

Robust Joint-Space Control for Accurate Space Manipulator Deployment in Orbital Servicing Missions

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ABSTRACT

Autonomous orbital servicing requires reliable, autonomous Space Manipulator Systems (SMS), to perform capturing and handling operations in the orbital environment. The manipulator autonomous initial deployment is a crucial first step of these operations and has to be performed under a joint-space controller with both adequate accuracy and robustness in terms of external disturbances (both modelled/ measured and unmodelled/ unmeasured), parametric uncertainties, and sensor noise. A model-based PD controller has the required accuracy and stability but is affected by parametric uncertainties and unmodelled dynamics effect, from external disturbances to joint-friction and sensor noise. In the present work, the simple model-based PD is augmented by a robust-control H_∞ element, resulting in a controller more robust to these effects. The performance of the proposed joint-space control scheme is validated both with full-scale 3D simulations and experimentally with the planar, scaled-down Space Robotics Emulator (SRE) at NTUA.

1 Introduction

For autonomous orbital servicing missions such as orbital assembly, maintenance, and infrastructure construction, Space Manipulator Systems (SMS), see Fig. 1, constitute a critical technology. SMS can execute complex operations such as capture of assembly elements, handling of large structural components, precision alignment, and final assembly in microgravity conditions.

Manipulator deployment represents the initial and mission-critical phase of capture operations, during which the manipulator must be accurately configured while accounting for the coupled spacecraft-manipulator dynamics before physical interaction with the target. To avoid excitation of structural flexibilities which disturb both the end-effector and spacecraft motions, or undesired contacts with itself (self-collisions) or with nearby structures, the manipulator motion must be precisely controlled.

In this context, a model-based PD (MBPD) controller can be employed to exploit the available dynamic model of the SMS and provide satisfactory joint-space tracking performance under nominal conditions. However, parametric uncertainties, disturbances from unmodelled dynamics such as vibrating flexible elements (e.g. solar panels) and joint-friction, and sensor noise, can deteriorate the controller performance, resulting in significant tracking and steady-state errors [1]-[4].

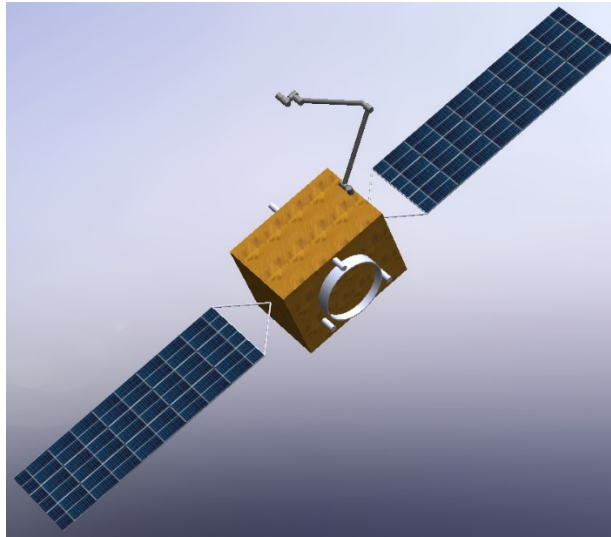


Figure 1. Concept of an SMS, including two flexible elements (solar panels).

To address the steady-state error that arises due to unmodelled dynamics and disturbances, a model-based PID (MBPID) controller can be employed. The addition of the integral term allows for the progressive elimination of steady-state tracking errors by counteracting constant or slowly varying disturbances. Nevertheless, careful tuning of the controller gains is required; inappropriate gain selection may amplify oscillatory modes, excite flexible dynamics, or compromise system stability margins, particularly in space lightweight structures [5], [6]. An alternative approach for improving robustness and eliminating steady-state error is to apply the MBPD structure and augment it with an additional robust control component based on H_∞ synthesis (MBPD plus H_∞). The H_∞ term is designed to attenuate the effect of bounded disturbances, modelling errors, and measurements noise, while preserving desirable nominal performance. By addressing uncertainty within a robust control framework, this combined approach improves disturbance rejection and maintains stability under modelling inaccuracies, without relying exclusively on integral action [4], [14].

Robust control techniques, and in particular H_∞ control, have been investigated in several space applications to address uncertainties and disturbances. Applications include large flexible space structures, satellites equipped with large solar arrays or antennas, and rendezvous and docking problems in which effects such as fuel sloshing are considered [7]–[10]. More recent studies have also considered H_∞ -based approaches for nonlinear systems such as space manipulator systems in free-floating and controlled-floating configurations [11]–[14]. These studies demonstrate the potential of robust control to improve tracking performance and disturbance rejection in the presence of modelling uncertainties. However, the application of H_∞ control to SMS remains challenging because of their inherently nonlinear and configuration-dependent dynamics. Linear H_∞ controllers offer a relatively transparent design and a direct interpretation of the frequency-domain performance through the weighting functions, but their application is generally associated with locally linearized or approximately linear models. Conversely, nonlinear H_∞ formulations can account more directly for nonlinear dynamics, but at the expense of increased design complexity and a less transparent relationship between the design parameters and the resulting system performance. Thus, the use of a linear H_∞ control component in conjunction with model-

based compensation to enhance the robustness of nonlinear, parametrically uncertain SMS subject to disturbances remains insufficiently investigated, particularly for motion control during manipulator deployment, [15], [16].

In this paper, three control approaches—model-based PD (MBPD), model-based PID (MBPID), and MBPD augmented with an H_∞ control component (MBPD + H_∞)—are systematically compared for space manipulator deployment. The comparison is performed using high-fidelity simulations of a realistic, full-scale spatial SMS, incorporating nonlinear rigid-body dynamics, flexible elements, joint-friction effects, and representative sensor noise. The considered control laws are further evaluated experimentally using the Space Robotics Emulator (SRE) at the National Technical University of Athens (NTUA). Although the experimental campaign is restricted to planar motion, the SRE reproduces the essential coupled dynamics between the manipulator and its floating base, enabling the experimental assessment of the considered controllers under realistic operating conditions. The combined simulation and experimental results provide a comprehensive comparison of tracking accuracy, robustness, disturbance rejection, and steady-state performance during manipulator deployment.

2 System Dynamics

In the context of space manipulator system (SMS) control, the coupled rigid-body dynamics of the system play a fundamental role in achieving accurate and stable motion. In this section, the equations of motion are formulated in a form suitable for the design of the developed MBPD + H_∞ control law.

During the capture phase, the end-effector (EE) is required to approach the contact point while the spacecraft (S/C) attitude is simultaneously regulated to its desired orientation. This coordinated motion is achieved through the combined actuation provided by the manipulator joint motors and the S/C reaction wheels (RWs). The selected generalized velocity vector for the control design is therefore defined as

$$\mathbf{v}_q = \begin{bmatrix} \boldsymbol{\omega}_b \\ \dot{\mathbf{q}} \end{bmatrix} \quad (1)$$

where $\boldsymbol{\omega}_b$ denotes the S/C angular velocity and $\dot{\mathbf{q}}$ is the vector of manipulator joint rates.

The applied actuation vector is given by,

$$\mathbf{Q}_c = \begin{bmatrix} \boldsymbol{\tau}_{rw} \\ \boldsymbol{\tau} \end{bmatrix} \quad (2)$$

where $\boldsymbol{\tau}$ and $\boldsymbol{\tau}_{rw}$ denote column vectors of the manipulator joints and the reaction wheels torques, respectively. The coupled rigid-body dynamics of the SMS are expressed as, [16],

$$\mathbf{M} \dot{\mathbf{v}} + \mathbf{c} = \begin{bmatrix} \mathbf{0} \\ \mathbf{0} \\ \boldsymbol{\tau}_{rw} \\ \boldsymbol{\tau} \end{bmatrix} + \mathbf{J}_b^T \begin{bmatrix} \mathbf{R}_b {}^b \mathbf{f}_b \\ \mathbf{R}_b {}^b \mathbf{n}_b \end{bmatrix} \quad (3)$$

where $\mathbf{M}$ denotes the system inertia matrix and $\mathbf{c}$ represents the vector of nonlinear velocity-dependent terms.

The generalized velocity vector is defined as

$$\mathbf{v} = \begin{bmatrix} \mathbf{v}_b^T & \boldsymbol{\omega}_b^T & \dot{\mathbf{q}}_{rw}^T & \dot{\mathbf{q}}^T \end{bmatrix}^T \quad (4)$$

where $\mathbf{v}_b$ denotes the S/C linear velocity, $\mathbf{q}_{rw}$ represents the RW relative angles and $(\dot{\cdot})$ denotes the time derivative of $(\cdot)$. The vectors ${}^b \mathbf{f}_b$, ${}^b \mathbf{n}_b$ denote the external forces and moments, acting on the S/C due to thruster

forces, expressed in the S/C frame. Furthermore, $\mathbf{R}_b$ denotes the rotation matrix relating the S/C body frame to the inertial frame. The mapping matrix $\mathbf{J}_b$, which extracts the S/C translational and angular velocities from the generalized velocity vector, is defined as

$$\begin{bmatrix} \mathbf{v}_b \\ \boldsymbol{\omega}_b \end{bmatrix} = \underbrace{\begin{bmatrix} \mathbf{I}_{3 \times 3} & \mathbf{0}_{3 \times 3} & \mathbf{0}_{3 \times 4} & \mathbf{0}_{3 \times N} \\ \mathbf{0}_{3 \times 3} & \mathbf{I}_{3 \times 3} & \mathbf{0}_{3 \times 4} & \mathbf{0}_{3 \times N} \end{bmatrix}}_{\mathbf{J}_b} \begin{bmatrix} \mathbf{v}_b \\ \boldsymbol{\omega}_b \\ \dot{\mathbf{q}}_{rw} \\ \dot{\mathbf{q}} \end{bmatrix} \quad (5)$$

where N is the number of the robotic arm joints, while $\mathbf{0}_{n \times m}$ and $\mathbf{I}_{n \times m}$ denote the $n \times m$ zero and identity matrices, respectively.

Since the thruster jets are deactivated during the considered capture phase, the corresponding external forces and moments are zero, i.e. ${}^b\mathbf{f}_b = \mathbf{0}$ and ${}^b\mathbf{n}_b = \mathbf{0}$. Therefore, Eq. (3) can be expressed in the following block-matrix form:

$$\underbrace{\begin{bmatrix} \mathbf{M}_{11} & \mathbf{M}_{12} & \mathbf{0} & \mathbf{M}_{14} \\ \mathbf{M}_{21} & \mathbf{M}_{22} & \mathbf{M}_{23} & \mathbf{M}_{24} \\ \mathbf{0} & \mathbf{M}_{32} & \mathbf{M}_{33} & \mathbf{M}_{34} \\ \mathbf{M}_{41} & \mathbf{M}_{42} & \mathbf{M}_{43} & \mathbf{M}_{44} \end{bmatrix}}_{\mathbf{M}} \begin{bmatrix} \dot{\mathbf{v}}_b \\ \dot{\boldsymbol{\omega}}_b \\ \ddot{\mathbf{q}}_{rw} \\ \ddot{\mathbf{q}} \end{bmatrix} + \underbrace{\begin{bmatrix} \mathbf{c}_{vb} \\ \mathbf{c}_{\omega b} \\ \mathbf{c}_{rw} \\ \mathbf{c}_q \end{bmatrix}}_{\mathbf{c}} = \begin{bmatrix} \mathbf{0} \\ \mathbf{0} \\ \boldsymbol{\tau}_{rw} \\ \boldsymbol{\tau} \end{bmatrix} \quad (6)$$

where the block structure of $\mathbf{M}$ and $\mathbf{c}$ corresponds to the ordering of $\mathbf{v}_b$, $\boldsymbol{\omega}_b$, $\dot{\mathbf{q}}_{rw}$, and $\dot{\mathbf{q}}$ in Eq. (4).

The control design, however, requires the dynamics to be expressed directly in terms of the selected controlled variables $\mathbf{v}_q$, defined in Eq. (1). Therefore, the accelerations $\dot{\mathbf{v}}_b$ and $\ddot{\mathbf{q}}_{rw}$, which are not included in $\mathbf{v}_q$, are eliminated from Eq. (6), [13]. The first and third block equations of Eq. (6) can be solved for $\dot{\mathbf{v}}_b$ and $\ddot{\mathbf{q}}_{rw}$, respectively, yielding:

$$\begin{aligned} \dot{\mathbf{v}}_b &= \mathbf{M}_{11}^{-1} (-\mathbf{c}_{vb} - \mathbf{M}_{12}\dot{\boldsymbol{\omega}}_b - \mathbf{M}_{14}\ddot{\mathbf{q}}) \\ \ddot{\mathbf{q}}_{rw} &= \mathbf{M}_{33}^{-1} (-\mathbf{c}_{rw} - \mathbf{M}_{32}\dot{\boldsymbol{\omega}}_b - \mathbf{M}_{34}\ddot{\mathbf{q}} + \boldsymbol{\tau}_{rw}) \end{aligned} \quad (7)$$

Substituting the variables $\dot{\mathbf{v}}_b$ and $\ddot{\mathbf{q}}_{rw}$ by use of Eq. (7) into the remaining block equations of Eq. (6) results in the following reduced-order representation of the system dynamics:

$$\mathbf{M}_q \dot{\mathbf{v}}_q + \mathbf{c}_q = \mathbf{J}_c^T \mathbf{Q}_c \quad (8)$$

where $\mathbf{v}_q$ and $\mathbf{Q}_c$ are given in Eqs. (1) and (2), respectively. The reduced inertia matrix $\mathbf{M}_q$, nonlinear velocity-dependent vector $\mathbf{c}_q$, and reduced actuation mapping matrix $\mathbf{J}_c^T$ are given by Eqs. (9) - (11).

$$\mathbf{M}_q = \begin{bmatrix} \mathbf{M}_{22} - \mathbf{M}_{21}\mathbf{M}_{11}^{-1}\mathbf{M}_{12} - \mathbf{M}_{23}\mathbf{M}_{33}^{-1}\mathbf{M}_{32} & \mathbf{M}_{24} - \mathbf{M}_{21}\mathbf{M}_{11}^{-1}\mathbf{M}_{14} - \mathbf{M}_{23}\mathbf{M}_{33}^{-1}\mathbf{M}_{34} \\ \mathbf{M}_{42} - \mathbf{M}_{41}\mathbf{M}_{11}^{-1}\mathbf{M}_{12} - \mathbf{M}_{43}\mathbf{M}_{33}^{-1}\mathbf{M}_{32} & \mathbf{M}_{44} - \mathbf{M}_{41}\mathbf{M}_{11}^{-1}\mathbf{M}_{14} - \mathbf{M}_{43}\mathbf{M}_{33}^{-1}\mathbf{M}_{34} \end{bmatrix} \quad (9)$$

$$\mathbf{c}_q = \begin{bmatrix} \mathbf{c}_{\omega b} - \mathbf{M}_{21}\mathbf{M}_{11}^{-1}\mathbf{c}_{vb} - \mathbf{M}_{23}\mathbf{M}_{33}^{-1}\mathbf{c}_{rw} \\ \mathbf{c}_q - \mathbf{M}_{41}\mathbf{M}_{11}^{-1}\mathbf{c}_{vb} - \mathbf{M}_{43}\mathbf{M}_{33}^{-1}\mathbf{c}_{rw} \end{bmatrix} \quad (10)$$

and

$$\mathbf{J}_c^T = \begin{bmatrix} -\mathbf{M}_{23}\mathbf{M}_{33}^{-1} & \mathbf{0}_{3 \times N} \\ -\mathbf{M}_{43}\mathbf{M}_{33}^{-1} & \mathbf{I}_{N \times N} \end{bmatrix} \quad (11)$$

The reduced model in Eq. (8) explicitly describes the dynamics associated with the selected controlled variables, namely the S/C angular velocity and the manipulator joint rates, while accounting for the dynamic coupling with the reaction wheels and the remaining spacecraft translational dynamics through the reduced matrices $\mathbf{M}_q$, $\mathbf{c}_q$, and $\mathbf{J}_c^T$.

It should be emphasized that Eq. (8) represents a reduced dynamic model and does not, by itself, describe the complete system dynamics. Equations (7) and (8) must therefore be considered together to fully characterize the system behaviour, [16]. Equation (8) is used for the derivation of the control law associated with the selected controlled variables, whereas Eq. (7) provides the remaining acceleration relations required to recover the full system dynamics.

3 Control scheme

3.1 Model-based PD

The control law is formulated using the reduced-order dynamics of Eq. (8). Specifically, the actuator torque vector $\mathbf{Q}_c$ is selected to compensate for the nonlinear system dynamics and impose the desired closed-loop acceleration dynamics. The resulting control torques are given by:

$$\mathbf{Q}_c = \mathbf{J}_c^{-T} (\mathbf{M}_q \mathbf{u} + \mathbf{c}_q) \quad (12)$$

where $\mathbf{u}$ denotes the auxiliary control input and is defined as

$$\mathbf{u} = \begin{bmatrix} \mathbf{u}_{RW} \\ \mathbf{u}_q \end{bmatrix} \quad (13)$$

where $\mathbf{u}_{RW}$ and $\mathbf{u}_q$ are the auxiliary acceleration commands associated with the S/C attitude and manipulator joint motion, respectively.

Substituting Eq. (12) into the reduced dynamics of Eq. (8), and since the reduced inertia matrix $\mathbf{M}_q$ is positive definite, the following is obtained:

$$\dot{\mathbf{v}}_q = \begin{bmatrix} \dot{\mathbf{\omega}}_b \\ \ddot{\mathbf{q}} \end{bmatrix} = \mathbf{u} \quad (14)$$

Thus, the nonlinear coupled dynamics are compensated through the model-based torque transformation, and the auxiliary input ($\mathbf{u}$) directly determines the accelerations of the selected controlled variables.

For S/C attitude control, Euler parameters are used to represent attitude error. The corresponding angular-velocity and attitude errors are employed to define the auxiliary control command $\mathbf{u}_{RW}$. Assuming that the attitude-error representation is well defined, the auxiliary control inputs are selected as, [17]

$$\begin{aligned} \mathbf{u}_{RW} &= \mathbf{R}_b \left({}^b\mathbf{R}_{b,d} {}^{b,d}\dot{\mathbf{\omega}}_{b,d} + {}^b\mathbf{\omega}_b \times {}^b\mathbf{e}_{b,\omega} - \mathbf{K}_{D\omega} {}^b\mathbf{e}_{b,\omega} - 2 \left(\mathbf{K}_{P\omega} - {}^b\mathbf{e}_{b,\omega}^T {}^b\mathbf{e}_{b,\omega} / 4 \right) \mathbf{e}_{b,\varepsilon} / e_{b,\eta} \right) \\ \mathbf{u}_q &= \ddot{\mathbf{q}}_d + \mathbf{K}_{Dq} \dot{\mathbf{e}}_q + \mathbf{K}_{Pq} \mathbf{e}_q \end{aligned} \quad (15)$$

where $(*)^\times$ denotes the cross-product matrix, the left superscript b denotes the S/C body frame, and the superscript d denotes the frame in which the desired quantity is defined. A missing left superscript denotes the inertial frame. The variables ω , ε and η denote angular velocity and the corresponding Euler parameters, respectively. $\mathbf{R}_b$ denotes

the rotation matrix from the S/C body frame to the inertial frame, while ${}^b\mathbf{R}_{b,d}$ denotes the rotation matrix from the desired S/C body frame to the actual S/C body frame.

The matrices $\mathbf{K}_{D\omega}$, $\mathbf{K}_{P\omega}$ denote the positive definite gain matrices of the S/C attitude while $\mathbf{K}_{Dq}$, $\mathbf{K}_{Pq}$, denote the corresponding gain matrices for the manipulator joint controller.

The S/C angular-velocity tracking error is defined as in [17],

$${}^b\mathbf{e}_{b,\omega} = {}^b\boldsymbol{\omega}_b - {}^b\mathbf{R}_{b,d} {}^{b,d}\boldsymbol{\omega}_{b,d} \quad (16)$$

while the corresponding attitude error in terms of the Euler parameters is given by, [16]

$$\begin{aligned} \mathbf{e}_{b,\varepsilon} &= \mathbf{E}_{b,d}^T \boldsymbol{\varepsilon}_b - \boldsymbol{\varepsilon}_{b,d} \eta_b \\ e_{b,\eta} &= \boldsymbol{\varepsilon}_{b,d}^T \boldsymbol{\varepsilon}_b + \eta_{b,d} \eta_b \end{aligned} \quad (17)$$

with

$$\mathbf{E}_{b,d} = \eta_{b,d} \mathbf{I}_{3 \times 3} + \boldsymbol{\varepsilon}_{b,d}^\times \quad (18)$$

where $\boldsymbol{\varepsilon}_{b,d}$, and $\eta_{b,d}$ denote the desired S/C attitude Euler parameters.

For the manipulator, the joint tracking error is defined as

$$\mathbf{e}_q = \mathbf{q}_d - \mathbf{q} \quad (19)$$

where $\mathbf{q}_d$ denotes the desired trajectory.

The nominal closed-loop error dynamics are given by,

$$\ddot{\mathbf{e}} + \mathbf{K}_D \dot{\mathbf{e}} + \mathbf{K}_P \mathbf{e} = \mathbf{0} \quad (20)$$

where

$$\mathbf{e} = \begin{bmatrix} {}^b\mathbf{e}_{b,\varepsilon} \\ e_{b,\eta} \end{bmatrix} \quad (21)$$

and

$$\mathbf{K}_P = \begin{bmatrix} \mathbf{K}_{P\omega} & \mathbf{0} \\ \mathbf{0} & \mathbf{K}_{Pq} \end{bmatrix} \quad (22)$$

$$\mathbf{K}_D = \begin{bmatrix} \mathbf{K}_{D\omega} & \mathbf{0} \\ \mathbf{0} & \mathbf{K}_{Dq} \end{bmatrix} \quad (23)$$

For the nominal reduced-order model, the error dynamics in Eq. (20) are asymptotically stable for positive-definite proportional and derivative gain matrices. Consequently, the S/C attitude and manipulator joint tracking errors converge asymptotically to zero.

The above formulation describes the nominal MBPD controller. Its sensitivity to modelling uncertainties, unmodelled dynamics, disturbances, and measurement noise is addressed by augmenting the nominal controller with the H_∞ control element described in the following section.

3.2 Robustness characteristics via H_∞ control element

In the presence of parameter uncertainty and nonlinearities, the excitation of flexible modes associated with solar-panel vibrations and propellant sloshing cannot always be avoided, even when appropriate precautions are taken. Uncertainty in the lowest resonant frequencies may lead to unintended excitation of flexible modes during

manipulator motion. As a result, modelling uncertainties and disturbances arising from solar-panel vibrations and propellant sloshing can significantly degrade the tracking performance of a MB PD controller.

To enhance the robustness of the nominal MBPD controller against such uncertainties and disturbances, an additional linear H_∞ control component is incorporated into the control law. The resulting control law is given by [15],

$$\mathbf{Q}_c = \mathbf{J}_c^{-T} (\mathbf{M}_q \mathbf{u} + \mathbf{c}_q - \mathbf{M}_q \mathbf{u}_\infty) \quad (24)$$

where $\mathbf{u}_\infty$ is the H_∞ contribution to the controller and the input $\mathbf{u}$ is given by the Eq. (13).

The robust control strategy is implemented in two stages. First, the model-based component pre-compensates for the nominal system dynamics. The robust controller H_∞ then compensates for the residual errors that remain after the model-based compensation due to modelling uncertainties and unmodelled dynamics.

In this case, the closed – loop system error dynamics is,

$$\ddot{\mathbf{e}} + \mathbf{K}_D \dot{\mathbf{e}} + \mathbf{K}_P \mathbf{e} = \mathbf{d} + \mathbf{d}_{\text{unc}} + \mathbf{u}_\infty \quad (25)$$

where the term $\mathbf{d}$ represents the disturbances due to the unmodelled flexible dynamics (e.g., solar panels, fuel sloshing, etc.) and $\mathbf{d}_{\text{unc}}$ is a term due to the parametric uncertainty and acts as a disturbance to the closed loop system formed by the model-based controller.

It is desired that the right-hand side of Eq. (25) is zero. This is feasible if $\mathbf{u}_\infty$ cancels $\mathbf{d}, \mathbf{d}_{\text{unc}}$ for all time. To design the linear H_∞ control law, the terms $\mathbf{d}, \mathbf{d}_{\text{unc}}$ are temporarily ignored and Eq. (25) is written as, [15]

$$\ddot{\mathbf{e}} + \mathbf{K}_D \dot{\mathbf{e}} + \mathbf{K}_P \mathbf{e} = \mathbf{u}_\infty \quad (26)$$

The above equation can be represented in state - space as, [12]

$$\dot{\mathbf{E}} = \mathbf{A}\mathbf{E} + \mathbf{B}\mathbf{u}_\infty \quad (27)$$

and

$$\mathbf{y} = \mathbf{E} = \mathbf{C}\mathbf{E} + \mathbf{D}\mathbf{u}_\infty \quad (28)$$

where

$$\mathbf{E} = [\mathbf{e}^T \quad \dot{\mathbf{e}}^T]^T \quad (29)$$

and

$$\mathbf{A} = \begin{bmatrix} \mathbf{0}_{N \times N} & \mathbf{I}_{N \times N} \\ -\mathbf{K}_P & -\mathbf{K}_D \end{bmatrix}, \quad \mathbf{B} = \begin{bmatrix} \mathbf{0}_{N \times N} \\ \mathbf{I}_{N \times N} \end{bmatrix} \quad (30)$$

$$\mathbf{C} = \begin{bmatrix} \mathbf{I}_{N \times N} & \mathbf{0}_{N \times N} \\ \mathbf{0}_{N \times N} & \mathbf{I}_{N \times N} \end{bmatrix}, \quad \mathbf{D} = \begin{bmatrix} \mathbf{0}_{N \times N} \\ \mathbf{0}_{N \times N} \end{bmatrix}$$

The design of the H_∞ controller involves the selection of the weighting matrices $\mathbf{W}_e(s)$, $\mathbf{W}_u(s)$, $\mathbf{W}_d(s)$, and $\mathbf{W}_n(s)$, which shape the desired performance and robustness characteristics of the closed-loop system. These weighting functions can be represented by diagonal matrices whose elements are either transfer functions or static gains, with each weighting matrix serving a specific purpose.

The control weighting function $\mathbf{W}_u(s)$ is applied to the control input and is used to limit the control effort. It is typically selected as a high-pass filter, thereby penalizing high-frequency control action. The performance weighting function $\mathbf{W}_e(s)$ is applied to the closed-loop tracking error and is used to specify the desired frequency-dependent performance. It can be constructed using a combination of low-pass filters and static gains.

The performance and control weighting functions are selected to shape the closed-loop response by requiring high loop gain in the low-frequency range, where accurate tracking and disturbance rejection are desired, and

reduced loop gain at higher frequencies, where measurement noise and unmodelled high-frequency dynamics become more significant.

The disturbance weighting function $\mathbf{W}_d(s)$ is used to characterize the expected frequency content and magnitude of external disturbances, including disturbances associated with modelling uncertainties. Conversely, the noise weighting function ($\mathbf{W}_n(s)$) describes the expected sensor-noise characteristics. Typically, $\mathbf{W}_d(s)$ and $\mathbf{W}_n(s)$ are selected as diagonal matrices, with their diagonal elements represented by low-pass and high-pass filters, respectively, [15]-[16].

Based on standard H_∞ design guidelines the weighting matrices $\mathbf{W}_u(s)$ and $\mathbf{W}_e(s)$ can be selected in the following form, [15],

$$\mathbf{W}_u(s) = \left(\frac{s + \omega_{bc} / \sqrt[k]{M_u}}{\sqrt[k]{\varepsilon_1} s + \omega_{bc}} \right)^k \cdot \mathbf{I}_{N \times N}, k \geq 1 \quad (31)$$

$$\mathbf{W}_e(s) = \left(\frac{s / \sqrt[k]{M_s} + \omega_b}{s + \omega_b \sqrt[k]{\varepsilon}} \right)^k \cdot \mathbf{I}_{2N \times 2N}, k \geq 1 \quad (32)$$

where $M_s = \|\mathbf{S}\|_\infty$ is the peak of the Sensitivity Function $\mathbf{S}$, ω_b is the desired closed-loop bandwidth, ε is the desired steady state error with respect to a step input, M_u is the maximum gain measured by $\|\mathbf{KS}\|_\infty$, ω_{bc} is the controller's bandwidth, ε_1 is a small real positive number, k is a positive integer that determines the roll-off rate of the closed – loop system and $\|(\cdot)\|_\infty$ indicates the H_∞ norm of $(\cdot)$.

4 Simulation Results

To validate the proposed control scheme, a detailed 3D simulated model of a full-scale SMS is developed, and tested in scenarios of initial manipulator deployment, with simple MBPD and with MBPD+ H_∞ , as seen in Section 3. The SMS includes its base, a 6 DoF manipulator and two solar panels, modelled as flexible elements. The sloshing fuel effect is modelled as a mass-spring-damper on the SMS base, while the flexible elements (solar panels) are modelled as Euler-Bernoulli beams. A measurement sampling rate of 20 Hz as well as realistic sensor noise models, are included, while a parametric uncertainty ranging in the [-5%, 5%] range is also assumed. The parameters of the modelled SMS are shown in Table 1, while the desired manipulator deployment trajectory (per joint) is described by fifth order polynomials with 100 s duration, see Fig. 2a, and the desired SMS base attitude trajectory is described by velocity trapezoidal profiles with a 45 s duration, resulting in the SMS base Euler Parameters' desired trajectories, seen in Fig. 2b.

Table 1. Simulated SMS system parameters.

Parameter	Value
SMS base mass (kg)	2000
SMS base dimensions (m)	3 x 3 x 1.5
Sloshing fuel mass (kg)	50
Sloshing fuel model stiffness (N/m)	0.41
Sloshing fuel model damping (Ns/m)	0.02
Manipulator links' length (m)	0.5, 2, 2, 0.4, 0.25, 0.25
Manipulator links' mass (kg)	6, 10, 10, 6, 6, 3
Flexible element mass (kg)	22
Flexible element first (bending) natural frequency (Hz)	0.7

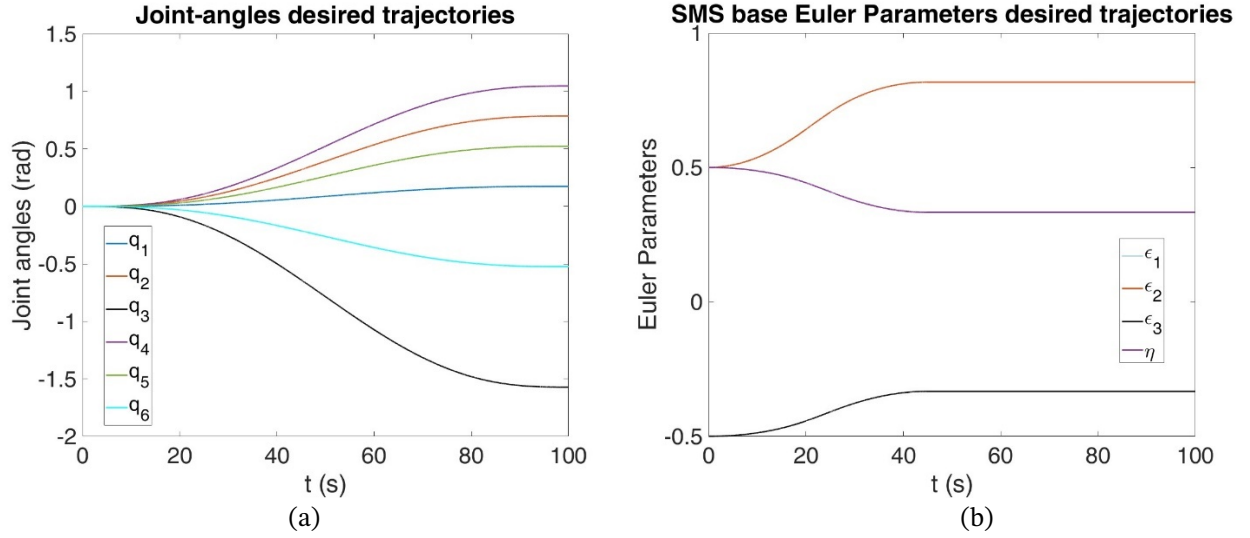


Figure 2. Simulated full-scale experiments' desired trajectories.

The joint angles' tracking errors, when using the MBPD controller, are displayed in Fig. 3, while the corresponding tracking errors when utilizing the MBPD+ H_∞ , are shown in Fig. 4. As can be seen by comparing the Fig. 3 to Fig. 4, the introduction of the robust H_∞ element resulted in lower maximum tracking errors for up to almost 50% (see especially e_{q4} and e_{q6}).

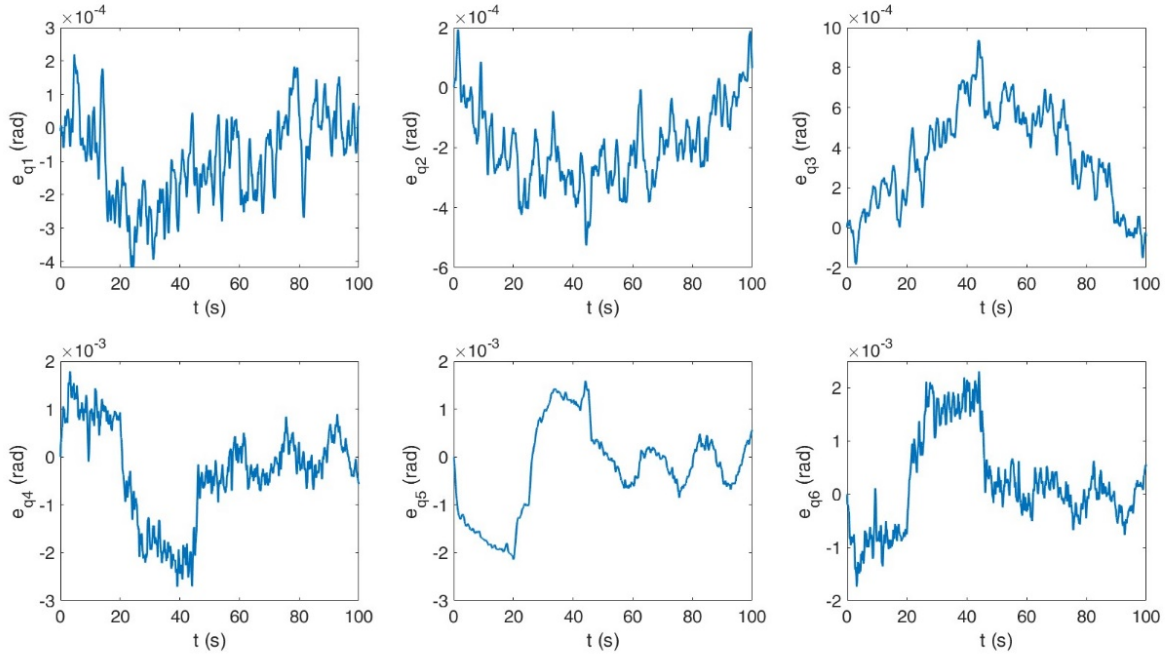


Figure 3. Joint angles tracking errors when utilizing the MBPD controller.

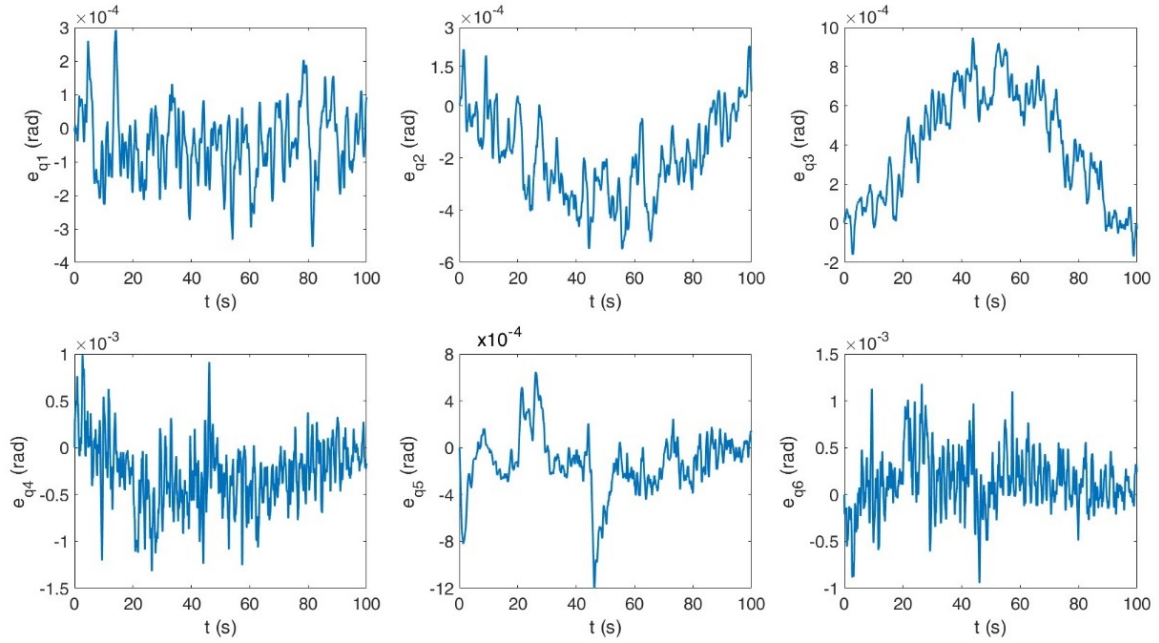


Figure 4. Joint angles tracking errors when utilizing the MBPD+ H_∞ controller.

The tracking errors for the SMS base Euler Parameters, with the simple MBPD and with the MBPD+ H_∞ , are displayed in Fig. 5 and Fig. 6, respectively. Inclusion of the robust H_∞ element reveals reduced SMS base Euler Parameters' tracking errors of at least half an order of magnitude, as can be seen by comparing Fig. 5 to Fig. 6

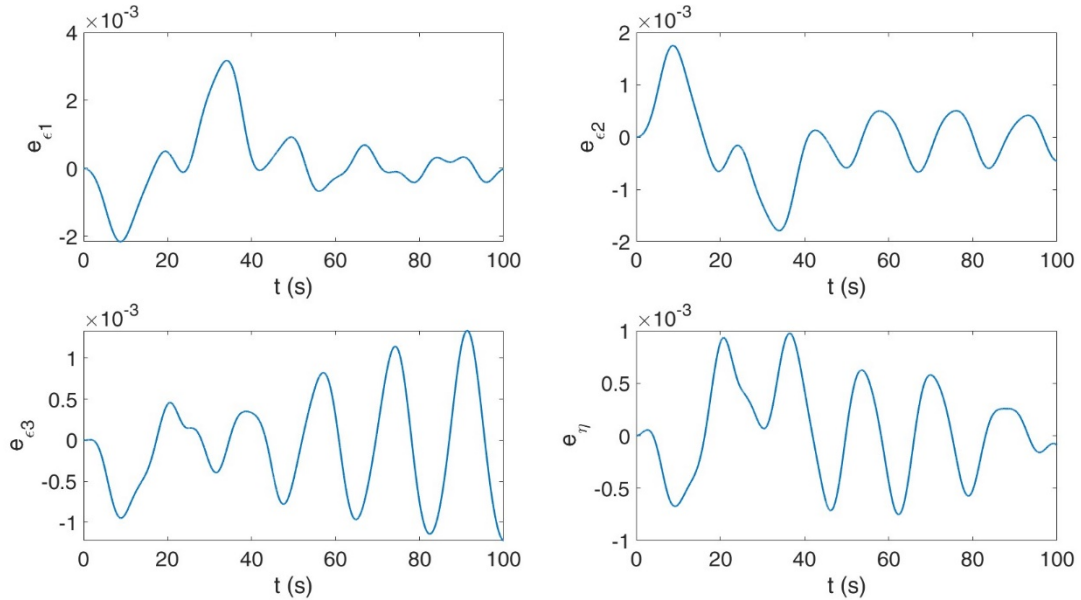


Figure 5. Simulated SMS base Euler Parameters' tracking errors when utilizing the MBPD controller.

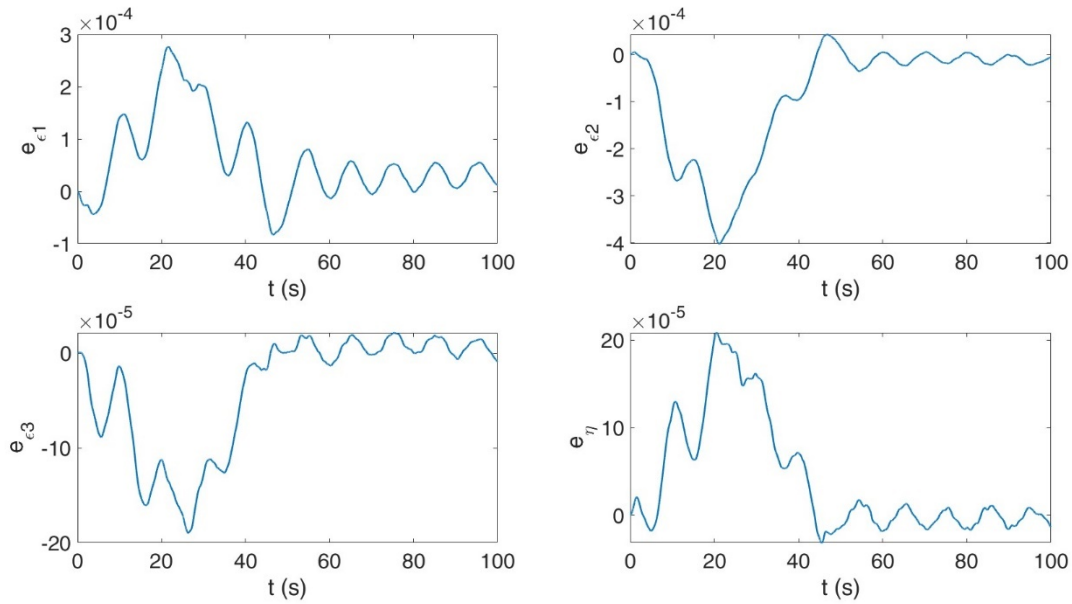


Figure 6. Simulated SMS base Euler Parameters' tracking errors when utilizing the $MBPD+H_\infty$ controller.

5 Experimental Tests Results

The proposed control scheme is further validated at the planar Space Robotics Emulator (SRE), in scenarios of initial manipulator deployment, with simple MBPD and with $MBPD+H_\infty$, as seen in Section 3. The scaled-down SMS mock-up includes its base (roughly of cylindrical shape), a 3 DoF planar manipulator, a thin aluminium plate as a solar panel mock-up, and a small tank with water (with viscosity similar to that of hydrazine) acting as the sloshing fuel mock-up, see Fig. 7.

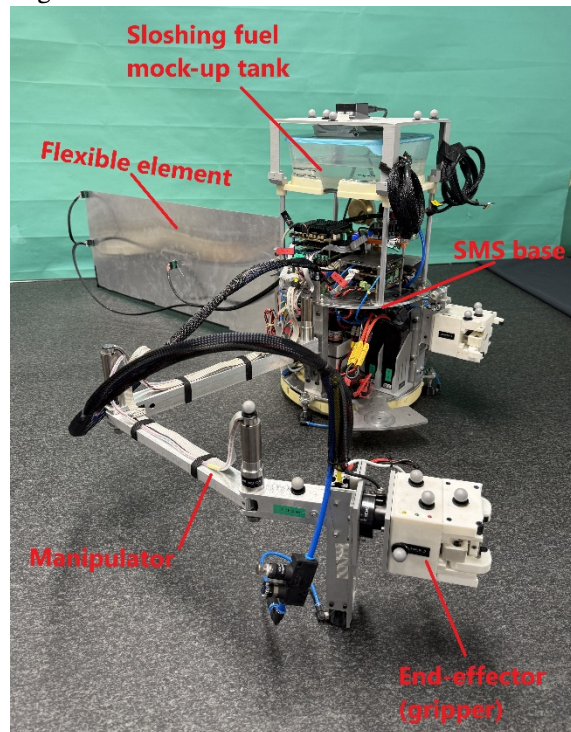


Figure 7. The scaled-down SMS mock-up at the SRE facility.

The sensor measurement sampling rate is at 100 Hz. The parameters of the modelled SMS are seen in Table 2, while the desired manipulator deployment trajectory (per joint) is described by fifth order polynomials with 25 s duration, see Fig. 8, and the desired SMS base attitude trajectory is to keep it initial orientation. As seen in Fig. 8, the two experiments (i.e., with and without the H_∞ augmentation), have slightly different initial joint-angles values, from which the desired trajectories start. However, this difference is less than 2° and does not significantly affect the experimental results.

Table 2. SRE SMS system parameters.

Parameter	Value
SMS base mass (kg)	13
SMS base dimensions (m)	0.35 (diameter) x 0.48 (height)
Manipulator links' length (m)	0.37, 0.286, 0.239
Manipulator links' mass (kg)	0.231, 0.1, 1.546
Flexible element mass (kg)	0.5
Flexible element first (bending) natural frequency (Hz)	0.7

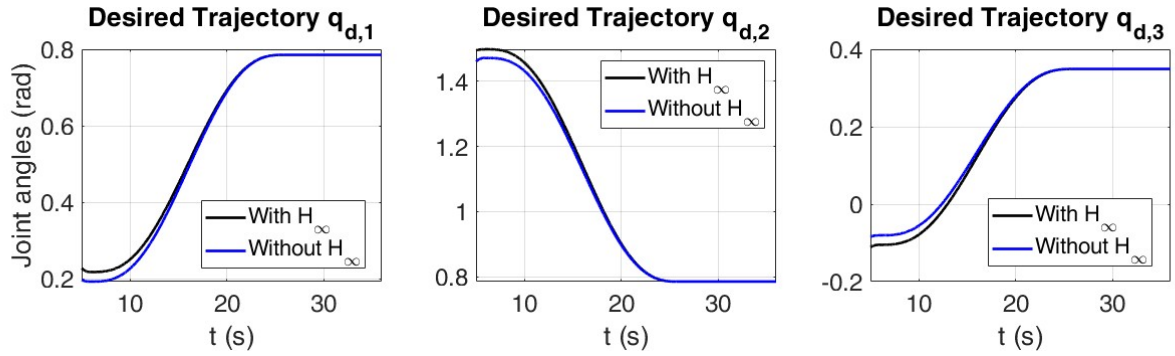


Figure 8. SRE experiments' desired trajectories.

The joint angles (q_i , $i = 1, 2, 3$) and the SMS base orientation (θ_0) tracking errors, when using the MBPD controller, are shown in Fig. 9, while the corresponding tracking errors when utilizing the MBPD+ H_∞ , are displayed in Fig. 10. As can be seen by comparing the Fig. 9 to Fig. 10, the introduction of the robust H_∞ element resulted in lower maximum tracking errors for up to more than 30% (see e.g., e_{q1}). Note that the larger initial tracking error of the SMS base orientation θ_0 , in the case of the MBPD+ H_∞ (see Fig. 10, as compared to Fig. 9) was intentional, to demonstrate the quick response of the developed control scheme, in reducing large errors.

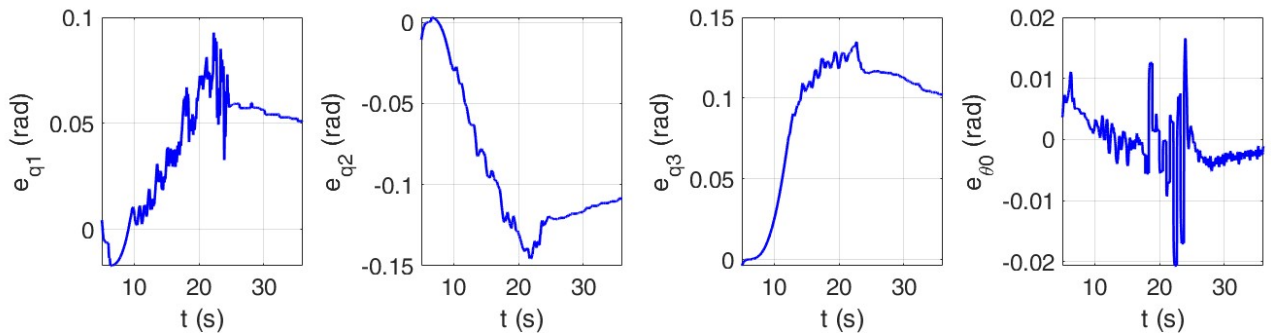


Figure 9. Tracking errors when utilizing the MBPD controller, at the SRE.

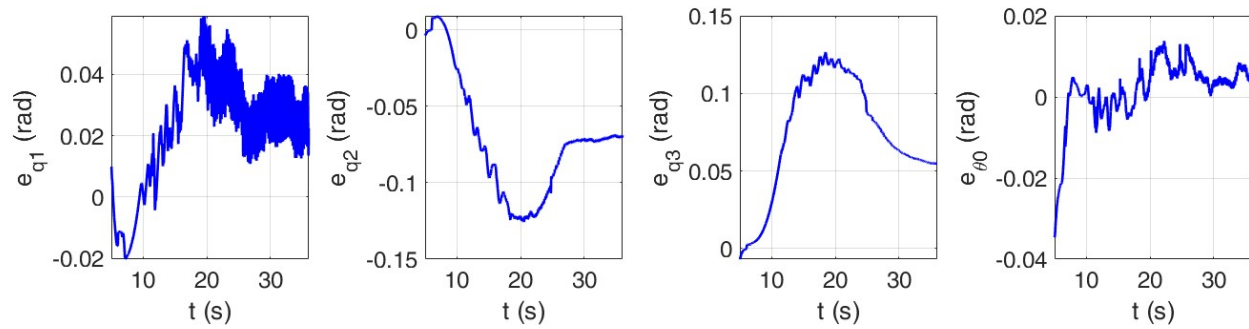


Figure 10. Tracking errors when utilizing the $MBPD+H_\infty$ controller, at the SRE.

6 Conclusion

The initial deployment of a Space Manipulator System (SMS) robotic arm is a crucial first step on critical orbital servicing operations and has to be reliably performed under a joint-space controller. In this work a model-based PD controller is augmented by a robust-control H_∞ element, resulting in a controller that, compared to the simple model-based PD, is more robust to the effects of external disturbances (both modelled/ measured and unmodelled/ unmeasured), parametric uncertainties, and sensor noise. The performance of the proposed joint-space control scheme was validated both in analytical full-scale 3D simulations and at the planar, scaled-down Space Robotics Emulator (SRE) at the NTUA facilities, verifying its required performance of decreased tracking errors compared to the ones when utilizing a simple model-base PD controller.

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