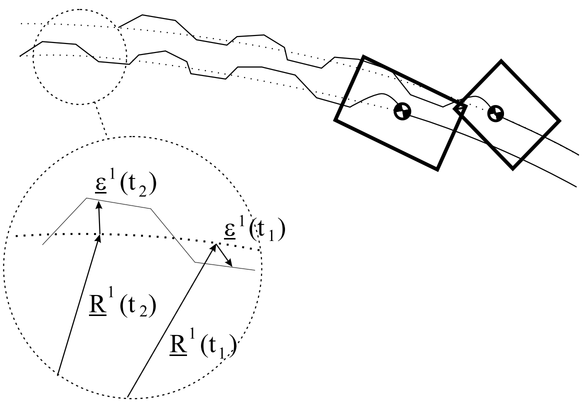

Figure 3: Motion decomposition

can be expressed mathematically as follows (c.f. equations (5) and (6)):

$$m^i\; \ddot{\underline{r}}^i = m^i\; (\ddot{\underline{R}}^i + \ddot{\underline{\epsilon}}^i) = \underline{f}_e^i + \underline{f}_c^i \tag{19}$$

Vectors $\underline{R}^i$ and $\underline{\epsilon}^i$ represent the orbital component and the additional component induced by the robot motion respectively. An assumption is now introduced which allows to decouple the two motion components: let the orbital component be independent of the robot motion. This assumption is justified by the physical condition for which

$$\underline{R}^i\,(t) \gg \underline{\epsilon}^i\,(t) \tag{20}$$

for any t, which results in the fact that

$$\underline{f}_e\,(\underline{r}^i) \simeq \underline{f}_e\,(\underline{R}^i), \tag{21}$$

to say that the gravitational force of attraction will not vary significantly for any robot motion.

Since the orbital motion is independent of the robot motion, then the following equation holds:

$$m^i\; \ddot{\underline{R}}^i = \underline{f}_e^i. \tag{22}$$

As a consequence, from equation (19), it follows that

$$m^i\; \ddot{\underline{\epsilon}}^i = \underline{f}_c^i \tag{23}$$

The result of this decomposition is that the terms relative to the orbital motion can be omitted from the mathematical formulation of the equations of motion of the mechanical system. The new state space variables can be taken to be

$$\mathbf{y}_I = [\,\epsilon^{11},\, \epsilon^{12},\, \phi^1,\, \theta]$$

$$\mathbf{y}_{II} = [\,\dot{\epsilon}^{11},\, \dot{\epsilon}^{12},\, \dot{\phi}^1,\, \dot{\theta}], \tag{24}$$

having expressed the vector quantities in the orbital frame O^o (see figure 4).

The above argument is posed to show how the external force $\underline{f}_e^i$ and the orbital position vector component $\underline{\ddot{R}}^i$ can be eliminated from the equations of motion (5) and (6). The reasoning to explain the independence of the orbital motion from the multibody motion can also be based on the fact that the forces which act on the two bodies due to the robot motion are *internal* forces and as such they do not affect the motion of the centre of mass of the overall system.

2.3 Formulation of the parameter identification problem

This problem has been tackled previously as for example in [3] and in [4]. A slightly different method is proposed here for the identification of the mass, inertia and centre of mass position of the base body. A desired manoeuvre of the robot is performed by applying a constant torque at the revolute joint of the system. The parameter identification is then based on the comparison between measured variables of the base motion and those obtained by integration of the equations of motion of the system. Integration of equations (18) yields the y_{II} vector as a function of time. The function $\dot{\theta}$ used in the simulation is taken to be measured during the manoeuvre and is given as an input to the simulated system in terms of a time excitation function on the joint. For this purpose, also θ and $\ddot{\theta}$ need to be derived from measurements. A typical output of the y_{II} vector is shown in figure 5, where the excitation function on the joint is such that $\ddot{\theta} = 0.1\, rad/s^2$.

The parameter identification is then posed as an optimisation problem where the cost function is defined to be the sum of the differences between the simulated (suffix s) and the measured (suffix m) velocity functions at a number of sampling points n, that is:

$$\kappa = \sum_{i=1}^{n} \left(v^{11}_{m\,i} - v^{11}_{s\,i}\right)^2 + \left(v^{12}_{m\,i} - v^{12}_{s\,i}\right)^2 + \left(\omega^{1}_{m\,i} - \omega^{1}_{s\,i}\right)^2, \qquad (25)$$

with the inclusion of an adequate scaling factor.

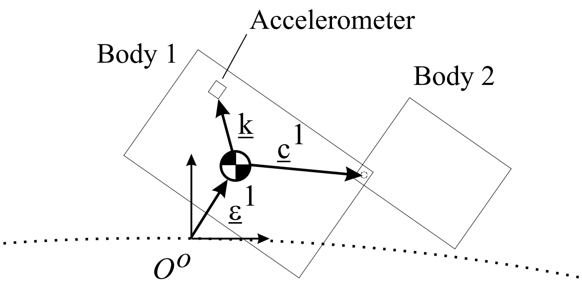

Figure 4: Position of accelerometer with respect to centre of mass

From the above formulation of the parameter identification problem the required measurements are the base body translational velocity vector and angular velocity in time, as well as their respective initial states, and the initial state of the angular position. Also, for the purpose of the integration with the time excitation on the revolute joint, measurements (or derivations from them) of θ, $\dot{\theta}$ and $\ddot{\theta}$ are also necessary.

While for the angular velocity the position of the sensors on the base body is not an issue, for the velocity measurements care has to be taken for the fact that the sensor does not coincide with the centre of mass of the body. It is proposed here to adopt accelerometers from which a velocity reading can be obtained by integration of the signal. The velocity of the centre of mass position is related to the measured velocity at the position of the sensor, $\underline{v}_m$, by the equation:

$$\underline{v}^1 = \underline{v}^1_m + \omega^1 \underline{k} \qquad (26)$$

Variable $\underline{k}$ is defined in figure 4.

The parameters of the identification problem are then m^1, $\underline{k}$ and I^1, where vector $\underline{c}^1$ can be expressed in terms of $\underline{k}$ and of the known distance between the sensor and the joint, defined by the constant vector $\underline{l}$. Note that for this reason, one could also choose m^1, I^1 and $\underline{c}^1$ as the independent parameters.

2.4 Results of the parameter identification

The above optimisation problem was solved with the method of Sequential Quadratic Programming (SQP).

The two-body system was assumed to have the following mechanical properties:

$$m^1 = 600.0 \;\; kg \quad I^1 = 100.0 \;\; kg\, m^2$$
$$\underline{c}^1 = [0.5 \;\; 0.5]^T\, m \;\; \underline{k} = [-0.25 \;\; 0.25]^T\, m$$
$$m^2 = 60.0 \;\; kg \quad I^2 = 10.0 \;\; kg\, m^2$$
$$\underline{c}^2 = [-0.5 \;\; -0.5]^T\, m$$

To test the optimisation method, the parameters were then set to some initial condition which differed from these values by some percentage error, as for example:

$$m^1_0 = 750.0 \;\; kg \quad I^1_0 = 130.0 \;\; kg\, m^2$$
$$\underline{c}^1_0 = [0.6 \;\; 0.6]^T\, m.$$

Also, the geometry of the base body was chosen to be such that $\underline{l} = \underline{c}_1 - \underline{k} = [-0.25,\ 0.25]^T\, m$.

The simulated response for the two sets of parameters above are shown in figures 5 and 6 and the joint functions θ, $\dot{\theta}$ in time is shown in figure 7.

With the exclusion of noise, external actions and model simplifications the optimisation method was found to converge well to the desired solution, with 6 sampling points (i.e., $n = 6$). The initial condition defined above yielded the sought values to four significant figures. The required precision of the measurements for convergence, however, was found to be rather high (see next section). The trend of the cost function with respect to the first two parameters is shown in figure 8. The figure clearly shows how the optimisation problem is well posed, due to the absence of strong nonlinearities and local minima. This, however, was found to worsen greatly for higher mass ratios m^1/m^2 than the one selected. This is because the dependence of the cost function on parameter m^1 becomes weaker as the ratio increases.

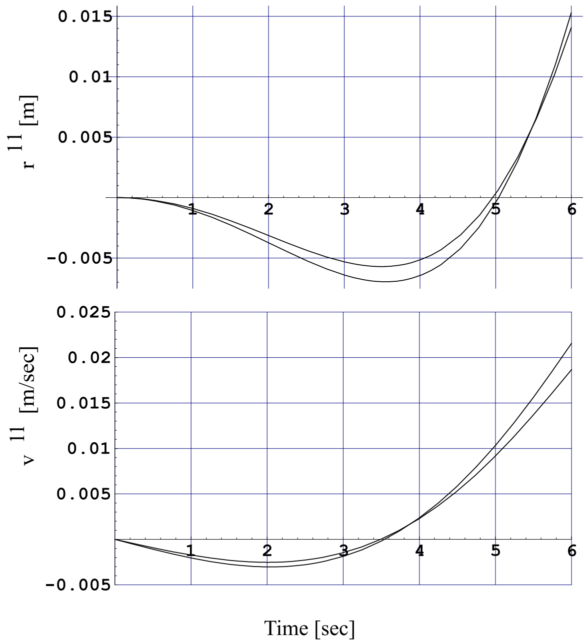

Figure 5: Base body translational motion - 1st coordinate- for two different sets of inertial parameters

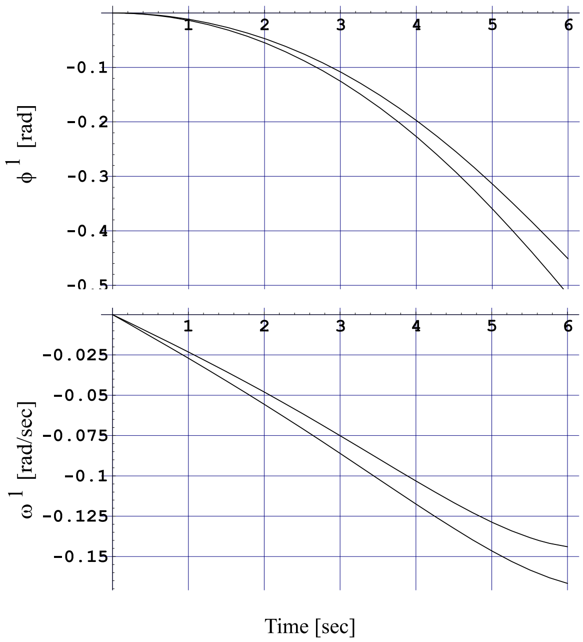

Figure 6: Base body rotational motion for two different sets of inertial parameters

2.5 Sources of error and some technology requirements

The main contributions to error were found to be those due to noise in the measurement signals. A noise with standard deviation $\sigma = 0.04$ (note that the noise on the ETS-VII measurement data was found to be $\sigma = 0.009$) was introduced in the attitude sensor signal and an error of up to 2 % was introduced in the solution, after some effort to overcome convergence problems (a grid method was adopted for the initial guess). This error is not significant and it can be reduced to a minimum by implementing very sensitive sensors.

For this study star sensor precision (2 arc sec [6]) was assumed for the attitude motion variables measurements. This could be achieved by a star sensor in conjunction with fiber optic technology gyros (for example, the Litton LN 200S gyro with drift rate $17.5 mrad/hr$). Regarding the translational acceleration measurements, the use of high precision accelerometers could be considered (for example the SuperSTAR-ONERA/CNES sensor with 10^{-9} m claimed precision). Finally, the joint motion functions could be obtained with position and torque sensors at the joint. From the former, the velocity variable could be obtained from derivation, where the noise introduced by this process can be assumed to be negligible.

The use of the dynamical equations of motion of the system allows to introduce the external torques which act on the spacecraft in case the manoeuvre is performed too slowly for these to be neglected. These torques can be modelled (see [1]), as for example the torque which arises from the rotational velocity of the reaction wheels. Regarding the gravity gradient torque this is also a function of the parameter I^1 (but only in three dimensions, where I^1 becomes a tensor with six independent parameters) so the optimisation problem becomes more complex than the one considered here if it is included. Also note that this torque depends on the orbital radius, but it is easily shown that GPS technology measurements would be accurate enough for the purpose of the parameter identification (note infact that the torque is proportional to the inverse of the cube of the orbital radius). The aim of a measuring procedure however, should be to make the manoeuvres as short and fast as possible, such that external torques ($o(10^{-3})$) are made to be second order with respect to those arising from the robot motion.

Other errors could arise from flexible elements of the structure and from sloshing, although these could be reduced to a reasonable level with a suitable design of the spacecraft (for example, cold gas propulsion could be used). Also the assumption that inertial parameters are known for the robot is only valid to within 5 % of their true value (accuracy derived from CAD data of the robot structure).

3 Control strategies for freeflying robots

When considering the possible control strategies of freeflying robots three different types can be defined for the control of the base body (spacecraft):

- position and attitude controlled;
- attitude controlled only;
- freefloating, i.e. no motion control.

Depending on the inertial properties of the base body in relation to those of the robot arm (and any load attached to the end-effector), the control of the translational motion of the base body might become a first necessity,

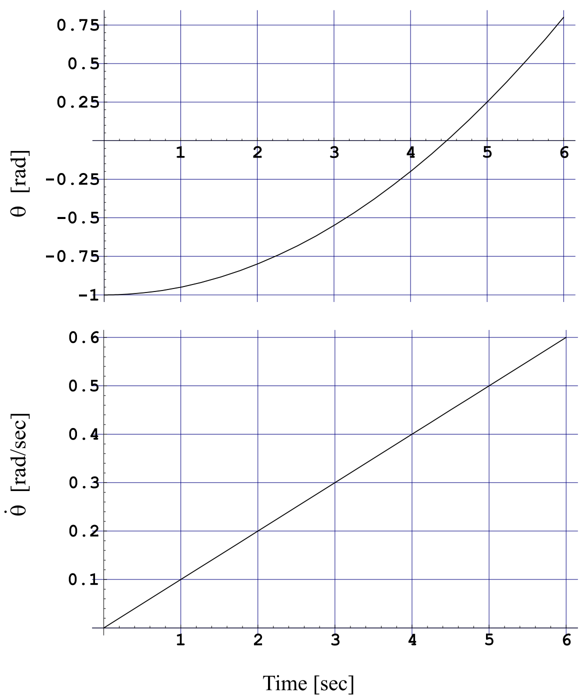

Figure 7: Joint motion

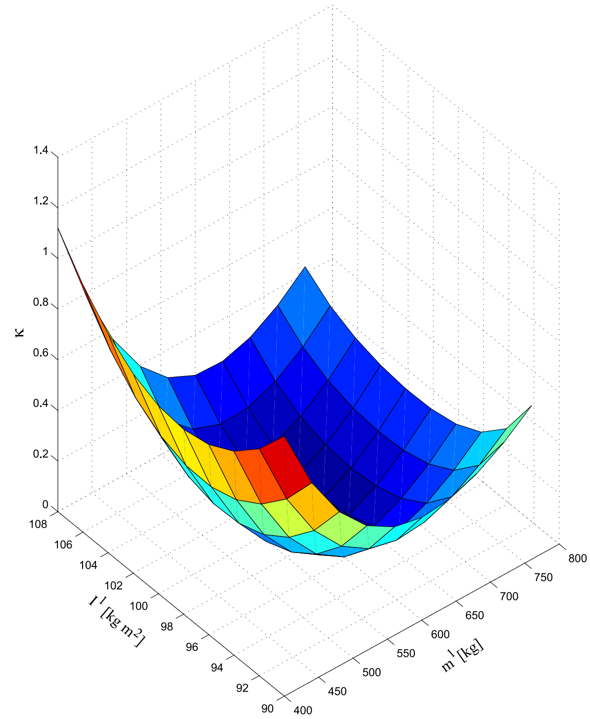

Figure 8: Cost function with respect to mass and inertia of base body

depending also on the particular application, as for example for in-orbit construction, where large loads could be in play. On the other spectrum of applications are conditions for which the interaction of the base motion with the robot motion is relatively small and, for particular applications, it can be neglected. The ETS-VII satellite, for example, had a very large mass compared to the robot mass (2500 kg versus 150 kg) and the induced translational motion was very small for any robot motion. The same arguments hold for the rotational motion of the base body, where limitations could arise from the necessity of high pointing accuracy for communication purposes.

Keeping in mind that the workspace of a freeflying robot might be seriously reduced if no control action is applied to account for the reaction of the base body to the robot motion, for certain applications an ideal freefloating base can be considered (i.e. one for which rotational and translational induced motion are negligible or not important), as for example could be for robots performing servicing or for maintenance of large space structures.

The ETS-VII Dynamic Motion experiments however have shown that the external forces acting in Low Earth Orbit require that a controller is always active to counteract their effect (see [1]). This does not necessarily require that the controller also counteracts the effects of the robot motion, if this is not necessary for the particular application. Such controller is conceptually described below. Only external torques are taken into consideration since it is assumed that external forces are negligible.

3.1 A control system for a freeflying robot

A large amount of literature has been written on the subject of control schemes for space robotic systems, based on control strategies of the types defined above (see [4], [7], [8], [9], [10]). The experience made with the ETS-VII satellite Dynamic Motion experiments, however, has shown how the non controlled mode of operation is unrealistic, since the external disturbances are generally not negligible. The advantages mentioned above for an ideally freefloating base however can still be exploited with the design of a controller which emulated such ideal conditions. In practice, the controller has to allow for the base to move in reaction to the robot motion but not to the external disturbances. This differs from previously used strategies, as for example on the ETS-VII, where all disturbances were added together and were counteracted by the attitude controller (for communication purposes). The control problem posed here is hence different as it involves the resolution of a tracking problem rather than that of a stabilization problem. While in the latter case the intention is to keep the satellite base fixed in a desired attitude position, in the former case the intention is to track some desired trajectory of the satellite base. This desired trajectory corresponds to that which the system would follow if it were only acted upon by the robot motion coupling. A tracking problem needs a reference model which provides the desired trajectory which the controller must follow. A drawback of this could be that the reference model is computationally expensive, although, for an autonomous system, the desired trajectory would follow with the path planing solution, which would anyway be performed off-line. Regarding teleoperated systems, the desired trajectory would need to be calculated for small time steps, but this could still give problems for real-time applicability and as such, it might require further development (such as model simplifications). The control scheme is described in figure 9.

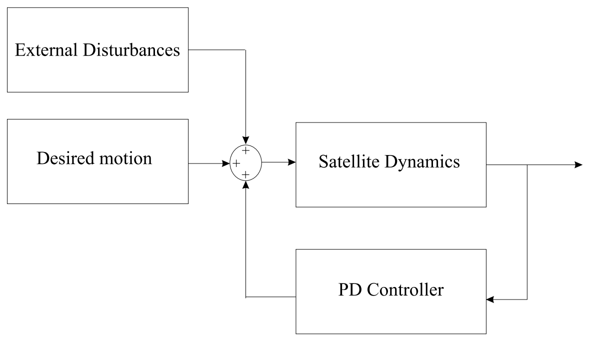

Figure 9: Control scheme for a freeflying robot

The control scheme above does not take account of the robot controller. This is taken to be independent of the satellite control and any discrepancies in the robot motion which will be reflected in a disturbance on the desired base motion are taken to be external disturbances.

3.2 Simulation and Results

The physical system module (Satellite Dynamics) in figure 9 was represented by the two dimensional system depicted in figure 2 with the parameter values defined in section 2.4. The actuator for the attitude control system was taken to be a reaction wheel which was introduced within the spacecraft to account for the external disturbances. The inertial properties of the reaction wheel were taken to be: mass, $m^{rw} = 1.6\, kg$ and inertia, $I^{rw} = 0.037\, kg\, m^2$. No translational motion actuators were considered as no significat external forces were assumed to be present.

The control law for the PD controller is of the standard form

$$\tau^{rw} \;=\; -k_d\, \dot{\phi}^1 \;-\; k_p\, \phi^1 \tag{27}$$

were τ^{rw} is the reaction wheel motor torque. The positive gains k_p and k_d were suitably selected for the control of the base during the robot motion dictated by a torque on the joint of $\ddot{\theta} = 0.01\, N\, m$ for a simulation time of 60 seconds.

The Desired Motion module consisted of a time function generator for the desired angular position and velocity of the base body. These were determined from the integration of the equations of motion of the two-dimensional system described in section 2.1.

The external disturbances were idealised for the two-dimensional case to be a constant of order $(10^{-2})\; N/m$, that is ten times greater than the typical magnitude of external torques acting in Low Earth Orbit.

The results show a perfect tracking capability of the controller for gain values equal to $k_p = 1.0$ and $k_d = 50.0$. Figure 10 shows the base body motion variables and figure 11 the reaction wheel motion variables, while figure 12 shows the control torque applied to the reaction wheel.

The control system above does not make use of feedforward for the control of the base motion. However, as was done in [7], a feedforward of the desired torque could be included to allow for greater stability providing for anticipative actions in the tracking task. This would be adequate, for example, for systems with uncertain parameters (such as the inertial parameters). Also note that the reference model could have some model simplifications, to account for the limited computational power on board of the spacecraft. A robust controller would then be the next step for a more reliable control system.

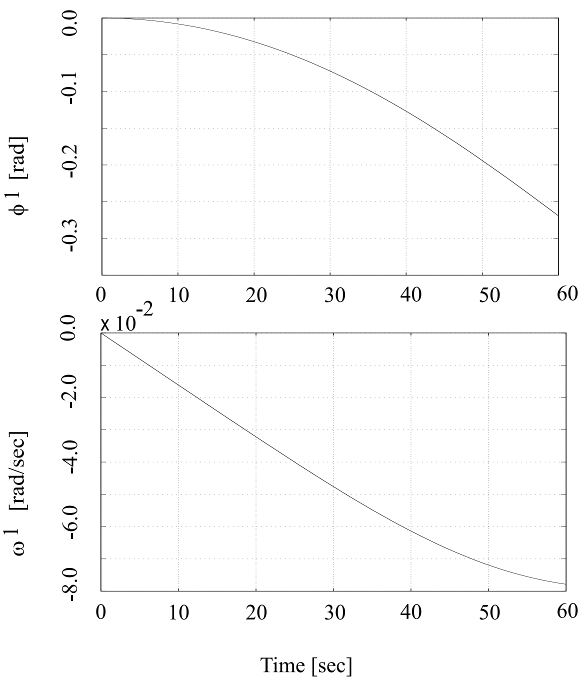

Figure 10: Base body rotational motion - angular position (top) and angular velocity (bottom)

Figure 13 shows the motor torque required to solve the stability problem (only the first 10 seconds are shown, although for the remaining time the torque is constant), for a fixed base attitude controller. A PID controller, with feedforward was implemented. The feedforward consisted in a constant torque input of $0.1\, N\, m$, since this was the minimum value for a stable output. A disturbance torque of $0.01\; N\; m$ was also included in the simulation. The gains of the PID controller were $k_p = 10.0$, $k_d = 500.0$ and $k_I = 3.0$ (integrator gain) respectively. The desired values for the base angular position and orientation were $0.0\; rad$ and $0.0\; rad/s$ respectively.

The figure shows the substantial increase in effort required to obtain the desired motion, if compared to the effort required for the controller shown in figure 12.

4 Conclusion

In this paper a parameter identification method has been proposed to determine the inertial parameters of the base body of a freeflying robot directly in space. It has been shown that the limitation of the inertial measurements are not an issue since the orbital motion can be decoupled from the multibody motion of the spacecraft-robot system. The optimisation method adopted for the identification process was limited in the convergence to an accurate solution by the level of noise in the measure-

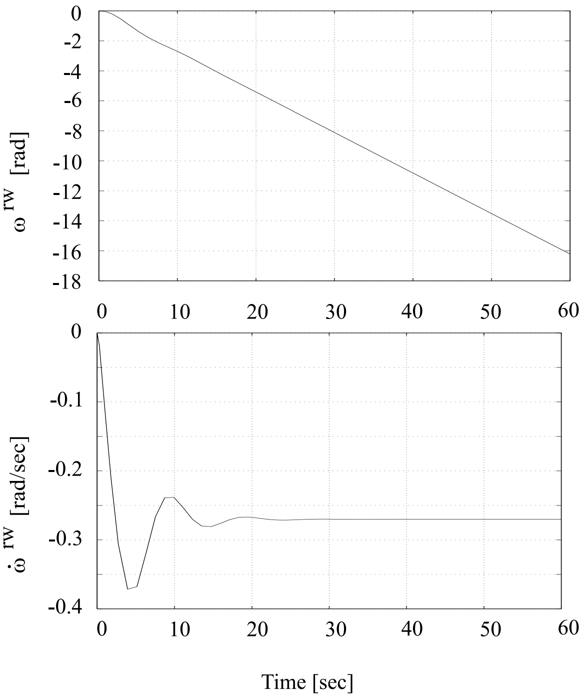

Figure 11: Reaction wheel rotational motion - angular velocity (top) and angular acceleration (bottom)

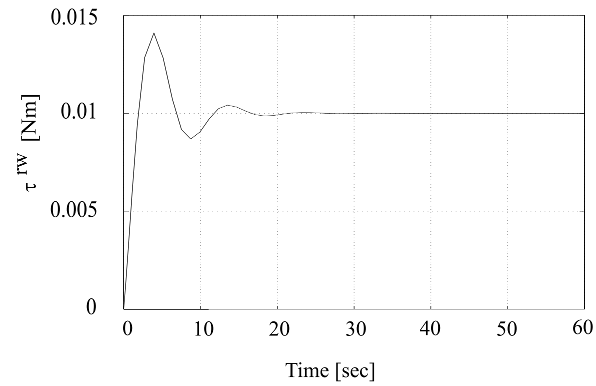

Figure 12: Control torque applied to the reaction wheel

ment signals and by high mass ratios between the spacecraft and the robot. The technology requirements to perform such identification has been addresses, with some suggestions for possible sensor candidates. The method needs to be extended to a three dimensional formulation.

A control scheme for a class of freeflying robots, for which the dynamic coupling between the base body and the robot motion is not important, has been proposed. This optimises the control effort by allowing the base body to move in reaction to the robot motion, while eliminating the effect of the external orbital disturbances, which have been shown in the ETS-VII Dynamic Motion experiments to be significant.

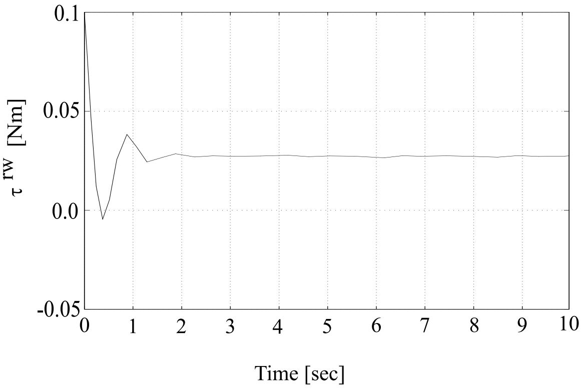

Figure 13: Control torque applied to the reaction wheel for the stability control problem

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